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PHYSICS

UNITED STATES ATOMIC ENERGY COMMISSION

**A CONCEPTUAL DESIGN OF A FOOD
IRRADIATION REACTOR**

September 30, 1956

**Internuclear Company
Clayton, Missouri**

Technical Information Service Extension, Oak Ridge, Tenn.

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INTERNUCLEAR COMPANY

Report Number INTERNUC 7

A CONCEPTUAL DESIGN
OF A
FOOD IRRADIATION REACTOR

Work Performed for the
Schenectady Operations Office
U. S. Atomic Energy Commission

Contract No. AT(30-3)-252

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1.0 Introduction

In April, 1956, the Schenectady Operations Office of the A.E.C. requested Internuclear Company to conduct studies of reactors for the irradiation of food for the Army Reactors Branch of the A.E.C. The studies involved the selection of the most promising reactor type for the large-scale production of gamma radiation for the food irradiation program of the QMC, and development of a preliminary design of the selected system to the point where the major problems foreseen in the design, construction, and operation of the reactor radiator complex could be ascertained. The following two reports covering the above work were submitted to the A.E.C. on July 1, 1956:

INTERNUC-2 - Selection of a Food Irradiation Reactor Type

INTERNUC-3 - Preliminary Design of a Food Irradiation Reactor

On July 30, 1956, the Schenectady Operations Office of the A.E.C. further requested Internuclear Company to:

1. conduct a program of investigation, study, and research necessary for the accomplishment and completion of the conceptual design of a reactor and food irradiation facility using indium as the gamma producer. The work was to be an elaboration and enlargement of the design reported in INTERNUC-3, and a final report was to be submitted to the A.E.C. on or before September 30, 1956.
2. prepare an estimate of the cost of design and construction of this reactor and food irradiation facility, and submit a cost estimate to the A.E.C. on or before August 31, 1956.
3. investigate the research and development problems involved in the design and construction of this facility and make additional plans and recommendations for the types of experiments required to solve these research and development problems; suggest locations where these experiments might best be performed; and estimate the time, money, and manpower that will be required to complete this research and development work. A report on this required research and development work was to be submitted to the Commission on or before August 31, 1956.

Items 2 and 3 of the above outline work have been completed and

reported respectively in:

INTERNUC-6 - Cost Estimate for the Food Irradiation
Reactor - August 23, 1956

INTERNUC-5 - Research and Development for the Food
Irradiation Reactor - August 20, 1956

This report presents the work completed toward a conceptual design for the Food Irradiation Reactor and is submitted in fulfillment of item 1 above.

2.0 General Description

The Food Irradiation Reactor design proposed herein is the result of more thorough consideration of the reactor concept outlined in INTERNUC-3. The reactor-radiator complex is based on meeting performance specifications (see Appendix 1) furnished by the A.E.C. The performance specifications which most strongly influenced the proposed design are briefly:

Dose (to most products), rep	2×10^6
Maximum dose rate, rep/hr	5×10^6
Production x dose, ton-megarep/hr	3
Production, lbs/hr	3000
Maximum-to-minimum dose	<1.25
Product sizes	No. 2 cans V3 boxes whole carcasses

The food irradiation facility described herein consists of: a fully enriched light-water-cooled and -moderated heterogeneous reactor core surrounded by a blanket of indium sulfate aqueous solution; an indium solution system which includes the reactor blanket and the gamma radiators; primary and auxiliary cooling water systems for removal of reactor heat; radiator cubicles into which the food is conveyed for irradiation; and buildings for housing the reactor, radiators, and operations personnel. (auth)

The reactor is designed to give a high leakage of neutrons from the core to the blanket, where they are thermalized and the major fraction are captured in In^{115} to form In^{116} . The radioactive In^{116} , which decays with a half-life of 54 minutes, is pumped from the reactor blanket to the radiators, where much of the decay gamma energy is available for irradiation of food and other products. The production-times-dose specification of 3 ton-megarep/hr is equivalent to about 7 KW absorbed in the food. The nominal reactor operating power necessary to provide this energy dose in the food is 10.5 MW, which is based on meeting all of the above performance specifications. However, because of the specified maximum-to-minimum dose ratio of less than 1.25, specifications are met in only one radiator, the drum-type radiator for No. 2 cans. While the nominal reactor operating power is only 10.5 MW, the reactor has been designed for 20 MW. The 20 MW design power is primarily to allow for uncertainties in reactor-

physics and radiator efficiency calculations. However, operation at higher than nominal operating powers is desirable since it provides additional flexibility. In this regard, the above-normal power rating provides for attainment of the desired specific activity in the indium solution in much shorter times, i.e., the desired radiator gamma flux can be reached in less time by operating at higher than normal power until the desired gamma flux is attained (see Chapter 11.0).

The reactor core, consisting of twenty-four, 3 foot long, 2.5 inch square aluminum clad plate-type fuel assemblies is arranged in the reactor assembly as shown by Figures 3.A and 3.B. The core is centered in a 17.75 inch I.D. aluminum reactor tank. About 2420 gpm of cooling water enters the bottom of the reactor vessel, flows upward through the core with a velocity of 7.5 ft/sec and leaves the reactor vessel at the top. The control rod drives are located in the sub-pile room below the reactor. A 4 inch thick annular blanket of indium sulfate solution, contained in stainless steel tanks, surrounds the reactor vessel at the reactor core level. A 3 inch thick thermal shield to reduce the gamma heat in the reactor pool and concrete shield surrounds the indium solution blanket.

The reactor assembly is located in the bottom of a 10 foot diameter, 27 foot deep water pool surrounded by a concrete biological shield. The entire reactor structure is located off-center in a 32 foot diameter, 71 foot high, containment type building. The reactor assembly (Figure 3.B) is entirely below grade.

The cooling systems (see Figure 5.A) for the reactor consist of: a primary cooling system to remove heat from the reactor core; an auxiliary cooling system to remove heat from the thermal shield, shielding pool, and indium system; and a secondary cooling system which disposes the heat, from both the above, to atmosphere. The primary cooling system is designed for 150 psig, with 190°F bulk water leaving the core at 20 MW reactor power. The auxiliary cooling system, which draws its water from the shielding pool and returns it thereto, operates at atmospheric pressure. All the primary and auxiliary loop equipment is located on two below-grade floors in the Reactor Building (see Figures 9.C and 9.D).

The indium solution system (see Figure 4.A) provides for circulation of the radioactive solution between the reactor blanket and any one or group of four radiators. A separate loop which circulates indium solution between the reactor blanket and a heat exchanger provides for removal of heat from the indium solution. The four principal radiators provided are: a drum-type radiator for No. 2 cans; a drum-type radiator for No. 10 cans; a multiple-slab radiator primarily for V3 boxes; and a single-slab radiator

for whole carcasses and other items of odd shape. In addition, several single tubes for indium solution circulation are provided in the single-slab radiator cubicle to provide for low-dose irradiations. All radiators and indium system equipment other than the heat exchanger and reactor blanket are located in the Radiator Building adjoining the Reactor Building (see Figures 9.A, 9.B, 9.C, 9.E and 9.F). Each of the four major radiators is contained in a separate cubicle.

The reactor control room, heating and ventilating equipment, electrical switchgear, fuel assembly vault, and offices for operations personnel are located in the Operations Building adjoining both the Reactor and Radiator Buildings (see Figures 9.B and 9.C).

The major basic differences between the design proposed herein and that of INTERNUC-3 are:

1. Reactor core - the cross sectional area of the reactor core is about 70% of that in INTERNUC-3. This change increases the fast neutron leakage from the reactor core, thereby lowering the reactor power requirements for a given gamma dose in the product. (See Chapter 6.0).
2. Radiator size - the 5×10^6 rep/hr maximum dose rate in product corresponds to a 24 minute dwell time for a dose of 2×10^6 rep in 3000 lb/hr. This 24 minute dwell time compared to the 10 minute dwell time in the No. 2 Can Radiator of INTERNUC-3, increases the volume of the indium solution in the radiators and thus results in better overall reactor-radiator efficiency and concomitant lower reactor power (see Chapter 8.0).

2.1 Design Data Summary

2.1.1 Overall Performance

Design power	MW Btu/hr	20 68.3×10^6
Normal operating power	MW	10.5
<u>No. 2 Can Radiator</u>		
Power absorbed in food	kw	7
Overall plant efficiency	percent	0.067
Food production	lb/hr	3,000
Dose in product	rep	2×10^6
Production times dose	ton-megarep/hr	3

Dose rate

rep/hr. 5×10^6

Maximum-to-minimum dose

1.17

2.1.2 Reactor DataCore

Equivalent diameter

in. 14.9

Height

in. 36

Volume

in³ 6,276

Metal-to-water ratio (average)

0.7

U²³⁵ content of new clean core

Kg 11.4

Cold clean critical mass (uranium)

Kg 6.0

Burnup (of full core)

percent 10 - 20

Power density (20 MW)

kw/liter 194.5

Average thermal neutron flux
at 20 MW in fresh coreneut/cm²sec 4.4×10^{13}

Cold, clean, excess reactivity

% $\Delta K/K$ ~ 12

Hot, clean, excess reactivity

% $\Delta K/K$ ~ 10 Hot, poisoned (xenon) excess
reactivity% $\Delta K/K$ < 6 Temperature coefficient of
reactivity (operating) $\frac{\Delta K}{K} / ^\circ C$ -5.2×10^{-4} Uranium mass coefficient of
reactivity $\frac{\Delta K}{K} / \frac{\Delta M}{M}$ 0.17

Reactivity per unit void in core

 $\frac{\Delta K}{K} / \text{cm}^3 \text{ void}$ -8.6×10^{-6}

Prompt neutron lifetime

sec 38×10^{-6}

Fuel Assemblies

Type of plates - rectangular, flat, U-Al core, clad in aluminum

Dimensions of fuel plates		Fuel Core	Overall
Thickness	in.	0.020	0.050
Width	in.	2.12	2.372
Length	in.	36	37
Dimensions of side plates			
Thickness	in.		0.098
Width	in.		2.460
Length	in.		39.750
Fuel plate core composition			
Uranium	wt. %		31.6
Aluminum	wt. %		68.4
Dimensions of fuel assembly (overall)			
Thickness	in.		2.46
Width	in.		2.46
Length	in.		46
Fuel plates per assembly			16
Space between fuel plates		in.	0.106
Number of fuel assemblies			24
Total number of fuel plates			384
Uranium (90-95% U ²³⁵) per assembly		grams	475
Metal-to-water ratio			0.58

Control Rods

Type:

Shim-safety: Aluminum with center of cadmium in upper part, 4 flat blades, one cruciform

Regulating: Beryllium lower part, aluminum-clad cadmium upper part, round rods

Number:

Shim-safety rods 5

Regulating rods 2

Travel:

Shim-safety rods	in.	36
Regulating rods	in.	24

Dimensions of shim-safety rods:

<u>Blades, Cruciform</u>		<u>Cadmium</u>	<u>Aluminum</u>	<u>Overall</u>
Thickness	in.	0.0625	0.102 (clad)	0.266
Width	in.	3.74, 4.75	4.0, 5.0	4.0, 5.0
Length	in.	36	83.5	83.5

Dimensions of regulating rods:

		<u>Beryllium</u>	<u>Cadmium</u>	<u>Overall</u>
Thickness	in.	-	0.062	-
Width	in.	1.5	1.375	1.5
Length	in.	36	24	68

Worth of shim-safety rods:

4 blades, total	% $\Delta K/K$	8-13
1 cruciform	% $\Delta K/K$	12-14

Heat Transfer and Flow Data for 20 MW Operation

Nominal operating pressure	psia	165
Equivalent saturation temperature	°F	366
Coolant inlet temperature at reactor	°F	133
Coolant outlet temperature at reactor	°F	190

Properties of coolant in core, ave.

Density at 133°F (inlet temp)	lb/ft ³	61.5
190°F (outlet temp)	lb/ft ³	60.4

Viscosity	lb/ft.sec	2.7×10^{-4}
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Thermal conductivity	Btu/hr.ft ² .°F/ft	0.38
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Specific heat	Btu/°F lb	1
Total coolant flow through reactor	gpm	2420
	lb/hr	1.2×10^6
Peak coolant temperature from fuel assembly	°F	233
Maximum fuel plate surface temp.	°F	313
Number of passes in core		one
Heat transfer area in core	ft ²	407
Coolant flow area through core	ft ²	0.73
Coolant flow rate through core	gpm	2280
Coolant velocity through core	ft/sec	7.5
Film coefficient of heat transfer in core (ave.)	Btu/hr.ft ² °F	2860
Average heat flux in core	Btu/hr.ft ²	1.68×10^5
Maximum heat flux in core	Btu/hr.ft ²	4.27×10^5
Heat transfer coefficient in core at point of highest heat flux	Btu/hr.ft ² °F	2950
Radial maximum-to-average heat flux		1.56
Axial maximum-to-average heat flux		1.64
Over-all maximum-to-average heat flux (normal)		2.56
Pressure drop through core	psi	1.7
Pressure drop through heat exchangers	psi	9.5
Pressure drop through complete system	psi	~22
<u>Reactor Core Tank</u>		
Outside diameter	in.	18-1/2
Wall thickness	in.	3/8

Length, flange-flange	in.	50
Material	6061-T6	Aluminum

Indium Blanket Tanks (inside dimensions)

Inside diameter of annulus	in.	19
Outside diameter of annulus	in.	27
Length, at I.D.	in.	36
Length, at O.D.	in.	28
Wall thickness	in.	0.125
Material	316 stainless steel	

Thermal Shield

Inside diameter	in.	27-1/2
Thickness	in.	3
Length	in.	48
Material		Steel

2.1.3 Reactor Cooling Systems

<u>Pumps (Major)</u>		<u>Primary</u>	<u>Auxiliary</u>	<u>Secondary</u>
Number		2	2	2
Number normally operated		2	2	2
Operating head	psi	22	28	142
Temperature at suction	°F	133	120	85
Total hydraulic horse-power		33	7	248

<u>Heat Exchangers</u>		<u>Primary</u>	<u>Auxiliary</u>	<u>Puri- fication</u>
No. of passes (tube side)		4	4	4
No. of passes (shell side)		2	1	1
Surface (tubes only)	ft ²	4450	1380	51
Full load heat transfer	Btu/hr	6.8 x 10 ⁷	4.3 x 10 ⁶	8.2 x 10 ⁴
Flow rate				
tube side	gpm	2420	430	5
shell side	gpm	3000	500	10
Pressure drop				
tube side	psi	9.5	4.2	0.1
shell side	psi	90	38.7	-
Inlet temperature				
tube side	°F	190	120	133
shell side	°F	88	85	85
Outlet temperature				
tube side	°F	133	100	100
shell side	°F	134	102	102
Tubes				
Number		5660	1760	20
O.D.	in.	3/8	3/8	3/4
Wall thickness	BWG	24	24	18
Length	ft.	8	8	6.5
Design pressure	psi	150	150	150
Material		304 SS	304 SS	304 SS
Shell				
O.D.	in.	45	26	8
Design pressure	psi	150	150	150

Water Purification Loop

Primary water flow	gpm	5
Water temperature	°F	100
Resin column		
Outside diameter	in.	18
Overall height	in.	90
Bed height	in.	60
Bed volume	ft ³	7.5
Design pressure	psi	150
Material		304 SS

2.1.4 Indium System

<u>Drum Radiators</u>		<u>No. 2 Cans</u>	<u>No. 10 Cans</u>
Number		1	1
No. of tubes		61	19
No. of cans per tube		15	10
Total number of cans		915	190
Weight of food (unit density)	lb.	1225	1310
Dwell time (3000 lb/hr)	min.	24.5	26.2
Maximum-to-minimum dose		1.17	1.27
Efficiency			
Radiation	%	~ 21	~ 22
Reactor-Radiation	%	~ 0.067	~ 0.067
Reactor power (3 ton-mega-reps/hr)	MW	10.3	11.6
Dimensions:			
<u>Drum</u>			
Side of hex.	in.	22.9	22.1
Height	in.	68.5	70
<u>Can tube</u>			
O.D.	in.	4	6.94
Wall thickness	in.	0.020	0.020
<u>Drum Tube</u>			
O.D.	in.	4-5/8	7-9/16
Wall thickness	in.	0.062	0.062
Volume of indium solution	ft ³	12.4	10.7

<u>Slab Radiators</u>		<u>Single-Slab</u>	<u>Multiple-Slab</u>		
Number		1	1		
Number of units in radiator		1	4		
Dimension of each unit slab	in.	3x84x108	3x20x108		
			<u>1-Unit</u>	<u>2-Unit</u>	<u>4-Unit</u>
No. of V3 boxes		40	10	20	40
Weight of food in V3 boxes (unit density)	lb.	2760	690	1380	2760
Dwell time (3000 lb/hr)	min.	55.5	13.9	27.8	55.5
Maximum-to-minimum dose		1.39	1.39	1.39	1.39
Efficiency:					
Radiator		0.35	0.35	0.35	0.35
Reactor-Radiator		0.115	0.057	0.085	0.115
Reactor power (3 ton-megarep/hr)	MW	6.1	12.2	8.2	6.1

Heat Exchanger (see Table 4.4.1)

Circulating Pumps, Radiator Loops

Number	4	
Type:	CF 3/4 Chempump or equivalent. Centrifugal, totally enclosed, 316 SS construction throughout, class H insulation	
Head developed	ft. H ₂ O	71
Capacity (under above head)	gpm	20

Transfer Pumps, Radioactive Service

Number	2	
Type:	CF 3/4 Chempump or equivalent. Centrifugal, totally enclosed, 316 SS construction throughout, Class H insulation	

Head developed	ft.H ₂ O Max.	90
Capacity (under above head)	gpm	10

Transfer Pumps, Nonradioactive Chemical Service

Number		1
--------	--	---

Type: CF 3/4 Chempump or equivalent. Centrifugal,
totally enclosed, 316 SS construction throughout,
class H insulation

Head developed	ft.H ₂ O max.	90
Capacity (under above head)	gpm	10

Tanks, Radioactive Service

Concentrated radioactive indium sulfate solution
storage tank:

Number		1
Nominal capacity	gal.	856
Maximum capacity	gal.	978
Material of construction		316 SS

Dilute radioactive solution storage tanks:

Number		2
Nominal capacity	gal.	1000
Maximum capacity	gal.	1272
Material of construction		316 SS

Concentrated radioactive waste storage tank:

Number		1
Nominal capacity	gal.	10,500
Maximum capacity	gal.	12,532
Material of construction		316 SS

Tanks, Nonradioactive Chemical Service

Number		2
Nominal capacity	gal.	400
Maximum capacity	gal.	444
Material of construction		316 SS

Concentrating Still

Number		1
Nominal volumetric capacity	gal.	160
Maximum volumetric capacity	gal.	370
Evaporation rate	lbs/hr	375
Packed height	ft.	12
Vapor velocity, packed column	ft/sec	4
Decontamination factor (est.)	$\frac{\text{specific activity feed}}{\text{specific activity condensate}}$	$10^4 - 10^6$
Material of construction		316 SS

Valves, On-Off, Radioactive Service

Number		70
Type:	Mason Neilan No. 875-1 or equivalent. Bellows sealed stem, remotely operated, on-off valve service only	
Material of construction		316 SS
Size	pipe connection, in.	1/4 & 3/4

Valves, Flow Control, Radioactive Service

Number		1
Type:	Mason Neilan No. 138-12 automatic flow control valve with bellows sealed stem, or equivalent	
Material of construction		316 SS
Size	pipe connection, in.	3/4

Valves, Flow Control, Nonradioactive Service

Number 3

Type: Mason Neilan No. 138-12 automatic flow control valve, or equivalent

Material of construction forged steel

Size pipe connection, in. 3/4 & 1-1/4

2.1.5 Shielding

Tolerance dose rate mr/hr 7.5

Reactor

Radial thickness of barytes concrete required around shielding pool at 20 MW for:

1/10 tolerance dose rate	ft.	5.4
tolerance dose rate	ft.	4.7
ten times tolerance dose rate	ft.	4.0

Water depth above core at 20 MW for tolerance dose rate	ft.	21.7
---	-----	------

Radiators

Thickness of slab of ordinary concrete required in horizontal direction (3 ton-megarep/hr in food) for:

		<u>Drum</u>	<u>Slab</u>
1/10 tolerance dose rate	ft.	~6.0	~6.4
tolerance dose rate	ft.	~5.4	~5.7
ten times tolerance dose rate	ft.	~4.7	~5.0

Indium System Equipment

Thickness of shield material
required (3 ton-megarep/hr in
food) for:

	1 inch Sch. 40 Pipe		Heat Ex- changer	Single Pump
	Pb	Concrete	Pb	Pb
	In.	Ft.	In.	In.
1/10 tolerance dose rate	12.9	5.2	14.8	14.9
tolerance dose rate	11.7	4.5	13.2	13.3
ten times tolerance dose rate	10.3	3.8	11.6	11.7

3.0 Reactor Components

The reactor portion of the conceptual design presented herein consists of a heterogeneous light water cooled and moderated core of aluminum clad plate-type fuel assemblies contained in a pressure vessel. The vessel is encircled by an indium solution blanket, a thermal shield, and a biological shield, successively. The general arrangement of the reactor assembly is shown by Figures 3.A and 3.B and the assembly location in the shielding pool is presented by Figures 9.A and 9.B.

3.1 Reactor Core Assembly

The reactor core assembly consists of twenty-four fuel assemblies, seven control rods, an assembly of aluminum filler blocks, rod guides, and upper and lower fuel assembly spacing grids. The arrangement of these units within the reactor tank is shown by Figures 3.A and 3.B.

Figure 3.A shows the arrangement of the twenty-four fuel assemblies in what could be called a staggered, square loading. This particular type of loading is advantageous for the Food Irradiation Reactor for two primary reasons. First, for a given core cross sectional area this pattern more closely approximates a cylinder than does a square loading and thereby permits use of a smaller diameter reactor vessel. Second, with the arrangement of the shim-safety blades in a cross pattern (see Figure 3.A), more control surface is obtained in the staggered square core than with a square loading of the same cross sectional area.

The solid aluminum filler blocks interspersed between the outermost fuel assemblies and the reactor vessel, displace water and thereby reduce parasitic neutron capture in this region. The reduction of parasitic neutron captures increases the number of neutrons available for capture in the indium blanket and thus raises the efficiency of the reactor core-blanket combination.

3.1.1 Fuel Assemblies

The twenty-four fuel assemblies, which make up the FIR reactor core, are of the aluminum-clad, flat plate type construction. The fuel assembly details are shown by Figure 3.C. The flat fuel plates and also the grooved side plates are basically similar to MTR assemblies but more similar to the more recent ETR design. Each FIR assembly contains sixteen 0.050 inch thick, 2.372 inch wide, 37 inch long fuel plates.

The clad-core-clad fuel plates have respective thicknesses of 15 mil aluminum, 20 mil uranium-aluminum alloy, and 15 mil aluminum giving what is commonly referred to as a 15-20-15 arrangement. The core, or 20 mil section, of each fuel plate is an aluminum-uranium alloy of approximately 31.6 weight percent fully enriched uranium, resulting in about 475 grams of uranium in the sixteen fuel plates contained in one fuel assembly. The twenty-four fresh assemblies in the full reactor core contain a total of approximately 11.4 Kg of uranium. It is estimated that this quantity is sufficient to achieve approximately 10 to 20% burnup of a full core. If only part of the fuel assemblies are replaced at shutdown (see Chapter 11.0), but at more frequent intervals, the burnout achieved per assembly will be significantly increased.

The sixteen fuel plates of each assembly, separated by 0.106 inch channels, are fabricated into a sub-assembly by furnace brazing into a pair of grooved 0.098 inch thick side plates. The width of the side plates and width of the finished assembly are identical in dimension (2.460 inches). To complete the fuel assembly, the plate sub-assembly is fitted with end boxes. The two end boxes which are identical and of rectangular cross section, are attached by plug welding to the two side plates only.

The two sides of the end boxes parallel to the fuel plates do not join the outer fuel plates. A vertical opening of 1-1/8 inches exists between the end box and the end of the outer fuel plate (see Figure 3.C). The open side permits free water flow from the flow stream inside the end box to the water flow channel between adjacent fuel assemblies. These openings in the assemblies eliminate hydrodynamic problems associated with providing controlled water flow between assemblies via gaps between the end box surfaces and the locating grids. The fuel assemblies are arranged in a staggered square lattice with 2.5 inch center-to-center spacing. The metal-to-water ratio of an individual fuel assembly is 0.58, and the overall metal-to-water ratio for the core, including the control rods and rod guide housing, is about 0.7.

The 7.5 ft/sec water velocity in the flow channels is low enough that no difficulty with vibration of the flat plates is expected. It is anticipated, by the time the FIR assemblies are manufactured, that fabrication techniques will be advanced to permit using higher strength alloys in place of 2S aluminum for the FIR final assemblies.

3.1.2 Control Rods

The reactor has seven control rods, of which five are shim-safety rods and two are regulating rods. Arrangement of the seven rods is shown by Figures 3.A and 3.B. The present concept is slightly different from that shown in INTERNUC-3 and provides the advantage of more poisoned area in the core. In the INTERNUC-3 proposal the center position is occupied by a small cruciform rod for regulation, while herein it is occupied by a larger cruciform rod used as a shim-safety rod. With the large center cross rod, it is anticipated that normal operational procedure would be to raise the center rod only after the four outer blade rods were near their upper position limits. Such operation may reduce the maximum-to-average ratios of neutron flux and power distribution in the core, and will increase the neutron leakage from the reactor to the indium blanket.

The blades of the shim-safety rods have cadmium in their upper sections and thus insert poison into the core as they are lowered from their uppermost position. The 0.062 inch thick cadmium is sandwiched between two 0.102 inch thick aluminum sheets and the sandwich assembly is fusion welded along its entire edge to form a water tight blade assembly. The cadmium section is 36 inches long while the lower section, which is entirely aluminum, is approximately 47 inches long. The shim safety rod drives, which are located below the reactor, have a quick release clutch allowing the rods to drop into the core. These features are discussed in more detail in Section 3.3.

To provide for fine control of the reactor, two regulating rods are located adjacent to the periphery of the core and diametrically opposite each other. As shown by Figure 3.A, the regulating rods pass through the aluminum filler blocks. Only one rod is necessary for normal operation of the reactor. The center section of a regulating rod is a 36 inch long, 1.5 inch diameter beryllium rod. In zero or normal position the beryllium is centered on the reactor midplane. A 1.5 inch O.D. air filled tube with aluminum clad cadmium walls extends 24 inches above the beryllium. The regulating rods are driven by power mechanisms, similar to the shim safety drives, located below the reactor and connected to the automatic control system for the reactor.

3.1.3 Core Structure

The reactor core structural parts are made entirely of aluminum as shown by Figures 3.A and 3.B. The lower grid, which positions the lower end boxes of the fuel assemblies, rests

on the inside lower flange of the aluminum center section of the reactor vessel. A guide cross approximately 43 inches long extends above the grid to provide channels through the core for the shim-safety rods. The aluminum filler blocks, which fill the region between the fuel lattice and the reactor vessel wall, form the outer wall of the core structure. They also extend upward 43 inches from the lower grid. The lower grid, guide cross, and filler blocks are fabricated as an integral sub-assembly before installation in the reactor vessel. The sub-assembly is attached to the inside lower flange of the reactor vessel center section. The upper grid and shim-safety housing are likewise assembled as a unit to facilitate removal and replacement during shutdown operations. The upper hold-down grid is similar in function to the lower grid in that it positions the fuel element upper end box. It has the additional function of retaining the fuel assemblies against the upward coolant flow. Small leaf-type springs in the upper grid absorb the small dimensional differences that may exist between individual fuel assemblies. The springs bear against the end surfaces of the upper end boxes at two points 180° apart. When refueling the core, the upper grid and shim-safety rod housing assembly is removed as a single unit and stored in the reactor shielding pool.

3.2 Reactor Vessel

The reactor vessel shown by Figure 3.B is an assembly of three separate tanks, viz., an upper stainless steel tank, a center aluminum tank, and a lower stainless steel tank. The center or core tank is made of aluminum because of its low neutron absorption cross section compared to steel. The need for a small cooling water annulus between the core assembly and the reactor vessel suggests that the inside surface of the latter be machined. This center section is made relatively short to ease the machining problem, i.e., it is approximately 4 feet long while the entire reactor vessel assembly is approximately 12 feet long. The aluminum center tank is 18.5 inches O.D. with 0.375 inch walls and has an inside lower flange which supports the core structure (see Section 3.1.3).

The aluminum center tank is supported by a stainless steel lower tank assembly. The lower tank assembly actually consists of two concentric tanks, an inner tank of 19.250 inch I.D. and an outer tank of 32.250 O.D. The inlet water to the reactor is directed down the annulus between the tanks and then upward within the inner tank and through the core. This provides a well distributed flow through the lower vessel and alleviates the problems that might be encountered with

vibrations from high velocity water entering perpendicular to the control blades. The thermal shield and the indium solution blanket tank are supported by the upper flange of the lower tank assembly. The bottom flange of this lower assembly is connected and sealed to the bottom shield plug, which extends through the bottom 4 foot thick concrete wall of the shielding pool.

The portion of the reactor vessel above the aluminum core tank is also a stainless steel cylindrical shell. The reactor vessel is capped with a blind flange which is removable to provide for changing of fuel assemblies and other reactor internal components.

3.3 Control Rod Drives

The seven control rod drives are located in the sub-pile room below the reactor tanks and shield plug. Their location is shown pictorially by Figure 9.A and a typical drive unit is shown by Figure 3.D. Vertical motion is imparted to the control rods by a rack and pinion. The use of a pinion permits use of a short pressure seal on the drive shaft rather than the control rod itself. Figure 3.D is presented as the shim-safety drive unit, however the regulating rod drive will be similar except for the lack of a clutch coupling.

The shim-safety rod drive unit consists of the drive pinion, pressure seal housing, a torque spring, magnetic clutch coupling reduction gearing, and a two phase A.C. low inertia servomotor. A position indicator and movement limit switches are also incorporated in the unit. The pressure seal is designed to use floating Stellite sealing rings on a chrome plated stainless steel shaft. Seal water is introduced at the head of the seal at a pressure slightly higher than that in the reactor vessel so as to reduce contaminated leakage outward through the seal. A leakage take-off is provided in the seal design. The seal has low friction and will allow free motion during a shim-safety rod scram. The clock type torque spring connected directly behind the seal on the drive shaft serves both to overcome the small seal friction and to overcome inertia of the shim-rod during a scram operation. Since the pinion will be engaged to the rod rack at all times, the position indicator and limit switches connected to the same pinion shaft provide continuous position indication. The magnetic clutch coupling installed between the above units and the power train is equipped with spring released holding plates. This reduces the scram time by allowing quicker action of the torque spring. Connected to the clutch coupling are the spur gear-worm gear reduction unit and drive motor, which give a reduction of approximately 1200:1. A low inertia, small

torque motor is used to give better start-stop characteristics to the system.

The power units for driving the two regulating rods are similar to the shim rod units except that the regulating rods do not require the scram feature of the shim system and therefore the torque spring and magnetic clutch coupling are eliminated. Thus, a regulating rod drive unit which consists of a seal, gear train, and motor, is a short compact package.

All rod drive units are connected to the bottom face of the bottom shield plug as illustrated by Figures 3.D and 9.A.

3.4 Blanket

The blanket of indium sulfate solution is contained in two tanks surrounding the reactor vessel at the reactor core level (see Figures 3.A and 3.B). The tanks, each forming an arc of 180°, form a 4 inch indium blanket surrounding the reactor core. They are 36 inches high at the inside diameter and taper off at a 45° angle to 28 inches high at the outside diameter. The total volume of the tanks is approximately 5.3 ft³. The indium sulfate solution is circulated between the blanket and the gamma radiators in the Radiator Building and also between the blanket and the indium system heat exchanger in the Reactor Building first sub-level. The blanket is split into two halves to facilitate its removal if necessary. Removal of the blanket would be accomplished in the following steps: 1) remove the thermal shield immediately surrounding it; 2) remove the hinge holding the two halves of the blanket together; and 3) pull the blanket tanks away from the reactor vessel and lift them up past the upper flange of the reactor vessel.

Stainless steel is used for the blanket tank as well as for the remainder of the indium solution system. Other materials, such as aluminum or zirconium, are preferable because of their low neutron absorption properties, but at present it appears wise to design on the basis of stainless steel. Although stainless steel seems a logical choice with respect to corrosion resistance, positive information as to its suitability in this regard is not available.

Based on efficiency studies presented in Chapter 8.0, the 4 inch blanket thickness is probably the optimum. Cooling of the blanket is discussed in Section 5.3.1. Cooling is accomplished by circulating the indium fluid from the blanket through a heat exchanger loop that is independent of the radiator loops. Cooling is aided somewhat by water passing down 1/8 inch annuli on both surfaces of the blanket. The cooling water that passes

through these annuli goes through the indium solution heat exchanger and is then returned to the reactor pool. In case of total loss of power, in which event the auxiliary cooling system would be inoperative, it is anticipated that natural convection in the $1/8$ inch annuli on both sides of the blanket would provide sufficient cooling to remove shutdown heat.

3.5 Thermal Shield

Surrounding the indium blanket and separated from it by a $1/8$ inch water annulus is a 3 inch thick stainless clad carbon steel thermal shield. The function of the thermal shield is to reduce the gamma heat dissipated in the reactor pool and concrete shield. Reduction of heat to the shielding pool permits a lower shield pool water temperature, which is important from the standpoint of human comfort in the building and also cooling loads during summer ventilation. Cooling of the thermal shield, which is discussed in Section 5.3.1, is accomplished by water flowing down the $1/8$ inch annuli along both surfaces. Thermal stress calculations for the thermal shield are reported in Section 7.6.1. The use of the steel thermal shield rather than water immediately surrounding the indium solution blanket has essentially no effect on over-all efficiency of the reactor-radiator complex (see Section 4.3.4 of INTERNUC-3).

3.6 Biological Shield

The biological shield for the reactor consists of a 27 foot deep, 10 foot diameter water-filled pool surrounded by concrete. In the lower portions of the pool where the radiation intensity is high, the pool walls are constructed of barytes concrete. The thickness of the concrete varies according to the particular location. In regions where personnel will normally be located, the thickness is approximately 6 feet, which is sufficient to reduce the radiation level to less than $1/10$ of tolerance. In other areas, such as the primary cooling system equipment room on the first subfloor of the Reactor Building, the thickness of concrete between the reactor and the equipment room is only about 3 feet, since during operation this will be an off-limits area, and after shutdown the 3 feet of concrete is sufficient shielding. The upper part of the pool wall is ordinary concrete, approximately 18 inches thick. The depth of the pool gives approximately 19 feet, 6 inches of water above the reactor core. This is considered sufficient to achieve tolerance radiation levels at the top of the reactor for the normal operating power of approximately 10 MW. During shutdown it is estimated that the pool water could be lowered to approximately 12 feet above the core. This would be an aid in changing fuel assemblies and conducting other work that

may be necessary inside the reactor vessel. The details of the shielding considerations are given in Chapter 7.0, to which the reader is referred for information on shielding required for different locations and different operational conditions. The thicknesses of the concrete at various points in the biological shield are shown most clearly by Figures 9.A, 9.B, 9.C, and 9.D. The pool tank itself, which forms the inner shell for the biological shielding concrete, is a stainless steel tank.

3.7 Reactor Control System

A block diagram of the nuclear instrumentation and control is shown by Figure 3.E. The control system provides a method of operating and controlling the reactor, in a safe manner, from a centrally located control room. The control system allows manual control at all times but provides for automatic control of the reactor power level at a constant value at the operator's discretion. Automatic insertion of control and safety rods called for by certain abnormal conditions will override any signals present which are calling for rod withdrawal. Speed control of rod drive mechanisms limits the rod withdrawal rate below a certain power level. Above this power level, the period and neutron level systems are working and can control the power level adequately for faster rates of rod withdrawal. Since it is expected that the FIR will be operated over a wide range of power levels, the regulating rod servo system derives its signal from a vibrating reed electrometer and a compensated ion chamber which would have a range of at least 10^5 by appropriate range changes at the electrometer. Below is a proposed list of safety signals for the FIR. This list may require appreciable change after further study.

Table 3.7 Safety Signals for FIR

	<u>Regulating Rod</u> <u>Run In</u>	<u>Slow</u> <u>Shutdown</u>	<u>Scram</u>
Neutron flux	-	1.1 N(normal)	1.25 N
Period	20 sec.	5 sec.	1 sec.
Primary flow	-	0.9 N	0.8 N
Primary CW temperature	-	1.2 N	-
Reactor pressure	-	0.95 N	0.9 N
Power calculator	1.2 N	1.4 N	1.6 N
Indium leak	-	x	-
Fuel rupture	-	x	-
Instrument power	-	-	x
Pressurizer temperature	-	1.1 N	1.2 N
Indium cooling loop pressure	-	1.1 N	1.2 N
Indium cooling loop flow	-	0.9 N	0.5 N
Indium cooling loop temp.	-	1.1 N	1.2 N

3.7.1 Instrumentation

Startup

There are two startup channels, one BF₃-scaler system having a sensitivity of approximately 5 counts/nv, and a fission counter-log CRM system having a sensitivity of approximately 0.5 counts/nv. The BF₃-scaler system is used when the source strength of the reactor is below the range of the fission-counter-log CRM system. At startup, both systems are located near the reactor, and as the power level increases the chambers are automatically withdrawn to other preset positions to prevent damage to the chambers and to keep the recorder indicator on scale. This automatic action is initiated by a switch in the log CRM recorder.

Log N-Period

A single log N-period system monitors and records the logarithm of neutron flux and the reactor period. This system uses a compensated ion chamber to supply the signal to the log N-period amplifier. The period recorder supplies signals to the regulating rod and slow shutdown circuits when the period exceeds preset limits, and the log N-period amplifier supplies a signal to the scram circuit when the period reaches one second.

High Neutron Level

Two identical compensated ion chamber vibrating reed electrometer systems supply the safety signals for high values of neutron flux. The electrometers operate a sensitive relay for a fast activation of the scram circuit when the flux reaches 1.25 Normal. The recorders provide a record of reactor operating flux and actuate the slow shutdown circuit when the flux exceeds 1.1 Normal.

Galvanometer

The galvanometer utilizes a compensated ion chamber to drive a light beam galvanometer with a zero center scaler. A null circuit is provided so that the galvanometer can be biased to give a zero deflection at any power level. Deflection from zero will then indicate deviations from that level. The galvanometer can also be used as a power level indicator by setting the null circuit to zero.

3.7.2 Reactor Console

A layout of the reactor control console is shown by

Figure 3.F. Besides the controls and nuclear instrumentation for the reactor, the panel instruments provide operational information on the reactor primary cooling system, the auxiliary cooling system, and the indium solution system.

4.0 Indium System

4.1 General Description

The indium system is designed to circulate a radioactive aqueous indium sulfate solution of up to 600 g/liter concentration through a system of radiators such that food and other materials may be exposed to the gamma radiation from the indium isotope. The indium is activated by neutrons leaking from the reactor core into the surrounding indium sulfate solution blanket. Separate pumps and piping loops provide for circulation of the activated indium sulfate between the blanket and the four radiators. This system is shown schematically by Figure 4.A.

Four gamma radiators are provided, each in separate cubicles to permit independent operation and maintenance. The two slab-type radiators are for irradiating large packages and odd shapes and sizes of food and other materials, while one drum-type radiator accommodates No. 2 food cans and the other No. 10 food cans. Each radiator cubicle is provided with cooling and heating facilities such that the radiation space may be maintained at any temperature between -4°F and $+167^{\circ}\text{F}$ with a maximum variation of $\pm 4^{\circ}\text{F}$.

The arrangement of the radiators and their circulating loops provides maximum flexibility in their operation, and facilitates maintenance of the radiators and their associated equipment. Individual shielding of each circulating pump permits maintenance thereof, while any of the other circulating pumps are in operation. The piping and valves are so arranged that any combination of radiators can be operated simultaneously. Any radiator loop can be started, operated, shut down, drained, flushed, decontaminated, and/or maintained while any other radiator is in operation.

To provide for the draining, flushing, and decontamination of the radiators and their circulating loops, additional tanks, pumps, piping, and equipment are necessary. Separate storage tanks are provided for the concentrated indium sulfate solution drained from the radiator and the circulating loop, the dilute indium sulfate solution produced when the loop is flushed, and the decontaminating solution used for final decontamination of the equipment in the loop. Any of the above solutions can be stored in any of the three tanks, to attain greatest flexibility in operation. Two transfer pumps, separately shielded, are provided to transfer solution from these three storage tanks, the indium sulfate solution make-up tank, or the concentrating still to any other point in the system. These pumps also move concentrated

radioactive liquid waste from the concentrating still to the permanent radioactive waste storage tank.

Spray rings external to each piece of equipment handling radioactive solution provide for external decontamination of the equipment (these lines are not shown in Figure 4.A). Such decontaminating solutions and leakage from any of the radioactive equipment drain to a sump at the lowest elevation in the system. These solutions can be returned to the decontaminating solution storage tank by means of a jet ejector located in the sump. To facilitate external decontamination of all equipment, each cubicle or space containing radioactive equipment is lined with stainless steel.

A concentrating still provides for concentration of the dilute indium sulfate solution produced when a radiator loop is flushed, and also the dilute decontaminating solution produced when a radiator loop is decontaminated. The concentrated indium sulfate solution is returned to the system for reuse while the concentrated decontaminating solution is stored in the permanent radioactive waste storage tank. The vapor from the still, in either case, will be condensed, passed through a monitoring device and sent to the normal area drain. In case of radioactive material carry-over into the vapor from the still, the radiation monitoring device will automatically change the flow of this stream from the drain to the dilute indium sulfate solution storage tank.

The entire indium sulfate solution system has a common vent system. This vent system discharges to the off-gas stack through a condenser and recombiner. The condenser removes condensibles in the off-gas while the recombiner reunites the hydrogen and oxygen gases from the decomposition of the water in the indium sulfate solution. Each radiator circulating loop has two paths into the vent system. The first path is through a normal on-off valve for filling the system, the second path is through a pressure relief valve set for a maximum pressure of 40 psig.

All cooling water or steam condensate that flows through heat exchange equipment in the indium sulfate system is monitored for radioactivity. Normally, there is no radioactivity in this water and it passes to the area drain. In case of a leak in the system, the monitoring equipment changes the flow of this water from the drain to the retention basin. Thus, the radioactivity of the indium sulfate system can be entirely contained within the immediate area of the food irradiation facility.

A remote sampling and analytical facility is provided to allow sampling and analysis of the solutions contained in the concentrated radioactive indium sulfate storage tank and the concentrating still.

4.2 Major Equipment

4.2.1 Radiators

The gamma radiators are the final step in transferring energy from the fission process to the food or other material processed in the food irradiation plant. The indium sulphate solution, which is circulated between the reactor blanket and the radiators, provides the energy in the form of decay gammas. The food from the processing plant is carried past or through the radiators by conveyor systems. In the presently conceived reactor-radiator complex, there are four main radiators. These consist of two drum-type radiators designed primarily for the processing of food in cans, and two slab-type radiators for the irradiation of food in other containers. The latter are also for the irradiation of irregularly sized items such as sides of beef, baskets of fruit, vegetables, military equipment, etc. The use of four radiators, which in some cases are divided into parts, provides for appreciable flexibility in the operation of the food irradiation facility.

Each radiator is housed in a separate cubicle. The arrangement of these cubicles and the radiators in the radiator building are shown most clearly by Figures 9.B, 9.F, and 9.A. Provision of four cubicles provides versatility in that more than one radiator can be used at any one time, and production need not cease when one or more of the radiators are out for repair. Provisions are made for carrying out low intensity irradiations in one slab radiator cubicle, while high level irradiations are carried out in other radiator cubicles. Provisions are also made for carrying out irradiations at relatively high intensities, that is, at intensities higher than those called for by the performance specifications (Appendix-1). Low intensity irradiations can be carried on by reducing the reactor power to a level which provides the right source intensity, provided sufficient time is allowed for decay from any previous higher power levels.

Studies of the efficiency of the radiators and the overall reactor-radiator complex are reported in Chapter 8.0, wherein the power levels necessary to achieve the desired production, dose and dose rates, are reported. These have

figured largely in the design of the radiators described below.

Drum-Type Radiators

The drum-type radiators have been used in determining the normal reactor operating power and the design of the reactor with respect to blanket thickness. This type of radiator has advantages in that relatively low maximum-to-minimum dose ratios can be attained in the products and also that reasonably high overall reactor-radiator efficiencies can be achieved with short dwell times. Two drum-type radiators, one for No. 2 cans, and one for No. 10 cans are provided.

An assembly of the No. 2 Can Radiator is shown by Figure 4.B. This radiator has 61 parallel tubes arranged in a triangular lattice in a hexagonal drum. The tubes are 15-cans long, giving a drum height of approximately 68 inches. The total weight of the product in the drum is approximately 1200 lbs. This number is based on a food throughput of 3000 lb/hr, which at a dose rate of 5×10^6 rep/hr receives a total dose of 2×10^6 rep in 24 minutes. The pattern of the tubes in the drum and also the detailed arrangement of the tubes themselves is shown by Figure 4.B. Two tubes are associated with each row of cans. The outer tube, called the drum-tube, is a part of the radiator drum and serves to contain the indium solution in the drum. The inner tube, called the can-tube, provides the means for guiding the cans through the radiator. A 1/4-inch dead air space between the can-tube and the drum-tube provides insulation between the drum contents and can contents. This insulation is necessary because of the extremes of temperature at which the product must be maintained. The can-tube has four internal ridges which hold the can centrally within it. This provides an air space of approximately 3/8-inch between the can-tube and the cans. The air for controlling the temperature of the cans passes through this space, and controls the temperature from -4°F to $+167^\circ\text{F}$. In order to provide uniform flow of the indium solution through the drum, horizontal baffles are used throughout the vertical height. These baffles are spaced approximately on 7 inch centers and are arranged so as to pass the indium solution from one side of the drum and then to the other. The indium solution in passing through the drum makes nine complete reversals in direction. This aids in producing a uniform dose in the cans in the various

portions of the drum as they pass upward through the drum.

The No. 10 Can Radiator, a cross section of which is shown by Figure 4.C, is similar to the No. 2 Can Radiator except that there are only 19 cans in a horizontal plane, and the number of cans stacked vertically upward is only 10. This provides a total of 190 cans in the radiator for a total weight of approximately 1300 lbs. The spacing between the drum-tubes of the No. 10 Can Radiator is 5/8-inch compared to 1/4-inch for the No. 2 Can Radiator. These spacings were determined from investigations reported in Chapter 8.0.

Approximate dimensions and weights for No. 2 and No. 10 cans are given in Table 4.2.1 below.

Table 4.2.1 Approximate Dimensions and Weights
for No. 2 and No. 10 Cans

Item	Units	Can	
		No. 2	No. 10
Overall dimensions			
Diameter	in.	3.38	6.19
Height	in.	4.52	6.97
Inside dimensions			
Diameter	in.	3.24	6.04
Height	in.	4.28	6.75
Can Weight	lb.	0.20	0.59
Can Contents (assumed full of water)	lb.	1.34	7.0

A typical drum radiator installation is shown by Figure 4.D. This figure shows the arrangement of the drum within the radiator cubicle, the auxiliary shielding provided above and below the radiator, and the entrances and exits from the radiator for loading and unloading cans. The hexagonal drum radiators, which are approximately 45 inches across the diagonal, are centered in

a 5-foot diameter hole in the radiator shield. Both immediately above and below the radiator are sections of steel through which the cans pass. These steel sections, which are approximately nine inches thick for the No. 2 Can Radiator and approximately seven inches thick for the No. 10 Can Radiator, provide auxiliary shielding at the top and bottom of the drum radiators to minimize streaming of gammas out the entrance and exit charging ports.

The Ford, Bacon and Davis firm of New York, which is doing preliminary design of food handling equipment for the Army Corps of Engineers, have tentatively proposed the scheme shown by Figure 4.D, where the cans are pushed upward through the drum radiator. The steps in handling the food into and out of the drum radiator are as follows:

The food is loaded by hand into a loading rack, which has positions corresponding to those of the drum radiator. This rack is passed through an auxiliary shield at the face of the radiator cubicle, at which point the cans are lowered down into a charging rack located directly beneath the loading rack. The cans are lowered from one rack to the other by a group of hydraulically or mechanically operated pistons. When the cans are in the charging rack, the rack is transferred to the inside of the radiator cubicle, and at this point a similar piston charging device pushes all cans simultaneously up into the radiator, where they are held by small spring-loaded pawls. At this time the upper row of cans, which has just been pushed out the top of the radiator, is picked up by a magnetic lift and carried off to the side of the radiator, but still within the shielded area. They are released to a conveyor belt, which carries them outside the primary shield and transfers them to another conveyor which carries the cans away. The secondary shield, which is located approximately one drum width from the primary shield, is primarily to stop radiation that streams out the charging ports.

Floor slabs immediately above the radiator cubicle can be removed, thereby permitting maintenance on the equipment within the cubicle or removal of equipment from the radiator cubicle.

In order to achieve uniform or relatively uniform temperature of the product in passing through the radiator, environmental conditioning air is passed through

the tubes surrounding the cans. This air is introduced at the top of the radiator cubicle and flows downward through the space between the cans and the can-tube and then flows out again through the bottom of the radiator cubicle from where it is directed to an air cooling and heating unit. This unit provides for removal of heat or addition of heat so as to maintain the product at its desired temperature.

Slab-Type Radiators

The slab-type radiators are intended primarily for irradiation of boxes or cartons of canned or packaged products and also for the irradiation of irregularly shaped articles such as sides of beef, baskets of food, etc. The slab radiators proposed in this report are shown by Figure 4.E. There are two slab radiators, the Single Slab Radiator, and the Multiple Slab Radiator. Each has its own particular advantages.

The Single Slab Radiator is intended primarily for irradiation of products in which the dwell time in the radiator cubicle can be relatively long (about one hour for 2×10^6 rep dose) and also for particularly large items, such as the sides of beef or hog carcasses. The dose rate in the large single slab at a given power is substantially lower than the other slabs. This is shown in Table 8.0 of Chapter 8.0. Although the dose rate of such a facility is low, its efficiency can be relatively high if there is no particular need to have either a low dwell time or a low maximum-to-minimum dose ratio in the product.

The construction of the slab, as shown by Figure 4.E, is preferred over the use of an array of round tubes since the flat-sided slab gives the larger volume per unit area of radiator for a given radiator thickness and thus is a more efficient arrangement. The type of structure shown by Figure 4.E is expected to be entirely satisfactory. The fabrication involves the formation of U-shaped channels, three inches wide with 2-inch legs. These channels are welded together along their complete length to form a single slab. Besides strengthening the slab irradiator, the channel-type construction provides baffles for directing the flow through the radiator in a satisfactory path. Because of appreciable decay in the intensity of the gamma fluid as it passes through the radiator, it is necessary to direct it crossways to the direction of the path of the product. This is necessary to assure that all products passing through the radiator

receive very near to equal doses, for, as shown in Appendix 1, the maximum-to-minimum dose should be 1.25 or less.

Two 8-foot long tubes, one 3-inch and one 1-inch diameter, at one end of the Single-Slab Radiator, provide for low dose irradiations when other radiators are in use with the reactor at relatively high power. The volume of the 3-inch tube is approximately 1/50 of that of the Single-Slab Radiator, and thus, for the same conveyor speed, would result in a dose lower than that of the Single-Slab Radiator by about the same factor. The 1-inch tube would provide for doses lower by another factor of ten. Through the use of these single tubes and various conveyor speeds, doses of about 10^4 to 10^5 rep can be accomplished in some products, while other irradiators are being utilized for producing doses of the order of 10^6 to 10^7 reps in other products.

The Multi-Slab Radiator, located in another cubicle, is sized primarily for V3 boxes since it is expected that much of the food in the larger packages will be in this size container. The slab is divided into four sections, each of which provides for the simultaneous irradiation of ten V3 boxes. These units are piped so that one or any combination of the four units can be used. Because of the smaller circulation system volume when only one unit is in use, higher dose rates can be achieved with the use of one unit. However, the reactor power required to achieve a given production-times-dose (lb/hr x rep) is high, that is, the efficiency of the overall reactor radiator system when utilizing only one unit of the Multi-Slab Radiator is relatively low. Use of two units of the Multi-Slab Radiator compares fairly closely with the No. 2 and No. 10 Can Radiators with regard to total product (lb/hr) handled. However, these are based on the use of V3 boxes, and here again the maximum-to-minimum dose rate in the product is relatively high, that is, it is about 1.39. The four units of the Multi-Slab Radiator, when used simultaneously, would produce about three ton-megarep/hr in the food with a reactor power of only 6.1 MW. However, this requires an increase in the dwell time to about 55 minutes.

The construction of the Multi-Slab Radiator is similar to that of the Single-Slab Radiator with the exception that it is built into four separate units, each of which is provided with an inlet and an outlet. The three-inch thickness of the slabs is based on the investigations reported in Sections 8.7 and 8.8.

4.2.2 Storage Tanks

The only two non-radioactive storage tanks provided in the indium system are designated as B-1 and B-2 in the schematic flowsheet, Figure 4.A. Pertinent data concerning these tanks is presented in Table 4.2.2. Tank B-1 is the indium sulfate solution make-up and storage tank; B-2 is the decontaminating solution make-up and storage tank. A portable-type mixer should be provided for each tank. The size of these tanks is based on the volume of indium sulfate contained in one complete radiator loop. The probable low frequency of use of these tanks favors keeping them small.

Four radioactive storage tanks are provided in the indium system. They are designated as C-1, C-2, C-3 and E-1 in the schematic flowsheet, Figure 4.A. Tank C-1 is the concentrated radioactive indium sulfate storage tank, C-2 is the dilute radioactive indium sulfate storage tank, C-3 is the dilute radioactive decontaminating solution storage tank, and E-1 is the permanent radioactive waste storage tank. Pertinent data concerning these tanks is presented in Table 4.2.2.

The size of tank C-1 is based on the volume of the entire radioactive indium sulfate system. It will hold all the indium sulfate solution required to fill the four radiators, the heat exchange loop, the blanket, and the radiator loops. It has additional capacity to allow for adjustments of the concentration of the indium sulfate solution it contains.

The sizes of tanks C-2 and C-3 are based on these tanks holding the equivalent of about ten system volumes from the one radiator loop containing the largest volume of indium sulfate solution.

The size of tank E-1 is based on an estimated rate of production of concentrated radioactive liquid waste of about 80 to 85 gallons per month. Thus tank E-1 would hold all of the concentrated radioactive liquid waste produced by ten years of operation. The volume adequacy of tank E-1 will depend to a large extent on the frequency with which decontamination of the radiator systems is required. It appears that decontamination of two radiator loops a month and a consequent 96% reduction in volume of the decontaminating solution used is a reasonable estimate.

Table 4.2.2 Data on Storage Tanks in Indium System

Tank		B-1	B-2	C-1	C-2	C-3	E-1
Description		Cylindrical vertically mounted elliptical heads 8"x10" top opening	Cylindrical vertically mounted elliptical heads 8"x10" top opening	Cylindrical horizontal mount elliptical heads	Cylindrical horizontal mount elliptical heads	Cylindrical horizontal mount elliptical heads	Cylindrical horizontal mount elliptical heads
Nominal capacity	Gallons	400	400	856	1000	1000	10,500
	Cubic Feet	53.5	53.5	114.4	133.7	133.7	1405
Maximum capacity	Gallons	444	444	978	1272	1272	12,533
	Cubic Feet	59.4	59.4	130.8	170.0	170.0	1675
Straight side length		5' - 0"	5' - 0"	5' - 0"	7' - 0"	7' - 0"	18' - 0"
Overall length		7' - 2 5/8"	7' - 2 5/8"	8' - 0 3/4"	10' - 0 3/4"	10' - 0 3/4"	23' - 8"
Wall thickness		1/4"	1/4"	3/8"	3/8"	3/8"	1/2"
Inside diameter		3' - 6"	3' - 6"	5' - 0"	5' - 0"	5' - 0"	10' - 0"
Mat'l. of Construction		316 ss	316 ss	316 ss	316 ss	316 ss	316 ss
Weight empty, lbs		1,133	1,133	2,224	2,707	2,707	16,739
Weight full of water, lbs		4,836	4,836	10,380	13,315	13,315	121,256

4.2.3 Concentrating and Decontaminating Still

The concentrating and decontamination still is designated as D-1 in the schematic flowsheet, Figure 4.A. Pertinent data concerning this evaporator is presented in Table 4.2.3. This evaporator serves a dual purpose; it concentrates the dilute radioactive indium sulfate solution produced whenever a radiator and its circulating loop are flushed; and it concentrates the radioactive solutions produced whenever a radiator and its circulating loop are decontaminated. In either case, the concentrated radioactive solution remains in the still pot and the non-radioactive vapor which comes off the top of the column is condensed and wasted to the normal drain system.

The size of the still and its decontaminating column is based on the evaporation rate desired. In this case it is assumed that it is desirable to evaporate the contents of both C-2 and C-3 in two days of still operation. This requires an evaporation rate of 375 lb/hr. The evaporation rate from the surface of the liquid in the still pot is about 20 lb/ft²-hr, and the vapor velocity in the still column is about 4 ft/sec. The ability of these conditions to accomplish good decontamination should be investigated experimentally.

4.2.4 Pumps

The indium system requires the use of seven pumps. They are designated P-1, P-2, P-3, P-4, P-5, P-6, and P-7 in the schematic flowsheet, Figure 4.A. All of these pumps except P-7 are used in radioactive solution service. Pumps P-1, P-2, P-3, and P-4 circulate the indium sulfate solution between the blanket and the various radiators. Pumps P-5 and P-6 transfer indium sulfate solutions, flush water and decontaminating solutions from one part of the system to the other, as required. Pump P-7 is the decontaminating solution transfer pump.

Two criteria determine the flow rate needed in the radiator loops. They are the half-life of the activated isotope and the temperature rise (due to self-absorption) of the activated solution in passing through the portion of the loop external to the blanket. The latter can be arrived at on the basis of the radiator efficiency and the dose to the product. Based on 7kw of gamma power absorbed in the food and a radiator efficiency of about 20%, a flow rate of 15 gpm results in an indium solution temperature rise of about 15°F in passing through the radiator. If the indium sulfate solution leaves the

Table 4.2.3 Data on the Concentrating and Decontaminating Still

Section		Still Pot	Still Column
Description		Cylindrical tank horizontal mount elliptical heads - 230 ft ² heat exchange coils lo- cated in bottom 9"	Cylindrical column vertically mounted over still pot elliptical top head packed with raschig rings or fiber glass
Nominal capacity	Gallons	160	--
	Cubic Feet	21.4	20 (of packed space)
Maximum capacity	Gallons	370	--
	Cubic Feet	49.5	--
Straight side length		6' - 0"	13' - 6"
Overall length		8' - 0 3/4"	14' - 0"
Wall thickness		3/8"	1/4"
Inside diameter		3' - 0"	17 1/2"
Mat'l of construction		316 ss	316 ss
Weight empty, lbs		1254	1231
Weight full of water (or raschigrings), lbs		4340	4773
Heat transfer surface, ft ²		230	None
Heat transferred, Btu/hr		422,000	--
Evaporation rate, lb/hr		375	--
Evaporation rate, lb/hr-ft ²		20	--
Lbs 40 psi steam/hr		420	--
Vapor rate, ft ³ /sec		6.473	6.473
Vapor velocity, ft/sec		--	3.88
Packed height		--	12' - 0"
Decontamination factor		10 - 100	10 ³ - 10 ⁴

reactor blanket at 180 to 190°F, it attains a maximum temperature of about 205°F with a flow rate of 15 gpm. Reference to Section 8.4 shows the 15-gpm flow rate to be entirely satisfactory from the standpoint of decay of the radioactive isotope. Therefore, the circulating pumps are sized for a minimum flow rate of 15 gpm. On the above basis, Model CF 3/4 Chempumps, or equivalent, are specified for the radiator loops. Calculations indicate they will maintain a flow rate of approximately 18 gpm under the most adverse operating conditions. No provision is made for controlling the flow rate in the radiator loops since the maximum flow rates attainable are most desirable.

The transfer pumps, P-5, P-6, and P-7, are also specified as Model CF-3/4 Chempumps, or equivalent, mainly on the basis of type of service and flow rates desired. It is also conceivable that if the need arose, these pumps could be used as spares for the radiator loops. Particular attention should be given to the insulation used in the pumps to assure that the pumps can withstand continuous operation in the high gamma fields encountered in this facility.

4.2.5 Piping and Valves

Piping in the circulating loop systems is 316 SS schedule 40 pipe finished with an 80 grit polish on the inside. Since radioactive contaminants are primarily mechanically entrained and polishing of the surface in contact with the radioactive solution will tend to reduce decontamination problems, an 80 grit polish is desired on the inside surfaces of all radioactive solution piping. Highly polished pipe is not deemed worthwhile since corrosion and erosion will eventually deface the surface. Nominal pipe size is either 3/4-inch or 1 inch. Welded joints are required in all radioactive solution service to minimize leakage.

Under the above conditions of internal pipe surface the friction heads, developed at a flow rate of 15 gpm, are approximately 0.57 ft per foot of 3/4-inch pipe and 0.18 ft per foot of 1 inch pipe. A total friction head of about 70 feet is developed at a flow rate of 15 gpm in the radiator loop having the highest resistance.

All valves in radioactive solution service are 1/4-inch or 3/4-inch Mason-Nielan No. 875-1, 316 SS on-off remotely controlled valves or equivalent. These valves were specifically designed for use with the high level radioactive solutions encountered at the Chemical Processing Plant, National Reactor Testing Station at Arco, Idaho, and have proven to be highly reliable in this service. On-off operation of the valve is accomplished from a remote location by means of an air line to the valve.

Valves in all non-radioactive solution service will be standard manually operated globe or gate valves, their materials of construction and working pressure determined by the service.

Service piping, valves and related equipment, as well as lines for external decontamination of radioactive equipment are not shown in the schematic flowsheet Figure 4.A. Services required will be 40 psig steam, demineralized water, cooling water, and instrument air.

4.2.6 Instrumentation

Instrumentation to accomplish operational control of the indium system is indicated in the schematic flowsheet, Figure 4.A. Data pertaining to all instrumentation described in this section are given in Table 4.2.6. Liquid level and density of stored solutions are measured by the differential pressure between two air bleed probes located at different levels in the tank. The differential pressure between the probes in the tank is converted to an equivalent absolute pressure by a transmitter located in a non-radioactive area. This equivalent absolute pressure is applied to a recording or indicating instrument at the indium system panel board to show the liquid level or density of the solution in the tank.

All temperature measurements in the indium system are made by resistance thermometers connected to remote indicating and recording instruments at the indium system panel board. At temperatures below about 400-500°F, resistance thermometers are more accurate and require less service than thermocouples.

The indium sulfate solution flow rate in each radiator loop is measured and remotely recorded at the indium system panel board by a Fisher & Porter Co. Fig. 52 Armored Flowrator Meter, size 8, or its equivalent. An electric transmitter on top of the Flowrator transmits the flow signal to the panel mounted recording instrument.

Table 4.2.6 Data for Indium System Instruments

Inst.	Receiver Location	Sensing Element	Transmitter	Receiver	Control	Range
DRCA-1	Control panel	Double dip tubes	Pneumatic	Recorder	Proportional low alarm	.9 - 1.9
FRA-1	CP-CR	Rotameter	Electric differential transformer	Recorder	Low alarm	0 - 50 gpm
FRA-2	CP-CR	Rotameter	Electric differential transformer	Recorder	Low alarm	0 - 50 gpm
FRA-3	CP-CR	Rotameter	Electric differential transformer	Recorder	Low alarm	0 - 50 gpm
FRA-4	CP-CR	Rotameter	Electric differential transformer	Recorder	Low alarm	0 - 50 gpm
FRA-5	CP-CR	Rotameter	Electric differential transformer	Recorder	Low alarm	0 - 50 gpm
LA-1	Local	Float operated switch	--	--	High alarm	
LIA-1	CP-CR	Double dip tubes	Pneumatic	Indicator		0 - 72 in.
LIA-2	CP-CR	Double dip tubes	Pneumatic	Indicator		0 - 72 in.
LRA-1	CP-CR	Double dip tubes	Pneumatic	Recorder	High alarm	0 - 120 in.
LRA/DR-1	Control panel	Double dip tubes	Pneumatic	2 pen recorder	High alarm	0 - 60 in. 1 - 1.6 sp.g.
LRA/DR-2	Control panel	Double dip tubes	Pneumatic	2 pen recorder	High alarm	0 - 6.0 in.
LRA/DR-3	Control panel	Double dip tubes	Pneumatic	2 pen recorder	High alarm	0 - 6.0 in. 1 - 1.6 sp.g.
LRCA-1	Control panel	Double dip tubes	Pneumatic	Recorder	Proportional low alarm	0 - 36 in.
pHR-1	CP-CR	pH electrodes	--	Recorder	--	
PI-1	Local	Gauge	--	--	--	0 - 100 psi
PI-2	Local	Gauge	--	--	--	0 - 100 psi
PI-3	Local	Gauge	--	--	--	0 - 100 psi
PRA-1	Control room	Diaphragm	Pneumatic	Recorder	High alarm	0 - 150 psi
PRCA-1	Control panel	Diaphragm or bellows	Pneumatic	Recorder	Proportional high alarm	0 - 10" H ₂ O vacuum

Table 4.2.6 (Cont.)

Inst.	Receiver Location	Sensing Element	Transmitter	Receiver	Control	Range
RRA-1		Ion chamber	Microammeter amplifier	Recorder	High-low alarms	0 - 10^7 r/hr
RRA-2			Microammeter amplifier	Recorder	High-low alarms	0 - 10^7 r/hr
RRA-3			Microammeter amplifier	Recorder	High-low alarms	0 - 10^7 r/hr
RRA-4			Microammeter amplifier	Recorder	High-low alarms	0 - 10^7 r/hr
RRA-5			Microammeter amplifier	Recorder	High-low alarms	0 - 10^7 r/hr
RRCA-1	Control panel	Ion chamber	Microammeter amplifier	Recorder	On-off alarm	0 - 1 r/hr
TR-1-A	Control panel	Resistance thermometer	--	Recorder	Multipoint recorder	0 - 250°F
TR-1-B	Control panel	Resistance thermometer	--	Recorder	Multipoint recorder	0 - 250°F
TRA-1	Control room	Resistance thermometer	--	Recorder	High alarm	0 - 250°F
TRA-2		Resistance thermometer	--	Recorder	High alarm	0 - 250°F
TRC-1		Resistance thermometer	--	Recorder	Proportional	0 - 250°F

The pH of the indium sulfate solution in the circulating system is continuously monitored and is recorded at the indium system panel board.

Pressures appearing in various parts of the indium system are measured locally and the measurement transmitted electrically to a panel mounted indicator or recorder. Knowledge of the absolute solution pressures within the slab radiators are particularly important since the slab radiators cannot be constructed to withstand more than moderate pressures without seriously affecting the radiator efficiency.

High level radiation instruments to measure the radiation intensity are located in every area containing indium sulfate solution. All radiation levels are indicated or recorded at the indium system panel. Instruments to continuously monitor low levels of radiation intensity are located near the indium system panel, in the area above each radiation cubicle and in the indium sulfate and decontaminating solution make-up area. "Friskers" to detect extremely low levels of radiation from food packages and/or personnel clothing are located at all entrances and exits of the Radiator Building and the food handling room at the face of the radiator cubicles.

The condensate from the concentrating still, and the cooling water and steam condensate from heat exchange equipment (in the Radiator Building) in direct contact with indium sulfate solution are continuously monitored by two radiation instruments. Both of these instruments give an alarm signal at the indium system control panel whenever preset maximum radiation levels are exceeded, and direct the stream from the normal drain to a retention basin.

4.2.7 Control

Control of all processes carried out within the indium system is accomplished from the indium system panel board, except for the make-up of indium sulfate and decontaminating solutions which are accomplished in the area in which tanks B-1 and B-2 are located. On-off valves for all process flows and for all services to the indium system process are controlled from or located at the indium system panel board.

The valve admitting dilute indium sulfate or decontaminating solutions to the concentrating still is the only flow control valve in radioactive service in the indium system, and its operation is automatically controlled by the

liquid level instrument for the concentrating still. This valve is a 3/4-inch Mason-Nielan No. 138-12 automatic flow control valve with bellows sealed stem, or its equivalent. The valve body and bellows are 316 SS. Manual control of this valve from the indium system panel board is possible.

The automatic control valve admitting steam to the coils of the concentrating still is controlled by signals from the still density instrument. The automatic control valve admitting cooling water to the still condenser is controlled by the temperature of the condensate from the condenser. The automatic control valve admitting steam to the steam jet ejector furnishing the vacuum to the vent header is controlled by signals from the pressure gauge in the vent header.

The pressure relief valves serving each of the four radiator loops will be preset and calibrated to the desired pressure prior to installation.

Analytical control of the indium system solution compositions and concentrations is accomplished by a remote sampling station and analytical facility. Equipment to be provided in this facility has not been determined. Designation of the equipment should await results from the research and development work on indium sulfate solutions.

Radiation cubicle environment control is accomplished from either the indium system control panel or a control panel located in the food processing facility.

4.3 Equipment Layout

4.3.1 Radiator Cubicles

The layout of the radiator cubicles as shown in Figures 9.A, 9.B, and 9.F is based primarily on the fact that the cubicles must be easily accessible from the food processing facility. Continuous conveyor lines from the food processing facilities into the radiator cubicles and back to the food processing facilities must be easy to set up, operate, and service. The comparative sizes of the slab and drum radiator cubicles and the necessity of providing equipment to handle the indium sulfate solution made it convenient to locate the slab radiator cubicles at either side and the drum radiator cubicles together in the center of the indium system facility. This arrangement gives sufficient space between the drum radiator cubicles, the slab radiator cubicles, and

the reactor building to adequately house and shield the indium sulfate solution equipment. The slab and drum radiator cubicles are at the level of the first floor of the Food Processing Building immediately adjacent to the Radiator Building.

The massive concrete structure surrounding the radiator cubicles provides shielding from the high intensity radiations. The wall (entrance wall) between the radiators and the Food Processing Building is of sufficient thickness to reduce radiation levels at its face to 1/10 of tolerance. The concrete above the radiators reduces the radiation in the upper floor level, which is used for normal operations and maintenance, to tolerance (7.5 mr/hr). The wall thicknesses between the radiators are based on reducing the radiation in a cubicle to ten times tolerance when an adjacent radiator is in operation. The shielding of the radiators is discussed more thoroughly in Section 7.5.

4.3.2 Non-Radioactive Equipment

The indium sulfate and decontaminating solution make-up and storage tanks and the remote sampling and analytical facility are located in the pit immediately below the Radiator Building operating floor level since there was a possibility of this equipment becoming radioactively contaminated. The indium system control panel is located at the operating floor level adjacent to the equipment pit. This location is convenient for conducting operations and also minimizes the length of the instrument and service lines between the equipment and the panel board. The air conditioners for radiator cubicle environment control are located at the operating floor level immediately above the radiator cubicles.

4.3.3 Radioactive Equipment

The location of the radioactive equipment in the indium sulfate solution system is based on the locations and relative elevations of the radiators and the reactor blanket. The locations of the equipment are shown by Figures 9.A, 9.B, 9.C, 9.E, and 9.F. The four circulating pumps are located slightly above the top of the reactor blanket so that solution in lines between the pumps and the blanket can drain to the blanket. The layout of the equipment and piping in the rest of the circulating loops is such that the equipment and lines will fill completely (no air traps) and drain completely (no liquid traps). The high point in each circulating system is the pressure

relief valve, which is slightly above the vent header.

The concentrated radioactive indium sulfate solution storage tank, the dilute radioactive indium sulfate solution storage tank, and the dilute radioactive decontaminating solution storage tank are located such that liquid contained in any of the radiator loops and the concentrating still can be drained into them by gravity (See Figures 9.E and 9.F).

The transfer pumps, P-5 and P-6, are located at the low point in the system so they can transfer the entire contents of any portion of the indium system and always operate with a positive suction head.

The vent header is sloped toward the concentrated radioactive indium sulfate solution storage tank. Vapor condensed in the vent header condenser and water produced in the recombiner drains down the vent header into the concentrated radioactive indium solution storage tank.

4.4 Cooling of the Indium System

4.4.1 Blanket Solution Cooling

Considerable heat is generated in the indium sulfate solution in the reactor blanket by core gammas, leakage neutrons, and indium decay within the blanket (see Section 7.2). The following conditions were assumed for considerations of blanket solution cooling:

Reactor Power	20 MW
Blanket Thickness	4 inches
Indium Sulfate Concentration	600 gms/liter

Under these conditions, the heat generated in the blanket is about 680 kw or 2,320,000 Btu/hr.

The following properties were assumed for aqueous indium sulfate solution of 600 gms/liter concentration:

Specific Heat, Cp.	0.70 Btu/lb °F
Thermal Conductivity, k	0.30 Btu.ft/hr-ft ² °F
Coefficient of cubical expansion	0.000215/°F below 200°F 0.000255/°F above 200°F

Since the viscosities of indium sulfate solutions were extremely important in the heat transfer and fluid flow calculations, a sample was sent to the St. Louis Testing Laboratories, 2317 Chouteau Avenue, St. Louis 3, Missouri,

for viscosity determinations. The viscosities of 600 g/liter and 400 g/liter aqueous indium sulfate solutions were determined at 77°F and 190°F. The resultant data is plotted in Figure 4.F, which was used for determining viscosities for heat transfer and fluid flow calculations.

Cooling of the indium sulfate solution is accomplished by an external heat exchanger located in the Reactor Building as indicated in Figures 9.C and 9.D. The heat is removed by the auxiliary cooling system, which utilizes water from the reactor shielding pool (see Section 5.3). This water is used to cool the indium sulfate heat exchanger because it would keep any contamination completely contained within the reactor building if a rupture occurred between the water and indium sulfate sides of the heat exchanger.

The heat exchanger consists of two hundred and ninety 6-foot long, 1/4-inch O.D., 1/8-inch I.D. tubes, inside an 8-inch Schedule 160 pipe, capped at both ends. Indium sulfate solution flows inside the 1/4-inch tubes while pool water flows between the tubes and the 8-inch pipe shell. The heat exchanger is mounted vertically to facilitate filling and draining the indium system. A Model Ch 7-1/2 Chempump, or equivalent, is used to circulate the indium sulfate solution through 1.5-inch Schedule 40 pipes between the blanket and the heat exchanger. Other pertinent data about the blanket heat exchange system is given in Table 4.4.1.

Under the conditions set forth at the beginning of this section, the external heat exchange system limits the maximum temperature attained by the indium sulfate solution in the blanket to about 180°F. At lower reactor powers, blanket thicknesses, or indium sulfate concentrations, lower indium sulfate solution temperatures are achieved. It appears advisable to maintain the maximum indium sulfate solution temperature in the 150°F to 160°F range. Below this temperature, the flow rate in the radiator loops might fall below 15 gpm due to the increased viscosity of the indium sulfate solution.

To avoid additional pressure drop in the blanket solution heat exchange system, no valves are provided in this system. The temperature of the indium sulfate solution in the blanket can be varied by changing the flow rate of pool water through the heat exchanger.

Indium sulfate solution film heat transfer coefficients should be checked experimentally.

Table 4.4.1 Data on Indium Sulfate Solution
Heat Exchanger Loop

Item	Indium Solution Side	Water Side
No. of tubes	290	--
Tube O.D., inches	0.25	8.625
Tube I.D., inches	0.125	6.813
Flow area, ft ²	0.0247	0.1543
Flow rate, gpm	150	430
Liquid velocity, ft/sec	13.5	6.21
Heat transfer area, ft ²	56.94	113.88
Film coefficient, Btu/hr-ft ² -°F	1862	1398
Tube length, feet	6.0	6.042
Pressure drop, psi	40.7	--
Solution inlet temp, °F	180	110
Solution outlet temp, °F	151	120
Heat transferred, Btu/hr	2,191,200	2,191,200

Volume of indium solution in loop - ~0.7 ft³.

4.4.2 Cooling of Solution in Radiator Loops

The heat from beta and gamma energy self-absorbed by the indium sulfate solution in the radiators is estimated from the data on radiator efficiencies presented in Section 8.5. The investigations are based on the drum radiators since self-absorption is higher in these than in the slab radiators. It is assumed that 7 kw of gamma power is absorbed in the food being irradiated. The corresponding heat production and temperature rise rates are about 2000 Btu/min and 3°F/min, respectively. It does not appear that these heat production rates warrant special cooling facilities for the radiators. The only time they present a problem is in the case of circulating pump failure. In this case, the spray showers for external decontamination can be turned on, or the refrigerating unit can be operated at its maximum capacity (it would then remove about 1600 Btu/min of heat). Either of these alternatives would supply sufficient cooling until the radiators can be drained. Therefore, no special provisions are made for cooling the indium sulfate solution in the radiator loops.

4.5 Cubicle Environmental Control

In order to maintain the radiation space at any temperature between -4°F and +167°F with a maximum variation of +4°F, it is necessary to provide environmental control apparatus. One of the most satisfactory methods of accomplishing such a wide range of environmental control is to use air as the heating and cooling medium. In this way, the problems of ice formation while refrigerating the radiation space are greatly minimized, and good humidity control can be accomplished while heating the radiation space. The environmental control equipment should provide independent control of the temperature in each radiator cubicle, and thus the use of individual units for each and every cubicle appears most desirable.

4.5.1 Cooling

The cooling loads on the environmental conditioning units for the radiation space are based on the following general assumptions:

Maximum indium sulfate solution temperature	190°F
Concrete structure temperature	80°F
Air (saturated) leakage into cubicle	80°F
Radiation space maintained at	-4°F ± 4°F
Power absorbed in food	7 kw

Slab Radiator

The temperature rise in the food, due to the absorption of 7 kw of power, is only about 4°F and therefore presents almost no appreciable heat load to the refrigeration unit. However, considerable gamma energy is absorbed and converted into heat in the stationary parts of the food conveying machinery, the metal walls of the slab radiator, the radiator cubicle liners, the insulation between the cubicle liner and the concrete, and in the first two or three inches of concrete nearest the cubicle. Practically all of the heat so produced must be removed by the radiator cubicle refrigeration unit. With a radiator efficiency of 20%, the total gamma power produced in the radiator is 35 kw when 7 kw is absorbed in the food. It is estimated that approximately 14 kw of this is absorbed in material within or close to the radiator cubicle such that it must be removed by the cubicle refrigeration unit. It is assumed that the remaining 21 kw is absorbed in the food and large mass of concrete surrounding the cubicle, and need not be removed by the refrigeration unit.

The estimated heat load to the refrigerating unit to maintain the slab radiator cubicles at -4°F under these conditions is:

Uninsulated slab radiator	13,000 Btu/hr
Insulated cubicle walls	6,000 Btu/hr
Air leakage into cubicle	3,000 Btu/hr
Gamma heating	48,000 Btu/hr
Heat picked up in ducts	<u>10,000 Btu/hr</u>
Total heat to be removed	80,000 Btu/hr

To remove this 80,000 Btu/hr, the refrigeration unit must cool 32,000 lb/hr (6,600 ft³/min) of dry air from 0°F to -10°F. In addition, removal of about 1.0 to 1.5 pounds of condensed water vapor is necessary. The air velocity in the slab radiator cubicle will be about 2.3 ft/sec.

Drum Radiators

In the drum radiator, refrigerating air flows only through the annular space between the can tubes and the cans, and therefore the gamma heat removed by this air is primarily that generated in the can tube itself. (It is again assumed that the cans of food will enter

the drum radiator at -4°F so that the 7 kw of gamma energy absorbed in the food does not present any additional heat load to the refrigeration unit.)

In refrigerating the No. 2 Can Radiator, the estimated heat load is:

Heat transferred from indium sulfate to cooling air	52,000 Btu/hr
Gamma heating	5,000 Btu/hr
Heat picked up in ducts	<u>10,000 Btu/hr</u>
Total heat to be removed	67,000 Btu/hr

To remove this 67,000 Btu/hr, the refrigeration unit must cool 27,000 lb/hr (or 5500 ft³/min) of dry air from 0°F to -10°F . Air leakage into the cooling air stream in this case is much lower than in the slab cubicles so that the ice formation rate on the cooling coils of the refrigeration unit is estimated to be about 0.1 lb/hr or less. The cooling air velocity in the can tubes is about 65 ft/sec.

The estimated heat load in the No. 10 Can Radiator is:

Heat transferred from indium sulfate to cooling air	36,000 Btu/hr
Gamma heating	3,000 Btu/hr
Heat picked up in ducts	<u>10,000 Btu/hr</u>
Total heat to be removed	49,000 Btu/hr

The estimate in this case is on the same basis as for the No. 2 Can Radiator. In both cases it is assumed that the space between the can tubes and the drum tubes is a dead air space.

To remove this 49,000 Btu/hr, the refrigeration unit must be capable of cooling 19,500 lb/hr (4,000 ft³/min) of dry air from 0°F to -10°F . Ice formation problems are comparable to those for the No. 2 Can Radiator. The cooling air velocity in the can tubes is about 70 ft/sec.

4.5.2 Heating

The heating loads on the environmental conditioning units for the radiation space are based on the following assumptions:

Minimum indium sulfate	
solution temperature	150°F
Concrete structure temperature	50°F
Air (saturated) leakage into cubicle at	50°F
There is no heating effect from the gammas	
Radiation space maintained at	167°F ± 4°F

Since the conditions in this case are much less severe than in the case of refrigerating the radiation spaces, consideration is given only to the slab radiator, since it presents the greatest heat load.

To maintain the slab radiator cubicle at 167°F under these conditions the estimated heat required is:

Slab radiator	500 Btu/hr
Cubicle walls	8,000 Btu/hr
Leakage air	2,000 Btu/hr
Air ducts	<u>12,000 Btu/hr</u>
Total	22,500 Btu/hr

To provide sufficient capacity, a heating capacity of 25,000 Btu/hr is assumed for each unit. To supply the 25,000 Btu/hr, the heating unit must heat about 12,800 lb/hr (800 ft³/min) of dry air from 163°F to 171°F. The corresponding air velocity in the slab radiator cubicle is about 0.3 ft/sec. The drum radiators will require considerably less heating, and it is quite possible that they may have to be cooled to maintain the radiation space at 167°F.

5.0 Reactor Cooling Systems

5.1 General Description

The reactor cooling system is designed for a power level of 20 MW. This provides a factor of safety to account for the uncertainties in forecasts of overall efficiency and also permits more operational flexibility. Based on present forecasts, the cooling system is adequate for simultaneous operation of both drum irradiators with each at normal capacity (7 kw absorbed in the food). Another important factor in sizing the cooling system for 20 MW is the feeling that for relatively little extra investment, a limiting factor is removed in case it is desired to raise plant output at some future date. The main difference between the system proposed and one sized for a lower power, is that the smaller system would use smaller pumps and heat exchangers.

The schematic flow diagram of the reactor cooling systems is shown by Figure 5.A. The reactor heat is removed from the core by 2420 gpm of primary cooling water flowing through the core at 7.5 ft/sec. The coolant enters at 133°F and leaves at 190°F. The heat from the primary coolant is transferred to a secondary water stream in a shell-and-tube exchanger. The secondary coolant circulates between the heat exchanger and a cooling tower. Included in the primary cooling loop is an electrically heated pressurizer to maintain the system at 150 psi, a make-up pump, a mixed-bed deionizer that keeps the water at <1 ppm impurity, circulating pumps, and the necessary piping and instrumentation. These are described in Section 5.2.2.

An auxiliary water stream cools the indium sulfate solution, thermal shield and reactor pool. Its flow is 430 gpm. The auxiliary cooling water flows by gravity from the reactor pool, through cooling annuli along the blanket and thermal shield surfaces, through the indium system heat exchanger, and is then pumped through the auxiliary heat exchanger and returned to the pool. Transfer of the heat to the secondary cooling water is accomplished in the auxiliary heat exchanger. Section 5.3 more fully describes the auxiliary cooling system.

5.2 Primary Cooling System

The primary cooling system is designed for operation at 150 psig. The use of this moderate pressure, rather than a pressure slightly above atmospheric, permits higher fuel-plate temperatures. The 150 psi selection is based also on the fact that it is the lowest standard rating of much of the

loop's conventional equipment. Because of the use of aluminum fuel assemblies, use of higher pressures does not appear warranted.

5.2.1 Core Cooling

The cooling of the FIR core is based on keeping the temperature of the hottest fuel-plate surface below 366°F, the saturation temperature corresponding to the system pressure (150 psig). This is certainly not a necessary condition, but it is a reasonable one upon which to design.

In conjunction with the nuclear studies outlined in Chapter 6.0, the heat transfer characteristics of reactor cores of various heights are investigated. These preliminary calculations are based on the 2.5 inch square fuel assemblies (see Figure 3.C) chosen by nuclear considerations (see Chapter 6.0). Studies are made of heat transfer in reactor cores of 2, 2.5, and 3-foot height, and of cross section as shown by Figure 3.A. The coolant velocity needed to adequately cool the core is based on calculations of the maximum fuel-plate surface temperature versus core water velocity. The calculations assume the radial maximum-to-average heat flux is 1.63 (INTERNUC-3, Figure 4.B) and the axial maximum-to-average heat flux is 1.57. It is further assumed that the axial distribution of heat is described by a cosine function. The heat transfer area of the 3 foot high core is 407 ft². The heat transfer areas of the shorter cores are proportional to the height. The average heat flux at 20 MW reactor power in the 3 foot high core is 1.68×10^5 Btu/hr.ft². The results of the calculations are shown by Figure 5.B. (See Appendix 3.0 for method.)

The shorter cores, because of the higher heat fluxes, require greater flow. To maintain a 320°F maximum surface temperature, flows of 7.3, 9.1, and 11.9 ft/sec are required for the 3-, 2.5-, and 2-foot high cores, respectively. The highest flow rate corresponds to 3,830 gpm reactor flow. The increased cost of the larger cooling systems for the shorter cores would not be excessive, and the decision on core height is based primarily on reactor-radiator efficiency. As shown in Chapter 8.0, the shorter cores have no advantage with regard to the reactor-radiator efficiency of the present facility. The FIR is therefore designed on the basis of a 3 foot high core of 2.5 inch square, 16 plate fuel assemblies. (See Figure 3.C).

Based on calculated maximum fuel-plate temperatures for various water velocities (see Figure 5.B), a core coolant velocity of 7.5 ft/sec is selected for the 3 foot core. The

corresponding maximum fuel-plate surface temperature is 318°F, which is 48°F below the saturation temperature. This margin allows for heat flux perturbations in the core. These are discussed in more detail in the section "Hot Channel Factors". The flow areas in the reactor tank at the core level are given in Table 5.2.1 below. At a velocity of 7.5 ft/sec through the core, the total flow is about 2420 gpm.

Table 5.2.1a Reactor Tank Flow Areas at Core

<u>Area</u>	<u>in²</u>
Through core	97.5
Annulus at tank wall	3.6
Flow area in cross	3.9
	<hr/>
	105 in ²
	= 0.73 ft ²

The effect of scale buildup on the surface of the fuel plates is neglected since the temperature of interest is the maximum temperature in contact with the water. While scale raises the temperature of the metal under the scale, this increase is relatively small. Assuming a scale coefficient of 25,000 Btu/hr.ft²°F, the temperature increase due to scale is only 17°F at the maximum heat flux of 4.27 x 10⁵ Btu/hr.ft². The temperature rise through the cladding is 4.5°F and the temperature rise from the cladding to the center of the fuel plate is about 2.2°F.

The calculations of maximum temperature outlined below are based on the following assumptions:

- a. bulk temperature of the reactor outlet water - 190°F. This assumed temperature is consistent with a design philosophy of keeping the primary coolant temperature below 212°F. While higher bulk outlet temperature, such as 200°F, could be used, a higher flow rate would be required.
- b. heat transfer coefficient is given by:

$$h = 150 (1 + 0.011t) \frac{v^{0.8}}{d^{0.2}}, \text{ Btu/hr.ft}^2\text{°F}$$

where t = bulk water temperature, °F
 v = water velocity, ft/sec
 d = equivalent diameter, in.

- c. a radial maximum-to-average heat flux of 1.56 obtained from the nuclear calculations (Section 6.2.2).
- d. an axial maximum-to-average heat flux of 1.5 for a chopped cosine distribution (Case I), and 1.65 for an unsymmetrical distribution (Case II).
- e. appropriate hot-channel factors, explained below.

Case I - Cosine Axial Flux Distribution

The reflector savings for the core in the axial direction is 8 cm. This gives an extrapolated length of 42.3 inches for a cosine flux distribution. Assuming that local heat is given by $\cos \pi x/42.3$, one obtains a 1.376 axial maximum-to-average. However, MTR operating data indicate that axial maximum-to-average values of over 1.5 occur. Since the higher value results in higher local temperatures, a cosine distribution case giving 1.5 as the maximum-to-average heat flux is examined. The corresponding extrapolated length is 37.8 inches. Figure 5.C shows the local bulk water and maximum fuel-plate temperatures calculated from these relations, for the hottest fuel plates. The maximum fuel-plate temperature of 311°F occurs at a point 12.9 inches above the centerline. The local bulk water temperature at this point is 194°F.

Case II - Unsymmetrical Axial Flux Distribution

The temperature at the hottest point depends on the flux pattern and the maximum-to-average heat generation. An unchopped cosine has a maximum-to-average ratio of 1.57. It is desired to obtain temperature distributions under conditions more realistic to actual operating conditions. Temperatures occurring with an unsymmetrical axial flux distribution approximating that existing in a cold clean core (control rods partially in) are calculated. The axial flux distribution is assumed to be that shown by Figure 5.D, which is a facsimile of the distribution in the central fuel assembly of a cold clean MTR core (taken from Figure 68 of IDO-16047*). The maximum-to-average heat flux, determined

*IDO-16047 "Neutron Flux Distributions in the Materials Testing Reactor", February 16, 1953, G. O. Bright and F. Schroeder

by graphical integration, is 1.64. The method of analysis is the same as for the cosine case, except that arithmetically determined terms, rather than analytical expressions, are used. The same (1.56) radial maximum-to-average ratio is used. The results are shown by Figure 5.C. The maximum fuel-plate temperature of 313°F occurs at 8.7 inches below the center line. The local bulk water temperature is 168°F.

Hot-Channel Factors

To account for possible variations in fuel concentration, fuel plate thickness, gap dimensions, flow rate, and the uncertainty in calculated heat-transfer coefficient, hot-channel factors are used. Two such allowances, F_t (for variations in the temperature rise through a channel), and F_f (for variations in the film temperature drop), are used. They are derived by considering the effects of the various tolerances in Table 5.2.1b.

Table 5.2.1b Hot-Channel Factors

<u>Quantity</u>	<u>Normal Figure</u>	<u>Abnor- mality</u>	<u>F_t</u>	<u>F_f</u>
Gap width, in.	0.106	0.091	1.12	1.022
Plate thickness	0.050	+0.001		
Fuel thickness, in.	0.020	+0.001	1.05	1.05
Fuel concentration	90 -95%	+ 1% (av .)	1.01	-
		+ 3% (local)	-	1.03
Flow inequality		+ 2%	1.02	1.016
Uncertainty in heat transfer coefficient		+ 10%	-	1.10
Total factor (product of factors)			1.211	1.236

The gap tolerance calculated is for the worst case, the gap between the adjacent fuel plates of adjacent assemblies. Allowing normal manufacturing tolerances for the fuel assemblies and grids, the smallest flow channel possible is 0.091 inch. The flow through this channel is 6.7 ft/sec compared to the normal 7.5 ft/sec. The temperature rise in this channel is $7.5/6.7 = 1.120$ times normal temperature rise. The heat transfer film coefficient is lowered by $(6.7/7.5)^{0.8}$ and raised by $(0.204/0.1748)^{0.2}$. The net effect is to increase the film temperature drop by 1.022.

Similarly the largest flow channel is 0.121 inch. The velocity is 8.2 ft/sec. The local film coefficient, considering the lower local water temperature, the higher velocity, and the larger equivalent diameter, is 3060 Btu/hr.ft²°F compared to 2950 Btu/hr.ft²°F for the normal case. Thus F_f is less than one and the larger gap clearly is no over-temperature problem.

The effects of variation in fuel plate thickness (± 0.001) on the flow through channels are negligible compared to the above effects and thus are disregarded.

It is assumed that local variations of 3% occur in fuel concentration but that the average deviation for a fuel channel is only 1%. Thus F_f , which is directly proportional to heat generation, carries a 1.03 factor, while F_t carries 1.01.

To account for the uncertainty in the predicted heat transfer coefficient, a 10% factor is assumed.

Applying these corrections to the maximum temperature given by Figure 5.C for Case II, the maximum temperature in contact with the water is 355°F.

$$\begin{aligned} t_m &= 1.211 \Delta t_w + 1.236 \Delta t_f + t_1 \\ &= (1.211 \times 35) + (1.236 \times 145) + 133 \\ &= 355^\circ\text{F} \end{aligned}$$

where

t_m = maximum fuel plate surface temperature, °F

t_1 = bulk water inlet temperature, °F

Δt_w = bulk water temperature rise, °F

Δt_f = film temperature difference, °F

With the pressurizer at 150 psi, the pressure at the outlet end of the fuel assembly is 164.4 psia, corresponding to 365.7°F saturation temperature. Thus there is a safety margin of 9°F. The temperatures calculated are the highest that can reasonably be expected; the margin is therefore adequate. In any case, local boiling would not be serious since the heat transfer coefficient rises significantly and an approximately constant temperature is maintained. The estimated burnout heat flux for the water velocity and degree of sub-cooling in the FIR core is about 2×10^6 Btu/hr.ft²,

or roughly five times that expected at the hottest spot.

Gap Between Fuel Plate and Control Rod Cross

The flow channel between the central control rod cross and an adjacent fuel plate is analyzed separately. As shown on the drawing (Figure 3.A) this gap is 0.096 inch wide, compared to the normal gap width of 0.106 inch. It is assumed that the heat from the fuel plate adjacent to the cross splits equally, half flowing to the 0.096 gap, and half to the 0.106 gap. The flow rate through the smaller gap is 7 ft/sec. The normal maximum fuel-plate surface temperature (neglecting hot channel factors) in the channel is about 313°F, which is equal to that in the normal gap. This is a desirable situation, since it indicates that local boiling would occur in these small gaps only for the same conditions under which it occurs in the normal fuel channel. Table 5.2.1c shows maximum surface temperature versus gap width, which is used in determining the desired spacing.

Table 5.2.1c Maximum Fuel Plate Surface Temperatures
for Gap Adjacent to Control Rod Cross

<u>Gap Width</u>	<u>Flow Rate</u>	<u>Maximum Temperature</u>
0.073	5.8 ft/sec	331
0.083	6.2	325
0.093	6.9	315
0.096	~7	~313

5.2.2 Description of Major Equipment

The flow diagram, Figure 5.A, shows the major portions of the cooling system. It consists of pumps, piping, a heat exchanger, a pressurizer, and a by-pass purification loop. These are described below.

The material of the main loop is Type 304 stainless steel, except for the valve bodies and pump casings which are cast steel. Although carbon steel or aluminum could probably be used, such a choice would need thorough investigation while there is a background of good experience relating to use of stainless steel in pressurized-water reactors.

The system is designed to be reasonably leak-tight, but the use of canned pumps and packless valves does not appear necessary. Pressure in the system is about 150 psi and

temperature is 190°F or less. The system is welded except for pump and valve seals and the flanged connections between the component tanks of the reactor vessel.

In the event of water leakage, the activity decays reasonably quickly. Since N¹⁷ prohibits access to the system when it is operating, ease of maintenance is not much improved by keeping leakage to zero. To restrict production of long-lived isotopes, the system water quality is kept at less than 1 ppm total dissolved solids. High quality water also minimizes corrosion and gas formation by radiolysis. Water resulting from leaks can be routed to the waste disposal facilities of the indium system. If it is desired to drain the system, the retention basin (which holds about six times primary system volume) can be used.

Heat Exchanger

A water-to-water shell-and-tube heat exchanger is used to remove the 20 MW generated in the reactor. The 2420 gpm of primary water is cooled from 190 to 133°F in flowing through the tubes. The 3000 gpm of cooling tower water, which flows through the shell side, enters at 88°F and leaves at 134°F. The design conditions for the exchanger are shown in Table 5.2.2a. In order to fit the exchanger to the building space available, the tubes are 3/8 inch diameter, 8 feet long; and four-pass tube flow is used. On the shell side, a two-pass arrangement is used since this appears more economical.

Table 5.2.2a Main Heat Exchangers - 20 MW Heat

	<u>Tube Side</u>	<u>Shell Side</u>
Flow rate, gpm	2420	3000
Inlet temperature, °F	190	87.9
Outlet temperature, °F	133.1	133.8
Service coefficient, Btu/hr.ft ² °F	319	
Corrected log mean ΔT, °F	48.5	
Area, ft ²	4450	
Material	304 SS	Carbon Steel
Number of passes	4	2
Number of tubes	5660	
Tube length, ft.	8	
Tubes	3/8" BWG 24	
Tube pitch, in.	9/16, triangular	
Shell diameter, in.		45, min.
Velocity, ft/sec	6.5	7, max.
Number of baffles		10
Baffle spacing, in.		9.6
Pressure drop, psi	9.4	90.5

Figure 5.E, which is based on the data given in Table 5.2.2b, gives overall heat transfer coefficient versus tube velocity. This curve is used in arriving at the heat exchanger surface area and related data summarized in Table 5.2.2a.

Table 5.2.2b Heat Transfer Coefficients for Main Heat Exchanger

<u>Item</u>	<u>Heat Transfer Coefficient</u>	<u>Resistance (corrected for geometry)</u>
Outside scale	1,000 Btu/hr.ft ² °F	0.001
Inside scale	2,000	0.000566
Tube wall	5,130	0.000183
Outside film coefficient*	1,910	0.000524
Inside film coefficient**	890 v ^{0.8}	0.001272/v ^{0.8}

*Perry's Handbook

$$h_o = 0.6 \times 370 \left[1 + 0.0067(t) \right] \frac{v^{0.6}}{d^{0.4}}$$

for outside shell coefficients where v = v max

$$**h = 150 (1 + .011 t) v^{0.8}/d^{0.2}$$

The highest single resistance to heat flow is the scale formed by cooling tower water. This is evaluated using the tabulated scale allowances in Perry's Chemical Engineers Handbook and other texts. It should be evaluated thoroughly when water conditions at the FIR site are known.

The question of whether to route primary water through the tubes or through the shell is complex. Other designs have chosen both courses. In the ETR and the Wright Field reactors primary water flows through the tubes, while in the University of Michigan research reactor, it flows through the shell. Favoring use of the shell side for the primary coolant is the fact that more scale tends to form on the cooling tower side of the heat exchanger, and its removal is easier if the secondary water passes through the tubes. Favoring direction of primary coolant through the tube side is the fact that in case of a fuel element break, and contamination of the cooling system, decontamination is easier

if the process water is in the tubes. Also, the shell and baffles of the exchanger need not be of stainless steel if the process water is confined in the tubes. If the cooling tower water is properly treated, excessive scaling does not occur. If a floating head exchanger is used, the tube bundle can be pulled and scale cleaned.

Exchanger design is carried only to a point that assures that a final design can be fitted into the available space.

An economic balance involving cooling-tower outlet temperature, secondary flow rate, and heat exchanger design is not attempted. Since the site of the plant is unknown, it is not possible to determine whether a cooling tower is, in fact, the best way of disposing of the reactor heat.

Pumps

Two vertical bowl-type pumps, each rated at 1210 gpm for a 53 ft. head are used for the system. Vertical pumps seem best suited since the pump internals can be removed vertically without disturbing piping connections, thereby facilitating maintenance of impeller, and impeller shaft. The low floor space requirement of a vertical pump is another advantage. The pumps use mechanical seals to keep leakage low. About 25 HP is required for each pump.

Use of two pumps, each delivering half of the primary coolant flow, provides for operation of the reactor at about 60% (12 MW) of full power (20 MW) in the event one pump fails.

The friction head losses that must be overcome by the primary loop pumps at full flow are given in Table 5.2.2c.

Table 5.2.2c Estimated Friction Head Losses
in Primary Cooling System

<u>Item</u>	<u>Pressure Drop</u>
Piping, from pump to reactor vessel	4.1 psi
Reactor	
From vessel inlet, to the core	2.3
Core	1.7
From core, through vessel exit	1.7
Piping, from reactor vessel to	
heat exchanger	1.2
Heat exchanger	9.4
Heat exchanger, to pump inlet	1.1
Total	21.5 psi

Flow rate - 2420 gpm

Temperatures as shown by Figure 5.A

Piping and Valves

The primary coolant circuit is constructed of 8-inch stainless steel pipe. Because of the low operating pressure, either Schedule 10 or 20 can be used. There is no flow-control valve in the main circuit, and therefore the system flow rate is normally determined by the capacity of the primary pumps. There appears to be no reason to control the flow rate over a wide range. Three quite widely different flow rates are permitted through operation of the two primary pumps, a single primary pump, or the 50 gpm shutdown cooling pump.

The valves in the primary coolant system (See Figure 5.A) consist principally of shutoff and check valves.

Pressurizer

The pressurizer is a 20 ft³ vessel, 2 ft. in diameter, and 5 ft. long. A 50 kw stainless steel immersion type electric heater is installed in the lower portion of the horizontally mounted vessel. In normal operation, the water level is at 14 inches above the bottom.

Starting with room temperature water in the pressurizer, it can bring the primary loop up to pressure in about one hour. The 50 kw heater can generate 7.7 ft³ of steam per minute. The pressurizer will maintain system pressure

with a continuous leak of $1/2$ gpm from the primary system.

The immersion heater is about eight inches overall diameter by 54 inches long. Current to the heater is determined by a pressure controller. A liquid level controller actuates the make-up pump, which feeds up to five gpm to the primary cooling system. An extra-low-level cutoff shuts off the heater in case the liquid level drops low enough to endanger the heater.

A turbine-type pump, rated at 5 gpm and 150 psi head, provides make-up water to the primary loop. It is connected to a 1-inch line that enters the primary circuit between the heat exchanger and the reactor exit.

Water Purification

To reduce water radioactivity, gas formation, and corrosion, continuous purification of the primary loop water is used. A side stream of 5 gpm is taken off downstream of the pump discharge and passed in series through a 10-micron-size sintered-stainless-steel filter, a mixed-bed deionizer, another sintered filter, and then returned to the main stream. The primary water is maintained at 1 ppm or less. Table 5.2.2d presents pertinent data on the purification loop.

The side stream processes the 7,300 gallons in the main circuit about once each day. A repurification rate this high is not needed, but there is little to be saved by using a lower flow rate. It is estimated that a resin volume of 7.5 ft^3 ($2/3$ anion, $1/3$ cation) will last about a year. This indicates that the system should be used until outlet process water contains too many impurities, at which time the resin would be flushed out and fresh resin added. The resins will cost about \$500. The sintered-stainless-steel filters can be used in a similar manner, i.e., the cartridges can be replaced when the pressure drop across them is too high.

To estimate the life of the ion-exchange bed, the surfaces in contact with 304 stainless steel and with aluminum are calculated, and expected corrosion rates used to determine the input of impurities. For 304 stainless steel, the corrosion rate is taken as $0.05 \text{ mg/cm}^2\text{mo}$ (for 400°F water - from ORNL-1613). For aluminum, $0.15 \text{ mg/cm}^2\text{mo}$ is assumed (for 100°C distilled water at 6.5 pH - from Draley, p. 391, Vol. 9, Geneva Conference Proceedings). The input of corrosion products are then 189, and 83.3 gm/mo for the stainless steel and aluminum, respectively. The above

Table 5.2.2d Data on Primary Coolant
Purification System

Deionizer

Maximum flow rate, gpm	18
Size, in.	18 x 72
Capacity (as CaCO_3), grains	54,000
Resin loading, ft^3	7.5
Height of resin bed, in.	54
Pressure drop at 5 gpm operation (max), psi	5

Filters

Type	sintered stainless steel
Pore size, microns	10
Filter area, ft^2	4
Pressure drop, psi	2

Heat Exchanger

Type	U tube
Shell diameter, in.	8
No. of tubes	20
Tube area, ft^2	51
Tube diameter, in.	$3/4$
Tube length (effective), ft	6.5
Tube side flow, gpm	5
Tube side temperatures, °F	133 → 100
Shell side flow, gpm	10
Shell side temperatures, °F	102 ← 85
Pressure drop thru tubes, psi	0.1

value for stainless steel is compared with data (given in ORNL-1613) for a stainless steel in-pile loop in which impurities increased by 1 ppm each 77 hours. The surface-to-volume ratio of the loop would have a large effect on this value, but applying it to the FIR primary loop gives an equivalent input of 258 gms/mo. Although the FIR operating conditions are probably much less corrosive than those of the above test, the latter larger value is used. Using the relation, given by Sivetz (TID-2002, p. 126), that 1 gm equivalent of corrosion product will be removed by 2 liters of anion resin plus 1 liter of cation resin, and an average molecular weight for stainless-steel corrosion products, it is found that 7.33 ft³ of resin would last about one year. Aluminum corrosion products contribute little to the capacity needed, the resin volume required being 0.65 ft³. The total resin volume needed to process the side stream for one year is then 8 ft³.

An alternate method of calculation, assuming that the ion-exchange resins have a capacity equal to that attained in removing CaCO₃, shows that 7.5 ft³ of resin will last about 400 days. Data on the commercial unit used as the basis for design are given in Table 5.2.2d.

The true operating life of the unit will depend on the actual corrosion rates in the primary loop and cannot be accurately predicted. The specified design has a life long enough so that changing of resins is not required at inconveniently frequent intervals. A flush system is used to remove spent radioactive resin.

The anion resin is temperature sensitive, and should not be subjected to over 100°F. To cool the water purification side stream, a U-tube heat exchanger is provided. Table 5.2.2d gives pertinent data on the water purification loop heat exchanger.

To help maintain water quality, make-up water is also treated. A 30 gpm mixed-bed deionizer feeds a 10,000 gal. demineralized water storage tank that provides make-up and fill for the primary system and reactor shielding pool, and flush water for both the reactor and indium systems. The deionizer is regenerated when capacity is exhausted.

When the system is first put into operation, it will undoubtedly contain considerable dirt and debris left from construction. Before reactor operation is started, water purity should be brought up to the highest level

possible, the amount of resin capacity left in the demineralizer should be determined and, if warranted, fresh resin should be exchanged for the partly spent resin. This would also serve as a check on the flush system for removal of radioactive resin.

Instrumentation and Control

Table 5.2.2e lists the instruments in the primary, auxiliary, and secondary water loops. Figure 5.A also shows schematically, their locations in the loops. The only control instrument needed for normal operation controls the make-up water flow so as to maintain a constant water level in the pressurizer. The pressurizer liquid-level controller actuates the make-up pump motor when the liquid level drops below a certain level.

Radiation detectors monitor radioactivity in the secondary streams, to warn of any leakage from the primary and auxiliary loops to the secondary loop. A fuel element failure detector, consisting of a two-bed deionizer and a scintillation counter, is installed on the reactor outlet water.

All major instruments on the primary and auxiliary water streams indicate and/or record in the control room. Various heat exchanger temperatures are printed on a multi-point recorder.

Abnormal operating conditions are determined by the instruments and, when necessary, the appropriate instrument(s) actuates the reactor control system to shut down or scram the reactor. (See Section 3.7)

5.2.3 Shutdown Cooling

A centrifugal pump of 50 gpm capacity provides water flow through the reactor to remove shutdown heat, and eliminates the need for operation of one of the larger primary coolant pumps during shutdown. Also, a high flow rate through the reactor during refueling would undoubtedly be a handicap, and the small shutdown pump is therefore provided.

5.2.4 Emergency Cooling

The shutdown pump is provided with both an electric motor drive and a gasoline engine drive so that shutdown flow can be maintained through the reactor in the event of electric power outage. This circulation distributes the reactor afterheat throughout the 7,300 gallons of reactor coolant and aids in dissipating much of it to water in the reactor.

Table 5.2.2e Instrumentation for Reactor Cooling Systems

Inst.	Variable	Receiver Location	Sensing Element	Transmitter	Receiver	Control*	Range
CICA-1	Primary water from purification	Local	Conductivity cell	--	Indicator	Proportional high alarm	0-10 μ mho
CICA-2	Softened water to storage	Local	Conductivity cell	--	Indicator	Proportional high alarm	1-20 μ mho
CR-1	Primary water to purification	Control room	Conductivity cell	--	2 pen recorder	--	0.1-10 μ mho
CR-2	Primary water from purification	Control room	Conductivity cell	--	2 pen recorder	--	
FR-1	Secondary water to auxiliary exchanger	Control room	Flow tube	Pneumatic	Recorder	Low alarm	0-600 gpm
FR-2	Secondary water to main exchanger	Control room	Flow tube	Pneumatic	Recorder	Low alarm	0-3000 gpm
FR-3	Primary water	Control room	Flow tube	Pneumatic	Recorder	Low alarm	0-2500 gpm
FR-4	Auxiliary water flow	Control room	Orifice	Pneumatic	Recorder	Low alarm	0-500 gpm
FR-5	Primary water to purification	Control room	Orifice	Pneumatic	Recorder	--	0-10 gpm
FI-1	Secondary water to auxiliary exchanger	Local	Flow tube	Pneumatic	Indicator	--	0-600 gpm
FI-2	Secondary water to main exchanger	Local	Flow tube	Pneumatic	Indicator	--	0-3000 gpm
FI-3	Primary water flow	Local	Flow tube	Pneumatic	Indicator	--	0-2500 gpm
LRCA-1	Pressurizer level	Control room	Differential pressure transmitted	Pneumatic	Recorder	On-off low alarm very low-cut off heaters	0-25"
pHR-1	Primary water to purification	Control room	pH cell	--	2 pen recorder	--	5-9
pHR-2	Primary water from purification	Control room	pH cell	--	2 pen recorder	--	5-9
PI-1 thru PI-11	Local pressure gauges	Local	Pressure gauges	--	--	--	1-3 - 0-50 psi 4-7 - 0-200 psi 8-9 - 0-3 psi 10-11 - 0-300 psi

Table 5.2.2e (Cont.)

Inst.	Variable	Receiver Location	Sensing Element	Transmitter	Receiver	Control*	Range
PI-12	Primary water reactor inlet	Control room	Pressure cell	Pneumatic	Indicator	Low alarm	0-200 psi
PI-13	Primary water reactor outlet	Control room	Pressure cell	Pneumatic	Indicator	Low alarm	0-200 psi
PI-14	Primary water make-up	Control room	Pressure cell	Pneumatic	Indicator	--	0-200 psi
PRC-1	Pressurizer control	Control room	Pressure cell	Pneumatic	Indicator	On-off control to electric heaters	0-200 psi
PwrC-1	Power calculator	Control room	Flow recorders; ΔT recorders	Electric	Computing recorder	--	0-25 MW
RRA-1	Primary heat exchanger leak	Control room	Scintillator	Count rate meter	Multipoint recorder	High alarm	--
RRA-2	Auxiliary heat exchanger leak	Control room	Scintillator	Count rate meter	Multipoint recorder	High alarm	--
RRA-3	Indium heat exchanger leak	Control room	Scintillator	Count rate meter	Multipoint recorder	High alarm	--
TR-1	Auxiliary water temp	Control room	Resistance thermometers	--	Multipoint recorder	High alarm	0-200°F
TR-2	Reactor outlet temp.	Control room	Resistance thermometers	--	Recorder	High alarm	0-200°F
TR-3 to TR-7	Misc. temps.	Control room	Resistance thermometers	--	Multipoint recorder	--	0-200°F
Δ TR-1	Auxiliary water ΔT	Control room	Matched resistance thermometers	--	Recorder	High alarm	0-200°F
Δ TR-2	Primary water ΔT	Control room	Matched resistance thermometers	--	Recorder	High alarm	0-200°F
SRA-1	Fuel element failure alarm	Control room	Scintillator	Count rate meter	Recorder	Alarm	--
RRA-4	Reactor room rad. level	Control room	Ion chamber	Microammeter	Recorder	High alarm	--

*See Table 3.7 for signals that will shut down the reactor.

pool and the secondary side of the main heat exchanger. The emergency cooling problem is treated further in Section 12.1.4.

5.3 Auxiliary Cooling System

5.3.1 Heat Removal from Blanket, Thermal Shield, and Pool

As described in Section 4.4.1, the bulk of the heat generated in the indium sulfate solution is removed by an external heat exchanger and forced circulation loop. Heat generated in the reactor vessel, the thermal shield and the reactor shielding pool water must also be removed. This heat is removed by water flowing through 1/8" annuli at the outer surfaces of the reactor vessel, indium blanket tank, and thermal shield (See Figures 3.A and 3.B). The inner annulus is located between the reactor vessel and the indium blanket tank, the middle annulus is located between the indium blanket tank and the thermal shield, and the outer annulus is located between the thermal shield and the outer shell.

Pool water is drawn downward through these three annuli at 430 gpm by the auxiliary cooling system pump. The water in the three streams unites in the header just beneath the indium blanket and flows through the indium heat exchanger before going to the auxiliary cooling system heat exchanger, where the heat is rejected to the secondary cooling water. The cooled water is then returned to the reactor shielding pool. This cooling loop is shown schematically in Figure 5.A.

Data concerning these three annuli are presented in Table 5.3.1a. The data in this table are based on the following conditions:

Reactor vessel O.D.	18.5 in.
Indium blanket thickness	4.0 in.
Thermal shield thickness	3.0 in.
Concentration of indium sulfate solution in blanket	600 gms/liter
Reactor power	20 MW

Under these conditions, the heat removed by the water flowing through the annuli and the indium heat exchanger is given by Table 5.3.1b.

Table 5.3.1a Data on Cooling Annuli Around
Indium Blanket and Thermal Shield
18.5" Reactor Vessel O.D. - 4" Thick
Blanket - 3" Thick Thermal Shield

		Channel		
		Inner Annulus (A ₁)	Middle Annulus (A ₂)	Outer Annulus (A ₃)
Cross-sectional area for water flow (ft ²)		0.051	0.075	0.092
Length of channel (as 1/8" width) (feet)		3.02	2.35	4.00
Heat transfer area at surface of annulus (through above length) (ft ²)		Inner 14.6 Outer 14.8	16.8 16.9	35.1 35.3
	Gallons/min.	101	173	155
Water flow rate through annulus	ft/sec	4.44	5.17	3.78
	$\frac{\text{lbs}}{\text{hr} - \text{ft}^2}$	990,600	1,152,400	842,500
Reynold's number		12,360	14,400	10,500
Water film heat transfer coefficient Btu/hr-ft ² -°F		1,300	1,470	1,140
Entrance temperature of water (°F)		100	100	100
Exit temperature of water (°F)		119	106	106
Heat removed Btu/hr		976,280	556,500	501,848
Friction head developed across channel (feet of H ₂ O)		1.374	1.374	1.374
Total heat removed in annula channels Btu/hr			2,035,000	
Mixed mean exit temperature of pool water °F			109.5	

Table 5.3.1b Total Heat Removed by
Auxiliary Cooling Water

Region	Heat Production Rate This Region Kw	Rate Heat Removed from This Region by Pool Water Kw	Rate Heat Removed from This Region by Pool Water Btu/hr
Reactor vessel wall	~100	150*	512,000
SS inner wall of indium blanket	113	113	386,000
Indium blanket	682	682	2,328,000
Thermal shield	258	258	880,000
Pool	50	50	171,000
Totals	1203	1253	4,277,000

* Includes estimated heat transferred from
primary coolant through reactor vessel wall.

5.3.2 Major Equipment

Heat Exchanger

The auxiliary heat exchanger flow and temperature conditions are shown in Table 5.3.2a. As with the main heat exchanger, 3/8-inch, 8-foot long tubes are used to conserve space. Four-pass tube flow is used. Auxiliary water flows on the tube side of the exchanger, while a side stream from the secondary loop flows through the shell side. The water is returned to the secondary loop before the latter enters the primary loop heat exchanger, thereby raising the secondary cooling water temperature from 85°F to 87.9°F.

The indium heat exchanger, which is cooled by the auxiliary cooling water, is described in Section 4.1.

Table 5.3.2.a Data on Auxiliary Heat Exchanger

	<u>Tube Side</u>	<u>Shell Side</u>
Flow, gpm	430	500
Inlet temperature, °F	120	85
Outlet temperature, °F	100	107
Service coefficient, Btu/hr-ft ² -°F	272	
Corrected log - mean Δt , °F	11.4	
Area - ft ²	1380	
Material	304 ss	Carbon steel
No. of passes	4	1
No. of tubes	1760	
Size	3/8" BWG 24	
Pitch, in.	5/8, triangular	
Tube length, ft	8	
Shell diameter, in.		26
No. of baffles		20
Baffle spacing, in.		5
Pressure drop, psi	4.2	38.7

Circulating Pumps

Two vertical bowl-type pumps are used for circulating the auxiliary coolant. This type pump is chosen for reasons similar to those for selecting this type for the primary coolant pumps. Rated flow through each pump is 215 gpm at a head of 65 ft. The power required for each pump is 5 HP.

Piping

All piping in the auxiliary cooling system is stainless steel with material specifications similar to the primary loop piping.

Water Purification

The auxiliary water is drawn from and returned to the reactor pool. The pool is initially filled with 17,900 gallons of demineralized water. Since the pool is lined with stainless steel, it is expected that the buildup of impurities in the auxiliary circuit will depend much upon the amount of operational work carried on in the pool and also on the cleanliness of the air in the Reactor Building. In any event, acceptable water quality can probably be maintained by continually purging some of the pool water (through the overflow drain) and replacing with make-up water from the demineralized water supply.

Instrumentation and Control

Table 5.2.2e given previously lists the instruments of the auxiliary circuit. Flow and temperatures are monitored. A radiation detector is used to detect leakage that may develop between auxiliary water and indium in the indium heat exchanger. This detector is a scintillation counter, arranged to detect the indium gammas.

Shutdown and Emergency Cooling

Because of the continued heat generation in the indium solution after reactor shutdown, the auxiliary pumps and the indium heat exchanger loop pump will normally be continued in operation after reactor shutdown. After the 54 minute indium¹¹⁰ has decayed sufficiently, the pumps can be shut down, and natural circulation will suffice to cool the solution.

At present there is no emergency cooling provided for the indium solution in the event of a power outage. However, after reactor shutdown over-temperature of the indium solution is not particularly hazardous, and natural convection in the blanket and through the indium heat exchanger loop may provide sufficient cooling. This problem needs thorough analysis in the final indium system design.

5.4 Secondary Cooling System

5.4.1 Heat Load

The secondary cooling system provides for ultimate dissipation of the reactor heat through a cooling tower. It can readily dispose of 20 MW of core heat plus the associated 2 MW generated outside the core. The cooling tower outlet temperature is 85°F, the inlet temperature is 133.8°F, and the flow rate is 3000 gpm. These temperatures and flow rate are selected as a basis for design. Further analysis, especially of the economic factors, may show more desirable figures.

5.4.2 Major Components

Cooling Tower

The secondary cooling system is based on an assumed 78°F wet bulb design temperature, and a 7°F approach in cooling the tower. The latter is largely determined by a desire to keep the water (auxiliary loop water) in the reactor shielding pool at about 100°F.

Scale formation may be considerably more of a problem at temperatures much above the 133.8°F proposed. As pointed out in the discussion of the primary heat exchanger, the scale allowance is the largest single resistance to heat flow.

The cooling tower operates over a range of about 48.8°F. Evaporation loss is then about 4.9%, to which is added 0.2% loss for drift, giving a total of 5.1% or 153 gpm of make-up water needed.

Circulating Pumps

Two pumps are used to circulate the secondary flow. Each operates at 1500 gpm and 330 ft of head with motors rated at 180 HP. The required pump head consists of 90.5 psi through the main exchanger, 36 psi through the assumed 2000 ft equivalent length of piping, and 15 psi to raise the water to the top of the 35 ft cooling tower. The pumps are centrifugal, split-casing units.

The secondary loop is constructed of carbon steel pipe of 10-inch diameter. The valves and instruments on this circuit are shown on the schematic flow diagram (Figure 5.A).

Water Treatment

The cooling tower water is softened in a zeolite unit, to prevent excessive scale formation. Blowdown ($1/4$ to $1/2$ gpm) is also used to prevent the buildup of impurities. Treatment to inhibit algae growth will probably be needed. A conventional chemical treatment, of phosphate and chlorine addition, is a likely choice. Until the site is selected and quality of the new water is known, little can be forecast about scale formation behavior.

5.5 Equipment Layout

The cooling equipment layout is shown by Figures 9.A, 9.C, and 9.D. The restricted amount of space available in the Reactor Building influences strongly the equipment layout. The primary heat exchanger location permits removal of the tube bundle. Also, the relatively high location of the primary heat exchanger promotes natural convection circulation through the primary loop. The pressurizer is the highest point in the primary loop to promote accumulation therein of gases formed during operation, or present when filling the system.

The indium heat exchanger and pump are located in a corner formed by the pool shield and the building wall. This minimizes the amount of additional shielding needed. The primary purification loop deionizer is similarly located.

A removable slab in the first floor of the Reactor Building permits use of the building crane for removal of cooling equipment from the sub-level floors. The flooring between the first and second sub-levels is iron grating. This permits easier washdown of contamination and provides more flexibility in placement of equipment.

6.0 Nuclear Considerations

Most of the reactor theory studies reported in this section fall into two main categories; first, investigations of the effects of design parameters on the number of neutrons absorbed in the blanket indium per uranium-235 absorption and, second, investigations of the factors which have direct bearing on the reactivity and operating characteristics of the reactor. Since the investigations of the first category with respect to design parameters external to the reactor vessel are quite thoroughly analyzed in the nuclear section of INTERNUC-3, the present study concerns itself mainly with the design parameters of the reactor core and the structures internal to the blanket and pressure vessel. On the basis of the results of these reactor core design studies, a theoretically improved model is chosen for a reference reactor design upon which to base the calculations of reactivity coefficients, neutron lifetimes, and the other numbers affecting the reactor operating characteristics. This latter series of studies comprises the second category of investigations.

In addition to the two main categories of investigation, two subsections are included which pertain to control rod worth evaluations and the effects of a beryllium blanket reflector upon blanket efficiency.

Insofar as it is practical the basic theory, assumptions, and constants are provided in Appendix 2, as noted in the text. All results bearing on reactor reactivity or criticality are obtained by means of two neutron group, multiregion diffusion theory as solved numerically on an IBM-650 electronic computer.

6.1 The Effects of Core Parameter Changes on Blanket Efficiency

The measure of blanket efficiency (in terms of nuclear characteristics) may be evaluated by reference to the uranium loading of the reactor, M_0 (kg), and the number of indium absorptions in the blanket per U^{235} absorption in the reactor core, Z . Provided the capacities of the investigated reactors are equally adequate with respect to removal of power and feasibility of both the necessary uranium loading and general mechanical structure, that reactor is best which has the highest Z for a given M_0 - from the standpoint of nuclear characteristics alone. The properties of the reactor core and the other regions up to the reactor pressure vessel determine the availability of leakage neutrons to the blanket and the

associated outer structures. It results that the blanket configurations (mainly blanket thickness) and the blanket composition [in terms of the $\text{In}_2(\text{SO}_4)_3$ concentration] affect Z quite independently of the magnitude of the leakage of neutrons from the core. The effects of these latter variables are reported in INTERNUC-3, so that the study of the effects of reactor design changes internal to the pressure vessel may be analyzed assuming one given blanket configuration and composition and one set of structures external to the blanket.

The reactor model upon which the calculations are based is shown in Figure 6.A as a series of seven concentric regions, the inner one of which is the reactor core.

It is assumed that the flux distribution in the axial direction is a chopped cosine in each region, so that the criticality of the reactor may be computed by two group, multiregion diffusion theory applied to the seven radial regions of the core system. For the core parameter study, region 2 is an aluminum-air zone of about 4.078 cm thickness. The effects of changing this region to solid aluminum and to solid beryllium are computed in Section 6.1.3. Region 3 is a series of aluminum and water layers including the pressure vessel. Region 4 is 1/8-inch of stainless steel. Six inches of blanket composed of an indium sulfate-water solution of density 1.342 gms/cm^3 ($400 \text{ gm In}_2(\text{SO}_4)_3$ per liter) comprises the fifth region. Regions 6 and 7 are 3 inches of iron thermal shield and 40 cm of light water, respectively. The exact dimensions and compositions of regions 2 through 7 are given in Section A2.2.1 of the Appendix.

The effects of changing the average volumetric metal-to-water ratio and the core radius are evaluated assuming a core height of 3 ft (91.44 cm). Following this study the core height is changed successively to 2.5 ft and 2.0 ft for a given reactor with a metal-to-water ratio of 0.7 and a core radius of about 18.92 cm. All of the core parameter study calculations are based on a reactor condition which corresponds to the hot poisoned operating state after 20% burnup of the original U^{235} loading. Xenon concentration and U^{235} burnup are assumed to occur uniformly over the reactor volume. The core is assumed to be at a uniform temperature of 77°C (170.6°F). The initial uranium loading is defined as M_0 . For the purposes of the calculations, the reactor power is taken as 15 MW.

6.1.1 The Effect of Metal-to-Water Ratio and Core Radius

A parameter, γ , is defined as the average volumetric metal-to-water ratio in the core. The reactor core

includes 24 fuel assemblies of center-to-center spacing, d , and a 0.71-inch thick control rod cross extending to the core boundary. A thin (0.020-inch) layer of water which bounds each reactor quarter section is also included as part of the core region. The region is considered to be a homogeneous mixture of all the included materials. Section A2.2.1 of the Appendix presents a resume' of the core compositions for all the parametric conditions. The center-to-center spacing, d , of the fuel assemblies is the parameter by which the core radius is varied. The procedure of the study is to choose one of three given values of γ (either 0.5, 0.6, or 0.7) and one value of d (either 2.5 in. or 3 in.) and vary the uranium loading until the multiregion reactor system is barely critical. This is repeated for each combination of the parameters γ and d . The values and the derivations of the constants used in the core parameter study are also given in Section A2.2.1. In the following presentation of results the uranium loading is, by definition, the amount of uranium enriched to 90-95% U^{235} required at startup in order to operate after 20% U^{235} burnup at the hot uniformly poisoned and burned out condition.

The Neutron Multiplication Constants

From the results of the studies of INTERNUC-3 it is indicated that for a 3-foot high reactor core of $\gamma \approx 0.7$ and $d = 3$ in., an M_0 of about 9.47 kg is required. This M_0 is used as a "first try" at each combination of d and γ in order that a direct correspondence of the neutron multiplication factor (K^*) and γ is obtained at this M_0 for either of the values of d . It must be remembered that K is that for the hot poisoned, burned-up reactor condition at a U^{235} content 80% as great as the initial loading M_0 .

Table 6.1.1a summarizes the direct results of the numerical solutions of the two neutron group diffusion equations for the seven region reactor complex.

- * K is defined as the total number of source neutrons divided by the total number of fissions in the reactor. As such it represents the effective multiplication constant for neutrons in the reactor.

Table 6.1.1a Final Multiplication Constant vs
Initial Uranium Loading (Reactor
Core Height = 3 feet)

d	3 inches		2.5 inches	
γ	M_0 (Kg)	K	M_0 (Kg)	K
.7	9.4700	0.98940	9.4700	0.95261
	9.5268	0.99070	11.3020	0.98202
	10.0440	1.00216	12.4380	0.99676
.6	9.4700	1.01185	9.4700	0.97830
	9.4064	1.01033	10.5250	0.99662
	8.8340	0.99587	--	--
.5	9.4700	1.03466	9.4700	1.00468
	8.0540	0.99547	9.2700	1.00070

The Mass Coefficients of Reactivity and the Masses for
Exact Criticality

The last value of K under each d and γ in Table 6.1.1a is the one nearest the condition of reactor criticality. If one denotes this value of K by K_0 and the corresponding uranium loading as M_{0c} , it is found that for d = 3 in. the uranium mass coefficient of reactivity defined by the

expression $\frac{\Delta K}{K_0} / \frac{\Delta M}{M_{0c}}$, is about 0.22. It is known that

the mass coefficient of reactivity decreases as M_0 increases, but for d = 3 in. M_0 varies only about 20% so that with the limited number of data points the variation in the coefficient is not apparent. However, for d = 2.5 in. and $\gamma = 0.7$ this variation in the mass coefficient of reactivity is readily apparent. By

plotting $\frac{\Delta K}{K_0}$ vs $\frac{\Delta M}{M_{0c}}$ for this case (d = 2.5 in.,

$\gamma = 0.7$) Figure 6.B results, from which is formed the coefficient, $\frac{\Delta K}{K_0} / \frac{\Delta M}{M_{0c}} = 0.158$. For the other two γ 's

(0.6 and 0.5) under d = 2.5 in. of Table 6.1.1a, the mass coefficient of reactivity is about 0.18. Again, the insufficient data for these two cases makes a

significant difference undetectable. From these mass coefficients of reactivity, one may estimate the value of M_0 at exact criticality, since the correct mass is

$$M_0 = M_{0c} \left[1 + \frac{(1 - K_0)/K_0}{\frac{\Delta K}{K_0} / \frac{\Delta M}{M_{0c}}} \right]$$

Table 6.1.1b lists the predicted values of M_0 at criticality for each γ and d .

Table 6.1.1b Initial Uranium Loading
for Final Criticality

$\gamma \backslash d$	3 inches	2.5 inches
.7	9.946 Kg	12.694 Kg
.6	9.001 Kg	10.723 Kg
.5	8.221 Kg	9.234 Kg

The Variation of K with Metal-to-Water Ratio

By plotting K vs γ for each case in which $M_0 = 9.47$ Kg, Figure 6.C is obtained for $d = 3$ in. and $d = 2.5$ in. This figure shows the variation of reactivity with metal-to-water ratio for constant uranium loading. As expected the slope of the curve for $d = 2.5$ in. is more negative than that for the curve for the larger reactor. This follows from the fact that increasing γ increases the effective Fermi Age, τ , in the reactor, and since there is more fast neutron leakage from the smaller reactor, a given increase in τ is more detrimental to the reactivity in the smaller reactor. Together with the appropriate uranium mass coefficient of reactivity, Figure 6.C is useful in estimating the effects of changes in metal-to-water ratio upon the necessary uranium loading.

The Neutron Flux Distribution

From the numerically solved multiregion diffusion theory the radial flux distributions for the two neutron groups are obtained directly (see Section A2.1). The midplane radial neutron flux distributions are shown for each combination of the core parameters, γ and d , by Figures A2.A through A2.L of Appendix 2. The flux distributions shown are those corresponding to the points nearest reactor criticality. For example, the flux distributions shown for $d = 3$ in. and $\gamma = 0.7$ correspond to an M_0 of 10.044 Kg and to a K of 1.00216, as listed in Table 6.1.1a. The departures of the flux distributions shown from the ones which would exist for $K = 1$ are insignificant.

The scale used as the ordinate for the flux distributions is such that there is an average of one neutron produced per cm^3 in a midplane reactor slab one cm in thickness, i.e.,

$$\nu_c \int_{R_f} \Sigma_f \phi_s r dr = \int_{R_f} r dr, \text{ where the integrals}$$

are evaluated over the core or fuel containing regions, R_f . Also,

ν_c = the number of neutrons per fission needed for criticality

Σ_f = the macroscopic fission cross section

ϕ_s = the thermal neutron flux

r = the variable, radius (from the reactor center).

The Neutron Balance

The neutron balances for $d = 3$ in. and $\gamma = 0.7, 0.6$, and 0.5 are presented as Tables A2.1a, A2.1b and A2.1c, respectively, in the Appendix. For $d = 2.5$ in. and $\gamma = 0.7, 0.6$, and 0.5 , the neutron balances are presented in Tables A2.1d, A2.1e, and A2.1f, respectively. These six neutron balances correspond to the cases nearest criticality for which M_0 and K are listed in Table 6.1.1a. A slight difference between the K listed in Table 6.1.1a and one calculated from a neutron balance is due to the fact that the numerical solution for the flux is not fully converged for the given M_0 (see Section A2.1). The K listed in Table 6.1.1a is corrected to correspond to the fully converged solutions.

The Blanket Efficiency, Z, at Exact Criticality

In Section A2.2.1 the derivation of the effective thermal neutron macroscopic absorption cross section for the indium in the blanket (region 5) is given. This cross section is called Σ_{aIn} . It follows that Z_c , the number of neutrons absorbed in indium per U235 absorption, as calculated from the numerical solutions of the fluxes is

$$Z_c = \frac{\Sigma_{aIn} I_{52}}{\Sigma_{a25} I_{12}}, \text{ where } I_{52} = \int_{\text{Region 5}} \phi_s \, r dr \text{ and}$$

$$I_{12} = \int_{R_f} \phi_s \, r dr.$$

$$\text{Now, } \Sigma_{a25} = (1 + \alpha) \Sigma_f, \text{ and } \Sigma_f I_{12} = \frac{1}{v_c} \int_{R_f} r dr = \frac{R_1^2}{2 v_c},$$

so that it follows that

$$Z_c = \frac{v_c \Sigma_{aIn} I_{52}}{(1 + \alpha)(R_1^2 / 2)},$$

where $R_1 \equiv$ the core radius.

It is noted that this manner of calculating Z does not eliminate the dependency of Z upon the reactivity of the reactor, and, as expected, Z_c is larger for subcritical reactors than for supercritical reactors. Since there is little interest here in values of Z for reactors other than critical ones, it is necessary to correct the direct numerical solutions for Z_c in order to find Z for exactly critical reactors. It is found that the flux distributions obtained for reactors with slight amounts of negative or positive reactivity ($\Delta K/K$) do not vary greatly from those of the exactly critical reactors. The correction coefficient for I_{52} is empirically determined to be approximated by

$$\frac{\Delta I_{52}}{I'_{52}} / \frac{\Delta K}{K} = 0.165, \text{ where } \frac{\Delta K}{K} = \frac{K-1}{K}$$

and $\frac{\Delta I_{52}}{I_{52}} = \frac{I_{52} - I'_{52}}{I'_{52}}$. (I'_{52} denotes the correct integral at exact criticality.)

Thus $I'_{52} = I_{52} / (1 - .165 \Delta K/K)$. Since $\nu = 2.46$ in the exactly critical case, the corresponding corrected formula for Z is

$$Z = \frac{2.46 \Sigma_{aIn} I_{52}}{(1+\alpha)(R^2_1/2)} \bigg/ \left[1 - 0.165 \Delta K/K \right].$$

Table 6.1.1c lists I_{52} , $\Delta K/K$, and the corrected Z values for each of the six combinations of d and γ .

Table 6.1.1c Blanket Efficiency, Z , at Exact Criticality

d γ	3.0 inches			2.5 inches		
	I_{52}	$\Delta K/K$	Z	I_{52}	$\Delta K/K$	Z
0.7	276.8960	+0.00216	0.46339	233.9313	-0.00324	0.54986
0.6	265.6773	-0.00413	0.44415	225.5193	-0.00338	0.53007
0.5	253.5223	-0.00453	0.42380	216.3178	+0.00070	0.50879

Curves A and C of Figure 6.D result from plotting the combined data of Tables 6.1.1b and 6.1.1c, thus showing Z versus initial uranium loading, M_0 , for exact criticality. Curve C of Figure 6.D results from interpolation formulae.

Interpolation Formulae for M_0 and Z

Because electronic computer solutions of the multiregion problem are obtained for only two values of the parameter, d , it is necessary to develop a means for interpolating between $d = 3$ in. and $d = 2.5$ in. in order to predict the manner in which Z changes with reactor core diameter. It is expected that the concept of a reflector savings might be employed to allow a fairly accurate prediction of reactivity on an equivalent bare reactor basis. Also, the fast neutron leakage should be quite well approximated on a "reflector savings" basis, and Z should be almost directly proportional to the fast neutron leakage so long as only the core design is changed. By two neutron group diffusion theory, the equivalent bare reactor core criticality is based on the formula,

$$K = \frac{\nu \Sigma_f}{\Sigma_a} \bigg/ \left[1 + \tau_h B_h^2 + \tau_r B_r^2 \right] \left[1 + L_h^2 B_h^2 + L_r^2 B_r^2 \right]$$

In the terminology of Section A2.2.1 of the Appendix,

$$\tau_h = \frac{D_f}{\Sigma_1}, \quad \tau_r = \frac{D_1}{\Sigma_1}, \quad L_h^2 = \frac{D_s}{\Sigma_a}, \quad \text{and} \quad L_r^2 = \frac{D_2}{\Sigma_a}.$$

If the reflector savings concept is used, $B_r^2 = 5.7831862/(R_1 + \delta_r)^2$, where the constant in the numerator is the square of the argument at the first zero of the Bessel Function, $J_0(r)$ and δ_r is the radial reflector savings. For the K and M_0 of the nearly critical cases of Table 6.1.1a, one may compute the value of δ_r which satisfies the equivalent bare reactor formula, above, for each of the six combinations of d and γ . All the constants for use in this formula are given in Section A2.2.1 except for Σ_f and Σ_a which are functions of the appropriate M_0 . Table 6.1.1d gives the values of Σ_f , Σ_a , and the solution of δ_r obtained for each of the six combinations of the parameters d and γ (for the 3 foot high reactor core).

Table 6.1.1d The Radial Reflector Savings for the Near Critical Reactor Cases

d	3.0 inches			2.5 inches		
γ	Σ_f	Σ_a	δ_r	Σ_f	Σ_a	δ_r
0.7	.0604051	.0945519	6.33852	.1051574	.1523439	6.36474
0.6	.0531281	.0854537	6.14358	.0889839	.1318822	6.15642
0.5	.0484372	.0796946	5.95975	.0783735	.1185480	5.95723

For reference, $\delta_r = \left[a + \sqrt{a^2 + b} \right]^{1/2} - R_1$, where

$$a = \frac{K \left[D_1 (\Sigma_a + D_s B_h^2) + D_2 (\Sigma_1 + D_f B_h^2) \right] (5.7831862)}{2 \left[\gamma \Sigma_f \Sigma_1 - K (\Sigma_a + D_s B_h^2) (\Sigma_1 + D_f B_h^2) \right]}, \quad \text{and}$$

$$b = \frac{K D_1 D_2 (5.7831862)^2}{\left[\gamma \Sigma_f \Sigma_1 - K (\Sigma_a + D_s B_h^2) (\Sigma_1 + D_f B_h^2) \right]}$$

It is apparent in these cases that δ_r depends only slightly on d , or reactor radius, and the main variation is with respect to γ . An approximate empirical formula for δ_r may be derived from the data of Table 6.1.1d. The formula is $\delta_r = 5.01 + 1.9 \gamma$ cm, which approximates the calculated δ_r 's of Table 6.1.1d with less than 0.4% deviation.

Now, choosing $d = 2.75$ inches as the intermediate d and using the formulae in the Appendix for Σ_f and Σ_a as a function of M_0 , one may solve the equivalent bare reactor criticality equation for the critical value of M_0 at each value of γ . (K is set equal to unity, $\nu = 2.46$ and δ_r is obtained by the empirical function of γ which is derived from the $d = 3$ inch and $d = 2.5$ inch reactor cases.)

The M_0 's computed in this manner for $d = 2.75$ inch and $\gamma = 0.7$, $\gamma = 0.6$, and $\gamma = 0.5$ are presented in Table 6.1.1e.

Table 6.1.1e Interpolated M_0 Values for $d = 2.75$ inches

γ	M_0 , Kg.
0.7	10.7876
0.6	9.5103
0.5	8.4783

Again using the reflector savings concept and equivalent bare reactor theory, the number of fast neutrons leaking radially from the core per U^{235} absorption is simply

$$\mathcal{L}_R = \frac{\nu}{(1 + \alpha)} \frac{\tau_r B_r^2}{[1 + \tau_h B_h^2 + \tau_r B_r^2]}$$

Because the thermal neutron leakage from the core is such a small fraction of the total, and because the fraction of the leakage neutrons absorbed by the indium in the blanket remains quite constant so long as only the core parameters are changed, Z may be approximated by

$$Z = (\text{constant}) \times \mathcal{L}_R.$$

The validity of such an approximation is demonstrated in Table 6.1.1f where the values of $\mathcal{L}_R$ (as obtained by

using $B_r^2 = \frac{5.7831862}{(R_1 + 5.01 + 1.9\gamma)^2}$ are given with the ratio $Z/\mathcal{L}_R$ (using the Z's of Table 6.1.1c).

Table 6.1.1f $\mathcal{L}_R$ and $Z/\mathcal{L}_R$ for Exact Criticality

d	3.0 inches		2.5 inches	
γ	$\mathcal{L}_R$	$Z/\mathcal{L}_R$	$\mathcal{L}_R$	$Z/\mathcal{L}_R$
0.7	0.647497	0.71566	0.767835	0.71611
0.6	0.620954	0.71527	0.740313	0.71601
0.5	0.592997	0.71467	0.710589	0.71601

The empirical formula, $Z = 0.7156 \mathcal{L}_R$ approximates the data of Table 6.1.1f with maximum deviations of less than 0.15%.

In the following table the values of $\mathcal{L}_R$ are given with the corresponding values of Z as computed by the empirical formulae for $d = 2.75$ inches.

Table 6.1.1g $\mathcal{L}_R$ and Z for Exact Criticality for
 $d = 2.75$ inches

γ	$\mathcal{L}_R$	$Z = 0.7157 \mathcal{L}_R$
0.7	0.704878	0.50441
0.6	0.677334	0.48470
0.5	0.648551	0.46410

The data of Table 6.1.1e and Table 6.1.1g are used to obtain curve B of Figure 6.D.

Discussion of the Results

It is apparent from Figure 6.D that for a given uranium loading, M_0 , one benefits in going to progressively lower reactor diameters (lower d's) and consequently, to lower metal-to-water ratios. As expected, the theory indicates that for a given M_0 one should progressively reduce the reactor diameter until $\gamma = 0$, as a limiting (but rather trivial) case, in order to obtain an optimum blanket efficiency, Z. While this result strongly

favours the theoretical efficiency of a homogeneous reactor core of pure water containing a sufficient amount of contained U^{235} , the present analysis must confine the ranges of the parameters d and γ to something commensurate with a core of MTR (or ETR) type fuel assemblies. There are three factors any one of which will limit Z in a practical case. The first factor is associated with the fact that as γ and d are reduced the heat transfer surface in the core must eventually be reduced beyond a desirable point. Secondly, if heat transfer limitations do not limit the extent to which d and γ may be reduced, a point is reached at which it becomes mechanically unfeasible to contain the necessary uranium in the core. If neither of these two factors limit Z , then finally one may increase Z by adding uranium mass until the increase in basic fuel costs for the reactor fail to be justified by sufficient increases in Z .

6.1.2 The Effect of Reactor Height on Blanket Efficiency, Z

The most efficient operating point in the foregoing parameter study (at a reactor height of three feet) is chosen as the point from which to study the effect of reducing the reactor height first to 2.5 feet and then to 2.0 feet. Thus, throughout the following analysis of the effects of reactor height, H , the parameters d and γ are fixed at $d = 2.5$ inches and $\gamma = 0.7$. The reactor constants used in the evaluation are derived and presented in the Appendix, Section A2.2.2. In these studies each region of the reactor is reduced to the same height while the thicknesses of each region remain constant.

In the following presentation of results it is expedient, at times, to repeat the results of Section 6.1.1 with respect to the case in which $d = 2.5$ inches and $\gamma = 0.7$ in order to provide a more comprehensive picture of the data for the other two reactor heights.

The Initial Uranium Loading, M_0

The neutron multiplication constants, flux distributions, etc. are obtained in the same manner as is employed to obtain the results of Section 6.1.1. For each new reactor height, the "first try" in the numerical calculation uses an $M_0 = 12.438$ Kg. It is found, coincidentally, that this mass approximates, quite well, the value needed for perfect criticality for $H = 2.5$ feet and $H = 2.0$ feet as well as for $H = 3.0$ feet. (Again, the criticality refers to the

reactor condition corresponding to the hot, poisoned operating core at 20% uniform U^{235} burnup). The first column in Table 6.1.2a gives the calculated K for the "first try" $M_0 = 12.438$ Kg. If it is assumed that the uranium mass coefficient of reactivity for $H = 2.5$ feet and $H = 2.0$ feet are the same as that for $H = 3$ feet

(i.e., $\frac{\Delta K}{K_0} / \frac{\Delta M}{M_0} = 0.158$), then at exact criticality the correct $M_0 = 12.434 \left[1 + \frac{(1-K_0)/K_0}{0.158} \right]$.

In the second column of Table 6.1.2a the estimated M_0 's for exact criticality at the final reactor conditions are given.

Table 6.1.2a The Initial Uranium Loading, M_0 , at Exact Criticality ($d = 2.5$ inches, $\gamma = 0.7$)

<u>H</u>	<u>K for $M_0 = 12.438$ Kg</u>	<u>M_0(Kg) for $K = 1.000$</u>
3.0 ft.	0.99676	12.694
2.5	1.00152	12.319
2.0	0.99724	12.656

The Neutron Flux Distributions

The axial neutron flux distributions are assumed to be chopped cosine functions; however, the "degree of chopping" varies with reactor height because of the fact that the axial reflector savings is not proportional to the reactor height. The reflector savings used for the heights 3.0 feet, 2.5 feet, and 2.0 feet are 8.0 cm, 7.5 cm, and 7.0 cm, respectively.

The midplane radial neutron flux distributions are presented for both the fast and thermal flux distributions in Figures A2.M through A2.P. These figures for $H = 2.5$ feet and $H = 2.0$ feet are to be compared to Figures A2.G and A2.H which pertain to the corresponding reactor at a height of 3.0 feet. The major differences are noted in the relative magnitudes of the thermal fluxes of the three cases. Because the uranium loading is nearly the same, there is a decrease in the average thermal neutron flux level as reactor core height is decreased and power level is held constant. The fluxes are normalized such that

$$v_c \int_{R_f} \Sigma_f \phi_s r dr = \int_{R_f} r dr. \quad (\text{See Appendix, Section A2.1})$$

The Neutron Balances

Neutron balances are presented in the Appendix in Table A2.1g and Table A2.1h for $H = 2.5$ feet and $H = 2.0$ feet, respectively. As in Section 6.1.1, the balances are taken from the near critical case results of the numerical calculations; and, again the fluxes are not perfectly converged. Therefore, the multiplication constants, as found by these neutron balances, are only approximately equal to the estimated values for the converged K 's listed in Table 6.1.2a.

The Blanket Efficiency, Z , at Exact Criticality

The formula given in Section 6.1.1 for Z at exact criticality is used to obtain the blanket efficiencies from the appropriate thermal flux integrals in the blanket for $H = 2.5$ feet and $H = 2.0$ feet. The flux integral, I_{52} , the $\Delta K/K$, and the estimated Z is listed in Table 6.1.2b for all three reactor heights. (See section 6.1.1 for the definitions of terms.) The variation of the blanket efficiency with reactor height is illustrated by Figure 6.E where the data of Table 6.1.2b is plotted.

Table 6.1.2b Blanket Efficiency, Z , at Exact Criticality

<u>H(ft.)</u>	<u>I_{52}</u>	<u>$\Delta K/K$</u>	<u>Z</u>
3.0	233.9313	-0.00324	0.54986
2.5	221.9089	+0.00152	0.52201
2.0	203.7998	-0.00276	0.47907

The Reflector Savings of the Equivalent Bare Reactors

As discussed in Section 6.1.1, the concept of a reflector savings is useful in transforming the multiregion problem to one of an equivalent bare reactor, in order to facilitate the calculations of intermediate reactor points. The mechanism of finding the radial reflector savings of the equivalent bare reactor is discussed in Section 6.1.1. Using the method as outlined, the radial reflector savings values calculated for the three reactor heights are presented in Table 6.1.2c. It is noted that the reflector savings decreases with decreasing reactor height.

Table 6.1.2c Reflector Savings versus Reactor Height

<u>H(ft)</u>	<u>Radial Reflector Savings, δ_r(cm)</u>	<u>Equivalent Bare Reactor Radius (cm)</u>
3.0	6.365	25.28549
2.5	6.235	25.15602
2.0	5.980	24.90110

The decrease is explained in part by the fact that the aluminum-air region immediately surrounding the core allows considerable axial leakage of neutrons (as shown in the neutron balances, Section A2.1 of the Appendix). Because the axial leakage of this second reactor region increases more rapidly than the core leakage with decreasing reactor height, the total core leakage increases more rapidly than one would predict from core shortening alone. Thus, the effective radial reflector savings decreases.

Discussions of the Results

The decrease in the blanket efficiency accompanying a decrease in reactor height results because of the increased axial leakage of neutrons in each region of the reactor, the major losses occurring in the core and the region adjacent to the core. It is apparent from the neutron balances (Tables A2.1d, A2.1g and A2.1h) that the loss is partly offset by the decrease in the parasitic thermal absorptions in the core. (Parasitic absorption decreases with increasing uranium concentration.) The loss of neutrons owing to the increase in axial leakage might be further reduced, to improve the blanket efficiency, by effectively end reflecting the second reactor region with beryllium instead of the water which is assumed. And although it may be difficult to reflect the core ends with a good material such as beryllium, it is advisable to make the aluminum-to-water ratio as low as possible at both ends of the fuel assemblies in order to keep fast axial leakage at a practical minimum.

For the smaller reactor heights, one might derive a net benefit from replacing the aluminum-air region by one of different composition which would reduce neutron leakage more than it would increase the thermal absorption of the region. This might be accomplished quite simply by the addition of a small volume percentage of water in small channels more or less uniformly placed throughout the present aluminum-air region. (A water-to-aluminum volumetric ratio of less than 5% should be sufficient for this purpose. However, the magnitude of the effect depends

on reactor height, or, more directly, on the relative importance of the axial neutron leakage losses.) Although Z may be increased by increasing the reactor height, it is noted that the slope of Z decreases with respect to increasing reactor height on Figure 6.E, and the curve should approach some value of Z less than 0.58 asymptotically. Greater reactor heights do not appear to be warranted.

6.1.3 The Effect of Solid Aluminum or Beryllium as a Core Reflector

Because of the simplicity of fabrication which would result if the air holes were removed from the aluminum of the second reactor region, the two group, multiregion numerical computations were performed for the case in which solid aluminum exists as the medium of reactor region 2. Also, as discussed in the previous section (6.1.2), there is a possibility that the better reflector properties of solid aluminum may result in a net improvement in reactor design.

The effect of solid beryllium in region 2 is evaluated to provide an indication of whether or not there may be a net gain in blanket efficiency attained through effective radial reflection of the core and simultaneous moderation of the radial fast neutron leakage. For both of these computations (involving the respective material change in the second reactor region) the active reactor height is 3.0 feet, $d = 2.5$ inches, and $\gamma = 0.7$ for the reactor core. All regions, with the exception of region 2 are identical in properties and dimensions to the reactor corresponding to $d = 2.5$ inches, and $\gamma = 0.7$ in Table 6.1.1a. Only the uranium loading of the core is subject to alteration in order to approach criticality for the hot, poisoned, operating reactor condition at 20% U^{235} burnup. The additional reactor constants used for these calculations are presented in the Appendix, Section A2.2.3.

The Effect of Solid Aluminum

For solid aluminum in the second reactor region, it is found that a uranium loading, M_0 , of 12.438 Kg is sufficiently near to criticality at the final operating reactor core condition to allow accurate estimation of the reactor characteristics at exact criticality. The neutron multiplication constant for an $M_0 = 12.438$ Kg is $K = 1.0137$. For exact criticality the uranium loading is estimated by the formula presented in Section 6.1.1, so that

$$M_0 = 12.438 \left[1 + \frac{1 - 1.0137}{(1.0137)(0.158)} \right] .$$

(A mass coefficient of reactivity of 0.158 is used.) Thus at $K = 1$, $M_0 = 11.374$ Kg. At exact criticality the blanket efficiency is obtained from the formula of Section 6.1.1 using a $\Delta K/K = +0.0137$ to correct the volume integral of the thermal flux in the blanket, I_{52} , obtained for the nearly critical case. Table 6.1.3a summarizes the results at exact criticality and compares the M_0 and Z values to the results obtained for an aluminum-air medium in region 2.

Table 6.1.3a The Results with Solid Aluminum in the Second Reactor Region

($H = 3.0$ feet, $d = 2.5$ inches, $\gamma = 0.7$)

Initial Uranium Loading, M_0 (kg)	I_{52} (Near Criticality)	Blanket Efficiency, Z	% Decrease in M_0 (from 12.694 kg)	% Decrease in Z (from 0.54986)
11.374	231.2781	0.54515	10.4	0.857

It is noted that the change to solid aluminum in the second region causes a slight drop in blanket efficiency. However, the increase in reactivity caused by the change allows a predicted 10% reduction in the initial uranium loading. The effect of the solid aluminum is to increase the reflectivity of the second region with respect to both the fast and thermal neutrons, causing a subsequent increase in the thermal neutron flux at the core boundary. It is noted on Figure 6.D that for $H = 3$ feet and $d = 2.5$ inches the uranium loading, M_0 , increases rapidly if γ is increased to effect an increase in Z . This is because the reactor core neutron leakage is so high that further small increases cause substantial losses in reactivity without actually raising the blanket neutron flux to any great extent. At this point, there is a net benefit in actually reducing the core leakage by using a better neutron reflector in the second region, even at some sacrifice of Z , in order to increase reactivity and save on the uranium loading. If the Z for the solid aluminum core reflector is located on Figure 6.D it is apparent that at $M_0 = 11.374$ Kg, the solid aluminum in region 2 causes an increase in Z of about 0.007. This is about 1.3% higher than the corresponding $d = 2.5$ inch curve point. By increasing γ above 0.7 for $d = 2.5$ inches the solid aluminum would result in a higher Z than is attained for the aluminum-air case at the same M_0 .

These results imply that further gains could be effected by substitution of other materials for the aluminum-air of the second reactor region. At the present stage of the study, however, it appears advisable to fabricate the second reactor region of solid aluminum as shown in the reactor drawings. To take advantage of the reactivity increases which accompany the use of better core reflectors it is necessary to reduce the reactor radius or change the metal-to-water ratio of the core, and there are major difficulties associated with such core changes which might be made to increase Z . (See Section 6.1.4)

There is no need to provide the neutron flux distributions for this case because the flux distributions are only slightly different from those of Figures A2.G and A2.H. The fluxes are slightly higher near the core boundary when solid aluminum is in region 2.

The Effect of Solid Beryllium

If solid beryllium replaces the aluminum-air material of region 2, the value of the hot, poisoned, operating reactor neutron multiplication constant (after 20% uniform U^{235} burnup) is $K = 1.1448$ for an initial uranium loading, $M_0 = 12.438$ Kg - as calculated numerically by two group, multiregion diffusion theory. At an M_0 of 6.00 Kg the neutron multiplication constant, K , is equal to 0.9950. The reactor characteristics at exact criticality are summarized in Table 6.1.3b. The thermal neutron flux integral for the blanket at the near critical point for $M_0 = 6.00$ Kg is $I_{52} = 196.59541$. The M_0 at exact criticality is estimated using a mass coefficient of reactivity of 0.25.

Table 6.1.3b The Results with Solid Beryllium in the
Second Reactor Region

($H = 3.0$ ft., $d = 2.5$ in., $\gamma = 0.7$)

Initial Uranium Loading, M_0 (Kg)	I_{52} Near Criticality	Blanket Efficiency, Z	% Decrease in M_0 (from 12.694)	% Decrease in Z (from 0.54986)
6.1206	196.59541	0.46197	51.8	8.40

The beryllium effects a drastic change in the reactor characteristics. M_0 is lowered beyond the range of the

previous results from the core parameter study. To interpret the results it is necessary to extrapolate the curve for $d = 2.5$ inches on Figure 6.D. Without taking recourse to the approximation formulae of Section 6.1.1, it appears that the $d = 2.5$ inch curve might extrapolate to $Z = 0.46$ at an M_0 of about 7.0 ± 0.5 Kg. Thus, for equivalent reactor diameters, the use of beryllium in region 2 apparently results in a saving of initial uranium mass of from 0.5 to 1.5 Kg at this low value of Z . It is difficult to guess the quantitative effect on uranium mass of increasing γ and/or decreasing the reactor diameter in order to attain higher Z 's with the beryllium core reflector. The considerations of Section 6.1.4, however, lead one to conclude that it becomes unfeasible to consider further reduction in reactor diameter, and in order to increase Z effectively this would be necessary with a beryllium core reflector. Until further study is made, there is no clear indication that the use of a beryllium reflector offers a much more efficient design than might be accomplished through reduction of core diameter and metal-to-water ratio in the present type of design.

The midplane radial neutron flux distributions for the near critical case ($M_0 = 6.00$ Kg) are presented in Figures A2.Q and A2.R. These figures should be compared to Figures A2.G and A2.H which pertain to the same reactor except for the change in the material of the second region and, of course, the uranium loading. It is noted that for the beryllium case, the fast flux is higher near the core boundary, but is lower in regions 3 through 7. The major change is noted in the thermal flux which is much higher in the core and region 2, but not too much higher in the blanket region (region 5).

6.1.4 Consideration of Reactor Uranium Mass Limitations

In the preceding sections there is occasional reference to a lower limit on reactor core diameter. In the core parameter studies, the reactor diameter corresponding to a center-to-center fuel element spacing, d , of 2.5 inches is the smallest considered. It is quite clear from Figure 6.D that there is benefit to be derived from decreasing the reactor diameter below that corresponding to $d = 2.5$ inches while simultaneously lowering the metal to water ratio, γ . The reasons for not doing so are associated with two problems. The first problem is one of heat transfer: even assuming that one uses the thinnest fuel element plates which are practical, reducing the reactor diameter and/or the metal-to-water ratio rapidly decreases the effective heat transfer surface in the

reactor and begins to complicate the whole heat removal problem of the reactor. For example, in going from $\gamma = 0.7$ to $\gamma = 0.5$ one loses about 30% of the initial heat transfer surface, and in going from $d = 3.0$ inches to $d = 2.5$ inches one loses about 35% of the initial heat transfer surface for a given fuel plate thickness. Such reductions require increases in water flow rate and/or higher water temperatures and pressures in the core. The second problem is one of reactor uranium loading capacity. Recent developments in the fabrication of plate-type fuel assemblies such as are employed in the present reactor design make it possible to produce entirely satisfactory plates containing up to 30 and even 35 weight percent uranium in the meat of the plate sandwich. However, there is some difficulty encountered in producing satisfactory fuel plates with meat containing higher weight percentages of uranium.

For the reactors with $d = 3.0$ inches and $d = 2.5$ inches, the weight percentages of uranium in the fuel plate meat are plotted versus uranium loading, M_0 on Figure 6.F. γ is a parameter on this figure and curves are shown for $\gamma = 0.7$, 0.6, and 0.5. The reactor core height, H , is 3 feet. For a given d and γ , one may find from Figure 6.F the weight percentage of uranium necessary in the meat for a given M_0 . Table 6.1.4a lists the mass, M_0 , at each γ for the reactors with $d = 2.5$ inches (obtained in the core parameter study of Section 6.1.1). Corresponding to each mass, a weight percentage of uranium, W , is presented in Table 6.1.4a.

Table 6.1.4a Weight Percentage of Uranium, W , in the
Fuel Plate Meat for Reactors with
 $d = 2.5$ inches and $H = 3.0$ feet

<u>γ</u>	<u>M_0 (Kg)</u>	<u>W (wt. %)</u>
.7	12.694	34.4
.6	10.723	34.1
.5	9.234	35.9

The fuel plate meat weight percentage of uranium needed in the reactor characterized by $H = 3.0$ feet, $d = 2.5$ inches, $\gamma = 0.7$ and solid aluminum in the second reactor region, is about 31.6% - corresponding to the calculated initial uranium loading of 11.37 Kg. Because of the high values of W needed for reactors of diameter corresponding to $d = 2.5$ in., it is advisable to limit present study to the consideration of reactors no smaller in diameter.

These criteria preclude the use of a thickness of beryllium

comparable to the thickness of region 2 to increase reactor blanket efficiency. The reactor diameter would have to be further decreased to attain blanket efficiencies as high as those attained with aluminum in region 2.

The effect of reducing the active reactor height is discussed in Section 6.1.2. Although Z decreases as height, H , is decreased, other considerations relating to the net efficiency of the entire blanket radiator system (see Section 8.3) show there are circumstances depending on the food irradiation facility specifications wherein it would be of benefit to reduce the reactor height to 2.5 feet or less. Again, the heat transfer problems are complicated by such changes, and the problem of attaining the necessary initial uranium loading might require that the weight percentages of uranium in the fuel plate meat be higher than those which can be satisfactorily produced, at present. Table 6.1.4b shows the effect of decreasing the reactor height upon the weight percentage, W , needed for reactors of $d = 2.5$ inches and $\gamma = 0.7$. This table indicates that for reactors shorter than three feet it may be necessary to increase core radius at the expense of blanket efficiency, Z , in order to attain feasible values of W . (The values of the uranium loadings, M_0 , in Table 6.1.4b are taken from Table 6.1.2a.)

Table 6.1.4b Approximate Weight Percentage of Uranium, W , in Fuel Plate Meat for Reactors with $d = 2.5$ inches and $\gamma = 0.7$ as Reactor Height Is Decreased (Al and Air in the Second Reactor Region)

<u>H (ft)</u>	<u>M_0 (Kg)</u>	<u>W (wt. %)</u>
3.0	12.694	34.4
2.5	12.319	38.9
2.0	12.656	47.2

6.2 The Nuclear Characteristics of the Reference Reactor

From the foregoing considerations, a reactor characterized by $H = 3.0$ feet, $d = 2.5$ inches, and $\gamma = 0.7$ is chosen as the reference reactor for the nuclear calculations. Hence, the nuclear calculations performed to determine the operating characteristics of the reference reactor pertain to the reactor obtained in the core parameter studies which corresponds to $H = 3.0$ feet, $d = 2.5$ inches, and $\gamma = 0.7$. The second region is aluminum and air as in the parameter study. The reactor design actually presented in the drawings

of this report differ, mainly, from this "nuclear reference reactor" in having solid aluminum in region 2, in having a 4-inch thick blanket, and in having an initial uranium loading of 11.374 Kg.

In the main, the computations of this section represent perturbations of various conditions in the nuclear reference reactor. The effects of the perturbations are obtained by means of the numerical solution of the two group, multi-region diffusion equations on an IBM-650 electronic computer.

6.2.1 Summary of the Constants for the Nuclear Reference Reactor

The reader is referred to the Appendix, Section A2.2.1, for the derivations of the following reactor constants. (The number of digits given in the constants is carried only to facilitate the estimation of small perturbations.)

The Core Region (Region 1)

The initial uranium loading, M_0	12.438 Kg
The neutron multiplication constant in the final hot, poisoned, operating reactor condition at 20% uniform U^{235} burnup, K	0.99676
Active core and reactor height, H	3.0 ft.= 91.44 cm.
Effective core radius for a cylinder of equal volume, R	18.920750 cm.
Center-to-center fuel assembly spacing, d	2.5 in.
Metal to water volumetric ratio, γ	0.7
Reactor core temperature (uniform), T	77°C=170.6°F
Core water density, ρ_m (by empirical formula)	0.9738259 $\frac{\text{gm}}{\text{cm}^3}$
Core area	174.3241 in ²
Axial reflector savings (assumed), δ_h	8.0 cm
Radial reflector savings (calculated), δ_r	6.36474 cm
Macroscopic core absorption cross section, Σ_a	0.15234386 cm ⁻¹

Macroscopic absorption cross section of water, Σ_{am}	0.0144503 cm ⁻¹
Macroscopic absorption cross section of U ²³⁵ , Σ_{a25}	0.12450637 cm ⁻¹
Macroscopic absorption cross section of aluminum, Σ_{aal}	0.00563244 cm ⁻¹
Equivalent removal cross section of fast neutron axial leakage, $D_f B_h^2$	0.00114392 cm ⁻¹
Equivalent removal cross section of thermal neutron axial leakage, $D_s B_h^2$	0.00023179 cm ⁻¹
Absorption of U ²³⁸ per U ²³⁵ absorption, A_{28}	0.000366155
Absorption of Sm ¹⁴⁹ per U ²³⁵ absorption, S	0.01182
Absorption of uniformly distributed Xe ¹³⁵ per U ²³⁵ absorption, X	0.0486730
Absorption of low cross section fission products including U ²³⁶ per U ²³⁵ absorption, F.P.	0.02552
Average thermal neutron flux at 20% U ²³⁵ burnup and at 15 MW of reactor power, ϕ_f	4.4917x10 ¹³ n's/cm ² sec
Average thermal neutron flux at startup and at 15 MW of reactor power, ϕ_o	3.5934x10 ¹³ n's/cm ² sec
Fast neutron diffusion constant, D_f	1.337918 cm
Thermal neutron diffusion constant, D_s	0.2710958 cm
Fast neutron diffusion area, τ	63.28794 cm ²
Thermal neutron diffusion area, L^2	1.77950 cm ²

The Two Group Constants for the Core Corrected for Non-Cylindrical Geometry

Equivalent fast neutron diffusion constant, D_1	1.444149 cm
Equivalent thermal neutron diffusion constant, D_2	0.2926208 cm

Equivalent fast neutron removal cross section, Σ_{1R}	0.0222841 cm ⁻¹
Equivalent thermal neutron removal cross section, Σ_{2R}	0.15257565 cm ⁻¹
Neutron slowing down constant, Σ_1	0.02114018 cm ⁻¹
Core fission cross section, Σ_f	0.1051574071 cm ⁻¹

The Al and Air Region (Region 2)

Fast neutron diffusion constant, D_1	5.0179 cm
Thermal neutron diffusion constant, D_2	6.1758 cm
Fast neutron removal cross section, Σ_{1R}	0.00451969 cm ⁻¹
Thermal neutron removal cross section, Σ_{2R}	0.0126536 cm ⁻¹
Fast neutron slowing down cross section, Σ_1	0.00022937 cm ⁻¹
Thermal neutron absorption cross section, Σ_a	0.0073733 cm ⁻¹
Region temperature, T	77°C
Region thickness, ΔR_2	4.077676 cm
Outer boundary of region, R_2	22.998426 cm

The Al and H₂O Region (Region 3)

$D_1 = 1.68983$ cm	$\Sigma_{1R} = 0.01068977$ cm ⁻¹
$D_2 = 0.56919$ cm	$\Sigma_{2R} = 0.01339566$ cm ⁻¹
$T = 77^\circ\text{C}$	$\Sigma_1 = 0.00924496$ cm ⁻¹
$\Delta R_3 = 1.74626$ cm	$\Sigma_a = 0.012909$ cm ⁻¹
$R_3 = 24.744686$ cm	

The Stainless Steel Region (Region 4)

$D_1 = 0.9077 \text{ cm}$	$\Sigma_{1R} = 0.00181882 \text{ cm}^{-1}$
$D_2 = 0.3563 \text{ cm}$	$\Sigma_{2R} = 0.1925 \text{ cm}^{-1}$
$T = 77^\circ\text{C}$	$\Sigma_1 = 0.00104273 \text{ cm}^{-1}$
$\Delta R_4 = 0.3175 \text{ cm}$	$\Sigma_a = 0.1922 \text{ cm}^{-1}$
$R_4 = 25.062186 \text{ cm}$	

The Blanket Region (Region 5)

20°C density of blanket solution of
 $\text{In}_2(\text{SO}_4)_3$ (400 gms of indium sulfate per
 liter of solution), ρ_o

1.342 gm/cm³

Region temperature, T

77°C

77°C solution density, ρ

1.3089 gm/cm³

77°C indium density, ρ_{in}

0.39013 gm/cm³

Indium absorptions per blanket
 absorption $\Sigma_{a\text{In}}/\Sigma_a$

0.926623

$D_1 = 1.29316 \text{ cm}$	$\Sigma_{1R} = 0.0359804 \text{ cm}^{-1}$
$D_2 = 0.17387 \text{ cm}$	$\Sigma_{2R} = 0.21880176 \text{ cm}^{-1}$
$\Delta R_5 = 15.24 \text{ cm}$	$\Sigma_1 = 0.0348747 \text{ cm}^{-1}$
$R_5 = 40.302186$	$\Sigma_a = 0.2186531 \text{ cm}^{-1}$

The Thermal Shield (Region 6)

$D_1 = 0.9077 \text{ cm}$	$\Sigma_{1R} = 0.00181882 \text{ cm}^{-1}$
$D_2 = 0.3563 \text{ cm}$	$\Sigma_{2R} = 0.1925 \text{ cm}^{-1}$
$T = 77^\circ\text{C}$	$\Sigma_1 = 0.00104273 \text{ cm}^{-1}$
$\Delta R_6 = 7.62 \text{ cm}$	$\Sigma_a = 0.1922 \text{ cm}^{-1}$
$R_6 = 47.922186 \text{ cm}$	

The External Water Pool (Region 7)

$D_1 = 1.19 \text{ cm}$	$\Sigma_{1R} = 0.0389156 \text{ cm}^{-1}$
$D_2 = 0.16 \text{ cm}$	$\Sigma_{2R} = 0.196635 \text{ cm}^{-1}$
$T = 20^\circ\text{C}$	$\Sigma_1 = 0.0378981 \text{ cm}^{-1}$
$\Delta R_7 = 40 \text{ cm}$	$\Sigma_a = 0.195267 \text{ cm}^{-1}$
$R_7 = 87.922186 \text{ cm}$	

Summary of Reactivity Coefficients and Core Operating Data
For the Hot, Poisoned, Depleted Core Condition

Uranium mass coefficient of reactivity $\frac{\Delta K}{K} / \frac{\Delta M}{M_0}$:

(For $M_0 = 12.438$) +0.158

(For $M_0 = 11.374$) +0.17

Void coefficients for the operating reactor:

Core Void Coefficient, $\frac{\Delta K}{K} / \frac{\Delta v}{v}$ -0.8876

Steam Void Coefficient, $\frac{\Delta K}{K} / \frac{\Delta v}{v_m}$ -0.5221

Reactivity per Unit Void, $\frac{\Delta K}{K} / \Delta v$ $-8.631 \times 10^{-6} \text{ cm}^{-3}$

Core temperature coefficient, $\Delta K/K / ^\circ\text{C}$ $-5.217 \times 10^{-4} / ^\circ\text{C}$

Prompt neutron lifetime, ℓ $-3.77 \times 10^{-5} \text{ sec}$

Ratio of maximum thermal flux to the
volumetric average, $\phi_{\max}/\phi_{av}$ -1.555

(midplane radial flux distributions for the fast
neutron flux and the thermal neutron flux are
shown by Figures A2.G and A2.H, respectively)

Blanket Efficiency, Z , $\frac{\text{Indium Absorptions}}{\text{U235 Absorptions}}$ 0.54986

6.2.2 The Uranium Mass Coefficient of Reactivity

Figure 6.B is derived from the data of Table 6.1.1a for the
reactor parameter set, $H = 3.0$ feet, $d = 2.5$ inches, and

$\gamma = 0.7$. Thus it pertains directly to the nuclear reference
reactor.

Reactivity is defined by $\Delta K/K = (K' - K)/K$, where K is the original value of the neutron multiplication constant and K' is the value being compared to K . In the same sense,

$$\frac{\Delta M}{M_0} = \frac{M_0' - M_0}{M_0}. \text{ The mass coefficient of reactivity is}$$

defined by the expression, $\frac{\Delta K/K}{\Delta M/M_0}$. Since Figure 6.B is

a plot of $\Delta K/K$ versus $\Delta M/M_0$, the mass coefficient of reactivity is found by simply taking the slope of the curve at $\Delta K/K = 0$ and $\Delta M/M_0 = 0$. The coefficient found in this manner is about 0.158.

It is apparent that the mass coefficient changes with a change in mass. At the mass of 11.374 Kg, which is obtained when region 2 is solid aluminum, the mass coefficient is increased to about 0.17. On Figure 6.B, $\Delta M/M_0$ actually is referred to an M_0 of 12.438, and $\Delta K/K = 0$ is actually based on $K = 0.99676$. The M_0 corresponding to a given K is,

$$M_0 = 12.438 \left[1 + \frac{(K - 0.99676/0.99676)}{0.158} \right], \text{ and the } K \text{ for a}$$

given M_0 is

$$K = 0.99676 \left[1 + \frac{0.158 (M_0 - 12.438)}{12.438} \right].$$

At exact criticality M_0 is 12.694 Kg by this formula. The values of M_0 and K for the reference reactor are near enough the values at exact criticality to warrant using the simpler formulae,

$$M_0 = 12.694 \left(1 + \frac{K-1}{0.158} \right) \quad \text{and} \quad K = \left[1 + \frac{0.158(M_0 - 12.694)}{12.694} \right]$$

to approximate the initial uranium loading needed in the reference reactor if the multiplication constant is changed to a given K value. Conversely the formulae may serve to evaluate the reactivity change due to a change in initial uranium loading in the reference reactor.

6.2.3 The Effect of Pure Water in the Blanket

In the event of a rupture of the blanket tank walls, or in other situations wherein water might replace the indium solution in the reactor blanket, it is necessary to control the positive reactivity which is thereby impressed upon the

reactor core. The result of replacing the indium solution in the reference reactor blanket with clean water, as estimated by the multiregion calculations, is that the neutron multiplication constant is increased (for the reference reactor) to $K' = 1.02918$. Hence the increase in reactivity is

$$\Delta K/K = \frac{1.02918 - 0.99676}{0.99676} = +0.03253$$

The constants for the fifth region (the blanket) which are changed for the computation of K' are:

$$\begin{array}{ll} D_1 = 1.22005 \text{ cm} & \Sigma_{1R} = 0.03505877 \text{ cm}^{-1} \\ D_2 = 0.16404 \text{ cm} & \Sigma_{2R} = 0.01756621 \text{ cm}^{-1} \\ T = 77^\circ\text{C} & \Sigma_1 = 0.03401562 \text{ cm}^{-1} \\ \rho_m = 0.973826 \text{ gms/cm}^3 & \Sigma_a = 0.01742595 \end{array}$$

There will be only a slight further increase in reactivity if the blanket were made clean water at 20°C , for the main increase in reactivity when the 77°C water replaces the blanket occurs because of the removal of the thermal neutron absorption represented by the indium.

The effects of a clean water blanket upon the core void coefficients and the neutron lifetime are presented in Sections 6.2.5 and 6.2.6, respectively.

The midplane radial flux distributions for the fast and the thermal neutron fluxes are presented in Figures A2.S and A2.T, respectively. By comparison of these figures with those for the reference reactor (Figures A2.G and A2.H), it is apparent that the removal of the indium sulfate from the blanket affects only the thermal flux in the core to any great extent. (That is, the fast leakage from the core is relatively unaffected by the indium removal.) The increase in reactivity stems mainly from the rise in the thermal flux at the core boundary, judging from the flux distribution comparisons. Because of the small diffusion area for thermal neutrons in the core, the rise in the thermal flux occurs only in the first three or four centimeters inside the core boundary.

6.2.4 The Effect of a Void in the Blanket

It is of interest to know the effect of a void in the blanket upon reactor reactivity, the core void coefficient, and the neutron lifetime. (See Chapter 12.0). The numerical

computation of reactivity by the two neutron group, multi-region diffusion equations as performed by the electronic computer methods restricts the range of the two group constants which may be used in a reactor region. For this reason, the machine calculation to estimate the effect of a void in the blanket is performed for clean water (in the blanket) of density, $\rho_m = 0.118812 \text{ gms/cm}^3$ or a void fraction of about 88.12%. The group diffusion constants for such a blanket are

$$\begin{aligned} D_1 &= 10.0 \text{ cm} & \Sigma_{1R} &= 0.013059906 \text{ cm}^{-1} \\ D_2 &= 1.3445378 \text{ cm} & \Sigma_{2R} &= 0.003473261 \text{ cm}^{-1} \\ \Sigma_1 &= 0.004509874 \text{ cm}^{-1} & \Sigma_a &= 0.002323677 \text{ cm}^{-1} \end{aligned}$$

The resultant K' for such a blanket composition in the reference reactor is $K' = 0.97619$. The reactivity effect of such a blanket change from the reference reactor condition is

$$\frac{\Delta K}{K} = \frac{0.97619 - 0.99676}{0.99676} = -0.020637.$$

The reactivity effect of such a blanket change from the condition of a 77°C clean water blanket is

$$\frac{\Delta K}{K} = \frac{0.97619 - 1.02918}{1.02918} = 0.051488.$$

Estimation of the Effect of a Complete Blanket Void

The effect upon reactivity of an empty blanket is roughly estimated by a simple extrapolation from the two multiplication constants obtained for clean water at densities of

$$\rho_m = 0.973826 \text{ gms/cm}^3 \quad \text{and} \quad \rho_m = 0.118812 \text{ gms/cm}^3.$$

Let

$$K_1 = 1.02918, \text{ and } \rho_1 = 0.973026, \quad K_2 = 0.97619,$$

and $\rho_2 = 0.118812$, with the subscripts 1 and 2 denoting the values for 77°C water and low density water, respectively. By single extrapolation, the multiplication constant for a completely void blanket is

$$K' = K_2 + \frac{(K_2 - K_1)}{(\rho_2 - \rho_1)} (\rho' - \rho_2), \text{ where } \rho' = 0.$$

$$\text{Hence, } K' = 0.97619 - \frac{(0.05299)}{(0.8550139)} = (0.118812)$$

$$= 0.97619 - 0.007363, \text{ or}$$

$$K' = 0.96883$$

The results are summarized in Table 6.2.4.

Table 6.2.4 The Effects of a Change to a Complete Void in the Blanket

<u>The Initial Blanket Condition</u>	<u>Initial Neutron Multiplication Constant</u>	<u>The Induced Change in Reactivity, $\Delta K/K$</u>
Clean H ₂ O at 77°C	1.02918	-0.05864
Reference reactor blanket indium solution at 400 gms/liter	0.99676	-0.02802

The effects of blanket void upon core void coefficients and neutron lifetime are considered in Sections 6.2.5 and 6.2.6, respectively. Figures A2.U and A2.V present the midplane radial flux distributions for the case in which the blanket contains water of density, $\rho_m = 0.118812$ grams per cm³. Upon comparison of these flux curves with those for the reference reactor (Figures A2.G and A2.H) it is noted that with the void in the blanket the fast flux in the core descends more rapidly, signifying greater fast neutron leakage. The thermal flux, however, is slightly flatter with the low density water blanket, as might be expected, because the remaining water still causes a slight peaking of the thermal flux in the blanket. The predominant effect of the void is to increase the fast neutron leakage and thereby reduce the reactivity.

6.2.5 The Void Coefficient of Reactivity

One of the numbers of importance with respect to the operating stability of the reactor is the core void coefficient of reactivity. In the event of a power excursion the water is soon heated, causing a reduction in the water density of the core, or actually causing voids to appear due to steam formation. To find the effect of a uniformly distributed void fraction in the core the two group, multiregion diffusion equations are solved for a case in which the water density in the reference reactor core is 95% of its former value.

Summarized below are the core constants which differ from the reference reactor constants in this case.

$$\Sigma_{am} = 0.010877785 \text{ cm}^{-1} \quad \tau = 63.16244 \text{ cm}^2$$

$$\Sigma_a = 0.151771345 \text{ cm}^{-1} \quad L_2 = 2.027765 \text{ cm}^2$$

Two Group Constants Corrected for Non-Cylindrical Geometry:

$$D_1 = 1.4950345 \text{ cm} \quad \Sigma_{1R} = 0.021190106 \text{ cm}^{-1}$$

$$D_2 = 0.3077566 \text{ cm} \quad \Sigma_{2R} = 0.152015165 \text{ cm}^{-1}$$

$$\Sigma_1 = 0.020005875 \text{ cm}^{-1}$$

The core void coefficient is defined by the expression

$$\frac{\Delta K}{K} \bigg/ \frac{\Delta v}{V}, \text{ where } \frac{\Delta K}{K} = \frac{K' - K}{K} \text{ and } \frac{\Delta v}{V} = \frac{\text{void volume}}{\text{core volume}}.$$

(K is the value without voids and K' is the value with voids.)
A steam void coefficient is also defined as

$$\frac{\Delta K}{K} \bigg/ \frac{\Delta v}{V_m}, \text{ where } V_m \text{ is the initial water volume in the}$$

core. In either of these cases Δv is given by

$$\Delta v = V_m \frac{(\rho_o - \rho_m)}{\rho_o} \text{ where } \rho_o \text{ is the initial water density}$$

and $\rho_m = 0.95 \rho_o$, so that $\Delta v = 0.05 V_m$. Hence

$\Delta v/V_m = 0.05$, and since $\gamma = (V - V_m)/V_m = 0.7$ (so that $V = 1.7 V_m$), it follows that $\Delta v/V = 0.05/1.7 = 0.0294118$.

In order to evaluate the effect of the blanket composition on the void coefficient, the core void computations were performed for the case with pure water in the blanket and for the case with a void in the blanket. Table 6.2.5 presents the multiplication constants for all the cases needed to compute the void coefficients for the various blanket conditions. All cases are calculated at $M_o = 12.438 \text{ Kg}$ for final core reactivity conditions. The core volume is $V = \pi R_1^2 H = 1.02840 \times 10^5 \text{ cm}^3$, so that the water volume is $V_m = 6.04942 \times 10^4 \text{ cm}^3$. Hence the void volume in the core is $3.02471 \times 10^3 \text{ cm}^3$.

Table 6.2.5 Void Coefficients for Various Blanket Conditions

Definition of Terms	Blanket Condition			
	Normal Reference Reactor Blanket	Clean Water at 77°C	Water and a 0.8812 Void Fraction	Complete Blanket Void Ex- trapolation
Neutron multi- plication constant without void in core, K	0.99676	1.02918	0.97619	0.96883
Neutron multi- plication con- stant with void in core, K'	0.97074	1.00866	0.94940	0.94117
$\frac{\Delta K}{K} = \frac{K' - K}{K}$	-0.026105	-0.019932	-0.027443	-0.028550
Steam void coefficient, $\frac{\Delta K}{K} / \frac{\Delta v}{V_m}$	-0.5221	-0.3986	-0.5489	-0.5710
Core void coefficient, $\frac{\Delta K}{K} / \frac{\Delta v}{V}$	-0.8876	-0.6777	-0.9331	-0.9707
Reactivity per Unit void, $\frac{\Delta K/K}{\Delta v}$	-8.631x10 ⁶ per cm ³	-6.590 x 10 ⁶ per cm ³	-9.073 x 10 ⁶ per cm ³	-9.439 x 10 ⁶ per cm ³

As expected, having pure water in the blanket increases the void coefficient because the neutron leakage from the core is least in this case; and since the predominant effect of voids in the core is an increase in leakage, there is smaller effect of core voids in cases wherein leakage is least.

6.2.6 The Prompt Neutron Lifetime

The techniques employed in solving the two neutron group multiregion diffusion equations are adapted to simple estimations of the prompt neutron lifetimes according to the methods of Goertzel as reported in the Reactor Handbook, Volume I, p. 531, et. seq. In brief, the method is based on the amount of reactivity change impressed upon the reactor by the introduction throughout all reactor space of a small amount of "1/v" absorber. Let

δ_s = the increase in the macroscopic thermal neutron removal cross section

δ_f = the increase in the macroscopic fast neutron removal cross section

v_s = the average thermal neutron velocity

v_f = the average fast neutron velocity

E_0 = the upper limit on fast neutron energy

E_s = the thermal neutron energy (corresponding to $T = 77^\circ\text{C}$)

Now, if the poison added is "1/v" in nature, $\delta_s = \alpha/v_s$ and $\delta_f = \alpha/v_f$, where α is some constant. v_s is found easily as the average thermal neutron velocity, assuming a Maxwell-Boltzmann thermal neutron distribution. The neutron energy at which the most probable neutron velocity occurs is $77^\circ\text{C} = 0.0301875 \text{ ev}$. The most probable neutron velocity is $2.407 \times 10^5 \text{ cm/sec}$, and

$$v_s = \frac{2}{\sqrt{\pi}} (2.407)10^5 = 2.716 \times 10^5 \text{ cm/sec, so that}$$

$$\alpha = 2.716 \times 10^5 \delta_s.$$

The flux weighted average of δ_f is given by

$$\delta_f = \alpha \frac{\int_{E_s}^{E_0} \frac{\phi(E) dE}{v}}{\int_{E_s}^{E_0} \phi(E) dE},$$

where $\phi(E)$ is the fast neutron flux as a function of energy. Hence the proper value of v_f is given by

$$v_f = \frac{\int_{E_s}^{E_0} \phi(E) dE}{\int_{E_s}^{E_0} \frac{\phi(E)}{v} dE}.$$

Since $\phi(E)$ varies approximately as $1/E$ at energies below about 0.1 mev, and the $1/v$ nature of the cross section preferentially weights the lower energy flux distribution, it is a fair approximation to assume that the entire flux distribution in energy is " $1/E$ " in nature.

Thus,

$$v_f = \frac{v_s}{\sqrt{E_s}} \int_{E_s}^{E_o} \frac{dE}{E} \bigg/ \int_{E_s}^{E_o} \frac{dE}{(E)^{3/2}} = \frac{v_s}{\sqrt{E_s}} \ln \left(\frac{E_o}{E_s} \right) \bigg/ 2 \left[\frac{1}{\sqrt{E_s}} - \frac{1}{\sqrt{E_o}} \right], \text{ or}$$

$$v_s = \frac{v_s}{2} \ln \left(\frac{E_o}{E_s} \right). \text{ Hence } \delta_f = 2 \delta_s / \ln \left(\frac{E_o}{E_s} \right). \text{ If } E_o$$

is about 2×10^6 ev, $\ln \left(\frac{E_o}{E_s} \right) = 18.01$ and $\delta_f = 0.111 \delta_s$.

By the method of Goertzel, the lifetime, ℓ , of the prompt neutrons is

$$\ell = - \frac{1}{\alpha} \frac{(\Delta K/K)}{(1 + \Delta K/K)}, \text{ where } \frac{\Delta K}{K} = \frac{K' - K}{K}$$

and K' is the neutron multiplication constant obtained as a result of adding the small cross section, δ_s , to the thermal neutron removal cross section and the cross section, δ_f , to the fast neutron removal cross section.

Since the accuracy of the method depends on there being a minimum disturbance of the neutron densities and fission source (i.e., a minimum of flux perturbation), the value of δ_s should be small with respect to the removal cross sections.

There is some question regarding the validity of the results in the blanket void cases, for even small changes in removal cross section produce significant flux variations in the blanket. There is subsequent perturbation of the fluxes at the core border. Also, the method used here to obtain δ_f overestimates δ_f if there is much of a peak in the higher energy portion of the fast flux spectrum instead of the assumed $1/E$ structure. (The error is in the magnitude of

$$\int_{E_s}^{E_o} \phi(E) dE.) \text{ Overestimation of } \delta_f \text{ causes a commensurate}$$

overestimate in the fast neutron lifetime. In most instances the fast neutron lifetime is only a small fraction of the total lifetime (fast plus thermal); however, it is noted that the calculated lifetimes for the reference reactor are quite small. In the cases in which there is void in the blanket, the estimated fast neutron lifetimes are almost half of the total. One must conclude that in all these cases the estimated neutron lifetimes are liable to depend quite strongly on the true fast neutron slowing down picture, so that the present estimates serve as only rough estimates.

Table 6.2.6 Prompt Neutron Lifetimes for Various Blanket Conditions

<u>Definition of Terms</u>	<u>Normal Reference Reactor Blanket</u>	<u>Clean Water at 77°C</u>	<u>Water and a Void Fraction of 0.8812</u>	<u>Complete Blanket Void (Extrapolation)</u>
K, before addition of $1/v$ absorber	0.99676	1.02918	0.97619	0.96883
K', after addition of $1/v$ absorber	0.99574	1.02796	0.97547	0.96817
$\frac{\Delta K}{K} = \frac{K' - K}{K}$	-0.001023	-0.001185	-0.0007376	-0.0006812
Prompt neutron lifetime	37.70 $\times 10^{-6}$ sec	43.68 $\times 10^{-6}$ sec	27.18 $\times 10^{-6}$ sec	25.10 $\times 10^{-6}$ sec

6.2.7 The Cold, Clean, Startup Reactivity

The maximum amount of excess reactivity the control system must counteract occurs at startup with a full loading of fresh fuel assemblies while the reactor is at room temperature, 20°C. In order to estimate the amount of excess reactivity available in this initial, non-operating core condition, the entire set of two neutron group constants for the diffusion theory computation is re-evaluated to correspond to this condition. The more important constants for this computation are listed below by regions. (See Section 6.2.1 for the definitions and values of the comparable terms for the nuclear reference reactor.)

Constants for Cold, Clean, Startup Reactivity Computations

(H = 3 feet, d = 2.5 inches, $\gamma = 0.7$, $M_0 = 12.438$ Kg.,
clean core, no U^{235} burnup)

The Core Region (Region 1)

$$\begin{aligned}\Sigma_{aa1} &= 0.006181168 \text{ cm}^{-1} & D_s &= 0.26451 \text{ cm} \\ \Sigma_{am} &= 0.01283066 \text{ cm}^{-1} & D_f &= 1.31369 \text{ cm} \\ \Sigma_{a25} &= 0.17280367 \text{ cm}^{-1} & \tau &= 60.576 \text{ cm}^2 \\ D_s B_h^2 &= 0.0002261569 \text{ cm}^{-1} & L^2 &= 1.37862 \text{ cm}^2 \\ D_f B_h^2 &= 0.001123209 \text{ cm}^{-1}\end{aligned}$$

The Two Group Constants Corrected for Non-Cylindrical Geometry:

$$\begin{aligned}D_1 &= 1.417997 \text{ cm} & \Sigma_{1R} &= 0.022809852 \text{ cm}^{-1} \\ D_2 &= 0.2855121 \text{ cm} & \Sigma_{2R} &= 0.192091482 \text{ cm}^{-1} \\ \Sigma_f &= 0.145949046 \text{ cm}^{-1} & \Sigma_1 &= 0.021686643 \text{ cm}^{-1} \\ T &= 20^\circ\text{C} & \Sigma_a &= 0.191865325 \text{ cm}^{-1}\end{aligned}$$

The Al and Air Region (Region 2)

$$\begin{aligned}D_1 &= 4.99724 \text{ cm} & \Sigma_{1R} &= 0.004502946 \text{ cm}^{-1} \\ D_2 &= 6.15077 \text{ cm} & \Sigma_{2R} &= 0.013350583 \text{ cm}^{-1} \\ \Sigma_1 &= 0.00023029 \text{ cm}^{-1} & \Sigma_a &= 0.008091655 \text{ cm}^{-1}\end{aligned}$$

The Al and H₂O Region (Region 3)

$$\begin{aligned}D_1 &= 1.65623 \text{ cm} & \Sigma_{1R} &= 0.01092864 \text{ cm}^{-1} \\ D_2 &= 0.553934 \text{ cm} & \Sigma_{2R} &= 0.01475104 \text{ cm}^{-1} \\ D_1 B_h^2 &= 0.001416082 \text{ cm}^{-1} & \Sigma_1 &= 0.00951256 \text{ cm}^{-1} \\ D_2 B_h^2 &= 0.00047362 \text{ cm}^{-1} & \Sigma_a &= 0.01427742 \text{ cm}^{-1} \\ T &= 20^\circ\text{C}\end{aligned}$$

The Stainless Steel Region (Region 4)

$$\begin{aligned}
 D_1 &= 0.9077 \text{ cm} & \Sigma_{1R} &= 0.00181882 \text{ cm}^{-1} \\
 D_2 &= 0.3563 \text{ cm} & \Sigma_{2R} &= 0.21040 \text{ cm}^{-1} \\
 T &= 20^\circ\text{C} & \Sigma_1 &= 0.00104273 \text{ cm}^{-1} \\
 & & \Sigma_a &= 0.210103 \text{ cm}^{-1}
 \end{aligned}$$

The Blanket Region (Region 5)

Solution Density at 20°C , $\rho_o = 1.342$ grams of indium sulfate per cm^3 of solution, $\rho_{in} = 0.400$ gms/cc

$$\begin{aligned}
 D_1 &= 1.26127 \text{ cm} & \Sigma_{1R} &= 0.0368349 \text{ cm}^{-1} \\
 D_2 &= 0.169583 \text{ cm} & \Sigma_{2R} &= 0.2456108 \text{ cm}^{-1} \\
 \frac{\Sigma_{ain}}{\Sigma_a} &= 0.9249457 & \Sigma_1 &= 0.0357565 \text{ cm}^{-1} \\
 T &= 20^\circ\text{C} & \Sigma_a &= 0.24546581 \text{ cm}^{-1}
 \end{aligned}$$

The Thermal Shield (Region 6)

$$\begin{aligned}
 D_1 &= 0.9077 \text{ cm} & \Sigma_{1R} &= 0.00181882 \text{ cm}^{-1} \\
 D_2 &= 0.3563 \text{ cm} & \Sigma_{2R} &= 0.21040 \text{ cm}^{-1} \\
 T &= 20^\circ\text{C} & \Sigma_1 &= 0.00104273 \text{ cm}^{-1} \\
 & & \Sigma_a &= 0.210103 \text{ cm}^{-1}
 \end{aligned}$$

The External Water Pool (Region 7)

$$\begin{aligned}
 D_1 &= 1.19 \text{ cm} & \Sigma_{1R} &= 0.0389156 \text{ cm}^{-1} \\
 D_2 &= 0.16 \text{ cm} & \Sigma_{2R} &= 0.0196635 \text{ cm}^{-1} \\
 T &= 20^\circ\text{C} & \Sigma_1 &= 0.0378981 \text{ cm}^{-1} \\
 & & \Sigma_a &= 0.0195267 \text{ cm}^{-1}
 \end{aligned}$$

The numerical computation of reactivity obtains for this case the value $K' = 1.12192$. This represents a net excess of reactivity over the final operating core reactivity of

$$\Delta K/K = \frac{1.12192 - 0.99676}{0.99676} = 0.12557$$

For the purposes of approximating the effects of small perturbations of core conditions, the equivalent bare reactor radius for the cold, clean reference reactor at startup is $R_{eq} = 25.27239$ cm, or, the radial reflector savings is

$\delta_r = 6.35164$ cm. The radial reflector savings for this case is only 0.0131 cm less than that of the reference reactor at the final operating condition.

6.2.8 The Temperature Coefficient of Reactivity

During normal operation of the reactor, normal power fluctuations cause temperature variations in the reactor. As a measure of the inherent stability of the reactor, the reactor core temperature coefficient of reactivity is the value of the change in reactivity per unit temperature change in the operating reactor core. The temperature coefficient is found by direct computation of the reactivity change obtained as a result of a small uniform increase in the core temperature. In the reference reactor core the temperature is uniformly 77°C. New core constants are obtained for a uniform core temperature of 87°C, a 10°C increase. The other reactor regions remain at their previous respective temperatures (77°C for regions 2 through 6 and 20°C for region 7).

It is assumed that the core boundaries are constant with respect to temperature, and, because the axial neutron leakage is small, it is also assumed that the axial buckling, B_n^2 , is constant. The factors which vary with temperature are as follows:

The Diffusion Constants of the Core

The fast and thermal neutron group diffusion constants vary with temperature according to the formulae given in Section A2.2.1 of the Appendix. The change in the water density and the small change in the aluminum density combine to effect a change in the effective metal-to-water ratio, γ' , and in the effective void fraction, f . The buckling factor, B_F , used to correct the diffusion constants for non-cylindrical geometry remains constant. The diffusion constants calculated for an 87°C core temperature are:

$$D_f = 1.34421103 \text{ cm}$$

$$D_1 = 1.4509414 \text{ cm}$$

$$D_s = 0.27285770 \text{ cm}$$

$$D_2 = 0.29452260 \text{ cm}$$

The change in the diffusion constants per unit temperature change are approximated by:

$$\frac{\Delta D}{\Delta T} = \frac{D_{77^\circ\text{C}} - D_{87^\circ\text{C}}}{77^\circ\text{C} - 87^\circ\text{C}}$$

Thus, $\frac{\Delta D_f}{\Delta T} = +0.0006293 \text{ cm}/^\circ\text{C}$, $\frac{D_1}{T} = +0.0006792 \text{ cm}/^\circ\text{C}$,

$\frac{D_s}{T} = +0.0001762 \text{ cm}/^\circ\text{C}$, and $\frac{D_2}{T} = +0.0001902 \text{ cm}/^\circ\text{C}$.

The Fission Cross Section

It is assumed that Σ_f varies with temperature as Σ_{a25} .

Thus $\Sigma_f = \Sigma_{a25} / (1 + \alpha)$ and

$$\frac{\Delta \Sigma_f}{\Delta T} = \frac{1}{1 + \alpha} \frac{\Delta \Sigma_{a25}}{\Delta T} \quad (\text{See } \Sigma_{a25} \text{ below.})$$

The Fast Neutron Slowing Down Cross Section

$\Sigma_1 = D_f / \tau$, and τ , as computed at the 87°C core temperature using the appropriately corrected values of the metal-to-water ratio and void fraction, is $\tau = 64.015697 \text{ cm}^2$.

Thus $\Sigma_1 = 0.020998147$ and $\Delta \Sigma_1 / \Delta T = -0.00001420 \text{ cm}^{-1}/^\circ\text{C}$.

The Thermal Neutron Absorption Cross Section

The thermal neutron absorption cross section (see Appendix, Section A2.2.1) is

$$\Sigma_a = \Sigma_{a25} (1 + X + S + \text{F.P.} + A_{28}) + \Sigma_{am} + \Sigma_{aal}.$$

The change in Σ_a is found by finding the change in each of its components.

The 87°C U^{235} cross section is

$$\Sigma_{a25} = \frac{(\Sigma_{a25})_{77^\circ\text{C}}}{(\sigma_{a25})_{77^\circ\text{C}}} (\sigma_{a25})_{87^\circ\text{C}} \quad \text{An empirical}$$

approximation to the actual value of σ_{a25} between 0.025 and 0.035 ev. is given by the formula,

$$\sigma_{a25}(\text{actual}) = 320.073/E^{0.551}, \text{ where } E \text{ is the}$$

energy (ev.) at which the most probable neutron velocity occurs. This formula is used (with $E = 0.03105 \text{ ev.}$) to obtain $\sigma_{a25}(\text{actual}) = 609.66 \text{ barns}$. The $1/v$ correction factor at 87°C is 0.9788, so that after correction of $\sigma_{a25}(\text{actual})$ for the non- $1/v$ behavior and for the effect of

the Maxwellian distribution of thermal neutrons, the effective cross section is $(\sigma_{a25})_{87^\circ\text{C}} = 528.8428$ barns.

The ratio of $(\sigma_{a25})_{77^\circ\text{C}}$ to $(\sigma_{a25})_{87^\circ\text{C}}$ is 1.017168.

Hence, at 87°C the effective U^{235} absorption cross section is $\Sigma_{a25} = 0.12240492 \text{ cm}^{-1}$ and

$$\frac{\Delta \Sigma_{a25}}{\Delta T} = -0.00210145 \text{ cm}^{-1}/^\circ\text{C}.$$

The number of Xe^{135} absorptions per U^{235} absorption, X , is corrected to 87°C by the formula,

$$X = (X)_{77^\circ\text{C}} \frac{(\sigma_{a25})_{77^\circ\text{C}}}{(\sigma_{a25})_{87^\circ\text{C}}} \frac{(\sigma_x)_{87^\circ\text{C}}}{(\sigma_x)_{77^\circ\text{C}}}. \quad \text{The ratio of the}$$

Maxwellian averaged Xe^{135} cross sections is

$$(\sigma_x)_{77^\circ\text{C}} / (\sigma_x)_{87^\circ\text{C}} = 1.004 \text{ so that } X = 0.049311 \text{ and}$$

$$\Delta X / \Delta T = +0.0000638 \text{ per } ^\circ\text{C}.$$

In a similar manner, for Sm^{149} ,

$$S = (S)_{77^\circ\text{C}} \frac{(\sigma_{a25})_{77^\circ\text{C}}}{(\sigma_{a25})_{87^\circ\text{C}}} \frac{(\sigma_s)_{87^\circ\text{C}}}{(\sigma_s)_{77^\circ\text{C}}}, \text{ and since the ratio}$$

of the Maxwellian averaged Sm^{149} cross section is

$$(\sigma_s)_{77^\circ\text{C}} / (\sigma_s)_{87^\circ\text{C}} = 0.996686,$$

$$S = 0.0120629 \text{ and } \Delta S / \Delta T = +0.00002429 \text{ per } ^\circ\text{C}.$$

Since the sum of the low cross section fission product cross sections may be assumed to behave as $1/v$ cross sections and

$$\sqrt{\frac{273 + 87}{273 + 77}} = 1.0141851,$$

$$\text{F.P.} = (\text{F.P.})_{77^\circ\text{C}} \frac{(\sigma_{a25})_{77^\circ\text{C}}}{(\sigma_{a25})_{87^\circ\text{C}}} \sqrt{\frac{350^\circ\text{K}}{360^\circ\text{K}}}. \quad \text{Hence,}$$

$$\text{F.P.} = 0.025595 \text{ and } \Delta \text{F.P.} / \Delta T = +0.0000075 \text{ per } ^\circ\text{C}.$$

By the same correction factor used for F.P., at 87°C

$A_{28} = 0.0003672$ and $\Delta A_{28}/\Delta T = +0.000000108$ per °C. The total fuel cross section is $\Sigma_{a25} (1+X+S+F.P.+A_{28}) = 0.133095288$ so that the change in the fuel cross section per unit temperature is

$$\Delta \Sigma_{a \text{ fuel}}/\Delta T = -0.0002165837 \text{ cm}^{-1}/^{\circ}\text{C}.$$

The aluminum cross section, Σ_{aal} , is corrected simply by multiplying the 77°C value by the ratio, $\sqrt{350/360}$, which corrects for $1/v$ decline of the microscopic absorption cross section. Thus $\Sigma_{a \text{ al}} = 0.00555366$ and

$$\Delta \Sigma_{a \text{ al}}/\Delta T = -0.000007878 \text{ cm}^{-1}/^{\circ}\text{C}.$$

The absorption cross section of the water, Σ_{am} , must be corrected for both the density change and for the $1/v$ drop in cross section. That is,

$$\Sigma_{am} = (\Sigma_{am})_{77^{\circ}\text{C}} \sqrt{\frac{350}{360}} \cdot \frac{(\rho_m)_{87^{\circ}\text{C}}}{(\rho_m)_{77^{\circ}\text{C}}} \quad (\text{The effect}$$

of the expansion of the aluminum in diminishing the effective density of the water may be neglected.) Since $(\rho_m)_{87^{\circ}\text{C}} = 0.9674427 \text{ gms/cm}^3$ (by the empirical formula in the Appendix, Section A2.2.1), it follows that

$$\Sigma_{am} = 0.0112161 \text{ cm}^{-1} \text{ and } \Delta \Sigma_{am}/\Delta T = -0.00002342 \text{ cm}^{-1}/^{\circ}\text{C}.$$

The total thermal neutron absorption cross section for the core is $\Sigma_a = 0.14986505$ and $\Delta \Sigma_a/\Delta T = -0.000247881 \text{ cm}^{-1}/^{\circ}\text{C}.$

The two group constants are derived directly since B_h^2 does not change.

The Two Group Constants for a Uniform 87°C Core (Corrected for Non-Cylindrical Geometry)

$D_1 = 1.4509414 \text{ cm}$	$\Sigma_{1R} = 0.022147452 \text{ cm}^{-1}$
$D_2 = 0.2945226 \text{ cm}$	$\Sigma_{2R} = 0.15009834 \text{ cm}^{-1}$
$(D_f B_h^2 = 0.001149305 \text{ cm}^{-1})$	$\Sigma_1 = 0.020998147 \text{ cm}^{-1}$
$(D_s B_h^2 = 0.00023329 \text{ cm}^{-1})$	$\Sigma_a = 0.14986505 \text{ cm}^{-1}$
	$\Sigma_f = 0.1033825357 \text{ cm}^{-1}$

The Results of the Temperature Coefficient Calculations

The numerical solution of the two group, multiregion diffusion equations for the reference reactor with a uniform increase of 10°C in the core temperature only, results in a neutron multiplication constant, $K' = 0.99156$. The original K value is 0.99676 at 77°C so that

$\Delta K/K = K' - K/K = -0.005217$. If the rate of change of reactivity with temperature may be assumed constant over the temperature range, 77° - 87°C, then the core temperature coefficient of reactivity is

$$\frac{\Delta K}{K} / 10^\circ\text{C} = -5.217 \times 10^{-4} \text{ per } ^\circ\text{C}.$$

6.2.9 Estimation of Long Term Poisoning, Burnup, and Net Temperature Effects

By employing the concept of reflector savings and the assumption that the reflector savings is constant, one may readily estimate the effects of uniformly distributed poisons and U^{235} burnup in the core. (See Section 6.1.1 for the interpolation formula for K .) By this method the results listed below in Table 6.2.9 are obtained.

(In the following, the word "depleted" refers to the core with 20% of the original U^{235} in M_0 burned out, "poisoned" refers to the presence of Xe^{135} , Sm^{149} , and low cross section fission products, "clean" refers to a core containing no Xe^{135} , Sm^{149} , or low cross section fission products, and "hot" refers to the core at 77°C.)

Table 6.2.9 Estimated Reactivity Effects

Initial Core Condition	K	Changed Core Condition	K'	$\frac{\Delta K}{K} = \frac{K' - K}{K}$
Cold, Undepleted, Clean	1.12192	Hot, Undepleted, Clean	1.10111	-0.01855 (due to temp.change)
Hot, Undepleted, Clean	1.10111	Hot, Undepleted, with Equilibrium Xe ¹³⁵	1.05706	-0.04000 (due to Xe ¹³⁵)
Hot, Undepleted, with Equilibrium Xe ¹³⁵	1.05706	Hot, Undepleted, with Eq. Xe ¹³⁵ and *Sm ¹⁴⁹	1.04654	-0.009952 (due to Sm ¹⁴⁹)
Hot, Undepleted, Clean	1.10111	Hot, Depleted, **Clean	1.07062	-0.02769 (due to U ²³⁵ de- pletion; hot)
Hot, Depleted, **Clean	1.07062	Hot, Depleted, with F.P.	1.04759	-0.02151 (due to F.P.)
Hot, Depleted, †with F.P.	1.04759	Hot, Depleted, with F.P., with Sm ¹⁴⁹	1.03726	-0.009861 (due to Sm ¹⁴⁹)
Hot, Depleted, with F.P., with Sm ¹⁴⁹	1.03726	Hot, Depleted, Poisoned	0.99676	-0.03905 (due to Xe ¹³⁵)
Cold, Depleted, ‡Clean	1.09127	Hot, Depleted, **Clean	1.07062	-0.01892 (due to temp.)
Cold, Undepleted, Clean	1.12192	Cold, Depleted, Clean	1.09127	-0.02732 (due to depletion)

*Equilibrium Sm¹⁴⁹ is assumed although this case is approached only after some burnup.

**This case provides an estimate of the reactivity due to U²³⁵ burnup although Sm¹⁴⁹ and low cross section fission products are always present at this point in the actual case.

†This case, though fictitious, provides an estimate of the effect of Sm¹⁴⁹ in the depleted case.

‡This case is fictitious but provides an estimation of the net effect of the 77°C temperature in the depleted core.

The Net Temperature Coefficient

The net effect of the rise in reactor temperature from 20°C to 77°C (operating temperature) is about $\Delta K/K = -0.019$.

Hence, the net temperature coefficient, defined as

$\Delta K/K/\Delta T$ (where $\Delta T = 77^\circ\text{C} - 20^\circ\text{C}$) is equal to -3.3×10^{-4} per °C. As expected, the net temperature coefficient is larger than the point value at 77°C, which value is -5.217×10^{-4} per °C. This is due chiefly to the fact that the water density changes more rapidly the higher the temperature.

Discussion of Non-Uniform Poisoning and Depletion Effects

It cannot be overemphasized that the assumptions of 20% uniform U²³⁵ burnup and uniform distribution of the reactor poisons are used only to provide a rough means of estimating the negative reactivity effects of long term reactor operation. A great deal of study and experimentation would be needed to provide a firm estimate of the actual amount of U²³⁵ burnup one might obtain with a given initial uranium loading. In this reactor design, neutron leakage characteristics are of great importance -- which assures that the effects of the non-uniform burnup and fission product poisoning will have considerable effect upon the reactivity.

If the Xe¹³⁵ and Sm¹⁴⁹ are distributed uniformly at an equilibrium concentration corresponding to the average thermal neutron flux at 15 MW of power, the reactivity effect is estimated at about -5% (see Table 6.2.9). Since the hot, clean undepleted excess reactivity is about 10%, there is a maximum of 5% left for the longer term effects of U²³⁵ burnup and low cross section fission product poisons. If non-uniformity factors increase the effects of Xe¹³⁵ and Sm¹⁴⁹ by only 50%, 2.5% excess reactivity is left for the long term operation, and less than 10% burnup of the U²³⁵ is possible. If the effects of non-uniformity in the reactor poison and burnup distributions are of such magnitude, it may be necessary to add reactivity by adding uranium and/or decreasing the reactor neutron leakage in some manner. For the purposes of estimating the lifetime of the reactor, 10% burnup is a reasonable value to use.

The Reactor Lifetime

If 10% U²³⁵ burnup is assumed, there will be, in the reference reactor ($M_0 = 12.438$ Kg), about 1.2 Kg of U²³⁵ consumed. For 193 mev per fission, there is 1.29 gms of U²³⁵ burned per megawatt day (MWD). Hence, at a power level of 10.5 MW, the fresh reactor loading may last $(1.2)/(1.29)(10.5) \approx 90$ days. The validity of the burnup percentage assumption does

not warrant estimation of a different reactor lifetime for the reactor design uranium loading of 11.374 Kg.

The Cold, Clean Critical Mass

By means of the simplified equivalent bare reactor model it is possible to approximate the minimum amount of 90-95% enriched uranium necessary to obtain criticality of the reactor in the fresh 20°C core condition with all other regions of the reactor at normal composition at 20°C. This minimum critical mass is about 6.0 Kg.

6.3 The Estimation of Control Rod Worth

The arrangement of the cadmium plates used for shim and safety control of the reactor is an especially difficult one for which to estimate the reactivity worth. However, a series of two group, multiregion numerical computations serve, for the present purposes, as a means of roughly estimating the adequacy of the control rods with respect to providing a sufficiency of negative reactivity. A series of calculations estimate the negative reactivity of a thin annular region of Cd at various distances from the center of the reactor. The results are subject to broad interpretation at the best, except, perhaps, those for the center cruciform rod.

The annular control rod region is considered to be composed of a mixture of aluminum and cadmium. The cadmium in the mixture comprises a volume fraction, w , of the total of about 0.0644. (This volume fraction is chosen simply because the largest value of the thermal neutron removal cross section permissible in the numerical electronic computer program is limited to 10 cm^{-1}). In the interests of conservatism, fast neutron group absorption in the Cd is neglected in an attempt to counterbalance the effect of neglecting the fast neutron group fissions.

The actual values of the diffusion constants and the fast neutron slowing down cross section of the annular Al-Cd region are unimportant for thin annuli. Since the Cd makes up only a small fraction of the nuclei of the assumed control region material, the fast neutron group constants are assumed to be the same as for aluminum. For media of such high thermal neutron absorptivity, the thermal neutron diffusion constant, D_2 , may be approximated by $1/\Sigma_a$.

Since the control elements do not replace any uranium, the annular control region is characterized by the normal fission cross section of the core.

6.3.1 The Reference Reactor for the Control Rod Calculations

Adding an annular region to the reactor core subdivides the core into three regions. Since the blanket efficiency is not of interest in the control rod calculations, the number of regions outside the core is reduced to two; namely, the usual aluminum-air region and the usual blanket material. Hence the control rod computations are reduced to a five reactor region problem. The constants of the respective regions are as follows:

(Region 1)

The first region is normally a core region. In one case it is a control rod region. The constants are identical to the other core region (region 3), except in the single case wherein the constants are identical to the control rod region (region 2). The region boundary is variable.

Control Rod Annulus (Region 2)

$$\begin{aligned} D_1 &= 3.3043 \text{ cm} & \Sigma_{1R} &= 0.00317342 \text{ cm}^{-1} \\ D_2 &= 0.10 \text{ cm} & \Sigma_{2R} &= 10.0 \text{ cm}^{-1} \\ \Sigma_f &= 0.1051574071 \text{ cm}^{-1} & \Sigma_1 &= 0.00034832 \text{ cm}^{-1} \end{aligned}$$

The outer boundary of the region is variable. The thickness of the region is $\Delta R_2 = 0.635 \text{ cm}$ ($1/4''$). (In one case this second region is made a core region.)

Outer Core Region (Region 3)

Region 3 is identical in properties to the normal reference reactor core medium. (See Section 6.2.1)

$$\begin{aligned} D_1 &= 1.444149 \text{ cm} & \Sigma_{1R} &= 0.0222841 \text{ cm}^{-1} \\ D_2 &= 0.2926208 \text{ cm} & \Sigma_{2R} &= 0.15257565 \text{ cm}^{-1} \\ \Sigma_f &= 0.1051574071 \text{ cm}^{-1} & \Sigma_1 &= 0.02114018 \text{ cm}^{-1} \\ T &= 77^\circ\text{C} & \Sigma_a &= 0.15234386 \text{ cm}^{-1} \end{aligned}$$

Outer boundary, $R_3 = 18.920750 \text{ cm}$.

Al and Air Region (Region 4)

$$D_1 = 5.0179 \text{ cm}$$

$$\Sigma_{1R} = 0.00451969 \text{ cm}^{-1}$$

$$D_2 = 6.1758 \text{ cm}$$

$$\Sigma_{2R} = 0.0126536 \text{ cm}^{-1}$$

$$T = 77^\circ\text{C}$$

$$\Sigma_1 = 0.00022937 \text{ cm}^{-1}$$

$$\Delta R_4 = 4.077676 \text{ cm}$$

$$\Sigma_a = 0.0073733 \text{ cm}^{-1}$$

$$R_4 = 22.998426 \text{ cm}$$

Blanket Region (Region 5)

$$D_1 = 1.29316 \text{ cm}$$

$$\Sigma_{1R} = 0.0359804 \text{ cm}^{-1}$$

$$D_2 = 0.17387 \text{ cm}$$

$$\Sigma_{2R} = 0.21880176 \text{ cm}^{-1}$$

$$\Delta R_5 = 30.0 \text{ cm}$$

$$\Sigma_1 = 0.0348747 \text{ cm}^{-1}$$

$$R_5 = 52.998426 \text{ cm}$$

$$\Sigma_a = 0.2186531 \text{ cm}^{-1}$$

6.3.2 The Results for the Reference Reactor without Control Rods

The neutron multiplication constant for the five region reference reactor is $K = 0.9782$. The neutron multiplication constants for various control rod regions will be referred to this K value in order to estimate the respective reactivity effects. It is of no consequence that the reference K is not exactly unity. The midplane radial flux distributions for the reference reactor are presented in Figures A2.W and A2.X of the Appendix. The fluxes, as in all the other cases, are scaled such that

$$\nu_c \int_{R_f} \Sigma_f \phi_s \text{ } r dr = \int_{R_f} r dr, \text{ where the integration is per-}$$

formed over every region for which $\Sigma_f \neq 0$. ν_c is the number of neutrons per fission needed for exact criticality, i.e., $\nu_c = 2.46/K$.

The ratio of the maximum thermal flux to the average thermal flux, $\phi_{\max}/\phi_{\text{av}}$,

(where $\phi_{\text{av}} = \int_{R_f} \phi_s \text{ } r dr / \int_{R_f} r dr$) is, for the reference control reactor, $\phi_{\max}/\phi_{\text{av}} = 1.59$.

6.3.3 The Effect of a Solid Central Control Rod

Wheeler, in N-2292, "Principles of Nuclear Power", Chapter 22: Control, presents a formula for finding the effective radius of a black imbedded control cross. The effective radius is defined as r_0 , the radius at which the thermal flux extrapolates to zero inside the rod. According to the formula,

$$r_0 = \frac{W\sqrt{2}}{4} \exp - \left[\frac{1}{\frac{W\sqrt{6}}{3\pi D_2} \left(1 - \frac{0.114W\sqrt{6}}{3\pi D_2} + \frac{0.048W6}{9\pi^2 D_2^2} \right)} \right],$$

where W is the width of each of the two plates forming the cross and D_2 is the thermal diffusion constant of the core. In the present case $W = 11.75$ cm, so that $r_0 = 4.075$ cm.

In order to compute the reactivity effect of this solid black rod, the first region is made a control region and the second and third regions of the reactor are made core regions.

The addition of this control region in the center of the reactor core causes the neutron multiplication constant to drop to $K' = 0.87398$ from $K = 0.9782$. Hence the change in reactivity is $\Delta K/K = (K' - K)/K = -0.1065$.

The midplane radial flux distributions for this case are shown on Figures A2.Y and A2.Z in the Appendix. In the control rod region the thermal flux drops to ~ 0.00125 and is constant ± 0.00005 over the inner three cm of the rod region radius.

The ratio of the maximum flux, $\phi_{\max}$, to the average flux, ϕ_{av} (where

$$\phi_{\text{av}} = \frac{\int_{R_f} \phi_s r dr}{\int_{R_f} r dr}, \text{ is } \phi_{\max}/\phi_{\text{av}} = 1.39.$$

6.3.4 The Effect of an Annular Control Rod Region

An annular control rod region 0.635 cm thick is placed at five separate radii and K' is computed for each case. The control rod "radius" is, by definition, the outer radius of the annular region, which is designated by R_2 .

Table 6.3.4 presents a summary of the basic data and the results.

Table 6.3.4 The Results of the Annular Control Rod Calculations

Control Rod Radius, R_2 (cm)	Multiplication Constant of the Reactor	Reactivity Worth of the Annular Region, $\Delta K/K$	Reactivity Worth per cm of Annular Rod Circumference, $\Delta K/K / C$	Ratio of Maximum Thermal Flux to the Volumetric Average, $\phi_{\max}/\phi_{av}$	The Appendix Figures Wherein Midplane Radial Flux Distributions are Presented
0.0000	0.9782	-	-	1.59	A2.W and A2.X
4.0315	0.8746	-0.1059	-0.004181	1.37	A2.AA and A2.BB
5.6055	0.8381	-0.1432	-0.004066	1.34	A2.CC and A2.DD
8.8900	0.7846	-0.1979	-0.003543	1.81	A2.EE and A2.FF
13.4938	0.8096	-0.1724	-0.002033	2.29	A2.GG and A2.HH
18.4150	0.9168	-0.06277	-0.0005425	1.92	A2.II and A2.JJ

The term $\Delta K/K / C$ refers to the reactivity value of a control rod divided by the control rod annulus outer circumference, i.e., $C = 2\pi R_2$. In all cases the reactivity worth of the rod is $\Delta K/K = K' - K/K$ where $K = 0.9782$ for the reference reactor) and K' is the multiplication constant with the control rod annulus present in the reactor. On Figure 6.G, the reactivity worth and the reactivity worth per centimeter of circumference are plotted versus rod radius, R_2 .

6.3.5 Discussion of the Results

It is noted that the effect of the solid control rod is only slightly more than that of a 0.635 cm annular rod, the outer boundary of which is nearly equal to the radius of the solid rod. This is in confirmation of the fact that the major effect of the central control rod stems from the increase in the radial neutron leakage from core. The presence or the lack of fissioning within the effective radius of the control rod appears to be of little significance.

Since the arms of the central control cross extend from the center a distance of 5.87 cm, the "rubber band" surface of the central rod is $(\sqrt{2})(4)(5.87) = 33.21$ cm. Hence an equivalent radius based on this figure is $R_{eq} = 33.21/2\pi = 5.286$ cm. On this basis the central rod would be worth -13.5% of reactivity. (See Figure 6.G) Based on the solid rod concept and Wheeler's formula for the effective radius of a cross rod, the central rod would be worth -10.6%, conservatively, for it was assumed in that calculation that r_0 was the actual radius. Again, by a formula of Wheeler, the actual radius of the rod should be about 4.66 cm, for which case Figure 6.G provides an estimate of about -12% for the worth of the central rod (providing the solid rod is worth about the same as an annular rod of the same radius -- as the results indicate).

The results indicate, then, that the central rod worth is about -12 to -13.5%.

The worth of the four outer shim plates is much more difficult to estimate from the results obtained for the annular control rod regions. A crude estimate of the range of reactivity the four outer rods may be worth is provided by means of the following procedure. The width of each of the four plates is 9.208 cm, so that the total width or the four rods is $4(9.208) = 36.83$. The radius to the tip of an outer control plate is 8.57 cm, and to the center of a control plate the radius is 13.18. The four outer rods should be worth less

than $(36.83)(\Delta K/K / C)_{8.57}$. Since $(\Delta K/K / C)_{8.57} = -0.00363$, the corresponding value for the worth of the rods is about -13%. (See Figure 6.G) On the other hand the four rods should be worth more than the comparably estimated value at a radius equal to 13.18 cm, the distance to the center of a control plate. This value is

$$(36.83) \left(\frac{\Delta K}{K} \right)_{13.18} = (36.83)(-0.00217) = -0.080.$$

Hence the reactivity worth of the four outer rods should be worth from -8 to -13% or, roughly, each rod should be worth from -2 to -3%.

An interesting effect of the central rod is apparent from the values of $\phi_{\max}/\phi_{\text{av}}$ listed on Table 6.3.4. The presence of the central rod lowers $\phi_{\max}/\phi_{\text{av}}$ about 15%. This is because the thermal flux is raised near the outer boundary of the reactor core by the presence of the rod at the center. It is the same effect which increases the blanket efficiency when the central rod is in the reactor core.

6.4 The Effect of a Beryllium Blanket Reflector

The results of a study reported in INTERNUC-3 regarding the effect of a thick graphite reflector in improving the blanket efficiency, Z, warrant the evaluation of beryllium as a blanket reflector. A beryllium reflector would need be less thick than a graphite one of the same effect, and there would be an additional benefit in the event that beryllium could be placed directly inside the blanket without cladding. If the beryllium is placed outside the blanket container, the beryllium reflector offers, again, the prospect of simpler design than would be necessary for a graphite reflector, for in all probability the beryllium need not be canned or otherwise clad.

An evaluation is made of the effect of various thicknesses of beryllium outside the blanket. The outer wall of the blanket container is 1/8" of stainless steel as in the case of the previous study performed with graphite as the blanket reflector. The beryllium comprises the seventh region of the reactor in this study, and the pool water again is considered the outer region. Hence the problem is one of eight reactor regions. All the regions are the same, therefore, as in the reference reactor of Section 6.2.1 except for the last three regions and except for the simple variation of the blanket thickness in some cases. The sixth region, previously the iron thermal shield, is, for the beryllium reflector study, simply reduced to 0.3175 cm (1/8 inch) and the neutron group constants remain the same.

The seventh region is beryllium (instead of the 20°C H₂O of the reference reactor), and the neutron group constants used are those for 20°C Be. The constants are:

$$\begin{array}{ll}
 D_1 = 0.76 \text{ cm} & \Sigma_{1R} = 0.00844 \text{ cm}^{-1} \\
 D_2 = 0.48 \text{ cm} & \Sigma_{2R} = 0.00151 \text{ cm}^{-1} \\
 T = 20^\circ\text{C} & \Sigma_1 = 0.00779 \text{ cm}^{-1} \\
 \left(\begin{array}{l} \Delta R_7 \text{ is variable} \\ R_7 \text{ is variable} \end{array} \right) & \Sigma_a = 0.00110 \text{ cm}^{-1}
 \end{array}$$

The eighth region is 40 cm of 20°C water. The neutron group constants are the same as for the seventh region of the reference reactor.

The effect of thicknesses of Be are evaluated for blanket thicknesses of three inches and six inches.

The results of the study appear in Table 6.4a. In no instance does going to more than 20 cm of Be appear warranted, for, in the case of a 3-inch blanket, less than a 1% increase in Z is attained when the beryllium thickness is increased from 20 cm to 40 cm. For the same 3-inch blanket thickness about 2.3% increase in Z is realized by increasing the beryllium thickness from 10 cm to 20 cm.

In Table 6.4b, the results for two different thicknesses of beryllium are compared to the results for cases in which the blanket is reflected by 60 cm of graphite or by the usual 3-inch iron thermal shield and the pool water. The results are in terms of the percentage reduction occurring in the blanket efficiency, Z , when the blanket thickness is reduced from six inches to three inches. The results indicate that 10 cm of beryllium reflector provides as much improvement in blanket efficiency as does 60 cm of graphite. The effect of the beryllium reflector on the midplane radial neutron flux distributions is apparent from Figures A2.KK through A2.NN. The cases to which these figures refer are those for the 3-inch blanket thickness with 10 cm and with 20 cm of beryllium reflector.

Neutron balances for the 10 cm and the 20 cm thicknesses of beryllium blanket reflector are presented in the Appendix, Tables A2.1i and A2.1j, respectively.

Table 6.4a The Effect of a Beryllium Blanket Reflector on Blanket Efficiency

Blanket Thickness (cm)		Be Thickness (cm)	Reactor Neutron Multiplication Constant, K	Near Critical Thermal Flux Integral in the Blanket, I ₅₂	Blanket Efficiency at Exact Criticality, Z
cm	inches				
15.24	6.0	10.0	0.99676	238.9481	0.56165
15.24	6.0	20.0	0.99676	240.6478	0.56565
7.62	3.0	10.0	1.00292	205.1694	0.48274
7.62	3.0	20.0	1.00350	209.8220	0.49374
7.62	3.0	40.0	1.00350	211.6926	0.49814

Table 6.4b Comparison of Beryllium to Other Reflectors

	Reflector Material			
	<u>*Normal</u>	<u>60 cm Graphite</u>	<u>10 cm Beryllium</u>	<u>20 cm Beryllium</u>
Reduction in Z Occurring When Blanket Thickness is Reduced from 6 inches to 3 inches	20.6%	14.6%	14.05%	12.71%

*Refers to usual reference reactor case wherein three inches of iron thermal shield and 40 cm of water are outside the blanket.

Since the increase in Z is small when a blanket reflector is used with a 6-inch blanket, (only 2 - 2.5%) it is probably worthwhile to consider blanket reflection only for blankets of smaller thickness. For a 3-inch blanket about 11% increase in the blanket efficiency, Z, results if a 10 cm beryllium reflector is used. In this case, if the beryllium were put inside the blanket (without cladding) the poisoning effect of the 1/8-inch stainless steel blanket container wall at the outside of the blanket would be nullified with a subsequent improvement in Z of about 3 - 5%.

7.0 Shielding and Internal Heating

7.1 General Considerations

7.1.1 Shield Geometry

Calculation of the biological shielding requirements and the heat production external to the reactor core are based on the reference design described in Chapter 4.0 of INTERNUC-3. This reference core is three feet high and has an effective radius of 22.434 cm. The dimensions and compositions of the core and surrounding annular regions are presented in Table 7.1.1.

Table 7.1.1 Pertinent Data on Reference Core for Shielding Calculations

<u>Region</u>	<u>cm Thickness</u>	<u>Composition</u>	<u>Amount or Density (g/cc)</u>
Core	22.434 radius	H ₂ O	0.5737
		Al	1.107
		U	0.0728
1	5.83	Al	1.042
		H ₂ O	0.2177
2	0.32	Steel	7.8
3	15.24 (6")	In ₂ (SO ₄) ₃ -H ₂ O	1 to 1.51
4	7.62 (3")	Steel	7.8
5	100.96	H ₂ O	1
6	Variable	Barytes Concrete	3.5

The proposed reactor described in Chapter 3.0 differs in that it is of smaller radius and employs a 4-inch rather than a 6-inch thick blanket of indium sulfate solution. Since the proposed design was selected at a late date, and since multi-region neutron distributions were performed only for the reference design, detailed calculations were completed for the reference core, and the effects of altered geometry were estimated wherever they seemed significant.

The change in geometry affects only the heat production in the external reactor structure (see discussion of effects in Sections 7.2.6 and 7.6.1). The effect upon reactor shielding appears negligible.

The blanket concentration of indium sulfate is considered to be either zero (all H_2O , $\rho = 1$) or 600 gms/liter ($\rho = 1.51$ g/cc), depending on which is the more conservative situation for a particular calculation.

Dose rates are calculated at typical locations in the shield and plant layout (Figures 9.A to 9.F) assuming 20 MW reactor operation. Required thicknesses are specified in terms of tolerance, 7.5 mr/hr, and tenth-tolerance, 0.75 mr/hr.

7.1.2 Methods of Calculation

An energy spectrum of gamma sources was constructed from the known sources in the core and apportioned to several energy groups. These energy groups were then attenuated separately through the components of the shield, using appropriate buildup factors.

Calculation of capture gamma production was facilitated by a detailed knowledge of the radial thermal neutron flux. The flux distribution was previously computed by two-group multi-region calculations using various indium concentrations, so the flux in each radial component could be obtained directly from the IBM data sheets for use in shielding calculations.

Gamma attenuation was computed for each geometrical situation with the equations recently assembled by Rockwell et al.* Buildup from Compton scattering was included by using an analytical buildup function:

$$B = A_1 e^{-\alpha_1 \mu x} - (A_1 - 1) e^{-\alpha_2 \mu x}$$

where the curve fitting parameters A_1 , α_1 , and α_2 have been plotted or tabulated versus initial energy for dose or energy absorption from point sources by Rockwell and by NDA**.

When expressed in this form, the buildup factor may be conveniently integrated as part of the point-to-point

* Rockwell, "Reactor Shielding Design Manual", Van Nostrand, New York, 1956

** NYO-3075, "Calculation of the Penetration of Gamma Rays", Goldstein and Wilkins, June, 1954

kernel, yielding an expression similar to that obtained without a buildup factor. If the dose or energy absorption calculated at a point without buildup is $D(\mu_s, \mu)$, where μ_s and μ are absorption coefficients for the initial gamma energy in the source and the shield, respectively, then the dose with buildup is

$$A_1 D \left[(1 + \alpha_1) \mu_s, (1 + \alpha_1) \mu \right] - (A_1 - 1) D \left[(1 + \alpha_2) \mu_s, (1 + \alpha_2) \mu \right]$$

provided the source and shield materials have similar* gamma attenuation properties.

7.2 Heat Generation in Reactor Components

7.2.1 Core Gammas

The sources of gamma radiation within the core are the prompt fission gammas, fission product decay gammas, capture gammas, and gammas from inelastic scattering of fast neutrons. The necessary data for the latter component is rather fragmentary, so inelastic scattering gammas have been neglected.

Capture gamma production by each material in the core is given by

$$\frac{\text{mev}}{\text{sec-watt}} = 3.25 \times 10^{10} \frac{\sum_i}{\sum_f} n(E) E$$

$$\text{where } \frac{\sum_i}{\sum_f} = \frac{\text{thermal capture cross section of } i^{\text{th}} \text{ material}}{\text{fission cross section}}$$

$n(E)$ = yield of capture gammas of energy group E per capture (See Table 7.2.1a)

* Actually μ_s and μ should pertain to the same material.

Table 7.2.1a Assumed Capture Gamma Spectra
from Core Materials, $n(E)$

E mev	H ₂ O $\Sigma/\Sigma_f = .1897$	U $\Sigma/\Sigma_f = .1840$	Al $\Sigma/\Sigma_f = .0948$
1		1.00*	.9*
2.2	1.0	.90*	-
3		.40*	.61
5		.20*	.37
7			.44

* Assumed from spectral type and binding energies.

Using the energy distribution of fission gammas and fission product decay gammas from infinite time of operation (tabulated by Rockwell, pages 34 and 39) these components may be grouped as shown in Table 7.2.1b. The contribution of decay gammas has been pessimistically adjusted as recommended by Rockwell because of the short-lived activities for which the decay schemes are unknown.

Table 7.2.1b Gamma Sources in Core

Mev	10 ¹⁰ mev/sec-watt			Total Volumetric Source* mev/cc-sec
	Fission Gammas	Decay Gammas	Capture Gammas	
1	10.4	16	.87	37.7 x 10 ¹²
1.5	3.90	3.3	-	10.0 x 10 ¹²
2.3	6.35	3.0	2.43	16.3 x 10 ¹²
3	1.46	0.1	1.28	3.9 x 10 ¹²
5	3.25		1.18	6.1 x 10 ¹²
7			.95	1.3 x 10 ¹²

* Power density = 2×10^7 watts/144,582 cc
= 138.3 watts/cc

The total volumetric source strengths are tabulated in the last column of Table 7.2.1b.

The accuracy of the gamma source construction can be roughly checked by solving for the dose rate at the surface of the reactor core, assuming it an infinite cylinder, and comparing the dose rate to the measured dose rate at the face of the BSR. The dose at the edge of the cylinder of radius R is*:

$$D(R) = \frac{S_V}{\mu \gamma} \left[1 - \mu R \int_{\mu R}^{\infty} K_1(\xi) I_0(\xi) \frac{d\xi}{\xi} \right] \quad (7.1)$$

where μ = composite cross section of core
 S_V = volumetric source, mev/cc,sec
 γ = energy flux to dose conversion

Adjusting the calculation to include buildup effects (dose buildups for water) as described in Section 7.1.2, and solving for the dose rate from each initial energy, the total dose rate of 58.3 r/hr-watt is obtained, of which 32.4 r/hr-watt is from the 1 mev group.

The dose rate at the face of BSR** is 63 r/hr-watt. Since the compositions are nearly identical, and since the volume of BSR is 0.61 times the volume of FIR, a dose rate of $0.61 \times 63 = 38.4$ r/hr-watt would be expected for the FIR. Although the inelastic gammas are neglected, the calculated dose rate from FIR is greater. This might be due to the following:

1. All low energy gammas are lumped at 1 mev. Those of energy lower than 1 mev are more rapidly attenuated, and therefore the contribution from this group is probably over-estimated.
2. Decay gammas are over-estimated.
3. Buildup for pure water is used, and the curve-fitting buildup parameters, which give the best fit for 0 - 20 relaxation lengths, generally yield over-estimates for points near the source.

* See Internuclear memo IC-WRP-56-4, where integral is tabulated.

** ORNL Dwg. 13769, corrected as per ORNL-CF-53-5-21.

Assuming that the discrepancy is primarily due to the first reason, the spectrum of Table 7.2.1b is used with the knowledge that the low energy component is more rapidly attenuated and has less heating effect on regions further from the core.

The energy flux at each boundary of the annular regions is calculated by replacing the finite cylindrical source with a finite line source displaced from the edge of the cylinder by a self-absorption distance Z . Rockwell presents two graphs (pp 362, 363) for determining an equivalent distance Z , which gives results in agreement with the exact solutions obtained by Taylor and Obenshain* for cylindrical sources with slab shields. The calculations are adjusted, as described in Section 7.1.2, to include the effects of buildup by using curve-fitting parameters for energy absorption buildup in water from point isotropic sources.

The heat production at any point is then:

$$Q = \phi_E (\mu - \sigma_s) K \text{ watts/cc} \quad (7.2)$$

where ϕ_E = energy flux, mev/cm²sec

$\mu - \sigma_s$ = energy absorption coefficient of the region in which the heating is calculated

$K = 1.602 \times 10^{-13}$ watt-sec/mev

The use of energy absorption buildup factors for water is subject to question, particularly in determining the heat production in the 3-inch steel thermal shield. Noting that the buildup factor is a measure of the degraded spectrum times the $(\mu - \sigma_s)$ for each degraded energy in the absorber, the use of water buildups is partly justified in that the spectral change, which is due primarily to the large amount of water traversed, should have more effect on buildup than does the variation of $(\mu - \sigma_s)$ over the degraded spectrum in steel relative to that variation in water. The use of water buildups probably results in an over-estimate of core gamma heating.

The heat production by core gammas in each annular region is indicated by the solid line in Figure 7.A for the case involving 600 gms/liter indium sulfate solution in the blanket. Heat production in the thermal shield, by core gammas, is about 30% greater if the blanket is filled only with water. The gamma absorption coefficients of indium sulfate solution are plotted in Figure 7.B.

*J. J. Taylor and F. E. Obenshain, WAPD-RM-213, "Flux from Homogeneous Cylinders Containing Uniform Source Distributions"

7.2.2 Capture Gammas from External Structure

The radial thermal neutron flux distribution through the annular regions was obtained from the IBM data sheets for the case of a pure water blanket and for the case of a 600 gms/liter concentration of indium sulfate. The average radial thermal neutron flux (midplane flux/axial max. to av. ratio) for 20 MW with 20% fuel burnup is given in Table 7.2.2, where x is the radial distance (cm) in each region measured from the inner boundary of the region.

Table 7.2.2 Thermal Neutron Flux in Annular Regions

<u>Region</u>		<u>0 gms/liter</u>	<u>600 gms/liter</u>
1	Al-H ₂ O	2.75×10^{13}	2.50×10^{13}
2	Fe	5.72×10^{13}	1.90×10^{13}
3	(blanket)	-	$1.43 \times 10^3 e^{-0.2x}$
4	thermal shield	$10^{13} [0.50 e^{-0.726x} + 0.00081 e^{0.595x}]$	$10^{13} [0.05 e^{-0.726x} + 0.00075 e^{0.595x}]$

The capture gamma production at each point is given by:

$$S_V(x, E) = \sum_i \phi_{th}(x) n(E) E \text{ mev/cc-sec} \quad (7.3)$$

The yield of gammas of energy E , $n(E)$, is given in Table 7.2.1a for Al and H₂O, while the gammas from steel are lumped into one gamma of 1.5 mev and 1.3 gammas of 5 mev each to facilitate calculation. The capture gamma spectrum from indium is uncertain below 3 mev, but from a knowledge of the spectral type* and the low yield of high energy gammas, a yield of 3.5 gammas of 2 mev energy is assumed. The thermal neutron cross section of indium sulfate, 0.3152 cm^{-1} , assumes a cadmium ratio of 3.26.

Assuming each annular region to be a slab source with either a uniform or an exponential gamma source distribution, the energy fluxes within the slabs and in adjacent regions are computed (see Rockwell, pp 395-398).

* Mittleman, P. S. and Liedtke, R. A., "Gamma Rays from Thermal Neutron Capture", Nucleonics, May 1955.

For convenience, the buildup is neglected and the energy fluxes converted to heat sources by Equation 7.2. The results are plotted in Figure 7.C.

7.2.3 Indium Decay

Each indium absorption leads to 1.806 mev of gamma decay, of which 95.6% is of energy 1.085 to 2.090 mev. It is assumed that 1.2 gammas of 1.5 mev each are released per absorption and that all the decay occurs in the blanket. This approximates the case in which the reactor is run at power without circulation of the indium solution. The average thermal neutron flux in a blanket of 600 gms/liter indium solution is 2.02×10^{13} neut/cm² sec. The gamma production is regarded as a uniform source of intensity:

$$S_v = \sum_{\text{In}} \phi_{\text{th}} \times 1.806 = 1.15 \times 10^{12} \text{ mev/cc-sec}$$

The blanket is again assumed to be a slab source. The resultant heat production is shown by Curve 3' of Figure 7.C.

The beta heating, which is entirely local, is also shown by Figure 7.C. Each absorption yields 1.436 mev of beta energy of which 0.77 mev is from the ground state transition of the 13-second isomer. Thus, during circulation of the indium solution, at least half the energy is released in the blanket, but it is again assumed that the total decay occurs in the blanket.

Heating due to beta and gamma decay of Al and steel is neglected.

7.2.4 Inelastic Scattering Gammas

The inelastic scattering gammas are again neglected, since their contribution to the heating should be small because of the rapid fall-off of fast flux in the blanket.

7.2.5 Slowing Down of Fast Neutrons

The heating of water due to elastic collisions of fast neutrons with hydrogen are estimated by the method suggested by Rockwell (p. 80).

For spherically symmetric scattering in the center of mass system, neutrons, on the average, lose one-half their energy in a hydrogen collision. The energy absorbed by hydrogen recoil is then:

$$Q = \frac{KN_H}{2} \int_0^{\infty} \phi(E) \sigma_H(E) E dE \text{ watts/cc} \quad (7.4)$$

where $\sigma_H(E)$ = hydrogen scattering cross section

N_H = hydrogen density, atoms/cc

$K = 1.602 \times 10^{-13}$ watts-sec/mev

Following Rockwell's method, it is assumed that the fast neutron flux, ϕ_F , given by the two-group calculation is the integral of $\phi(E)dE$ above 0.1-mev, and also that in the energy region from 0.1-mev to 5-mev, where most of the heat is generated, the neutron flux in light water is approximated by:

$$\phi(E) = \phi_F \left[0.63 e^{-E/1.53} - 0.57 e^{-E/1.20} \right]$$

The hydrogen cross section, over the same energy region, is approximated by:

$$\sigma_H(E) = 2.4 e^{-E/10.75} + 5.3 e^{-E/1.2}$$

Substituting these expressions in Equation 7.4 and integrating from 0.1 mev to ∞ , the heating becomes:

$$Q = \left[\frac{KN_H}{2} (1.538 \phi_F) \right]$$

Using the fast flux distributions obtained from the IBM data sheets, the heat due to slowing down of fast neutrons is shown by Figure 7.A.

7.2.6 Combined Heat Sources

The combined heating effect of capture gammas and indium decay, with 600 gms/liter indium solution in the blanket, compared to the core gamma and neutron heating, is shown by Figure 7.A. The heating from capture gammas has arbitrarily been raised by a factor of 1.5 [effectively the ratio $\mu/(\mu - \sigma_s)$] to compensate for the neglect of buildup. The total heating curves are shown by Figure 7.D.

Since the gamma sources are assumed to be uniformly distributed in the core, and since the capture gamma production is based on an "average" radial neutron flux, the total heat production in each region is approximately:

$$\text{watts} = L \int_{r_0}^{r_1} Q(r) 2\pi r dr \quad (7.5)$$

where L = reactor height, 91.44 cm.

The pool heating is roughly estimated by converting the total energy flux at the surface of the thermal shield to watts/cm² and multiplying by the surface area of the thermal shield.

The total heating for the reference geometry is presented in Table 7.2.6 and compared to the estimated heat generation for the proposed design (4" blanket and smaller core - see Chapter 3.0).

Table 7.2.6 Total Estimated Heat in Components, KW
(600 gms/liter Indium Sulfate Solution)

	<u>Region</u>	<u>Reference Geometry</u>	<u>Proposed Design</u>
1	Al-H ₂ O-Air	293	315
2	Fe	84	113
3	Blanket	666	682
4	Thermal shield	208	258
5	Pool water	<u>33</u>	<u>50</u>
	Total	1284	1418

The above heat estimates for the proposed design are based for the calculational procedure outlined herein in the case of core gammas, but the capture gamma contribution is estimated without a knowledge of thermal neutron flux distributions. The heating from capture gammas and indium decay was arbitrarily increased by the ratio of fast neutron leakages, 1.186, and displaced by the 2-inch reduction in blanket thickness.

In the proposed design the volumetric heat sources are considerably greater, due to increased leakage, but the volumes of the components are less.

The total calculated heat generation in the reference design, 6.4% of the reactor heat, appears consistent with the 5.8% value estimated for the reflector and annular regions of MTR*. The uncertainties in source construction and in the applicability of Equation(7.5) and the conservative use of water buildups for core gammas, suggest that such agreement is somewhat fortuitous.

7.3 Biological Shielding

7.3.1 Radial Concrete Thickness

The dose rate received from core gammas is computed with the same cylindrical calculation used for heating, but with dose buildup parameters for point isotropic sources in concrete. The buildup parameters are plotted versus energy for an ordinary concrete by Rockwell. The same energy distribution of core gammas is assumed. The values of μ for a barytes concrete of density 3.5 are tabulated in Table 7.3.1.

Table 7.3.1 Barytes Cross Sections and High Energy Capture Gamma Yields

E, mev	μ for Barytes	n(E)		
		In	Steel	Al
3	0.128 cm ⁻¹	?	0.2	0.61
4	0.119	0.36	-	-
5	0.114	-	0.3	0.37
6	0.111	0.04	-	-
7	0.109	0	0.6	0.44

- *See: 1) "Research Reactors" Selected Reference Material, USAEC Program, p 215
 2) "Peaceful Uses of Atomic Energy, Vol. 2, Research Reactors", United Nations, p 403

The penetration of capture gammas generated in the annular components is computed by replacing each region with a truncated right circular cone of the same gamma source density and the same thickness as the annular region. The cone is assumed to subtend the same angle as the outer diameter or height (three feet) of each region, whichever is greater (see inset in Figure 7.E).

The capture gamma production is again given by Equation (7.3), using the thermal neutron flux indicated for each region in Table 7.2.2. The gamma yield at each energy is tabulated in Table 7.3.1. It is noted that the 7-mev group includes the large 7.6-mev peaks of Al and Fe and the even more energetic gammas from alloying elements in steel. Since μ for barytes does not fall off rapidly at high energies (as does μ for ordinary concrete), the high energy gammas are lumped as shown at 7 mev.

The dose rate received from capture gammas at varying depths in the barytes shield is computed using the calculation for truncated cone sources with either uniform or exponential source distributions (see Rockwell, pp 398-400), and using dose buildup parameters for ordinary concrete. (The use of concrete buildups applied over the total number of relaxation lengths traversed in water and barytes seems valid since the concrete represents 6.5 times as many relaxation lengths as does the water.)

The dose rate both from core gammas and captures is greater when no indium sulfate is present. The calculated dose rates with no indium are shown in Figure 7.E. Tolerance is obtained with 4.67 feet of barytes, while 1/10-tolerance requires 5.35 feet. These dimensions are applicable to the inhabited areas of the first sub-level in the Reactor Building (see Figures 9.A and 9.C). It is noted that at these thicknesses the captures from the external structure (primarily from the thermal shield) appear to predominate. The dose from neutrons and from captures in the concrete are negligible.

The coolant equipment room is shielded from the reactor by 3 feet of barytes, hence the gamma dose rate during 20 MW operation is several r/hr (see Figure 7.E). The neutron dose at this location is estimated to be less than 1 rem/hr, so that both neutron and gamma dosages are less than the dose levels from N^{16} and N^{17} in the coolant.

The sub-pile room is shielded by four feet of barytes so that dose rates are near tolerance and neutron activation

is nil. This neglects streaming around the rod drive assemblies, however, and this effect must be examined after final design of the seals and mechanisms.

7.3.2 Pool Depth

Gamma penetration through the pool water is calculated, assuming the reactor core to be a point source emitting the spectrum of Table 7.2.1b. Self-shielding by the core is accounted for by assuming the source strength at each energy to equal

$$S_p = \frac{S}{\mu_s h} \text{ mev/sec} \quad (7.6)$$

where S = mev/sec from entire core

μ_s = composite core cross section

h = core height

Using the dose buildup for point sources in water and regarding the 7-mev group as 7.6-mev gammas (the energy at which the Al capture gamma yield peaks), tolerance is obtained with 21'8" of water above the core during 20 MW operation. At 19'6" of water (as shown by Figure 9.A), five times tolerance results, and reduction to tolerance requires interposition of about three inches of steel. Since the steel vessel, the lid, and the rod guides above the core represent nearly this thickness, and since normal operation at 20 MW is not anticipated, the 19'6" dimension is considered satisfactory.

Calculation of dose rates received through the slant shield thickness in the upper portion of the pool wall indicates that the barytes concrete wall must be 2.4 feet thick at the first floor level (9.5 feet above the core) and 1.5 feet thick at a point 4.5 feet above the first floor (see Figure 9.A).

7.3.3 Primary Coolant System

The primary coolant equipment is housed in the sub-level rooms adjacent to the reactor, and these areas must remain inaccessible during reactor operation. The required shield thickness for N^{16} gammas and N^{17} neutrons is not computed since the thickness depends on the arrangement and size of components, which may differ in the final design. An estimate of the shielding required is based on APPR. The APPR, a 10 MW reactor of similar cycle time

and with a similar layout of equipment, requires four feet of ordinary concrete for shielding of the primary coolant equipment. This is equivalent to 2.13 feet of barytes for N^{16} radiation. Adding 2.5 inches of barytes to compensate for a factor of two in power gives a shield thickness of 2'4" of barytes. This thickness is more than sufficient for N^{17} neutrons. Detailed shielding calculations should accompany final equipment layout. In the final layout an effort should be made to locate the hottest components near the reactor shield.

7.4 Reactor Shielding after Shutdown

7.4.1 Pool Depth

The penetration of decay gammas from fission products and from indium after infinite time operation at 20 MW are examined at one hour after shutdown. The fission product gamma sources are apportioned to the seven energy groups studied by NDA*, and the indium gammas are lumped in the appropriate groups. It is assumed that all indium decays occur in the blanket (no circulation of indium solution).

Regarding the blanket and the core as isotropic point sources with the self-shielding correction of Equation 7.6, and using dose buildups for water, tolerance at the water surface is obtained with 12 feet of water above the core, and tenth-tolerance results at 13.5 feet. At these distances approximately half the dose rate is due to indium (primarily the 2.09 mev gamma). For longer times after shutdown (and also for much shorter times) the fission products dominate the dose rate at whatever pool depth is required for tolerance.

7.4.2 Coolant Equipment Room

The dose rate in the equipment room from core and blanket decay is less than tolerance immediately upon shutdown. The predominant coolant activities, N^{16} , N^{17} , and O^{19} , decay to insignificant levels in a matter of minutes, and the radiation field within the equipment room is then due primarily to induced activity in coolant contaminants, Na^{24} recoils from the fuel plates, equipment activation

* NDA-27-39, "Decay of Fission Product Gammas", F. H. Clark, Dec., 1954

from N^{17} neutrons, and deposition of radioactive impurities in the water. Experience with MTR indicates that, with proper control of coolant purity, access to primary loop equipment after shutdown is not a significant problem.

7.4.3 Sub-Reactor Room

Neglecting the streaming through the rod drive equipment, dose rates in the sub-reactor room are less than tolerance immediately upon shutdown.

7.5 Shielding of Indium Loop

Essentially all the gamma activity of irradiated indium is from the four most energetic gammas of the 54 minute half-life In^{116} . Together these groups comprise 95.6% of the 1.806 mev of gammas released per absorption in normal indium. The fractional energy release is shown in Table 7.5.

Table 7.5 Gamma Yield from Indium

<u>E, mev</u>	<u>Energy Fraction</u>
1.085	0.2357
1.274	0.3844
1.487	0.1256
2.090	<u>0.2102</u>
Total	0.9559

Shielding requirements for the gamma radiators are based on the specific activity required in each radiator for that unit to alone meet capacity specifications (3 ton-megarep/hr or 7 kw). Since a transfer of fluid from a small volume radiator to a large volume radiator, without sufficient decay time to reduce the specific activity to the new desired value, results in greater dose levels, the incremental shield requirements are indicated in the discussion wherever they are applicable.

Shielding requirements for other indium loop components are based on the maximum specific activity expected, i.e., the specific activity in the smallest radiator volume which alone produced 7 kw in the food.

7.5.1 Slab Radiator

The Multiple-Slab Radiator (see Figure 4.E) is comprised of four unit slabs, each 9' x 20" x 3". It is required that the total food irradiation capacity (7 kw of absorption based on the minimum dose rate within the food) be available from one unit, or from the aggregate of four units operated simultaneously.

The efficiency of each radiator type has been computed in Chapter 8.0 as:

$$E_4 = \frac{\text{kw absorbed in food}}{\text{kw released in radiator by 4 most energetic gammas}}$$

The total source strength of each gamma activity in the radiator, when operated at full capacity, is therefore

$$S(E) \text{ mev/sec} = \frac{7000 \text{ watts} \times f(E)}{.9559 \times E_4 \times 1.602 \times 10^{-13} \text{ watt-sec/mev}} \quad (7.7)$$

where $f(E)$ = energy fraction from Table 7.5.

It is assumed that the slab radiator is parallel to a slab shield of ordinary concrete ($\rho = 2.3 \text{ gms/cc}$) and separated from the concrete by a distance of three feet, with no food interposed between the slabs. The single-unit slab is treated as a finite line source of strength:

$$S_L(E) = \frac{S(E)}{(9 \times 30.48) \text{ cm}}$$

and the four-unit slab is treated as a disk source of strength:

$$S_A(E) = \frac{S(E)}{63 \text{ ft}^2 \times (30.48)^2 \text{ cm}^2/\text{ft}^2}$$

Assuming the efficiency E_4 is 26%, and neglecting self-shielding in the 3-inch thick source slabs, and using dose buildup parameters for ordinary concrete, the dose rates are shown by Figure 7.F. The 26% value for the efficiency E_4 is based on irradiation of V3 boxes, which were approximated by a 6-inch slab of unit density

tissue. A thickness of 5.7 feet is required for tolerance and 6.4 feet for tenth-tolerance for the single-unit slab radiator.

The required concrete thickness directly above the slab is roughly estimated by treating the single-unit slab as a finite line source, but with a self-shielding factor such that only points within one relaxation length of the top of the slab contribute to the dose. For $\mu_s = 0.055 \text{ cm}^{-1}$ (2.09 mev gammas in 200 g/l solution) only $1/\mu_s$ or 7.16 inches of the 20-inch slab contribute to the dose, and the vertical dose is $7.16/20$ or 0.36 as great as the horizontal dose. Four inches less concrete is therefore required in the vertical direction.

7.5.2 Drum Radiators

The drum radiators for No. 2 cans and for No. 10 cans are described in Section 4.2.1. The total source strength, $S(E)$, of each gamma activity in the radiators is again given by Equation 7.7, and the volumetric source strength in each radiator is:

$$S_v(E) = \frac{S(E)}{\text{Total volume of radiator}}$$

$$\begin{aligned} \text{where Volume} &= 1.50 \times 10^6 \text{ cc for No. 2 Can Radiator} \\ &= 1.22 \times 10^6 \text{ cc for No. 10 Can Radiator} \end{aligned}$$

To include the effects of self-shielding, composite gamma cross sections are computed for the drums, using the cross sections for indium sulfate plotted in Figure 7.B, and assuming that no food is present in the radiators.

Dose rates are computed, using the calculation for a self-shielded finite cylinder (see Section 7.2.1), shielded with a slab of ordinary concrete ($\rho = 2.3 \text{ gms/cc}$). The calculations are adjusted, as described in Section 7.1.2, with the dose buildup parameters for ordinary concrete.

The dose rates are plotted in Figure 7.F for 200 and 600 gms/liter solution in the No. 2 Can Radiator and 600 gms/liter solution in the No. 10 Can Radiator. The efficiencies E_4 are assumed to be 23% for No. 2 cans and 21% for No. 10 cans. For the worst condition, i.e., the No. 10 Can Radiator with low soup concentrations, the shield thicknesses required are about 5.5 feet for tolerance and 6.3 feet for tenth-tolerance.

If the can-tubes of the drum are empty (an unlikely situation), shielding requirements above the drums are greater than indicated by Figure 7.F because of the streaming upward through the tubes. If self-shielding within the drums is neglected, an additional foot of concrete is required. Since the effect of streaming gives a dose rate somewhere between that from a homogeneous self-shielded drum and that from a drum with no self-shielding, it is recommended the drum end shielding be six-inches greater than the values given in Figure 7.F.

The effect of valving a drum radiator into the line while the specific activity of the coolant is that which gives 7 kw production from a single slab-unit radiator may be estimated as follows:

Since the specific activity in watts/liter of soup in each radiator producing 7 kw of irradiation is:

$$\text{watts/liter} = \frac{7000 \text{ watts}}{E_4 \times \text{Soup Volume}},$$

the drum radiators would normally operate at a specific activity which is less than that of a single-unit slab by the ratio:

$$\begin{aligned} & \frac{E_4 (\text{drum}) \times \text{Volume (drum)}}{E_4 (\text{slab} \times \text{Volume (slab)})} \\ &= \frac{0.23 \times 340.4 \text{ liters}}{0.26 \times 106.2 \text{ liters}} = 2.84 \text{ for No. 2 Can Radiator} \\ &= \frac{0.21 \times 303 \text{ liters}}{0.26 \times 106.2 \text{ liters}} = 2.30 \text{ for No. 10 Can Radiator} \end{aligned}$$

Cutting in a drum radiator without activity dilution of the soup will increase the dose rates from the drums by these ratios. Activity dilution of the soup necessitated by the increased volume will, after complete mixing, reduce the ratios by the factor:

Volume of blanket, pipes, and slab

Volume of blanket, pipes, and drum

The ratio 2.84 corresponds to 4 inches of shielding in addition to that indicated in Figure 7.F.

7.5.3 Circulating Lines and Components

The maximum specific activity of the indium sulfate solution occurs when the 7 kw of food irradiation capacity is obtained from the radiator unit of smallest volume (a one-unit slab of volume 3.75 ft³). Assuming an efficiency of 26% for a 3-inch slab source and a 6-inch slab target of unit density tissue, the volumetric source strength is, from Equation 7.7:

$$S_V(E) = \frac{7000 \text{ watts} \times f(E)}{.9559 \times .26 \times 1.602 \times 10^{-13} \times 3.75 \times (30.48)^3}$$

$$= 1.66 \times 10^{12} \times f(E) \frac{\text{mev}}{\text{cc-sec}} \quad (7.8)$$

Regarding a pipe of area $A \text{ cm}^2$ containing fluid of this specific activity as a line source, its linear source strength is:

$$S_L(E) = AS_V(E) \text{ mev/cm-sec}$$

The dose attenuation from a 1-inch schedule 40 pipe of infinite length, shielded with ordinary concrete or with lead, is computed using dose buildup parameters for ordinary concrete and for lead. The results are shown in Figure 7.G.

The indium heat exchanger and pumps will also contain indium sulfate of the specific activity given by Equation 7.8. These components are shielded by lead slabs.

Treating a single pump as a sphere containing 2,265 cc of 200 g/l solution, and using dose buildup parameters for lead, 13.3 inches of lead are required for tolerance (see Figure 7.G).

The heat exchanger contains 4,255 cc of solution. Assuming a 200 g/l concentration, composite cross sections are computed to include self-shielding by all materials in the vessel, and dose rates are determined using the self-shielded finite cylinder calculation and dose buildup parameters for lead. A thickness of 11.7 inches of lead is indicated for ten times tolerance (see Figure 7.G).

7.6 Stresses and Temperatures

The temperature distribution within a slab containing a heat source distribution $Q(x)$ is obtained by solving the diffusion equation:

$$\frac{d^2T(x)}{dx^2} + \frac{Q(x)}{K} = 0$$

where K = conductivity of slab.

If we let $Q(x) = Q_0 e^{-\mu(x-x_0)}$, and impose the boundary conditions that the edges (x_0, x_1) of the slab are maintained at temperatures T_0 and T_1 , then:

$$T(x) - T_0 = C_0 \left[1 - e^{-\mu(x-x_0)} \right] - \left[(T_0 - T_1) + C_0(1 - e^{-\mu t}) \right] \frac{(x-x_0)}{t} \quad (7.9)$$

where t = slab thickness

$$C_0 = Q_0/\mu^2 K,$$

and the temperature is found to maximize at a distance:

$$(x-x_0) = -\frac{1}{\mu} \ln \left[\frac{1 - e^{-\mu t}}{\mu t} \right]$$

7.6.1 Thermal Shield

It is assumed that the inner surface of the thermal shield (radius r_0) is maintained at T_0 , where $T_0 \geq T_1$, the temperature of the outer surface (radius r_1). If the shield is free to expand axially, then the maximum stress

in the shell is the axial or tangential tensile stress at r_1 . From Timoshenko*, this stress is:

$$\sigma_z = \frac{\alpha E}{1 - \nu} \left\{ \left[\frac{2}{r_1^2 - r_0^2} \int_{r_0}^{r_1} [T(r) - T_0] r dr \right] + (T_0 - T_1) \right\} \quad (7.10)$$

For the thermal shield, the temperature profile for a slab is assumed adequate (Equation 7.9), and Equation 7.10 is integrated to find the maximum stress.

The following values are assumed for steel:

$$\begin{aligned} \alpha &= 6.7 \times 10^{-6}/^{\circ}\text{F} \\ E &= 30 \times 10^6 \text{ psi} \\ K &= 25 \text{ Btu/hr-ft-}^{\circ}\text{F} \end{aligned}$$

For the reference geometry, for which detailed neutron fluxes are available,

$$Q(r) = 3.4 e^{-0.371(r-r_0)} \text{ watts/cc}$$

for the "average" radial position (see Figure 7.D) and $4.76 e^{-0.371(r-r_0)}$ at the reactor midplane. The midplane stress is found to be:

$$\sigma_z(r_1) = [8900 + 140 (T_0 - T_1)] \text{ psi}$$

For the modified reactor (4-inch blanket and smaller core), $Q(r)$ at the midplane is estimated to be $7.9 e^{-0.371(r-r_0)}$ watts/cc and

$$\sigma_z(r_1) = [13,400 + 139 (T_0 - T_1)] \text{ psi}$$

* Timoshenko and Goodier, "Theory of Elasticity", McGraw-Hill, 1951

It should be noted that these stresses would be ~ 3.5 as great if stainless steel were used for the thermal shield. This is due primarily to the lower conductivity of stainless.

7.6.2 Reactor Vessel

The heat generation in the vessel is 6 watts/cc in the reference reactor and ~ 9.6 watts/cc in the modified reactor. (Note that the heat generation of Figure 7.D is for the composite Al-H₂O-air region; $\mu - \sigma_s$ for aluminum is about twice as great as is the composite $\mu - \sigma_s$.) Even with a midplane heating value greater by 1.4, the high conductivity and the thin dimension of the shell (3/8") appear to preclude thermal stress problems.

7.6.3 Reactor Biological Shield

With water (no indium) in the blanket, the radial heat source in the barytes concrete shield is estimated to be $6 \times 10^{-4} e^{-0.087(r-r_0)}$ watts/cc. Assuming a conductivity of 0.87 Btu/hr.ft.^{°F} for barytes, and using Equation (7.9) with $T_0 = T_1$, the temperature appears to peak at $\sim 7^\circ\text{F}$ above ambient at a distance one foot from the pool water. The thermal shield has evidently eliminated any stress problems in the concrete.

7.6.4 Radiator Biological Shields

Calculation of the heat production in the concrete slab assumes the same geometry as in Section 7.5.1 (3-foot gap, 7 kw performance). With no food present, the heat production in the concrete opposite the midpoint of the radiator is about $0.02 e^{-0.117(x-x_0)}$ watts/cc. Assuming a conductivity of 1 Btu/hr.ft.^{°F} for concrete, Equation (7.9) indicates that the peak temperature occurs about one foot from the concrete surface and that this temperature is 125°F above the surface temperature. A single-unit slab of 7 kw performance gives a temperature rise about twice as great. Thermal shielding or auxiliary cooling of the concrete will evidently be required on the walls of the slab compartment.

The No. 2 Can Radiator is assumed to be adjacent to a concrete slab shield. At 200 g/l and 7 kw performance with no food present, the heating is estimated to be $0.012 e^{-0.106(x-x_0)}$ watts/cc and the temperature peak in the concrete is 92°F above the surface temperatures, again occurring at a depth of ~ 1 foot. Cooling or shielding of the concrete may also be necessary in this case.

8.0 Reactor-Radiator Efficiencies - Reactor Power Requirements

This section gives the results of investigations carried out primarily to determine the effects on reactor power of varying certain design parameters. Major consideration is given to meeting the following performance specifications (see Appendix A,0).

Maximum dose rate	5×10^6 rep/hr
Production rate	3000 lb/hr
Dose to product	2×10^6 rep
Dose x Production	3 ton-megarep/hr
Maximum-to-minimum dose	<1.25

The irradiation of No. 2 and No. 10 cans in drum-type radiators and V3 boxes in slab-type radiators was investigated. The following table gives a brief summary of the results pertinent to the above specifications.

Table 8.0 Summary of Results of Reactor-Radiator Studies

	Radiator				
	Drum		Multiple Slab		
	No.2 Can	No.10 Can	1-Unit	2-Unit	4-Unit
Product	No. 2 cans	No. 10 cans	V3 boxes	V3 boxes	V3 boxes
Reactor Power for 3 ton-megarep/hr, MW	10.3	11.6	12.2	8.2	6.1
Reactor Power for 5×10^6 rep/hr dose rate, MW	10.5	12.7	7.1	9.4	14.2
Dwell Time for 3000 lb/hr production rate, min.	24.5	26.2	13.9	27.8	55.5
Maximum-to-minimum dose in product, approx.	1.17	1.27	1.39	1.39	1.39

From the above table it is seen that only the No. 2 can radiator meets the performance specification calling for a maximum-to-minimum dose in product of < 1.25 . The No. 2 can radiator is therefore the basis for setting the normal operating power of the reactor at 10.5 megawatts (sufficient for 5×10^6 rep/hr dose rate).

The following design values resulted from the studies.

No. 2 Can Radiator (Drum Type)	
Space between adjacent drum-tubes	1/4 inch
No. 10 Can Radiator (Drum Type)	
Space between adjacent drum-tubes	5/8 inch
Slab Radiator Thickness	3 inches
Reactor Blanket Thickness	4 inches
Indium Solution Density	1.4 gms/cc

The above are strongly tied to the dose, dose rate, and production performance specifications since these determine the size of the radiators. Any appreciable change in the production (lb/hr) may result in optimum values considerably different from the above.

The final establishment of design specifications for the reactor-radiator complex must await experimental results from radiator efficiency and dose distribution studies as well as reactor criticality studies.

8.1 Factors Affecting Reactor Power

The efficiency of the reactor-radiator complex and the FIR reactor power is affected by many parameters. In brief these include the following:

1. reactor core size, shape, and composition.
2. blanket thickness and volume.
3. density (concentration) of indium sulfate solution.
4. radiator geometry and volume.
5. volume of pumps, valves, piping in blanket-radiator loop and indium solution heat exchanger loop.

In order to investigate the efficiency of the reactor-radiator complex and arrive at the reactor power required to meet specifications, the overall efficiency (E) is divided into four efficiency factors, namely

E_1 - energy efficiency factor

E_2 - core-blanket efficiency factor

E_3 - volume-circulation efficiency factor

E_4 - radiator efficiency factor

Each of the above efficiency factors is discussed below and their dependence upon various design parameters is shown. The various efficiency factors are then combined to arrive at optimum values for the design parameters and reactor power.

8.2 E_1 - Energy Efficiency Factor

The efficiency factor E_1 , relates to the gamma energy available from neutron capture in indium and the fission energy from U235 neutron absorption. Specifically it is defined as

$$E_1 = \frac{\gamma \text{ energy/indium absorption}}{\text{fission energy/U}^{235} \text{ absorption}}$$

The number and energy of decay gammas per neutron absorption in the natural element indium are given in Table 8.2 below.

Table 8.2 Decay Gammas from Indium

<u>Gamma Energy</u>	<u>Gammas per Absorption</u>	<u>Mev per Absorption</u>
0.137	0.02180	0.00300
0.192	0.01229	0.00235
0.406	0.18168	0.07376
0.548	0.00038	0.00021
0.715	0.00038	0.00027
1.085	0.3924	0.42575
1.274	0.5451	0.69446
1.487	0.1526	0.22692
2.090	0.1817	0.37975

Total 1.806

Since self-absorption will account for an appreciable loss of the low energy gammas, and also since the gammas below 1 mev represent only a small portion of the total energy available, they are neglected. The gamma energy per neutron absorption is thus taken as the sum of the energy from the gammas above 1 mev. This is approximately 1.73 mev/absorption.

The efficiency factor E_1 is then

$$E_1 = \frac{1.73 \text{ mev}}{\text{In abs.}} \times \frac{1.184 \text{ U}^{235} \text{ abs.}}{193 \text{ mev}} = 0.0106$$

8.3 E_2 - Core-Blanket Efficiency Factor

The efficiency factor E_2 , which takes into account the neutrons absorbed in indium in the blanket compared to those absorbed in fuel in the reactor core is defined specifically as

$$E_2 = \frac{\text{Indium absorptions}}{\text{U}^{235} \text{ absorption}}$$

This efficiency factor, which is identical to the factor Z used in Chapter 6.0 of this report and Chapter 4.0 of INTERNUC-3, depends upon the geometry and compositions of the reactor core and blanket. From the curves of Figures 4.G and 4.I of INTERNUC-3, it is concluded that, for indium solution densities of interest, the relative effect of solution density (ρ) on E_2 can be considered the same for all blanket thicknesses. This follows since in Figure 4.G (INTERNUC-3) it is shown that reducing the blanket thickness changes only slightly the neutron flux in the remaining portion of the blanket. Also, the curves in Figure 4.I (INTERNUC-3) for indium concentrations of 0.2, 0.4, and 0.6 grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$ solution are all approximated by $e^{-0.19x}$, where x is the distance (in cm) radially outward from inner blanket wall.

Note that discussions in this chapter are generally on the basis of solution density (grams/cc) while those of Chapter 6.0 and INTERNUC-3 are on the basis of solution concentration (grams $\text{In}_2(\text{SO}_4)_3/\text{liter}$ or grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$). Figure 8.A shows the relationship between solution density and solution concentration.

The variation of E_2 with solution density is shown by Figure 8.B, which is based on Figure 4.H of INTERNUC-3. The curve value has been arbitrarily normalized to unity at an indium solution density of 1.6 g/cc, the highest solution density involved in the investigations in this chapter. The curve, which is based on calculations for a 6 inch blanket thickness and the larger reactor core of INTERNUC-3 is used for all blanket thicknesses in the parametric evaluations of this chapter.

The variation of E_2 with blanket thickness is shown by Figure 8.C, which is based on Figure 4.F of INTERNUC 3. The curve has been arbitrarily normalized to unity at a blanket thickness

of 9 inches, the greatest blanket thickness investigated herein. Figure 8.C, which is based on calculations for an indium concentration of 0.2 grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$ and the larger reactor core of INTERNUC-3, is used for all indium solution concentrations investigated in this chapter.

Reactor physics calculations (see Chapter 6.0) show that decreasing the size of the core by decreasing the size of the fuel assemblies from the 3"x3" cross section of INTERNUC-3 to 2.5" x 2.5" cross section results in a substantial increase in the neutron leakage from the reactor. For a blanket thickness of 6 inches and an indium concentration of 0.4 grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$ the core of twenty-four 3 foot long, 2.5 inch square fuel assemblies gave $E_2 = Z = 0.550$ compared to $E_2 = Z = 0.468$ for the core of 3 inch square fuel assemblies shown in INTERNUC-3, an increase of about 17.5%. The metal-to-water ratio in both cores is 0.7. Naturally the required fuel is greater for the smaller diameter core, being 12.7 Kg compared to 9.5 Kg for the larger core. Since the smaller core is satisfactory from the standpoint of heat transfer area, further studies were based on the use of the smaller fuel assemblies. The effect of reactor height on $E_2(Z)$ was investigated for the small reactor with indium solution of 0.4 grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$ concentration in a 6 inch thick blanket (see Chapter 6.0). The values of $E_2(Z)$ obtained are as follows:

<u>Reactor Core Height, ft.</u>	<u>$E_2(Z)$</u>
3.0	0.550
2.5	0.522
2.0	0.479

Utilizing Figures 8.B and 8.C, values of E_2 for different blanket thicknesses and indium solution densities were obtained. These are plotted against blanket volume in Figure 8.D. In all cases the blanket height is equal to reactor core height. The relationship of blanket volume to blanket thickness for various reactor core heights is shown by Figure 8.E.

Figure 8.D shows that, for blanket volumes above 6 ft³ the 3 ft. high core gives highest efficiency, for blanket volumes from about 3.6 to 6 ft³, the 2.5 ft. high core gives highest efficiency, and for blanket volumes below about 3.6 ft³, the 2 ft. high core gives highest efficiency.

8.4 E_3 - Volume-Circulation Efficiency Factor

The efficiency factor E_3 takes into account the effects of volume distribution and circulation rate of the indium solution. Specifically it is defined as

$$E_3 = \frac{\text{disintegrations in radiator}}{\text{total disintegrations}}$$

The effects of circulation rate and volume distribution on E_3 have been investigated for slug flow and for perfect mixing in both the reactor blanket and the radiators. Slug flow refers to the situation in which the indium solution enters one end of the blanket or radiator and flows through it with a uniform velocity until it emerges at the other end. In order to achieve slug flow in the blanket or radiator, baffles are required to direct the flow along the desired path. Section 8.4.1 deals with the cases in which the neutron flux distribution in the blanket is uniform, while Section 8.4.2 considers the effects of the non-uniformity of neutron flux distributions in the blanket.

8.4.1 Uniform Neutron Flux Distributions

Assuming the neutron flux distribution to be uniform throughout the blanket and neglecting the effects of piping, etc. between the blanket and radiator, only four cases exist. They are:

- Case I - slug flow both in the reactor blanket and in the radiator
- Case II - slug flow in the blanket and perfect mixing in the radiator
- Case III - perfect mixing in the blanket and slug flow in the radiator
- Case IV - perfect mixing in both the reactor blanket and the radiator

(Cases III and IV, which involve perfect mixing in the blanket, are thereby valid without regard to conditions of neutron flux uniformity.)

The applicable formulae, derivations of which are given in IC-GEP-56-17*, are:

$$\text{Case I} \quad E_3 = \frac{\left[1 - e^{-a(1-\omega)} \right] \left[1 - e^{-a\omega} \right]}{a(1-\omega) \left[1 - e^{-a} \right]}$$

*IC-GEP-56-17 "The Effect of Perfect Mixing on the Efficiency of an External Loop Irradiator", G. E. Putnam, August 7, 1956

Case II

$$E_3 = \frac{\omega \left[1 - e^{-a(1-\omega)} \right]}{(1-\omega) \left[1 + a\omega - e^{-a(1-\omega)} \right]}$$

Case III

$$E_3 = \frac{\left[1 - e^{-a\omega} \right]}{a(1-\omega) + 1 - e^{-a\omega}}$$

Case IV

$$E_3 = \frac{\omega}{1 + a\omega(1-\omega)}$$

where

$$\omega = V_R / (V_B + V_R)$$

V_R = solution volume in radiator

V_B = solution volume in blanket

$$a = \lambda T_c$$

T_c = cycle time of blanket-radiator loop

λ = decay constant of radioactive isotope

Values of E_3 , calculated by the above equations are shown by Figures 8.F, 8.G, 8.H, and 8.I for Cases I, II, III, and IV respectively. Reference to the above figures shows that highest efficiencies are obtained with slug flow in both the blanket and radiator, while lowest efficiencies are obtained with perfect mixing in both the blanket and radiator. The curves also show that unless the circulation rate between the blanket and radiator is sufficiently large, losses in efficiency result.

Case III (perfect mixing in the blanket and slug flow in the radiator) most closely approximates the flow in the conceptual design reported herein. Through use of an independent high-flow heat exchanger loop (see Sections 4.1 and 4.4) in the indium solution system, perfect mixing conditions are promoted in the blanket. Slug flow in the radiators is assured through the use of baffles (see Section 4.2.1). The mean life of the In^{116} isotope is 78 minutes. The volume of indium solution involved in the operation of one radiator is about 150 gallons. With the specified minimum

circulation rate of 15 gpm in each radiator loop, the loop cycle time is not greater than 10 minutes. The value of (a) is then

$$a = \frac{\text{loop cycle time}}{\text{mean life of radioactive isotope}} = \frac{10}{78} = \frac{1}{8}$$

Reference to Figure 8.H shows that with $a = 1/8$, E_3 is at most about 1.5% lower than if $a = 0$ (infinite circulation rate). Since this is a negligible change, the effects of flow rate are neglected in investigations of overall reactor-radiator efficiency reported in Sections 8.6 and 8.8.

8.4.2 Non-Uniform Neutron Flux Distributions

The effect of non-uniform neutron flux distributions on the efficiency factor E_3 have been investigated for conditions in which baffles in the blanket tank direct the flow along chosen paths. Particular interest in this problem is with respect to the gains that might be realized through direction of flow in the blanket along paths leading from regions of low neutron flux to regions of high neutron flux, with the exit lines at the latter regions. The effects on E_3 of flow in the opposite direction are also given.

At a given flow rate, the number of disintegrations which occur in the radiator are directly proportional to the specific activity of the solution leaving the blanket (neglecting interconnecting piping). The effect of non-uniform neutron flux compared to uniform neutron flux on the blanket exit specific activity under slug flow conditions is given by β which is defined as

$$\beta = \frac{S_{NU}}{S_U}$$

where

S_{NU} = blanket exit specific activity for non-uniform neutron flux

S_U = blanket exit specific activity for uniform neutron flux equal to the volumetric average of the non-uniform flux of S_{NU} .

The equation for β , which is derived in IC-GEP-56-16*, is:

*IC-GEP-56-16 "The Effects of Non-Uniform Neutron Flux Distribution on the Efficiency of an External Loop Irradiator", G. E. Putnam, August 6, 1956

$$\beta = \frac{\lambda t_1}{\left(\frac{C \lambda^2}{v^2} - 1\right)} \frac{\left\{ \left[\frac{\lambda}{v} \phi(x_1) - \phi'(x_1) \right] - \left[\frac{\lambda}{v} \phi(x_0) - \phi'(x_0) \right] e^{-\lambda t_1} \right\}}{\left[\phi'(x_1) - \phi'(x_0) \right] \left(1 - e^{-\lambda t_1} \right)}$$

where $\phi(x) = C\phi''(x)$, (C is a constant)

$\phi(x)$ = neutron flux, neutrons/cm²sec (ϕ' and ϕ'' designate the first and second derivatives with respect to x, respectively)

x = position variable, cm

x_0 = entrance to blanket

x_1 = exit from blanket

v = velocity of solution flowing from x_0 to x_1 , so that

$$v = \frac{x_1 - x_0}{t}, \text{ cm/min.}$$

t = time in blanket, i.e., the time required to go from x_0 to x_1 , min.

λ = decay constant of radioactive isotope, min⁻¹

Figure 8.J shows the relative effect on E_3 of vertical slug flow in the reactor blanket when the vertical neutron flux distribution is taken into account. The vertical neutron distribution is assumed to be a chopped cosine function going to zero at 8 cm beyond the top and bottom of the 3 ft. high blanket. Curve A shows β , the appropriate factor for correction of E_3 , when the indium solution enters the blanket at the ends and leaves the blanket at the horizontal midplane. Curve B shows the correction factor β when the flow is reversed. As would be expected, Curve A, which represents the case where the neutron flux at the exit is higher than for an assumed uniform flux throughout the blanket, shows an increase of E_3 . However, for the present indium system design the correction is negligible. There is less than 6 cubic feet of indium solution in the blanket, a minimum flow rate of fifteen gpm in the radiator loops, and a corresponding blanket residence time of about three minutes. The appropriate correction factor from Curve A of Figure 8.J is 1.0035.

Figure 8.K shows the relative effect on E_3 of radially directed slug flow in the reactor blanket when the radial neutron flux distribution is taken into account. The radial neutron distribution, $\phi(x) = e^{-0.19x}$, is an approximation of the radial distributions shown by Figure 4.I of INTERNUC-3. Curve A shows the correction factor β for the case when the solution enters the blanket at the outer radius and leaves the blanket at the inner radius. Curve B shows β when the flow direction is reversed. The curves are based on a six-inch blanket thickness and thus give a greater correction than would be obtained for the four-inch blanket of the present design. The radial velocity of flow was assumed constant, i.e., no allowance was made for the radial variation in the slug velocity as occurs in an annular section. Since neglect of the radial variation results in a higher proportion of the time being spent in the higher flux regions, the corrections shown by Figure 8.K represent slight overestimates in this regard. For the three minute blanket residence time, the correction factor, β , from Curve A of Figure 8.K is about 1.008.

The considerations given above to the gains in efficiency that could be achieved by baffling in the blanket tank to provide the optimum flow path show that such gains are negligible. Also, the gains that would be achieved through providing the optimum flow path would most likely be overbalanced by losses due to neutron capture in the baffle material, especially if that material is stainless steel.

Since at the minimum circulation rate of 15 gpm in the radiator loops the combined effects of flow and flow distribution are shown by the above considerations to be small (2 - 2.5%), these effects are neglected in results presented in succeeding sections. The efficiency factor E_3 used henceforth is based on volume distribution only and is given by

$$E_3 = \frac{V_R}{V_T} = \frac{V_R}{V_R + V_B + V_{PP}}$$

V_R = volume of solution in radiator

V_T = total solution volume concerned with operation of radiator loop

V_B = volume of solution in blanket

V_{PP} = volume of solution in piping, pump, valves, etc. of the radiator and indium system heat exchanger loops.

8.5 E₄ - Radiator Efficiency Factor - Drum Radiators

The efficiency factor E₄ considers the efficiency of absorption of the gamma energy produced in the radiator. Specifically it is defined as

$$E_4 = \frac{\gamma \text{ energy absorbed in the product}}{\gamma \text{ energy from disintegrations in radiator}}$$

Results of investigations of radiator efficiency (E₄) are given in this section for the No. 2 can and No. 10 can drum radiators and in Section 8.7 for slab radiators. In all cases the product is assumed to be unit density material.

The drum radiators upon which the following results are based are similar in all respects to those shown by Figures 4.B and 4.C, with the only variables being the space between drum-tubes (or drum tube diameter) and the distance between the outer drum-tubes and the drum wall. The latter was varied with s, distance between drum-tubes, and was always taken to be 1.5 s.

The radiator efficiency factors for No. 2 can and No. 10 can drum radiators have been estimated in IC-OJE-56-1*. Pertinent data upon which the studies are based are:

Table 8.5 Data Pertinent to Drum Radiator Studies

<u>Item</u>	<u>Radiator</u>	
	<u>No. 2 Can</u>	<u>No. 10 Can</u>
No. of can tubes in radiator	61	19
Height of radiator, cans	15	10
Height of radiator, inches	68	70
Center-to-center tube spacing, in.	4.875	8.1875
Drum tube wall thickness, inches	0.063	0.063
Can tube wall thickness, inches	0.020	0.020
Can tube O.D., inches	3.5625	6.375
Weight of one can, grams	90.8	270
Weight of contents of one can, gms.	608	3140
Total product in radiator, lbs.	1225	1310

The center-to-center spacing of the cans is maintained constant at values based on an estimate of the space needed between

*IC-OJE-56-1 "Drum Radiator Efficiency", O. J. Elgert, August 31, 1956

cans to facilitate their loading into and unloading from the drums. The amount of indium solution in the drums is varied by changing the diameter of the drum-tubes and thereby the distance(s) between drum-tubes. The latter is used as a variable in plotting the results.

The overall radiator efficiency (E_4) is the product of the following five efficiency factors.

E_{4a} - mass absorption efficiency factor

E_{4b} = source self-absorption correction factor

E_{4c} - radial self-absorption factor for drum

E_{4d} - axial self-absorption factor for drum

E_{4e} - minimum-to-average dose in product

8.5.1 E_{4a} - Mass Absorption Efficiency Factor

In evaluation of this efficiency factor, no attention is paid to source distribution and it is assumed that the gamma energy absorbed in the drum is distributed among the materials present solely on the basis of the mass of the material multiplied by its absorption coefficient. The can is assumed to contain water. The indium sulfate solution is assumed to be water, but with a density equivalent to that of the indium solution. A macroscopic absorption coefficient, for an "average" gamma energy (1.22 mev) emitted by the indium is used. The coefficients for water and steel are 0.0641 and 0.0540 $\text{cm}^2/\text{gm.}$, respectively. The efficiency factor E_4 is given by

$$E_{4a} = \frac{(\mu M)_{\text{product}}}{(\sum \mu_i M_i)_{\text{drum}}}$$

where

$(\mu M)_{\text{product}}$ = mass of can contents multiplied by its absorption coefficient

$(\sum \mu_i M_i)_{\text{drum}}$ = the sum of the products of the masses of all materials in the drum times their respective absorption coefficients

Figure 8.L shows values of E_{4a} for various indium solution densities and drum tube spacings for both No. 2 can and No. 10 can drum radiators. In order to give an idea of the distribution of energy absorption in the various drum radiator components and contents, the fraction of energy

absorbed in each is shown by Figure 8.M (for a No. 2 can radiator and indium solution density of 1.4 gms/cc). Even though self-absorption in the source has been neglected, a significant amount of γ energy is absorbed in the indium solution. An appreciable amount is also absorbed in the drum tubes.

8.5.2 E_{4b} - Source Self-Absorption Correction Factor

A correction factor to account for the self-absorption in the source (indium solution) is made on the basis of an average thickness of the indium solution between the drum tubes (see IC-OJE-56-1). This simple approach is used since the complicated geometry of the drum tube does not readily lend itself to refined radiation absorption calculations. Also, this approximation is intended primarily to show relative effects of changes in drum tube spacing and indium solution density and for that purpose its use appears justified. The correction factor E_{4b} is based on the gamma leakage from a slab source of thickness equal to the average thickness of indium solution between the drum tubes. The leakage fractions were obtained from Figure XIV of KAPL-783. Plots of E_{4b} are shown by Figure 8.N.

8.5.3 E_{4c} - Radial Self-Absorption Factor

The factor E_{4c} corrects for the gammas that leak radially from the drum and thus are not available for absorption in the product. The equation for the radial leakage from infinite cylinders is given in KAPL-783. For $\mu R > 2$, the equation reduces to the approximate form

$$f_r = \frac{2}{9} \frac{1}{(\mu R)^3} \left[\frac{9}{4} (\mu R)^2 - \frac{1}{2} \right]$$

where

μ = mass absorption coefficient, cm⁻¹

R = radius of cylinder, cm

Assuming the drum and its contents are a homogeneous mass and source, E_{4c} is taken as (1 = f_r) where f_r is calculated by the above equation. Figure 8.0 shows E_{4c} for both No. 2 and No. 10 can radiators.

8.5.4 E_{4d} - Axial Self-Absorption Factor

The factor E_{4d} corrects for the gammas that leak axially out the top and bottom of the drum. The fraction of gammas

leaking from an infinite slab source is given in KAPL-783. For $\mu t > 6$ the equation reduces to the approximate form

$$f_a = \frac{1}{2\mu t}$$

where

μ = mass absorption coefficient, cm^{-1}

t = slab thickness, cm

Assuming the drum and its contents are a homogeneous mass and source, E_{4d} is taken as $(1 - f_a)$ where f_a is calculated by the above equation. Figure 8.P shows E_{4d} for both No. 2 and No. 10 can radiators.

8.5.5 E_{4e} - Minimum-to-Average Dose in Product

Since dose specifications are based on subjecting the food to a certain minimum dose, an efficiency correction is made for the excess dose given to some parts of the food. This correction is simply

$$E_{4e} = \frac{\text{minimum dose in product}}{\text{average dose in product}}$$

The factor E_{4e} can be considered as being the product of two components, the minimum-to-average dose in an individual can and the minimum-to-average dose in the drum.

The values for the minimum-to-average dose in individual cans are obtained through use of Figure 6.B of INTERNUC-3. This figure gives minimum-to-maximum dose ratios for cylindrical targets surrounded by a source of 1 mev gammas. Through use of the upper curve (Figure 6.B - INTERNUC-3), which includes buildup, values of 0.928 and 0.871 are obtained for the minimum-to-average dose ratio in No. 2 cans and No. 10 cans, respectively. These values are probably low since the indium γ 's range from about 1.1 to 2.1 mev (see section 8.2) while the curve (Figure 6.B - INTERNUC-3) is based on 1 mev gammas.

The minimum-to-average dose in the drum is estimated through use of Figure 6.D of INTERNUC-3. This figure shows three curves for the radial dose distribution in cylindrical drum radiators, one for uniform source strength throughout and two for higher source strengths in an outer annular region. The latter two are attempts to approximate the dose distribution in the drum radiators, which have higher average source strengths in the outer annulus since the ratio of

of source to non-source materials is higher in this region. Case III shown by Figure 6.D of INTERNUC-3 has an average source strength in the outer annulus 1.6 times that in the inner region. Case III, which gives a value of about 0.95 for the minimum-to-average dose in the drum is considered representative of the No. 2 and No. 10 can radiators. In any event, the drums will have to be built to approximate a flat gamma distribution across the drum in order to achieve a low maximum-to-minimum dose for the total product in the radiator, and the 0.95 value above appears to be a reasonable choice. This value is used for both the No. 2 and No. 10 can radiators.

The values of E_{4e} are then about 0.88 and 0.83 for the No. 2 can and No. 10 can radiators, respectively.

8.5.6 E_4 - Radiator Efficiency

The above five factors are multiplied together to give the overall drum radiator efficiency (E_4). The efficiencies for various drum tube spacings and indium solution densities are shown by Figures 8.Q and 8.R for the No. 2 can and No. 10 can radiators, respectively.

8.6 E - Reactor-Radiator Efficiency - Drum Radiators

As a first step in determining the reactor-radiator design that gives the highest overall reactor-radiator efficiency for the drum radiators, the volume-circulation efficiency factor (E_3) is obtained for various radiator tube spacings and plotted against $(V_T - V_R) = (V_B + V_{pp})$ where V_T , V_R , V_B and V_{pp} are as previously defined. These plots are shown by Figures 8.S and 8.T for No. 2 can and No. 10 can radiators, respectively. The effects of circulation have been neglected, i.e., $E_3 = V_R/V_T$.

Multiplication of E_3 by E_4 and plotting against $(V_T - V_R) = (V_B + V_{pp})$ shows quite clearly which tube spacings are most desirable. These plots are shown by Figures 8.U, 8.V, 8.W for the No. 2 can radiator, and by Figures 8.X, 8.Y, 8.Z for the No. 10 can radiator. Assuming that a minimum space of 1/4 inch between drum tubes is desirable in order to promote uniform flow through the drum and also lessen the dose variation in the product, Figures 8.U, 8.V, and 8.W show this spacing to be the best for the No. 2 can radiator when the volume of $(V_B + V_{pp})$ is less than 12 ft³. (Figure 8.V shows that higher efficiency would result with still smaller tube spacings.) Figures 8.X, 8.Y, and 8.Q indicate that a drum tube spacing of close to 0.5 inches is optimum for the No. 10 can radiator. Comparison of Figures 8.U, 8.V, and 8.W for the No. 2 can radiator with Figures 8.X, 8.Y, and 8.Z for the No. 10 can

radiator on the above basis shows that the efficiencies of both are about the same when the volume ($V_B + V_{pp}$) is 6 to 6.5 ft³. Because of this little difference and since products in No. 2 cans will probably represent the greatest proportion of production, the determination of optimum reactor blanket thickness is based on the No. 2 can radiator.

The product $E_2 \times E_3 \times E_4$ is determined for the No. 2 can radiator with 0.25 inch drum-tube spacing and the 3 foot high reactor core using the appropriate curves of Figures 8.U, 8.V, 8.W, and 8.D. The results are shown by Figure 8.AA where the product is plotted versus blanket volume. A curve for the 2.5 foot high core is also given on Figure 8.AA to show the negligible gain that could be realized through use of the shorter core. The figure shows that the optimum blanket volume is 6 to 6.5 ft³. Taking the optimum blanket volume as 6 ft³, the optimum blanket thickness is 4 inches (from Figure 8.E).

The blanket shown by Figure 3.B shows the blanket tank as proposed, i.e., with the outer edges at top and bottom chopped off at a 45° angle. The effect, of chopping of the edges, on the overall reactor-radiator efficiency, depends on the changes in the efficiency factors E_2 and E_3 . E_3 (core-blanket efficiency factor) decreases slightly because of the loss in neutron captures in the blanket. Assuming the radial neutron flux distribution (for 0.4 grams $\text{In}_2(\text{SO}_4)_3/\text{cc}$) shown by Figure 4.I of INTERNUC-3 and a chopped cosine vertical flux distribution (as in Section 8.4.2), the captures in the blanket are reduced by a factor of about 0.968. The lowering of the volume in the blanket increases E_3 (volume-circulation efficiency factor) by a factor of about 1.04. Multiplication of these two factors gives a net maximum increase in E (overall reactor-radiator efficiency) of about 1/2% due to chopping off the outer edges of the blanket tank. Although the beveled tank offers no advantage efficiency-wise, it is used primarily because of its structural advantages.

Values of E (overall-reactor radiator efficiency) are obtained by multiplying values from Figure 8.AA by $E_1 = 0.0106$ (see Section 8.2). The resulting curves, given in Figure 8.BB, show that highest efficiency is obtained with an indium solution density of 1.4 gms/cc.

The reactor power required to produce the three ton-megareps energy absorbed in the food at a minimum dose rate of about 5×10^6 rep/hr is shown by Figure 8.CC. The curves show the importance of keeping low the volume of pipes, pumps, etc. (V_{pp}) in the indium system. Estimates of the volumes in piping, pumps, etc. for the proposed indium solution system (Chapter 4.0) are as follows:

Circulating loop of one radiator	0.5 ft ³
Heat exchanger loop	<u>0.75</u>
Total	1.25 ft ³

From the above, the operating power of the reactor based on producing three ton-megareps of energy in the product in the No. 2 can radiator is about 10.3 MW.

The reactor-radiator efficiency of the No. 10 can radiator is approximately the same for drum tube spacings of 0.5 and 0.625 inches as shown by Figure 8.DD, which is based on Figure 8.Y. The 0.625 inch space is chosen since the larger spacing will produce a more uniform dose in the product. The reactor power required for the No. 10 can radiator (0.625 inch drum-tube spacing) is shown by Figure 8.EE. The power requirements for the No. 10 can radiator are just slightly higher than for the No. 2 can radiator.

8.7 E₄ - Radiator Efficiency Factor - Slab Radiators

The radiator efficiency factors for slab radiators are based on the processing of V3 boxes. In order to provide flexibility in production, dose rate, and dose to product, one of the slab radiators is made in four sections sized particularly for V3 boxes. The slab radiator studies are based on this multiple-unit slab radiator. Data relevant to the slab radiator studies are given in Table 8.7.

Table 8.7 Data Pertinent to Slab Radiator Studies

V3 box - dimensions, inches	6 x 16 x 20
- volume, ft ³	1.112
- weight at unit density, lb.	69.4
Individual slab	
- overall dimensions, inches	24 x 108
- area, in ²	2160
Space between boxes, inches	2

The general arrangement for the slab radiator studies is shown by Figure 8.FF. The studies particularly investigate the effects of slab thickness and indium solution density. The overall radiator efficiency (E₄) for the slab radiator is the product of the following two factors:

- E_{4f} - infinite slab efficiency factor
- E_{4g} - product area/source area factor

As defined in Section 8.5, E_4 is

$$E_4 = \frac{\gamma \text{ energy absorbed in the product}}{\gamma \text{ energy from disintegrations in radiator}}$$

8.7.1 E_{4f} - Infinite Slab Efficiency Factor

The efficiency factor E_{4f} is based on irradiation of a 6 inch thick target (unit density) by slab sources on both sides. The factor is based on the dose at the center of the target and thus represents efficiencies relevant to the minimum dose in the target. The efficiencies are based on γ 's of average energy 1.27 mev (the major contribution from indium). A 1/16 inch steel plate has been assumed for the source container walls. Values of E_{4f} for various slab thicknesses and indium solution densities are shown by Figures 8.GG and 8.HH. Figure 8.HH is based on use of internal 1/16 inch steel reinforcing webs on 2 inch centers throughout the length of the slab. Figure 8.GG is based on a slab source without the reinforcing webs.

8.7.2 E_{4g} - Product Area/Source Area Factor

In order to achieve a dose distribution along the edges of the targets comparable to that through the centers, it is necessary that the source extend beyond the edge of the targets. With a 2 inch distance between source and target it is estimated that the source should extend about two inches beyond the target. For the 16 inch wide V3 boxes, the width of the slab source is then 20 inches. Because of conveying problems, it is also necessary to have a space between targets traveling along the conveyor. The space required between V3 boxes is estimated to be 2 inches. For a slab source with 5 V3 boxes on each side, the source is 20 inches wide by 108 inches long. The factor E_{4g} is then

$$E_{4g} = \frac{\text{product area}}{\text{source area}} = \frac{5 \times 16 \times 20}{20 \times 108} = 0.74$$

8.7.3 E_4 - Radiator Efficiency

The overall radiator efficiency, E_4 , is obtained by multiplying the values of E_{4f} given in Figure 8.HH by 0.74 (E_{4g}). The results are shown by Figure 8.II.

8.8 E - Reactor-Radiator Efficiency - Slab Radiators

Overall reactor-radiator efficiencies are given herein for operation of one, two, and four units of the multiple-unit slab

radiator. The volume-circulation efficiency factors (E_3) are given for the one-, two-, and four-unit slab radiators by Figures 8.JJ, 8.KK, and 8.LL, respectively. Since an indium solution density of 1.4 gms/cc was established as the design value by the drum radiator studies, it is used as the basis for determining the thickness of slab radiators. Figures 8.MM, 8.NN, and 8.OO show the product $E_3 \times E_4$ for the one-, two-, and four-unit slab radiators, respectively. From these figures a thickness of three inches is selected for the slab radiators. This selection is based on a volume of 7 - 8 ft³ in the blanket and piping, etc. ($V_B + V_{PP}$). At these volumes, the three inch slab gives maximum efficiency in the four-unit radiator, and slightly less in the two-unit and one-unit radiators.

The reactor powers required for the one-, two-, and four-unit slabs to meet the specified 3 ton-megarep/hr production-times-dose are shown by Figure 8.PP. While the reactor power required for the four-unit slab is about half that of the one-unit slab, the dose rate is also half and the dwell time is twice that for the one-unit slab.

A plot of the product $E_2 \times E_3 \times E_4$ versus solution density for the two-unit slab radiator is given by Figure 8.QQ. The figure shows that an indium solution density of 1.4 gms/cc gives an overall reactor-radiator efficiency within about 1% of the maximum attainable with optimum indium solution density.

8.9 Production-Dose-Power Relationships

Relationships between production, dose, dwell time and reactor power are given by Figures 8.RR, 8.SS, and 8.TT for both slab and drum radiators. Figure 8.RR shows the product production-times-dose versus reactor power for the various radiators. Figure 8.SS shows the dose rate versus reactor power and Figure 8.TT shows the dwell time versus production. Thus, knowing the production (lb/hr) and minimum dose (rep) required, the dwell time is given by Figure 8.TT and the required reactor power is given by Figure 8.RR. With the reactor power thus determined, the minimum dose rate (rep/hr) to the product is obtained from Figure 8.SS.

8.9.1 Maximum-to-Minimum Dose Ratios

The maximum-to-minimum dose ratios for individual No. 2 and No. 10 cans in the drum radiators are about 1.11 and 1.21, respectively, as determined from Figure 6.B of INTERNUC-3. Incorporating an additional factor of 1.05 to account for the dose variation across the drums, the maximum-

to-minimum dose ratio for V3 boxes (6 inch thickness) irradiated by an infinite slab source 3 inches thick is approximately 1.32 as determined from Figure 10.B of IC-WRP-56-3*. To take into account the dose variation across the width of the target because of the finiteness of source and target, the above is multiplied by 1.05, giving a maximum-to-minimum ratio of 1.39 for V3 boxes irradiated by the slab sources.

*IC-WRP-56-3 "Gamma Attenuation in Targets Irradiated by Slab Sources", W. R. Pearce, May 28, 1956

9.0 Buildings and Equipment Arrangements

Incorporated on the drawings of the buildings are the general arrangements of much of the equipment associated with the reactor and the radiators. The arrangements of the equipment themselves have been discussed in the appropriate preceding chapters.

9.1 Reactor Building

Various sections through the Reactor Building are shown by Figures 9.A, 9.B, 9.C, and 9.D. The Reactor Building is a containment type structure in which the containment vessel is a 1/4-inch thick, 32-foot diameter, 71-foot high steel shell. The use of a containment type building has been dictated largely by the fact that the FIR will undoubtedly be located in a populated area. This construction feature also entered largely into the decision to provide essentially separate buildings for the reactor and the radiator, since satisfactory closure of the containment building would be most difficult to accomplish if the food conveyors were to enter the building. The diameter of the building is kept relatively small by offsetting the reactor pool to one side. The level of the first floor of the building is determined primarily by the need for a truck entrance for removal of the coffin for shipment of spent fuel assemblies. The reactor pool walls extend approximately 11 feet above the first floor level. An annular steel-grated working area is provided around the top of the reactor pool. A 20-ton crane provides for the transfer of the fuel assembly shipping coffin from the reactor pool to the shipping truck. Most of the equipment associated with cooling the reactor and the indium system is located on two sub-levels below the main floor. A third sub-level is provided for auxiliary equipment, and it is also at this level at which the subpile room is located. The subpile room is primarily for access to the control rod drives which are located below the reactor shield plug.

9.2 Radiator Building

The gamma radiators and equipment for filling and emptying the radiators, circulating the indium fluid through the radiators, and decontaminating the various pieces of equipment are located in the Radiator Building. Figures 9.A, 9.B, 9.C, 9.E, and 9.F show various sections through this building. The Radiator Building is a 33-foot by 65-foot structure. It consists essentially of three main levels; the top level, which is the operating and

maintenance level; the ground level, at which the radiators and non-radioactive equipment are situated; and the sub-level, at which the pumps, major valves, and storage tanks for handling the highly radioactive material are situated. A plan view of the first floor level, which shows the arrangement of the various radiator cubicles, is shown by Figure 9.B. A major consideration in the arrangement of the radiators is the shielding requirements. These are summarized in detail in Section 7.5, but, in general, the shielding thicknesses required are four to six feet of ordinary concrete. Six feet of ordinary concrete is required to reduce the radiation intensity to one-tenth of tolerance, while 4.5 feet is required to reduce the radiation intensity to ten times tolerance. All of the radiator cubicles, the decontamination still cubicle, and the equipment pit are provided with removable floor slabs at the operating floor level to permit maintenance on the various system components. A 5-ton crane, which travels the entire length of the building, provides for handling of the floor slabs as well as heavy items of equipment. A service and personnel entrance to serve the Reactor, Radiator, and Operations Buildings is provided at one end of the Radiator Building. Also located at this point is an elevator which serves the two upper levels of the Radiator Building as well as the basement level of the Operations Building.

9.3 Operations Building

The Operations Building lies immediately adjacent to both the Reactor Building and the Radiator Building. The first floor level of the Operations Building is shown by Figure 9.B. At this floor level are located the control room, the laboratories, locker room, health physics room, and office space for supervisors and operators. The control room is placed immediately adjacent to the Reactor Building so as to minimize the length of the many leads, both instrumentation and control, that lead from the Reactor Building to the control room. The control room location is also such that the distances from it to the pump and radiator cubicles in the Radiator Building are not great, thereby minimizing the length of the instrumentation leads necessary to provide information in the control room about the radiator systems. The truck entrance to the Reactor Building is also a part of the Operations Building. The basement level of the Operations Building provides for location of heating and ventilating equipment, switch gear associated with reactor equipment, and also provides a general storage and utility area. A vault for storage of fresh fuel assemblies is located in one corner of the Operations Building basement.

10.0 Reactor Loading Procedure and Equipment

At the end of a specified period the reactor is shut down for unloading the spent fuel assemblies. The shutdown period may also be utilized for routine maintenance check procedure. The unloading procedure is performed manually, whenever possible, to keep the operation simple and safe. The proposed FIR design, in which the control rod drives are located below the reactor, becomes a great advantage during shutdown operations, since no complicated top plug is involved.

10.1 Handling Tools

The equipment for normal loading and unloading is:

1. Crane - The overhead Reactor Building crane with a capacity of 20 tons has a small rider crane with a capacity of 2 tons for light utility use in the majority of loading and unloading work. The travel of the crane unit affords service to any point inside the entire Reactor Building.
2. Nut Removal Tool - A modified impact wrench is used to remove the nuts which hold the reactor top blind flange. After a nut is loosened, it is retained by means of a magnet while the operator lifts it off the stud into a nearby bucket. The tool is a commercial type impact wrench with an extension added and a modified wrench socket to accommodate a magnet and magnet release.
3. Upper Grid Assembly Removal Tool - The upper grid assembly is equipped with four holding clamps on the shim rod housing and eight upper grid hold-down bolts, which must be removed before the upper grid assembly can be lifted from the tank interior. This tool is used for both the above fastening devices and consists of a socket wrench, an extension rod, and a handle which is turned manually by the operator.
4. Fuel Assembly Removal and Transport Tool - The fuel assemblies are provided with a $\frac{3}{8}$ -inch rod across the opening in each end box to facilitate handling the assemblies. The handling tool consists of the operators handle, extension, and a gripping mechanism. The gripping is accomplished by simply closing a hook around the rod in the end box. The assembly can then be lifted from the lower grid and transported to storage racks on the wall of the shield pool.

5. End Cut-Off Saw - The function of the saw is to cut off the fuel assembly end boxes preparatory to shipping the spent fuel to a chemical processing plant. This operation shortens the fuel assembly by approximately 8 inches, providing considerable weight savings in the transfer coffin. The saw is a mechanical-hydraulic device with a reciprocating head which slides in guides. The head and blade are mounted in a vertical plane. A saw table, with clamping frame, is provided to hold the fuel assembly during cut-off. The table is equipped with a chip collector under the saw for catching the heavy saw particles.
6. Miscellaneous Hand Tools - Several utility tools are required in the shield pool for use in picking up items such as buckets, light fixture, dropped articles, etc. These tools are simple hook-type or spring-loaded mechanical fingers.
7. Shield Pool Lighting - Since handling of assemblies and manual operations are carried on under 12 feet or more of water, it is necessary that good underwater lighting be provided. Commercially available sealed beam lights for underwater use are satisfactory for this purpose.

10.2 Unloading and Loading Procedure

Soon after the reactor is shut down the shield pool water level may be reduced until it reaches a point twelve feet above the reactor core. However, for access to the reactor at short times after shutdown the full water shield above the reactor is probably necessary.

The sequence for unloading is as follows:

1. The nut-removal tool is attached to the small utility crane and lowered into the pool. The nuts of the reactor top flange are removed and placed in a bucket or container hanging in the pool at approximately the same elevation.
2. The reactor vessel top blind flange is then lifted by the small crane and suspended on one side of the pool or deposited on a rack.
3. With the aid of the upper grid assembly removal tool, the upper assembly is next removed. The upper grid assembly consists of the shim rod guide housing and the grid that holds down the fuel assemblies. The guide housing has four clamps to the tank wall.

These clamps must be released and the upper grid must be detached from the reactor core structure. Once the upper grid assembly is unlatched, it can be lifted out of the reactor tank and suspended at the shielding pool wall until refueling operations are complete.

4. With the upper grid assembly removed, the fuel assemblies selected for removal are lifted from the core by the fuel transport tool. The removed assemblies are placed in the underwater storage racks near the bottom of the shielding pool.

The loading procedure is the reverse of the procedure outlined above; the same tools are used in loading. The sequence is as follows:

1. New fuel assemblies are placed in the core.
2. The upper grid assembly is lowered into place and latched in position.
3. The reactor vessel cover flange is lowered into place.
4. The nut-removal tool is used to replace and tighten the nuts.

10.3 Shipping Coffin

The shipping coffin is conceived to be similar in design to the MTR shipping coffin. It contains four fuel assemblies, without end boxes, when loaded for shipment. The assemblies are arranged in a square pattern with spacer plates separating the chamber into quadrants. The 12-15 ton coffin is lowered into the pool, either during shutdown or reactor operation, and loaded with spent fuel assemblies, which have had the end boxes removed. The coffin cover is replaced and the coffin lifted from the shield pool to a truck or lumber-type carrier for shipment.

11.0 Operation of Reactor-Radiator Complex

Herein are discussed some of the factors which influence the operation of the FIR. It is shown that through operation at startup powers higher than normal reactor power the time required to get a radiator "on-the-line" is appreciably reduced. Also, the factors involved in changing from one radiator to another and one power level to another are examined. A brief discussion of the gains that might be realized through "cycling" of fuel assemblies is included.

11.1 Startup Procedure and Load Changes

Considerable time is required for the activity of the indium loop to approach equilibrium after reactor startup because of the 54.3 minute half-life of In^{116} . Similar time delays are necessary to restore equilibrium following step changes in power level or reductions of the indium loop volume. These time delays are dependent upon the permissible variation of dose rates in food, since this specification determines how closely the indium activity must approach a new equilibrium value before food production is resumed.

11.1.1 Basic Equations for Transient Indium Activity

The gamma dose contribution from the longer and shorter indium activities are negligible, and the loop cycle time is much shorter than the dominant half-life; hence calculation of transient activity may be based on a 54.3 minute half-life and the dose rates in the food may be assumed to vary directly as the blanket activity (see Section 8.4). The saturated specific activity (dis/sec-cc) anywhere in the loop is directly proportional to the activity of the solution leaving the blanket. At constant indium concentration the neutron flux in the blanket is proportional to reactor power. Therefore the saturated activity (A_{∞}) anywhere in the loop is proportional to reactor power (P) and inversely proportional to the total indium loop volume (V), or:

$$A_{\infty} \propto \frac{P}{V} \quad (11.1)$$

After operation for time t at constant flux, and with constant indium concentration, the transient specific activity of the solution is given by:

$$A(t) = A_{\infty}(1 - e^{-\lambda t}) + A(0)e^{-\lambda t} \quad (11.2)$$

where λ = decay constant, 0.766 hr^{-1}

$A(0)$ = specific activity at time zero

From Equations 11.1 and 11.2, all the transient effects may be computed, subject only to the qualification that the indium concentration and blanket volume remain constant. For variable indium concentrations, Equation 11.1 must be adjusted by a functional relationship between total activity and power, which may be determined after initial operation of the reactor and indium loop.

For the most general transient of interest (see diagram), assume that the reactor has operated at an initial constant power P_1 and loop volume V_1 for sufficient time to saturate the activity. The reactor is then shut down to zero power. After a shutdown period, t_s , the reactor is restarted with the objective of approaching the saturated activity corresponding to power P_0 and loop volume V_0 . As is subsequently developed, it is advantageous to charge the activity at some other constant power, P_2 , until the desired specific activity or some tolerable percentage of the desired activity is attained. After time, t_s , of shutdown the reactor is operated at constant power, P_2 , and loop volume, V_2 , until the activity becomes an acceptable fraction of the saturated activity corresponding to the objective power, P_0 , and loop volume, V_0 . The volume, V_2 , during charging equals either V_1 or V_0 .

Provided the indium concentration is maintained constant, there are three solutions to the general problem described, depending on the manner in which the volume is changed and time of the volume change. These may be summarized as follows:

The fraction of saturated activity corresponding to P_0 and V_0 after t seconds of operation at power P_2 is:

$$\frac{A(t)}{A_\infty} = \frac{P_2 V_0}{P_0 V_2} \left(1 - e^{-\lambda t} \right) + \frac{P_1 V_0}{P_0 V_1} e^{-\lambda(t+t_s)} \quad (11.3)$$

and the time required to reach a given fraction $A(t)/A_\infty$ is:

$$t = \frac{1}{\lambda} \ln \left[\frac{\frac{P_2 V_0}{P_0 V_2} - \frac{P_1 V_0}{P_0 V_1} e^{-\lambda t_s}}{\frac{P_2 V_0}{P_0 V_2} - \frac{A(t)}{A_\infty}} \right] \quad (11.4)$$

where $V_0 = V_1 = V_2$ if the loop volume is held constant or increased by adding non-radioactive indium solution of the same indium concentration.

$V_2 = V_0$ if the volume is decreased by fluid removal before application of power P_2 .

$V_2 = V_1$ if the volume is decreased by fluid removal after application of power P_2 .

Analysis of the special cases of the general transient described by Equations 11.3 and 11.4 serve to describe the time-dependence of the indium activity.

11.1.2 Activity Buildup Following Startup

Cases I through IV of Table 11.1.2 summarize the time behavior of activity following normal hot or cold startup of the reactor-radiator complex with constant loop volume.

Table 11.1.2 Transient Indium Activity
Following Cold and Hot Startup

Case

I Cold startup at nominal power P_0 :

$$\frac{A(t)}{A_\infty} = (1 - e^{-\lambda t}) \quad t = \frac{1}{\lambda} \ln \left[\frac{1}{1 - \frac{A(t)}{A_\infty}} \right]$$

II Cold startup at power $P > P_0$:

$$\frac{A(t)}{A_\infty} = \frac{P}{P_0} (1 - e^{-\lambda t}) \quad t = \frac{1}{\lambda} \ln \left[\frac{P/P_0}{P/P_0 - \frac{A(t)}{A_\infty}} \right]$$

III Shutdown from equilibrium at power P_1 for time t_s , followed by hot startup at power P_0 :

$$\frac{A(t)}{A_\infty} = (1 - e^{-\lambda t}) + \frac{P_1}{P_0} e^{-\lambda(t+t_s)} \quad t = \frac{1}{\lambda} \ln \left[\frac{\frac{P_1}{P_0} (1 - e^{-\lambda t_s})}{1 - \frac{A(t)}{A_\infty}} \right]$$

IV Shutdown from equilibrium at power P_1 for time t_s , followed by hot startup at power $P_2 > P_0$:

$$\frac{A(t)}{A_\infty} = \frac{P_2}{P_0} (1 - e^{-\lambda t}) + \frac{P_1}{P_0} e^{-\lambda(t+t_s)} \quad t = \frac{1}{\lambda} \ln \left[\frac{\frac{P_2}{P_0} - \frac{P_1}{P_0} e^{-\lambda t_s}}{\frac{P_2}{P_0} - \frac{A(t)}{A_\infty}} \right]$$

* $(A(t)/A_\infty)$ is fraction of equilibrium activity corresponding to nominal power P_0 ,

t = time required to reach fraction $A(t)/A_\infty$ of saturated activity

The buildup of activity for the cold startup Cases I and II is shown in Figure 11.A. Evidently the startup time may be greatly reduced by operating at greater than nominal power until the desired fraction of equilibrium activity is attained; then reducing power to the nominal rating.

Startup at greater than nominal power is also accompanied by an energy saving. If the time required to obtain a given fraction $[A(t)/A_\infty]$ is t_0 for startup at nominal power P_0 , and t for startup at greater power P , then, from Table 11.1.2, the energy saved per unit of nominal power is:

$$\frac{E_0 - E}{P_0} = \frac{P_0 t_0 - P t}{P_0}$$

$$= \left\{ \frac{1}{\lambda} \ln \left[\frac{1}{1 - \frac{A(t)}{A_\infty}} \right] - \frac{P}{P_0 \lambda} \ln \left[\frac{P/P_0}{\frac{P}{P_0} - \frac{A(t)}{A_\infty}} \right] \right\} \frac{\text{MW-hr}}{\text{MW}}$$

The resulting energy savings are shown by Figure 11.B. If 99% of saturated activity is required before production begins, then startup at 50% greater than nominal power would result in an energy savings equivalent to nearly four hours of operation at nominal power.

The time required for hot startup, and the effect of restarting at greater than nominal power (Cases III and IV of Table 11.1.2) are shown in Figures 11.C and 11.D. It is obviously desirable to minimize shutdown time and to restart at greater than nominal power if the time of restart is to be minimized. As in the case of cold startup, energy savings will again accompany startup at higher power.

11.1.3 Transient Activity Following Step Changes in Power Level, Loop Volume, or Both

A change in radiator volume, and hence a change in the indium loop volume, alters the "volumetric efficiency" (E_3 - volume - circulation efficiency - Section 8.4) of the reactor-radiator complex, and requires a new reactor

power level if full production capacity is to be maintained after the change in radiator usage. Consequently, power changes usually accompany a volume change. Since the fraction of gamma energy released in a radiator is the radiator volume divided by the total loop volume, power and volume changes will normally be inversely related by:

$$\frac{P_1}{P_0} = \frac{V_{R0}V_1E_{40}}{V_{R1}V_0E_{41}}$$

where V_R = radiator volume

E_4 = radiator efficiency (See Section 8.5)

Power and volume need not always vary inversely, however, so it is necessary to consider all combinations of step change.

Assuming that the plant has attained equilibrium at initial power P_1 and loop volume V_1 , the transients following a step change in power, volume, or both power and volume, are summarized in Table 11.1.3.

Decreased Volume

In the case of decreasing volume, radioactive solution must be removed from the system and stored. Since the stored activity is essentially lost and must be re-generated, the activity buildup and time requirements depend upon whether the activity loss occurs before or after application of the charging power P_2 . From Equation 11.4 it can be seen that, if the power is to be increased, then less time and energy is required to approach the new equilibrium if the volume reduction is effected before charging the system. The case of volume reduction preceded by power transition has therefore been neglected in Table 11.1.3.

In the case of power change preceded by volume reduction (Cases V and VI), time and energy are required: (1) to restore the system to equilibrium at the reduced volume, and (2) to accommodate any increase in power level. The required overall change in specific activity is the factor P_0V_1/P_1V_0 . If power is held constant, then it follows that sufficient time is required to increase specific activity by the volume ratio.

Table 11.1.3 Transient Indium Activity
Following Step Change in Reactor
Power, Indium Loop Volume, or Both

Case

For Decreased Volume Preceding Power Change

- V Transition from equilibrium at P_1 and V_1 to P_0 and V_0 by operating at power P_0 :

$$\frac{A(t)}{A_\infty} = (1 - e^{-\lambda t}) + \frac{P_1 V_0}{P_0 V_1} e^{-\lambda t} \quad t = \frac{1}{\lambda} \ln \left[\frac{1 - P_1 V_0 / P_0 V_1}{1 - A(t) / A_\infty} \right]$$

- VI Transition from equilibrium at P_1 and V_1 to P_0 and V_0 by operating at charging power P_2 :

$$\frac{A(t)}{A_\infty} = \frac{P_2}{P_0} (1 - e^{-\lambda t}) + \frac{P_1 V_0}{P_0 V_1} e^{-\lambda t} \quad t = \frac{1}{\lambda} \ln \left[\frac{P_2 / P_0 - P_1 V_0 / P_0 V_1}{P_2 / P_0 - A(t) / A_\infty} \right]$$

For Constant Volume or Increased Volume

- VII Transition from equilibrium at P_1 and V_1 to P_0 and V_0 by operating at power P_0 :

$$\frac{A(t)}{A_\infty} = (1 - e^{-\lambda t}) + \frac{P_1}{P_0} e^{-\lambda t} \quad t = \frac{1}{\lambda} \ln \left[\frac{1 - P_1 / P_0}{1 - A(t) / A_\infty} \right]$$

- VIII Transition from equilibrium at P_1 and V_1 to P_0 and V_0 by operating at charging power P_2 :

$$\frac{A(t)}{A_\infty} = \frac{P_2}{P_0} (1 - e^{-\lambda t}) + \frac{P_1}{P_0} e^{-\lambda t} \quad t = \frac{1}{\lambda} \ln \left[\frac{P_2 / P_0 - P_1 / P_0}{P_2 / P_0 - A(t) / A_\infty} \right]$$

$A(t)/A_\infty$ is fraction of equilibrium activity corresponding to final power P_0 and volume V_0 .

t = time required to reach activity $A(t)/A_\infty$.

The time required to approach a new equilibrium after a step increase in P_0V_1/P_1V_0 (Case V) is plotted in Figure 11.E. The delay time is greater for greater increments in specific activity, and it increases rapidly as the required fraction of equilibrium approaches unity. (Infinite time is required to saturate at the new conditions.)

As in the case of startup, both time and energy are saved by charging the system at a power level greater than the final power, until the saturated activity corresponding to the final power is attained; then reducing power to the final power. This effect is shown in Figure 11.F. At constant volume, the activity is doubled in a half-hour by operating at four times the initial power, while more than five hours is required to reach 99% equilibrium if the system is charged at twice the initial power.

Constant or Increased Volume

A system initially at equilibrium activity will remain at equilibrium if the volume of the system is increased by the addition of non-radioactive fluid. Since no time or energy is required to restore equilibrium after the volume change, the transients for constant volume and for increased volume are identical. In this case (Cases VII and VIII) the specific activity must be changed only according to the power ratio P_0/P_1 .

The time required to approach saturation after a step increase in power (Case VII) and the time needed to gain equilibrium by operating at a charging power greater than the final power (Case VIII) are effectively the values plotted in Figures 11.E and 11.F for variable volume transients. If the volume ratios are neglected on either graph, then the results are valid for constant or increased volume. Figure 11.F is replotted for constant or increased volume in Figure 11.G for a fixed charging power of 20 MW. Since the FIR design power is 20 MW, this figure shows the time required at full power to effect an increase in the loop dose rate corresponding to P_1 to a new dose rate corresponding to P_0 . The dose rate is, of course, directly proportional to reactor power level.

Reduced Power

The equations of Table 11.1.3 are also applicable to power decrements at constant or increased volume, or to decreased P/V at decreased volume. (In these cases equilibrium at the final power P_0 is approached as $A(t)/A_\infty$ reduces to unity.)

It is clear that the delay time to reduce the activity to a given fraction of the equilibrium value corresponding to the final conditions can be decreased by operating at a discharging power P_2 less than P_0 , and that the optimum power P_2 is zero - corresponding to reactor shutdown. In this case, the decay is simply $e^{-\lambda t}$ and the time delay is

$$t = \frac{1}{\lambda} \ln \left[\frac{P_1 V_0 / A(t)}{P_0 V_1 / A_\infty} \right] \text{ for decreased volume}$$

$$\text{or } t = \frac{1}{\lambda} \ln \left[\frac{P_1 / A(t)}{P_0 / A_\infty} \right] \text{ for constant or increased volume.}$$

This exponential decay is shown in Figure 11.A for short times after shutdown. Figure 11.H shows the contribution of the 50 day half-life activity for long periods of shutdown after sufficient time of operation to saturate both activities. It is noted that the longer activity contributes only $\sim 10^{-5}$ of the gammas useable for food irradiation (greater than 1 mev).

11.2 Reactor Loadings and Cycles

In Section 6.2.9, the life of a full core of fresh fuel assemblies is estimated to be about 90 days for a reactor power level of 10.5 MW. This estimate is based on a fuel burnup of 10%. Unless there is a great need to avoid reactor shutdowns for a period as long as 90 days, it is quite possible that cycling of fuel assemblies would give appreciable gains in the fuel burnup per assembly and at the same time improve the operating characteristics of the reactor.

It is conceivable that the reactor could be shut down, fuel assembly changes made, and the reactor restarted in a time as short as one to two hours. With only this

amount of time required for reactor fuel changes it may be desirable to consider an operating cycle of two to three weeks, with several fresh fuel assemblies being added at each shutdown between cycles. Fuel cycling in the MTR resulted in gains of 50 to 100% in the burnup achieved in individual fuel assemblies. Similar gains could probably be attained in the FIR.

In the FIR a refueling schedule in which four spent fuel assemblies are replaced with four fresh assemblies at the end of each cycle may be logical. If no improvement in burnup per assembly were realized a three-week cycle would result, based on the 90-day life of a fresh core. However, a cycle of greater than three weeks would probably result. The four fresh assemblies could be added at the periphery of the core and moved inward during succeeding cycles, spending the last cycle in the center positions. Figure 11.1 shows an appropriate core-numbering system in which the control rod cross is oriented NE-SW and the core quadrants are oriented N-E-S-W. Thus fresh fuel assemblies would be started in the position 6 of each quadrant and moved successively to the 4, 5, 3, 2, and 1 positions. This particular cycling scheme has the following advantages due to the fuel loading being higher at the edges of the core than it is in the center.

1. The power density is higher in the center core regions than with a uniformly loaded core and therefore the leakage to the indium blanket is increased.
2. The maximum-to-average power distribution is less than in a uniformly loaded core and therefore the permissible power level for a given maximum fuel plate temperature is higher. The core can be operated at higher power levels than a uniformly loaded core.

The above advantages might be better accomplished by some other sequence of cycling the fuel assemblies through the core.

The relative merits of fuel assembly cycling should be investigated thoroughly, at least on a theoretical basis, before the FIR is build, and the effects of cycling should be examined experimentally during the early stages of operation.

12.0 Reactor Hazards Considerations

The potential hazards of a nuclear reactor may be reduced to the point where, under normal operating conditions, the presence and use of a reactor provide no significant hazard to employees or to the general public. However, the hazard potential under other than normal operating conditions must also be tolerable under the most unusual, though credible, circumstances.

The Food Irradiation Reactor, which has an aluminum-clad-fuel-plate core moderated and forced-circulation cooled by light water, is similar in these respects to the Low Intensity Training Reactor at the Oak Ridge National Laboratory and the Materials Testing Reactor at NRTS, both of which have several years of safe operation to their credit. The BORAX-1 and SPERT-1 reactors also with cores of the same type fuel assembly, have been tested at Arco over a large range of deliberately imposed severe conditions. The experimental establishment of reactor behavior with large values of excess reactivity allows design to proceed with much more confidence regarding the achievement of an always tolerable safety condition. Although the BORAX and SPERT data do not insure that the proposed reactor is absolutely safe, the information has made possible more certain evaluation of the inherent hazards.

Since this reactor will operate on a QMC site which employs many persons, and in close proximity to associated food handling and processing facilities, the design criteria emphasize safety. This is reflected in the gas tight steel containment shell of the reactor building, which will be capable of withstanding the shock wave generated by the maximum anticipated nuclear incident. This containment shell also provides an emergency secondary container for the primary water should the primary loop rupture at any point.

12.1 Normal Operational Hazards

In normal operation of the facility and especially the operation of the gamma irradiation facility, one must live with radiation levels which are potentially lethal and always dangerous. Interlocks, detecting devices, and barriers will be designed to prevent anyone from inadvertently entering the highly radioactive areas during operation or until such areas are declared safe by health physics personnel. Also, various safety circuits are necessary for corrective action to counter abnormal conditions in the indium system and reactor cooling systems.

12.1.1 Hazards Associated with Operation of Gamma Radiators

The principal hazard associated with use of the radiation facilities appears to be the accidental entry of personnel

into a radiator cubicle during operation, or accidental operation of a radiator when personnel are inside the associated cubicle. During operation it would be difficult to find a location inside the slab radiator, for example, where the radiation level was less than 5×10^5 r/hr, and a person so trapped would receive a lethal dose of radiation in 3 to 5 seconds.

It should be noted that scrambling the reactor the instant a person accidentally enters the gamma radiator will not produce any rapid reduction of the radiation level in the gamma radiators.

12.1.2 Leaks in the Primary or Auxiliary Cooling Systems

To minimize the possibility of leaks, both the primary and auxiliary cooling systems will use welded joints wherever possible. Before the facility goes critical the system will be thoroughly checked, inspected, and tested, and all irregularities corrected.

Leaks which develop as a result of continued operation, such as leaks at pump glands, control rod seals, or at valve stems, will create no radiation hazard since areas where these leaks can occur will normally be uninhabited, and a radiation check will be made before entrance to these areas is gained.

The equilibrium activity of the water leaving the core will be about 100 microcuries per cc due to O^{19} and N^{16} activity. Other activities are kept low since the water is continuously repurified by a side stream demineralizer. Thus in the event small leaks occur they can be repaired within a short time after detection and reactor shutdown. Sumps will be installed at locations such as pump glands and rod drive seals where some leakage may be desirable. Such leakage will be collected and sent to warm drains.

Seal water leakage around rod drives and pump shafts of the primary water system is ordinarily replaced by the makeup pump. If this leakage exceeds the pumping rate the pressurizer will make up the difference temporarily, but subsequently the pressure in the primary loop will fall to a level which rings an alarm in the control room. If the difficulty is not corrected, the system pressure will fall to a point where localized boiling will occur in the fuel channels. As long as the primary coolant flow is maintained, steam blanketing of fuel elements is improbable and most likely impossible.

12.1.3 Leaks in Indium Solution System

Indium sulfate leaks in the gamma radiator area do not create

an appreciable hazard since the only possible volatile material which could be released would be SO_2 , O_2 , or N_2 . Because of their short half-lives, O_2 and N_2 constitute little hazard. The SO_2 content of the irradiator space is some 100 times occupational tolerance or 3.4×10^{-4} microcuries/cc if one-half of all the fluid in the loop leaks into the cell, provided that one part in ten thousand of the sulfur in the loop is present as SO_2 . This fraction is a function of the resistance of the solution to radiation damage and of the off-gas system capacity. There is little doubt that the final detailed design of the indium loop system can be such that hazards due to release of volatile activity are reduced to tolerable levels.

All gamma radiator and indium sulfate pumping areas will be lined with stainless steel or other liner material to prevent radioactive solution from penetrating the concrete in case of a leak.

A leak from the indium solution blanket will result in serious contamination of the pool water and an increase in the radiation levels in the reactor building and the surrounding area. About 6×10^6 curies of In^{116} (54 min.) could be released into the pool along with comparable amounts of In^{116} (13 sec.) activity, although the 13-second activity does not emit a gamma on decay. Such a release, if it were uniformly mixed with the pool water, would result in radiation levels at the surface of the pool of about 50000 r/hr. Radiation levels in the control room and other areas outside the reactor building would be much less than 50000 r/hr due to the biological shielding surrounding the pool and the distance from the pool. Leakage of indium solution from the blanket would be quickly detected by the conductivity alarms in the auxiliary cooling system.

12.1.4 Electrical Power Failure

The system is designed to prevent any hazard in the case of a power failure. The instrumentation and control circuits can and will be designed to fail safe, e.g., by deenergizing the magnetic clutches of the shim safety rod drives causing insertion of the shim safety rods into the core. From instant of power failure to the point that the safetys are in full down position should require less than 0.75 seconds.

The flow of coolant in the primary loop is upward through the core and the heat exchanger is so located that natural convection will dissipate considerable heat to the coolant on the secondary side of the heat exchanger even though no coolant is flowing over the cooling tower. Fuel element

surface temperatures may reach 300°F after reactor scram for the natural circulation cases. Although natural circulation can cool the core adequately, a gasoline driven pump is provided to maintain circulation in the primary loop in case of power failure.

If power failure is of such duration that the primary loop temperature reaches 200°F, the transfer of heat through the aluminum pressure vessel to the relatively cool reactor pool will remove about 50 kw of heat, which is more than that released into the primary coolant stream by the decay heat from the core.

Natural convection of water through the auxiliary cooling system as well as in the water annuli at the surfaces of the blanket and thermal shield would be sufficient to remove shutdown heat generated in these components, thereby preventing their damage in event of power failure.

Likewise since electrical outage results in reactor shutdown, the heat from decay in the indium solution will fall off approximately a factor of two every 54 minutes. Emergency cooling of the indium sulfate solution in the radiators does not present any problem. This is discussed in Section 4.4.2.

12.1.5 Failure of Control Equipment

The possibility of failure of one or more components of the control system cannot be overlooked. Two features of the reactor, however, mitigate against any serious damage due to this failure.

1. The reactor possesses the inherent self regulating properties of water lattice systems.
2. Control rod and shim-safety rod withdrawal rates are limited by control interlock circuits so that reactivity insertion rates are always safe.

Failure of the automatic power control system during operation at power has no hazard associated with it. Should such a failure result in control rod withdrawal, the reactor power can rise slowly. This rise may either be stopped by a power level safety scram, by outlet coolant temperature scram, or by the operator taking corrective action.

A more severe potential hazard is the startup accident. This hazard is minimized in the food irradiation reactor by control interlock circuits which limit net rod withdrawal rates in the sub-power regions so that the power range is reached before the reactor can become prompt critical. Since

the neutron lifetime is about 38 microseconds, reactivity should not be added at a rate greater than about 0.02% per second to maintain absolute safety during startup.

Rod withdrawal is prevented by interlock unless the fission counter channel is recording a certain minimum signal.

In order for equipment failure to be serious, the fission counter circuit must fail in such a manner that the interlock signal is maintained. Simultaneously the compensated ion chamber circuit must fail in order for the reactor power to increase faster than allowed by the period meter. Following this it must be postulated that the safety channels fail. In the event all these improbable failures occurred in the proper sequence, boiling in the core would be expected to quickly offset excess reactivity until the situation could be brought under control by the operator. Based on experimental results at the LITR, over 1-1/2 minutes (for addition of 1.8% ΔK) is available for operator action before severe power pulsing begins. SPERT tests, however, are expected to give more applicable information in regard to pressurized systems.

12.1.6 Mechanical Equipment Failure

The pumps and motors on the primary and auxiliary cooling systems comprise the major mechanical equipment subject to failure. Failure of these components will initiate immediate shutdown of the reactor, and as previously indicated, natural circulation is sufficient to dissipate after-heat from the core. Thirty seconds after scram the heat generated in the core of the Food Irradiation Reactor will be about 3% of full power heat and the heat generated in the blanket will be about 30 kw. Both the core heat and the indium blanket heat can be dissipated by natural convection.

Failure of the pumps in the secondary (cooling tower) cooling system would not create any hazard. The flow in this system is tied to an alarm in the control room, but if the operator failed to shut down the reactor, the high temperature of the primary water would initiate reactor shutdown.

If the pressurizer makeup pump fails, the check valve between it and the primary water loop will close. Normal leakage from the primary loop would then result in lowering the pressurizer water level until the heaters cut off. Further leakage would cause the primary system pressure to drop. The loss of 25 gallons of water will drop the pressure in the system by about 10 psi assuming no heat input into the

pressurizer. Failure of the pressurizer makeup pump will sound an alarm in the control room so that the operator can take appropriate action.

12.1.7 Reloading Accidents

At 10 MW power the reactor would be expected to operate continuously for about three months. However, as pointed out in Section 11.2 loading only part of the core with new assemblies has advantages and such procedure would result in shutdown at more frequent intervals, say two to four weeks. Changing fuel elements will require removal of the top reactor plug. The fuel can then be removed or rearranged by long handled grappling tools through a water shield some 16 feet deep. Spent fuel assembly storage is provided in the reactor pool. The spent fuel assemblies are loaded into a shipping coffin in the pool and removed from the pool by means of the crane, and transferred to a truck. Since all operations involved in the handling of individual fuel assemblies are carried out under sufficient water, in this regard there is no hazard connected with unloading the reactor fuel.

12.2 Reactor Excursions

In general the size and nature of a reactor excursion depends on the rate and amount of reactivity which is added to the core. Small slow changes in reactivity result in small amplitude easily controlled power fluctuations. Rapid addition of reactivity of large magnitude results in progressively larger and faster power fluctuations, approaching as a limit the violent nuclear excursions such as were made to occur with BORAX-I.

Aside from changes in reactivity due to xenon poisoning temperature changes and fuel burnout, the food irradiation reactor is subject to changes in reactivity and other nuclear characteristics due to perturbations in the indium blanket.

12.2.1 Perturbations Due to Indium Blanket

The reference reactor described in Chapter 6.0 has a 6-inch thick blanket which contains 400 grams of indium sulfate per liter. This reference reactor used for the physics calculations differs slightly from the conceptual design proposed in this report but is satisfactory for the considerations below. Perturbations in this blanket result in changes in the reactor characteristics as indicated in Table 12.2.1.

Table 12.2.1 Neutron Lifetimes and Void Coefficients for
Reactor with 400 gms/liter Indium Sulfate
Solution in 6 inch Blanket

Reactor*	K_{eff}	Neutron Lifetime Micro- seconds	Steam Void Coeff.in Core	$\Delta K/K$ per unit void
			$\frac{\Delta K/K}{\Delta v/V_m}$	$\frac{\Delta K/K}{\Delta v}$
				$10^{-6}/cm^3$
Reference 6" blanket 400 gms/liter indium sulfate	1.00	38	-.522	-8.63
Pure H ₂ O in 77°C in blanket	1.032	44	-.399	-6.59
Void in blanket	.97	25	-.571	-9.44

*all data were calculated for the hot, poisoned, burned out core

Replacing the indium sulfate in the core by pure water increases the reactivity by only 3.2% because:

1. Water is of value as a reflector mainly because of its fast neutron properties. Due to its thermal cross section it is a poor thermal neutron reflector.
2. The core is, in effect, partially shielded from the blanket by the steel and water layers separating the core and blanket, and neutrons slowed down in the blanket have a small chance of reaching the core.
3. The reflected thermal leakage is small compared to the reflected fast leakage. Reducing the indium concentration in the blanket does not significantly increase the reflected fast leakage.

The indium loop contains about 160 gallons of indium sulfate. It is conceivable that a break in this loop could result in the sulfate being replaced by pure water. Since the blanket surrounding the core contains about 40 gallons and the indium heat exchanger pump has a capacity of no more than 150 gpm, the blanket could be replaced by water in about

15 seconds or reactivity could be added at a rate of 0.213% per second.

The reactor kinetic equations have been investigated by S. Wallach* and M. A. Shultz** and the solution of these equations for a ramp input is shown in equation (1).

$$\frac{n(t)}{n(o)} = \sigma \int_0^{\infty} e^{x \left(\frac{\delta K - \beta}{\gamma} - \frac{x^2 l^*}{2 \gamma} \right)} (x - \lambda)^{\beta \lambda / \gamma} dx \quad (1)$$

where

n = neutron density (n/cm^3)

$\delta K = \alpha + \gamma t = 0.001 + .0021 t$

β = delayed neutron fraction 7×10^{-3}

l^* = lifetime of neutron 38×10^{-6} sec

λ = average delayed neutron decay constant 0.10 sec^{-1}

x = variable of integration sec^{-1}

σ = normalization factor which makes $n(t)/n(o) = 1$ when $t = 0$

The solution of this equation shows that 1 second after the blanket begins to be replaced by water the neutron level is 1.5 times its original level. The power level scram would release the safety rods prior to this point and the rods would have fallen to the point where K_{eff} was below 1.0.

If the power level scram failed, the period scram would act to shut down the reactor. If all safety systems failed, the power level would reach about 80 times its original level 3.04 seconds after the blanket began to be replaced by pure water. Prior to this level, of course, boiling would take place in the core and the steam coefficient should act to keep the power within safe levels.

*Wallach, S., "Solutions of the Pile Kinetic Equations When the Reactivity is a Linear Function of Time", WAPD-13, November 30, 1950.

**Shultz, M. A., "Control of Nuclear Reactors and Power Plants", McGraw Hill, 1955

If the indium loop should fail in such a way that the indium sulfate were replaced by air, Keff would decrease and the reactor would shut itself off. Although no extensive examination has been made, it does not appear that a void in the blanket would change the gamma and neutron heating in the blanket structure, the reactor vessel, or the thermal shield sufficiently to cause damage.

12.2.2 Boiling in the Reactor Core

If small increases in reactivity are added to the food irradiation core and if the servo control, the power level and period scrams fail simultaneously, the reactor power and coolant temperature will rise until localized boiling will occur at the hottest fuel plates. A detailed analysis of this event should await data from the proposed SPERT tests where it is planned to conduct tests to observe what happens under these and more severe circumstances. Since the saturation temperature ($\sim 360^{\circ}\text{F}$) corresponding to the pressure in the system is considerably above the temperature ($\sim 190^{\circ}\text{F}$) of the bulk coolant, the vapor produced by boiling can be expected to collapse in the bulk coolant soon after it was formed at the surface of the fuel plate. However this vapor would tend to reduce the reactivity of the system as indicated by the steam coefficient of reactivity in Table 12.2.1. Since the flow of coolant is up through the core, the chance of reduced flow through a given fuel channel due to localized boiling is less than if the flow were downward.

12.3 Fission Products Released Into Primary Loop

If one postulates the release of all the fission products in the core (about 60 megacuries after extended operation at 10 MW reactor power) then the activity level would be about 3400 curies per liter. This would result in radiation levels of several ~~thousand~~ r/hr on the floor above the heat exchangers and near the truck entrance air lock outside the reactor building. Doses in the control rooms and other areas around the reactor building will depend on their placement with relation to the reactor building. In case of such an accident, prudence would require that everyone in the area be evacuated as soon as possible. However, the reactor building and primary loop are so constructed that all the activity would remain in the reactor building.

A much more logical assumption is that a small part of the fission products in one or a few fuel plates would be distributed through the primary loop before corrective action could be taken.

12.3.1 Breaks in Fuel Plate Cladding

Prior experience with fuel elements of almost identical design has shown that breaks in the cladding or corrosion can expose the meat of the core and release small amounts of fission products into the primary water system. In the absence of a simultaneous break in the primary loop it is not possible for these fission products to migrate out of the primary loop. Upon detection of fission product activity in the primary loop, the reactor and the primary coolant system pumps would be shut down. It would then be possible for the maintenance and operating crew to locate and remove the defective fuel element and initiate cleanup operations.

If, for example, all the activity in three fuel plates were released into the primary coolant system, about 10^6 curies could be released into the 4,700 gallon primary system. About $1/3$ of the primary coolant is contained in the heat exchangers and pumps located in the equipment room. Personnel will be prohibited from entering the primary heat exchanger room during normal operation. A person standing on the floor above the heat exchanger room might be subjected to a dose rate of about 10 r/hr from the source described above. In such an improbable event it seems entirely reasonable to evacuate all personnel from the reactor building and from the control room within five minutes. The total integrated dose to any individual would then be less than 1 r.

A much more realistic assumption is that a fission break could be detected, the reactor shut down, and corrective action taken while the major portion of the activity remained within the fuel element. Certainly this is the experience to date of the reactors using this type fuel element. In this event only a few curies or tens of curies would be released, and little, if any, radiation hazard would be created.

12.4 Fission Products Released Within Gas Tight Reactor Building

In this discussion it is assumed that certain portions of the activity in the core are somehow distributed throughout the volume of the reactor building. No mechanism for initiating this distribution is postulated.

As one estimate, it is assumed that one-half the fission products are released into the reactor building following extended operation of the reactor at 10 MW. Under such conditions the activity in the building will average 25 curies per liter.

All personnel within the building would receive lethal doses of radiation within 10 seconds. Assuming that the individual has the equivalent of six inches of concrete between himself and the activity within the building, he might receive 200 r during the first hour following the accident if he remained at a distance of 200 feet from the reactor building. Initial dose rates in the control rooms and operating areas adjacent to the reactor building would be on the order of 50,000 r/hr, although this dose rate will depend strongly on the location of these areas with respect to the reactor building. Clearly, such an accident would require evacuation of a large area and many persons within this area might receive dangerous doses of radiation before such evacuation could be completed.

If the more logical assumption is made that only the volatile fission products are released within the building, the initial dose rates would be less than half of those described above.

To prevent any significant activity from entering the atmosphere, radiation monitors will be provided within the reactor building. These monitors will cause dampers in the ventilating system to close on a radiation signal.

12.5 Break in Primary Loop

The primary loop is so designed that no matter where it should break the reactor core cannot be uncovered unless a simultaneous break occurs which drains the pool water. No break will result in an increase in reactivity unless the indium loop is simultaneously ruptured. The bulk water temperature in the primary loop does not rise above 212°F so no flashing will result if the pressure is suddenly decreased. However, localized boiling can and probably will occur in some fuel channels before the reactor power could be reduced to prevent this. Detailed analysis of fluid flow, heat production, and fuel plate burnout with respect to rupture of the primary loop should be made.

12.6 Metal-Water Reaction

By far the greatest possible non-nuclear energy release within the reactor complex would be a reaction between the aluminum in the fuel plates and the water moderator. The reaction of all the aluminum in the fuel with water could evaporate 3000 to 4000 pounds of water and raise the equilibrium pressure within the building to 20 to 30 psi. Of course shock waves of many times this intensity could be produced. However, any energy which went into heating the pool water would reduce the equilibrium pressure as well as the intensity of the shock waves.

The maximum possible energy released by the reaction of the aluminum on the fuel plates and the water in the primary loop would exceed by many fold the energy released in the final excursion of BORAX-I. However, the essentially instantaneous addition of the excessive amount of reactivity necessary to initiate such an explosion, is impossible in the FIR.

12.7 Earthquake

A sufficiently intense earthquake could damage the reactor and irradiator facilities. Unless these facilities were built directly across the fault involved, it is difficult to imagine how the quake could create any appreciable hazard.

12.8 Safeguard Problems Requiring Further Study

1. Meteorological study of site selected for food irradiation reactor.
2. Hydrological and seismological survey of site selected for food irradiation reactor.
3. More detailed analysis of effect on K_{eff} of adding last few fuel elements to the core.

In order to maximize the leakage of neutrons into the indium blanket where they can activate indium for subsequent use in the irradiators, the uranium to moderator ratio is kept high. It is conceivable under such conditions that the addition of the last fuel element will so increase the uranium to water ratio that the reactor actually becomes less critical. Vice versa, the removal of a single element may actually increase K_{eff} . The magnitude and size of this effect need careful investigation.

4. Detailed analysis of rod worth as a function of blanket perturbations.

Because the food irradiation reactor is designed for high leakage, the control rods increase or decrease K_{eff} as much by changing the flux pattern and the leakage as by introducing poison into the core. Thus a rod which has a certain value when the core is surrounded by the indium sulfate blanket may have a much smaller value when the indium sulfate blanket is replaced by water or void.

5. Detailed analysis of fluid flow and heat fluxes under varying emergency conditions.

Although the design of the food irradiation reactor is such that preliminary calculations and comparisons with similar

type reactors leads to the tentative conclusion that emergency conditions in the primary loop can be handled safely, each of the possible accidents needs more detailed study to properly assess all the factors such as pressure drop, heat transfer, boiling, and other characteristics of the final system design. This can be done properly only when the system design is essentially complete.

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Appendix 1.0 Performance Specifications

The following performance specifications for the Food Irradiation Reactor were furnished by the A.E.C.

- A. Items to be exposed in the irradiation facility will require doses in the range from 10^4 to 10^7 rep. Doses required for foods will be 6×10^6 rep or less, with most items being given 2×10^6 rep or less.
- B. Maximum dose rate, in at least one irradiation chamber, shall exceed 5×10^6 rep/hour in order to minimize the size of the radiation volume and the time of exposure.
- C. The source should be capable of delivering a dose of 2×10^6 rep to 1,000 tons of food or other substances of unit density per month, with operation on a continual basis. However, it must be assumed that there will be frequent shutdowns to permit modifications in food processing lines, thus reducing the actual output of the plant. A production rate of three ton-megarep per hour is, therefore, considered acceptable.
- D. The radiation chambers should be designed to provide for:
 - 1. Items of unit density from sizes of No. 2 cans ($3 \frac{7}{16}$ " x $4 \frac{9}{16}$ ") up to V3 boxes (6" x 16" x 30").
 - 2. Sacks of flour, potatoes, etc.
 - 3. A conveyor system to permit treatment of whole carcasses, military equipment, etc.
- E. Temperature of radiation chambers should be controlled within 5° over a range of -20 to $+75^\circ\text{C}$.
- F. Dosage delivered may vary between the limits of 100 to 125% of the required amount. This is an outside figure with a maximum variation in dose being from 100 to 110% if this can be obtained.
- G. Neutron radiation must be minimized to eliminate induced activity. Experience at the MTR indicates that a total dose of 1.8×10^7 neutrons/cm² is tolerable.

Appendix 2.0 Nuclear Calculation Data

Inasmuch as the interpretation of the results of nuclear studies depends on an understanding of the assumptions employed and the values of the constants used to obtain the results, this section is a presentation of the derivations or sources of the more important nuclear data. Included in this section are brief explanations pertaining to the methods of computation and interpretations of the results in the light of the specific methods which are used.

A2.1 Discussion of the Two Group, Multiregion Methods

The Two Group Equations and the Solution Method

The familiar two neutron group flux equations, according to diffusion theory, may be reduced to a one-dimensional set for a cylindrical reactor composed of a number of radial regions if the solutions for the axial flux distributions are assumed to be independent of the radial variable. (The radial flux distribution is the one of major interest because the blanket absorptions depend primarily upon the radial leakage of neutrons from the core.) The assumption is made that each radial region in the reactor is the same height and that this height is equal to the core height, H , plus twice the axial reflector savings value, δ_h , used for the core. In diffusion theory, it follows from this assumption that the axial flux distribution in each region of equivalent height, $H + 2\delta_h$, is a cosine such that the axial neutron flux, ϕ_h , is given by

$$\phi_h = C \cos \pi \left(\frac{z}{H + 2\delta_h} \right), \text{ where } C \text{ is a constant of proportionality and } z \text{ is the axial distance from the center of the reactor.}$$

The two group equations, reduced in this manner to a function of only the radial variable, r , may be written (for any region of the reactor) in the form:

$$(1) \quad D_1 \nabla^2 \phi_f - D_1 B_h^2 \phi_f - \Sigma_1 \phi_f + \nu_c \Sigma_f \phi_s = 0, \text{ and}$$

$$(2) \quad D_2 \nabla^2 \phi_s - D_2 B_h^2 \phi_s - \Sigma_a \phi_s + \Sigma_1 \phi_f = 0, \text{ where}$$

ν_c = the number of neutrons per fission needed for exact criticality of the entire reactor,

ϕ_f = the fast neutron flux as a function of radius, r ,

ϕ_s = the thermal neutron flux as a function of radius, r ,

D_1 = the fast neutron diffusion constant,

D_2 = the thermal neutron diffusion constant,

Σ_1 = the slowing down cross section for the fast neutrons (it is assumed that there is no actual fast neutron absorption),

Σ_a = the thermal absorption cross section,

Σ_f = the fission cross section, and

$B_h^2 = \frac{2}{(H + 2\delta_h)^2}$. (It is also assumed that there are no fissions caused by fast neutrons.)

It is expedient to define:

$$\Sigma_{1R} = \Sigma_1 + D_1 B_h^2 \text{ and } \Sigma_{2R} = \Sigma_a + D_2 B_h^2.$$

In order to obtain the radial flux distribution throughout all the reactor regions, the method employed to obtain the results of this report involves the following assumptions and procedures:

- The constants to which equations (1) and (2) refer are derived for each of the respective reactor regions. (The detailed derivations are presented in the following sections.) If Σ_f equals zero for a region, the last term in equation (1) is, of course, nonexistent.
- Equations (1) and (2) are written in finite difference form in order to facilitate numerical solution of the equations in each region.
- The flux solutions in each region are coupled to those of adjacent regions by means of the boundary conditions,

$$D\nabla\phi \Big|_{\text{right}} = D\nabla\phi \Big|_{\text{left}}, \text{ and } \phi \Big|_{\text{right}} = \phi \Big|_{\text{left}}$$

for both the fast and thermal neutron fluxes. At the reactor center, $D\nabla\phi = 0$ is applied and $\phi = 0$ at the outer boundary of the outermost region for both the fast and thermal neutron fluxes.

- Each region of the reactor is divided into a number of annular subregions of equal thickness. The number of these intervals into which a region is subdivided is referred to as $n = \Delta R / \Delta r$ where ΔR is the region thickness and Δr is the subregion thickness in a region. The number of

intervals, n , for each region is selected so that the numerical solutions may reliably show expected flux variations.

- e. A guessed value of $\nu_c \sum_f \phi_s$ is used as a first approximation of the source in equation (1) at each reactor subregion boundary for which a value of $\sum_f$ exists.
- f. The set of difference equations, subject to the boundary conditions, is solved for the assumed source using a relatively high speed electronic computer (in this case, an IBM 650). The solutions are obtained in the form of flux values corresponding to each reactor subregion boundary.
- g. In general, the source guess is not a correct one. (If it were, the thermal flux solution would be proportional, at each point, to the assumed source.) Hence, a better source guess is obtained in the following manner:

Find $\int_{R_f} \sum_f \phi_s \, r dr$, where ϕ_s is the thermal neutron flux

solution, and the integral is effectively over all reactor space. (It is, of course, null in regions where $\sum_f = 0$, so that the region of integration, R_f , may pertain to only those regions containing fissionable material.)

Divide $\int_{R_f} (\text{Source guess}) \, r dr$ by $\int_{R_f} \sum_f \phi_s \, r dr$ to obtain

the number of neutrons needed per fission for criticality, ν_c . Multiply each value of ϕ_s in each region containing fissionable material by $\nu_c \sum_f$. This provides a new source distribution normalized such that there is a volumetric average of one fission neutron produced per cm^3 of fissionable region volume in a reactor slab disc 1 cm thick.

That is $\int_{R_f} (\text{Source}) \, r dr = \int_{R_f} r dr$

- h. New reactor flux distributions are obtained for successively better estimates of the source distribution until ν_c no longer differs from one case to the next. At this point, the ν_c represents the final solution for the number of neutrons needed per fission for criticality of the multi-region reactor and the flux distributions are the desired solutions of the set of difference equations.

The neutron multiplication constant, K , of the reactor is found by the simple relationship $K = 2.46 / \nu_c$. (The actual ν of U235 is equal to 2.46, see BNL-325.)

Interpretation of the Flux Distributions of Figures A2.A Through A2.NN

Figures A2.A through A2.NN are the flux distributions obtained for various reactor cases discussed in the main body of the report in Chapter 6, Nuclear Considerations.

The degree of convergence in the flux distributions and the value of ν_c to which reference is made in step h, above, is compromised with machine solution time in a practical case. For a given flux distribution the degree of convergence is carried beyond the point at which two successive values of ν_c differ by 0.1%.

From this degree of convergence no significant flux variation takes place for successive iterations. Values of the neutron multiplication constant for each of the flux distributions of Figures A2.A through A2.NN are given at various points in Chapter 6, so that the value of ν_c for a given set of flux distributions (the fast and thermal fluxes) may be obtained from the fact that $\nu_c = 2.46/K$.

It is of significance that the source is volumetrically normalized, for it follows that the resultant scale (for the flux distributions directly obtained as solutions) is such that the ratio of the maximum thermal flux to the average is simply $\nu_c \Sigma_f (\phi_s)_{\max}$ because $(\phi_s)_{\text{av}} = \frac{\int_{R_f} \phi_s r dr}{\int_{R_f} r dr}$

$= 1/\nu_c \Sigma_f$. It also follows that the flux scale gives a radially averaged value for the number of fissions per cm^3 (per cm of reactor height) of simply $1/\nu_c$. Hence, the maximum flux in a reactor is given by

$$(3) \quad \phi_{\max} = \frac{CP}{VR_h} \nu_c (\phi_s)_{\max} \frac{\text{neutrons}}{\text{sec.cm}^2}, \text{ where}$$

P = reactor power,

C = fissions/sec per unit power,

V = reactor core volume in cm^3 ,

R_h = the ratio of the maximum axial thermal flux to the average, and

$(\phi_s)_{\max}$ = the maximum thermal core flux according to the scale of Figures A2.A through A2.NN.

In the figures, the fluxes are termed "midplane" fluxes to add emphasis to the fact that, whereas these radial flux solutions may represent good approximations far from the axial boundary, differences in the compositions of the axial reflectors of the various regions, among other factors, may easily cause relatively large perturbations in the radial flux distributions near the axial boundaries.

Interpretation of the Neutron Balances, Tables A2.1a Through A2.1j

A neutron balance is useful in presenting the results of a two group calculation in a manner well suited to the purposes of blanket efficiency analyses. The neutron balance shows the relative numbers of neutrons lost to the three possible causes of loss (fast leakage, slow leakage, and thermal absorption) in each of the reactor regions.

Fast neutrons are removed from the fast group by virtue of Σ_{1R} , which cross section is the sum of the slowing down cross section, Σ_1 , and the effective axial fast neutron leakage cross section, $D_1 B_h^2$. Hence the number of neutrons slowed down from the fast group to the thermal in the i th region per U^{235} absorption, defined as A_{11} is given by

$$A_{11} = \frac{I_{11} \Sigma_{11}}{I_{12} \Sigma_{a25}}, \text{ where } I_{11} = \int_{R_1} \phi_f r dr,$$

$$I_{12} = \int_{R_f} \phi_s r dr \text{ (the fissioning region is region 1), and}$$

Σ_{a25} is the U^{235} absorption cross section.

In the i th region the loss of fast neutrons to axial leakage per U^{235} absorption is similarly obtained as

$$L_{11} = \frac{I_{11} D_{11} B_h^2}{I_{12} \Sigma_{a25}}$$

The number of neutrons lost to thermal absorption per U^{235} absorption is, for the i th region,

$$A_{12} = \frac{I_{12} \Sigma_{1a}}{I_{12} \Sigma_{a25}}, \text{ where } I_{12} = \int_{R_1} \phi_s r dr.$$

$$L_{12} = \frac{I_{12} D_{12} B_h^2}{I_{12} \Sigma_{a25}} \quad \text{is the loss of thermal neutrons to axial}$$

leakage per U^{235} absorption. Finally if a region is the outermost radial region it is subject to loss of neutrons by means of radial leakage. In all of the cases studied there is very little radial leakage because the neutron flux is subject to such great attenuation in the regions surrounding the core. In these cases, it is sufficient to approximate the fast radial leakage per U^{235} absorption and the thermal neutron leakage per U^{235} absorption by the first order expressions,

$$L_{1R} = \frac{-D_{m1}}{I_{12} \Sigma_{a25}} \frac{\Delta \phi_f}{\Delta r_m} \quad \text{and} \quad L_{2R} = \frac{-D_m^2}{I_{12} \Sigma_{a25}} \frac{\Delta \phi_s}{\Delta r_m},$$

respectively. In these expressions "m" denotes the final region ($1 \leq i \leq m$), $\Delta \phi_f$ is the difference between the last two flux points in the m^{th} region and $\Delta \phi_s$ is the comparable thermal flux difference. Δr_m is the subregion thickness (or the interval width) which is used in the m^{th} region.

It follows from these definitions that if the number of neutrons produced per U^{235} absorption is equal to ν_c ,

($\eta_c = \nu_c / (1 + \alpha)$ where α is the capture to fission ratio for U^{235}), then

$$(10) \quad \eta_c = \left[\sum_{i=1}^m (A_{i1} + L_{i1}) \right] + L_{1R},$$

$$(11) \quad \eta_c = \left[\sum_{i=1}^m (A_{i2} + L_{i2}) \right] + L_{2R}, \quad \text{and}$$

$$(12) \quad \left[\sum_{i=1}^m (A_{i2} + L_{i2}) \right] + L_{2R} = \left[\sum_{i=1}^m (A_{i1}) \right]. \quad \text{These}$$

relationships are easily demonstrated in fact by the results reported in Tables A2.1a through A2.1j.

Since the multiplication constant is given by $K = \frac{\eta_c(1+\alpha)}{2.46}$,

the multiplication constant (for the degree of convergence represented by the machine results from which the data of the neutron balances were directly taken) is easily obtained from the neutron balance numbers. (This value of K is slightly difference, for reasons stated above, from the converged K values given in Chapter 6.0 for the respective reactor cases.)

Table A2.1a Neutron Balance, Reactor Conditions: $H = 3 \text{ ft}$, $d = 3.0 \text{ inches}$
 $\gamma = 0.7$, $M_0 = 10.044 \text{ Kg}$

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.37406	0.00261	0.04160	0.00076	0.47895	0.00172	0.02621	1.92591
Axial fast neutron leakage, L_{11}	0.07436	0.04886	0.00650	0.00057	0.01518	0.00128	0.00076*	0.14751
Axial slow neutron leakage, L_{12}	0.00324	0.01127	0.00044	0.00004	0.00034	0.00003	0.00014*	0.01550
Slow neutron absorption, A_{12}	1.32204	0.01574	0.01172	0.02750	0.49934	0.01634	0.01773	1.91041
$\eta_c = 2.07342$								

Table A2.1b Neutron Balance, Reactor Conditions: $H = 3.0 \text{ ft}$, $d = 3.0 \text{ inches}$
 $\gamma = 0.6$, $M_0 = 8.834 \text{ Kg}$

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.41855	0.00250	0.03974	0.00073	0.45748	0.00162	0.02504	1.94566
Axial fast neutron leakage, L_{11}	0.07107	0.04667	0.00621	0.00054	0.01450	0.00120	0.00073*	0.14092
Axial slow neutron leakage, L_{12}	0.00348	0.01155	0.00046	0.00004	0.00033	0.00002	0.00014*	0.01602
Slow neutron absorption, A_{12}	1.35848	0.01613	0.01213	0.02802	0.48235	0.01560	0.01693	1.92964
$\eta_c = 2.08658$								

* Includes radial leakage from Region 7

Table A2.1c. Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 3.0$ inches
 $\gamma = 0.5$, $M_0 = 8.054$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.45692	0.00236	0.03755	0.00052	0.43231	0.00155	0.02353	1.95474
Axial fast neutron leakage, L_{11}	0.06722	0.04410	0.00587	0.00039	0.01371	0.00116	0.00068*	0.13313
Axial slow neutron leakage, L_{12}	0.00359	0.01215	0.00046	0.00004	0.00031	0.00002	0.00013*	0.01670
Slow neutron absorption, A_{12}	1.38963	0.01697	0.01227	0.02799	0.46044	0.01475	0.01599	1.93804
$\eta_c = 2.08787$								

Table A2.1d. Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 2.5$ inches
(Reference Reactor) $\gamma = 0.7$, $M_0 = 12.438$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.25136	0.00312	0.05036	0.00092	0.58816	0.00216	0.03302	1.92898
Axial fast neutron leakage, L_{11}	0.06771	0.05832	0.00787	0.00069	0.01864	0.00160	0.00096*	0.15579
Axial slow neutron leakage, L_{12}	0.00186	0.01009	0.00043	0.00004	0.00041	0.00003	0.00018*	0.01304
Slow neutron absorption, A_{12}	1.22358	0.01408	0.01128	0.02798	0.59621	0.02046	0.02235	1.91594
$\eta_c = 2.08477$								

* Includes radial neutron leakage from Region 7

Table A2.1e Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 2.5$ inches
 $\gamma = 0.6$, $M_0 = 10.525$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.29057	0.00297	0.04800	0.00088	0.56106	0.00205	0.03148	1.93701
Axial fast neutron leakage, L_{11}	0.06462	0.05558	0.00750	0.00066	0.01779	0.00153	0.00091*	0.14859
Axial slow neutron leakage, L_{12}	0.00208	0.01083	0.00044	0.00004	0.00039	0.00003	0.00017*	0.01398
Slow neutron absorption, A_{12}	1.25177	0.01512	0.01180	0.02863	0.57488	0.01951	0.02132	1.92303
$\eta_c = 2.08560$								

Table A2.1f Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 2.5$ inches
 $\gamma = 0.5$, $M_0 = 9.27$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.32526	0.00281	0.04541	0.00083	0.53077	0.00194	0.02978	1.93680
Axial fast neutron leakage, L_{11}	0.06115	0.05258	0.00710	0.00062	0.01683	0.00144	0.00086*	0.14058
Axial slow neutron leakage, L_{12}	0.00222	0.01132	0.00046	0.00005	0.00037	0.00003	0.00016*	0.01461
Slow neutron absorption, A_{12}	1.27754	0.01580	0.01210	0.02884	0.54927	0.01846	0.02018	1.92219
$\eta_c = 2.07738$								

* Includes radial neutron leakage from Region 7

Table A2.1g Neutron Balance, Reactor Conditions: $H = 2.5 \text{ ft}$, $d = 2.5 \text{ inches}$
 $\gamma = 0.7$, $M_0 = 12.438 \text{ Kg}$

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.22165	0.00300	0.04832	0.00089	0.56048	0.00202	0.03068	1.86740
Axial fast neutron leakage, L_{11}	0.09175	0.07783	0.01048	0.00091	0.02466	0.00209	0.00116*	0.20888
Axial slow neutron leakage, L_{12}	0.00215	0.01204	0.00052	0.00005	0.00053	0.00004	0.00022*	0.01554
Slow neutron absorption, A_{12}	1.20127	0.01211	0.00993	0.02516	0.56318	0.01915	0.02069	1,85150
$\eta_c = 2.07592$								

Table A2.1h Neutron Balance, Reactor Conditions: $H = 2.0 \text{ ft}$, $d = 2.5 \text{ inches}$
 $\gamma = 0.7$, $M_0 = 12.438 \text{ Kg}$

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.19224	0.00285	0.04558	0.00084	0.52381	0.00184	0.02745	1.79471
Axial fast neutron leakage, L_{11}	0.13253	0.10936	0.01467	0.00128	0.03411	0.00281	0.00157*	0.29633
Axial slow neutron leakage, L_{12}	0.00255	0.01463	0.00065	0.00007	0.00073	0.00006	0.00028*	0.01897
Slow neutron absorption, A_{12}	1.17865	0.00995	0.00844	0.02198	0.52097	0.01736	0.01839	1.77574
$\eta_c = 2.09104$								

* Includes radial neutron leakage from Region 7

Table A2.11 Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 2.5$ inches
 (10 cm Be in Region 7, 3 inch
 Blanket)
 $\gamma = 0.7$, $M_0 = 12.438$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 Be	8 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.25050	0.00318	0.05176	0.00096	0.48857	0.00042	0.05575	0.06267	1.91381
Axial fast leakage, L_{11}	0.06767	0.05946	0.00809	0.00071	0.01549	0.00031	0.00463	0.00182*	0.15818
Axial slow leakage, L_{12}	0.00186	0.01030	0.00044	0.00005	0.00035	0.00003	0.00216	0.00057*	0.01576
Slow neutron absorption, A_{12}	1.22358	0.01439	0.01157	0.02885	0.51975	0.01928	0.00578	0.07485	1.89805
								$\eta_c = 2.07199$	

Table A2.1j Neutron Balance, Reactor Conditions: $H = 3.0$ ft, $d = 2.5$ inches
 (20 cm Be in Region 7, 3 inch
 Blanket)
 $\gamma = 0.7$, $M_0 = 12.438$ Kg

Region Neutron Fate	1 Core	2 Al + Air	3 Al + H ₂ O	4 Fe	5 Blanket	6 Fe	7 Be	8 H ₂ O	7 Region Total
Neutrons slowed down, A_{11}	1.25036	0.00318	0.05189	0.00096	0.49392	0.00043	0.08634	0.02375	1.91083
Axial fast leakage, L_{11}	0.06767	0.05956	0.00811	0.00071	0.01566	0.00032	0.00718	0.00069*	0.15990
Axial slow leakage, L_{12}	0.00186	0.01032	0.00044	0.00005	0.00036	0.00003	0.00534	0.00034*	0.01874
Slow neutron absorption, A_{12}	1.22358	0.01441	0.01160	0.02893	0.53120	0.02173	0.01431	0.04633	1.89209
								$\eta_c = 2.07073$	

* Includes radial neutron leakage from Region 8

A2.2 The Development of the Reactor Constants for the Core

Parameter Study

For the study of the effects of changes in the core parameters, a seven region analytical model of the reactor is defined, the central region of which is the core. The core is represented in the analytical model (see Figure 6.A) by a right circular cylinder of volume equal to that of the actual core volume of the design. The core includes, by definition, all the material volume inside the irregularly shaped aluminum which bounds the designed arrangement of the twenty-four fuel assemblies.

The order in which the derivations of the reactor constants are treated in this section corresponds to the order in which the core parameter study proceeds. Hence, the complete set of reactor constants for all regions is developed for an active reactor height of 3.0 feet; then the changed values of certain of the constants are derived for active reactor heights of 2.5 feet and 2.0 feet; and, finally, the constants needed to represent the replacement of the material of the core reflector (region 2) by solid aluminum and by beryllium are derived. In the derivation of the initial set of reactor constants for $H = 3.0$ feet, the reactor core constants are developed for nine cases corresponding to the possible combinations of the chosen values of the two parameters d and χ . (d is the center-to-center spacing of fuel assemblies and χ is the volumetric ratio of core metal to core water.)

A2.2.1 The Reactor Constants for $H = 3.0$ feet

In this section, the two group constants for the core are derived for various values of the parameter d and χ . The complete set of constants needed for the core calculation consists of D_1 , D_2 , Σ_{1R} , Σ_{2R} , Σ_1 , Σ_f , and the core radius, R_1 .

The parameter, d , determines the reactor radius, in that it represents the center-to-center spacing of the fuel assemblies in each reactor quarter. It is assumed that the dimensions of the control rod cross are the same within the core region for each different d . The parameter, χ , is, by definition, the total volume of metal in the core divided by the total volume of water. Specifically, the parametric value of χ is that for the core at 20°C.

The Core Radii and the Volume Ratios

The radius of the core is determined by the sum of the areas of the control rod cross, the fuel assemblies and any excess

water bordering all four reactor quarters. The area of the twenty-four fuel assemblies is simply $(24)d^2$. Around the six assemblies in each reactor quarter is a 0.02 inch layer of excess water. This area is given by

$$4 (0.24d + 0.0016) = (0.96d + 0.0064) \text{ in}^2$$

The control cross area is based on a thickness of 0.71 inches so that the total cross area is given by

$$0.71 [12d + 0.71 + 8(0.02)] = (8.52d + 0.6177) \text{ in}^2. \text{ Hence}$$

the total core area is

$$24d^2 + (0.96d + 0.0064) + (8.52d + 0.6177) = 24d^2 + 9.48d + 0.6241 \text{ in}^2.$$

The equivalent core radius is simply the square root of this area divided by the square root of π . The parametric values chosen for d are 3.0 inches, 2.75 inches, and 2.0 inches.

Table A2.2.1a lists the values of the core areas, equivalent radii and other pertinent data corresponding to the values chosen for d .

Table A2.2.1a Core Dimensional Data

	<u>$d = 3.0$ inches</u>	<u>$d = 2.75$ inches</u>	<u>$d = 2.5$ inches</u>
Core area (in ²)	245.0641	208.1941	174.3241
R_1 (inches)	8.832119	8.140654	7.449100
R_1 (cm)	22.433626	20.677301	18.920750
$R_1^2 / 2$ (cm ²)	251.6338	213.7754	178.9974

The area of the water in the control cross is a function of d . In the control cross shown in INTERNUC-3, Figure 3.A, the area of the control cross water would be $(2.52d - 0.2991) \text{ in}^2$ if the cross thickness of 0.75 inches were reduced to 0.71 inches at the expense of the water spaces on either side of the movable control plates. (In the proposed design, the cross thickness is 0.6445 inches and there is less water area in the cross than the formula would predict. The purposes of the nuclear studies are served by the earlier assumption of the slightly thicker control cross and the additional assumption that the width of the movable control plates is proportional to d .) By simple subtraction the aluminum area of the control cross is $(6d + 0.9168) \text{ in}^2$.

The parameter, γ , is the ratio of the volume of metal to the volume of water in the core. The metal-to-water ratio in the core exclusive of the cross volume is of interest also. The latter ratio is designated as γ_a . It follows that

$$\gamma_a = \frac{[(6d + 0.9168) - (24d^2 + 3.48d - 0.2927)\gamma_c]}{[(3.48d - 0.2927)\gamma_c - (24d^2 + 6d + 0.9168)]}$$

because the total aluminum area of the core is

$$\left[\frac{24d^2 \gamma_a}{1 + \gamma_a} + (6d + 0.9168) \right] \text{ in}^2, \text{ and the total water area of the core is } \left[\frac{24d^2}{1 + \gamma_a} + (0.96d + 0.0064) + (2.52d - 0.2991) \right] \text{ in}^2.$$

Table A2.2.1b presents the values of γ_a and the pertinent volume ratios needed in the derivations of core constants for each of the nine combinations of the chosen values of the parameters d and γ .

The Atomic Densities

The density of aluminum as a function of temperature is

$$\rho_{al} = \frac{2.699}{[1 + (7.14)(10^{-5})(T - 20)]} \text{ gms/cm}^3, \text{ where } T \text{ is the}$$

temperature in degrees, Centigrade. At 77°C, —

$\rho_{al} = 2.688 \text{ gms/cm}^3$. Hence, the average atomic density of the aluminum in the core, W_{al} , is $N_{al} = (0.060039)(10^{24})$ atoms per cm^3 of aluminum. The density of water is obtained by means of an empirical formula for water between 20 and 100°C. The formula is

$$\rho_m = 1.0022957 - (1.320288)(10^{-4})T - (3.087121)(10^{-6})T^2 \text{ gm/cm}^3.$$

For $T = 20^\circ\text{C}$, $\rho_{mo} = 0.99840 \text{ gms/cm}^3$, and at $T = 77^\circ\text{C}$,

$\rho_m = 0.973826 \text{ gms/cm}^3$ so that the average atomic density of the water in the core at 77°C is $N_m = (0.0325618)(10^{24})$ atoms per cm^3 of water.

The density of the U^{235} in the core is based on the initial uranium mass, M_0 Kg, the core volume, V_t , cm^3 and a fractional burnup figure, x . The atomic density of U^{235} in the core at

Table A2.2.1b Material Volume Ratios

d inches	γ	$\frac{V_{ala}}{V_t}$	$\frac{V_{ma}}{V_t}$	γ_a	$\frac{V_{alc}}{V_t}$	$\frac{V_{mc}}{V_t}$
3.00	0.7	0.33457347	0.54682858	0.61184343	0.077191233	0.029628575
	0.6	0.29780767	0.58359328	0.51030191		
	0.5	0.25614210	0.62525995	0.40965698		
2.75	0.7	0.32810816	0.54367447	0.60350113	0.083656549	0.031849606
	0.6	0.29134345	0.58043918	0.50193623		
	0.5	0.24967678	0.622105845	0.40134132		
2.50	0.7	0.32041494	0.54005129	0.59330464	0.091305792	0.034423812
	0.6	0.28365875	0.57680747	0.49177371		
	0.5	0.24200065	0.61846557	0.39129204		

Definitions:

- γ_a = metal-to-water ratio in the core exclusive of the control cross
- V_{ala}/V_t = metal volume in core (exclusive of control cross) per total core volume
- V_{alc}/V_t = metal volume in control cross per total core volume
- V_{ma}/V_t = water volume in core (exclusive of control cross) per total core volume
- V_{mc}/V_t = water volume in control cross per total core volume

startup, N_{250} , is defined by the formula

$$N_{250} = (2.3922)(10^{24}) \frac{M_0}{V_t} \text{ atoms per cm}^3 \text{ of core. The nuclear}$$

calculations for the core parameter study are based on a reactor condition at 20% burnup of the U^{235} and an active core height of 3.0 feet = 91.44 cm so that the U^{235} atomic density at 20% burnup is

$$N_{25} = \frac{(2.3922)(10^{24})(0.8)M_0}{\pi R_1^2 H} = \frac{(0.00666193)(10^{24})M_0}{R_1^2} \text{ atoms/cm}^3.$$

All other materials in the core are considered as poisons and, as such, they are evaluated with respect to the U^{235} absorption in a later sub-section.

The Absorption Cross Sections of Water, Aluminum, and U^{235}

The cross sections of the major materials in the core are calculated on the basis of average volume ratios for the entire core, including the control cross. (The control cross includes the aluminum of the cadmium rod followers.) Because there is no U^{235} in the control cross, one must correct for the fact that the thermal neutron flux is as much as 100% greater in the control cross than in the rest of the core. (See ANL-5211) This is, in effect, a doubling of the relative aluminum and water absorptions in the cross when referred to the average core flux. Therefore, the formulae for the macroscopic cross sections of the aluminum and water in the core, Σ_{aal} and Σ_{am} , respectively, are obtained as follows:

$$\Sigma_{aal} = N_{al} \sigma_{aal} \left[\frac{V_{ala}}{V_t} + \frac{2V_{alc}}{V_t} \right], \text{ and}$$

$$\Sigma_{am} = N_m \sigma_{am} \left[\frac{V_{ma}}{V_t} + \frac{2V_{mc}}{V_t} \right], \text{ where } \sigma_{aal} \text{ and } \sigma_{am} \text{ are the}$$

effective microscopic absorption cross sections of aluminum and water, respectively. The U^{235} macroscopic absorption cross section is simply $\Sigma_{a25} = N_{25} \sigma_{a25}$, by the definition of N_{25} .

The microscopic cross sections of water and aluminum are " $1/v$ ", so that the effective values are simply the 20°C values times

$$\sqrt{\frac{(273+20)^\circ K}{(273+77)^\circ K}} \text{ and times the factor, } \sqrt{\pi}/2, \text{ which}$$

corrects for an assumed Maxwellian distribution in energy of the thermal neutrons. For U²³⁵, the total cross section is taken from the curve of BNL-325, reduced by a 10-barn scattering component, multiplied by the non-1/v factor, f , and then multiplied by $\sqrt{\pi}/2$ to obtain the effective "Maxwellian averaged" cross section. Table A2.2.1c presents the basic cross section data used, and the effective microscopic cross sections at 20°C and 77°C.

Table A2.2.1c Microscopic Cross Section Data

<u>Data Term</u>	<u>20°C Point Value</u>	<u>77°C Point Value</u>	<u>20°C Maxwellian Average</u>	<u>77°C Maxwellian Average</u>
σ_{aal} , barns	0.230	0.214397	0.203832	0.186497
σ_{am} , barns	0.660	0.603870	0.584910	0.535166
σ_{25} (total) barns	697	630		
f	0.981	0.979		
σ_{a25} barns	687	620	597.270	537.922

The Relative Absorptions of Core Poisons

The criticality of the reactors studied is evaluated at a reactor condition corresponding to 20% U²³⁵ burnup and 77°C temperature, during operation. Hence, it is assumed that equilibrium Xe¹³⁵ and Sm¹⁴⁹ are present with the low cross section fission products. U²³⁸ is also considered a poison.

The Xenon-135: If uniform distribution of the Xe¹³⁵ is assumed, the ratio of Xe¹³⁵ absorptions to U²³⁵ absorption at equilibrium, X , is given by

$$X = \frac{Y_X \sigma_X \bar{\phi}_s}{(1+\alpha)(\lambda_X + \sigma_X \bar{\phi}_s)}, \text{ where } Y_X \text{ is the effective yield}$$

of Xe¹³⁵ ($Y_X = 0.067$ is assumed), σ_X is the Maxwellian averaged Xe¹³⁵ absorption cross section at 77°C, λ_X is the Xe¹³⁵ decay constant ($\lambda_X = 2.1 \times 10^{-5} \text{ sec}^{-1}$), and $(1+\alpha)$ is the absorption to fission ratio for thermal neutrons in U²³⁵ ($1+\alpha = 1.184$). The average thermal flux, $\bar{\phi}_s$ depends on both reactor power and M_0 . At a reactor power level of 15 megawatts, if one assumes there are 193 mev of energy released per fission (i.e., $3.088 \times 10^{-17} \text{ MW sec/fiss}$), it is obtained that

$\bar{\phi}_s = \frac{4.4694 \times 10^{14}}{(1-x)(M_0, \text{ kg})}$ neutrons/cm²sec, where x is the fractional burnup of U²³⁵. If x = 0.20, $\bar{\phi}_s = \frac{5.5868 \times 10^{14}}{(M_0, \text{ kg})}$ neutrons/cm²sec.

The Maxwellian averaged value of σ_x at 77°C is $\sigma_x = 2.875 \times 10^6$ barns, so that

$$x = \frac{0.056588}{(1 + 0.013074 M_0)} \quad \text{at 20\% burnup of the U}^{235}.$$

The Sm¹⁴⁹: Again, if uniform space distribution of fissions occurs, the effective value of Sm¹⁴⁹ absorptions per U²³⁵ absorption (defined as S) is equal, at equilibrium, to

$$S = Y_s / (1 + \alpha), \quad \text{where } Y_s \text{ is the effective yield of Sm}^{149} \text{ (} Y_s = 0.014 \text{). Therefore, } S = 0.01182.$$

The Low Cross Section Fission Products:

Including U²³⁶, the effective macroscopic absorption cross section of the low cross section fission products is estimated from an empirical formula which approximates the results of J. W. Webster, "The Low Cross Section Fission Product Poisons", IDO 16100. The formula is good for total values of ϕt (where ϕt is the integrated value of (flux) x (time) for the core) between 10^{19} and 10^{22} neutrons per cm². If Σ_{fp} is defined as the macroscopic cross section of the fission products,

$$\Sigma_{fp} = \sigma_{a25} F \left[0.152 - 0.2188 \ln \left(\frac{\phi t}{10^{20}} \right) \right], \quad \text{where } F \text{ is the number of U}^{235} \text{ fissions, given by the formula } F = \frac{N_{250}(x)}{(1 + \alpha)}.$$

For uniform space distribution of the fissions, the ratio of the absorptions in low cross section fission products to the absorptions in U²³⁵, defined as F.P., is given by

$$\text{F.P.} = \left[0.152 - 0.02188 \ln \left(\frac{\phi t}{10^{20}} \right) \right] \frac{(1-x)}{(1 + \alpha)(1-x)}. \quad \text{At 20\% burn-}$$

up ϕt is about 4.148×10^{20} , so that F.P. = 0.02552.

The U²³⁸: Because the exact enrichment of the initial loading is not specified, the ratio of U²³⁸ absorption to U²³⁵ absorption (A_{28}) is simply approximated by the value,

$$A_{28} = 0.000366.$$

The Total Absorption Cross Section for the Core, Σ_a

The foregoing definitions of the cross sections and the effects of core poisons are such that the total thermal neutron effective absorption cross section, Σ_a , is simply

$$\Sigma_a = a_{25} (1 + X + S + \text{F.P.} + A_{28}) + \Sigma_{aal} + \Sigma_{am}.$$

If one defines the variable $G(M_0)$ as

$$\begin{aligned} G(M_0) &= [1 + X + S + \text{F.P.} + A_{28}] \\ &= \frac{1 + 0.056588}{1 + 0.013074 M_0} + 0.01182 + 0.02552 + 0.000366 \\ &= \frac{1.094294 + 0.013567 M_0}{1 + 0.013074 M_0}, \text{ it follows that} \end{aligned}$$

$$\Sigma_a = a M_0 G(M_0) + b, \quad \text{where } a = \frac{\Sigma_{a25}}{M_0} \text{ and } b = \Sigma_{aal} + \Sigma_{am}.$$

For the nine combinations of the chosen parametric values of d and γ , Table 2.2.1d is prepared. The values of a and b in the expression for Σ_a are given to facilitate estimation of Σ_a as a function of M_0 because the method of attaining criticality in a given reactor of the study involves varying the initial uranium loading, M_0 . The fission cross section for the core, Σ_f , is given by $\Sigma_f = \Sigma_{a25}/(1+\alpha)$. The values of Σ_f/M_0 are also included in Table 2.2.1d.

The Neutron Diffusion Constants for the Core

The fast and thermal neutron diffusion constants, denoted by D_f and D_s , respectively, are calculated as a function of temperature by means of formulae obtained by J. W. Webster for Al-H₂O mixtures. D_f is averaged with respect to the fast neutron flux energy distribution given in WAPD-45.

The formulae are,

$$D_f = (1.19 + 0.1767 \gamma') \frac{1}{(1-f_v)} \quad \text{and}$$

$$D_s = (0.16 + 0.1493 \gamma') \frac{1}{(1-f_v)},$$

where γ' is the effective metal-to-water ratio at temperature and f_v is an effective void fraction at temperature. γ' is

Table A2.2.1d The Macroscopic Absorption Cross Sections for the Core

Parameter Study $\left[\sum a = a M_0 G(M_0) + b, \text{ where} \right.$

$$(1.094294 + .013567 M_0)$$

$$G(M_0) = \frac{(1 + .013074 M_0)}{(1 + .013074 M_0)}$$

d (inches)		$\sum_{aal}$	$\sum_{am}$	$b = \sum_{aal} + \sum_{am}$	$a = \frac{\sum a_{25}}{M_0}$	$\frac{\sum f}{M_0}$
3.00	0.7	0.00547489	0.01128317	0.01675806	0.007120636	0.006014050
	0.6	0.00506323	0.01192383	0.01698706		
	0.5	0.00459669	0.01264991	0.01724660		
2.75	0.7	0.00553375	0.01136058	0.01689433	0.00838166	0.007079105
	0.6	0.00513562	0.01200124	0.01713686		
	0.5	0.00466908	0.01272732	0.01739640		
2.50	0.7	0.00563244	0.0114503	0.01708274	0.01001016	0.008454527
	0.6	0.00522088	0.0120910	0.01731188		
	0.5	0.00475443	0.01281704	0.01757147		

simply $\delta \left(\frac{\rho_{al}}{\rho_{oal}} \right) \left(\frac{\rho_{om}}{\rho_m} \right)$, where δ is the volume ratio of

metal-to-water at 20°C, ρ_{oal} is the density of aluminum at 20°C, and ρ_{om} is the 20°C water density. Also

$$(1=f_v) = \frac{V_m}{V_t} \frac{\rho_m}{\rho_{om}} + \frac{V_{al}}{V_t} \frac{\rho_{al}}{\rho_{oal}}, \text{ where}$$

$$\frac{V_m}{V_t} = \frac{1}{1 + \delta} \quad \text{and} \quad \frac{V_{al}}{V_t} = \frac{\delta}{1 + \delta}.$$

The solutions of the multiregion diffusion equations (see Section A2.1) are obtained for an analytical model of the actual core configuration. Because the neutron leakage would be less for the perfectly circular cylinder than that for the actual irregular core shape, it is desirable to correct the radial diffusion constants by a factor which takes into account the difference in the radial bucklings of the two core configurations. The factor is defined as the ratio of the actual radial buckling expected to that obtained for the circular cylinder. If the reactor core fuel assemblies were simply shifted to present a 6 x 4 rectangular array, the buckling, denoted by B_s^2 , would be

$$B_s^2 = \pi^2 \left[\frac{1}{(L_1 + \delta_s)^2} + \frac{1}{(L_2 + \delta_s)^2} \right], \text{ where } L_1 \text{ is one}$$

dimension of the rectangle and L_2 is the other. δ_s is the equivalent reflector savings, which is assumed to be the same in either direction. It is also approximated that $\delta_s = \delta_r$, where δ_r is the reflector savings of the cylindrical reactor. The results of previous calculations indicate that δ_r is about 6.6 cm. With these gross assumptions it is found that the ratio of B_s^2 to B_r^2 is 1.0794, where B_r^2 is equal to $(2.4048)^2 / (R_1 + \delta_r)^2$, evaluated for the R_1 corresponding to $d = 3.0$ inches. (It is assumed, also, that $L_1 = 2/3 L_2$ and $L_1 L_2 = \pi R_1^2$.) If B_s^2 / B_r^2 is denoted by $B_F = 1.0794$, the corrected diffusion constants are simply

$$D_1 = D_f B_F \quad \text{and} \quad D_2 = D_s B_F.$$

The subscripts, 1 and 2, on D denote the values to be used in obtaining the radial solutions of the multiregion diffusion equations. Since the axial buckling is not affected by the assumption of a circular cylinder, the diffusion constants

employed to find the axial neutron leakage remain D_f and D_s for the fast and slow neutrons, respectively.

Table A2.2.1e presents the calculated values of δ' , f_v , and the corresponding axial and radial diffusion constants for each parametric value of δ . (The assumptions are such that there is no variation of D with reactor core size.)

Table A2.2.1e The Diffusion Constants at 77°C

	δ' Hot Metal to Water Ratio	f_v Effective Void Fraction	Axial Constants		Radial Constants	
			D_f	D_s	D_1	D_2
0.7	0.714772	0.0161578	1.337918	0.2710958	1.444149	0.2926208
0.6	0.612662	0.0169145	1.320595	0.2557971	1.425450	0.2761074
0.5	0.510552	0.0177721	1.303378	0.2404995	1.406866	0.2595952

The Neutron Slowing Down Constants for the Core

The effective "absorption" cross section of the fast neutrons is assumed to be only the slowing down cross section, Σ_1 (see Section A2.1. Σ_1 is defined by the ratio of the effective fast neutron diffusion constant, D_f , to the fast neutron diffusion area (denoted by τ). The formulae for τ (as a function of metal-to-water ratio and temperature) is an empirical one which fits the experimental data (ORNL-294, 1949) at the lower values of δ and is extrapolated to the higher values of δ by means of the calculated Dismuke and Arnette values of τ for Al-H₂O mixtures.

The temperature corrections are applied directly in δ' and the effective void fraction, f_v , as in the case of the diffusion constants. The formula derived for τ is

$$\tau = \left[31.4 + 37.2 \delta' + 6.4 (\delta')^2 \right] \frac{1}{(1-f_v)^2}$$

The values of τ and Σ_1 obtained for each value of δ are listed on Table A2.2.1f.

Table A2.2.1f The Slowing Down Cross Section (77°C)

γ	τ , fast neutron diffusion area, cm ²	Σ_1 , slowing down cross section, cm ⁻¹
0.7	63.28794	0.02114018
0.6	58.55748	0.02255211
0.5	53.96174	0.02415374

The Neutron Removal Cross Sections for the Core

Since the neutron removal cross sections are defined as $\Sigma_{1R} = \Sigma_1 + D_f B_h^2$, and $\Sigma_{2R} = \Sigma_a + D_s B_h^2$, for the fast and thermal neutron groups, respectively, the value of the axial buckling, B_h^2 , must be estimated. A reflector savings, denoted by δ_h , is added to either end of the active core to effect an equivalent reactor height of $H + 2\delta_h$ cm. δ_h is assumed to be 8 cm for the three foot reactor height. Hence, $B_h^2 = \left[\frac{\pi}{H + 2\delta_h} \right]^2 =$

0.00085500 cm⁻².

The values of $D_f B_h^2$, $D_s B_h^2$, and Σ_{1R} are listed in Table A2.2.1g for the chosen parametric values of γ . Since Σ_1 is a function only of γ , the value of Σ_{1R} may be written directly. Σ_{2R} remains a function of d and M_0 (see Table A2.2.1d).

Table A2.2.1g The Equivalent Removal Cross Sections

γ	$D_s B_h^2$, cm ⁻¹	$D_f B_h^2$, cm ⁻¹	Σ_{1R} , cm ⁻¹
0.7	0.00023179	0.00114392	0.0222841
0.6	0.00021871	0.00112911	0.02368122
0.5	0.00020563	0.00111439	0.02526813

The Group Constants for Al and Air, Region 2

The second reactor region, for the core parameter study, is composed of aluminum and air spaces. The air spaces are added to increase core leakage and to add to the probability that leakage neutrons penetrate to the blanket (region 5). The equivalent void fraction for the region is 0.3415 and it is approximated that the voids are a homogeneous part

of the region. All the nuclear multiregion calculations are performed using a thickness, ΔR_2 , for the second region of 4.077676 cm. R_2 is equal to $R_1 + \Delta R_2$, by definition.

The fast diffusion constant for solid aluminum is found from an average of $1/\sigma_{tr}$ with respect to the fast flux energy distribution of the MTR. (σ_{tr} is the microscopic transport cross section of aluminum.)

The fast diffusion area, τ , is estimated for solid aluminum from the average of $1/\sigma_s \sigma_{tr}$ from 2 mev to thermal energy, where σ_s is the scattering cross section. The values obtained for D_f and τ for solid aluminum at 20°C are $D_{fo} = 3.2907$ cm and $\tau_o = 9409.6$ cm², respectively. D_{so} is calculated from the value of σ_{tr} of aluminum at 20°C. Also, σ_{tr} is assumed invariant in the temperature range 20-100°C. The value of D_s obtained in this manner is $D_2 = 4.0503$ cm.

Σ_a for solid aluminum is calculated using the Maxwellian average cross section at 77°C from Table A2.2.1c, and an atomic density, N_{al} , given by $N_{oal} \rho_{al}/\rho_{oal}$ where N_{oal} is the atomic density at 20°C, ρ_{oal} is the density at 20°C, and ρ_{al} is the density at 77°C.

($N_{oal} = 0.060285 \times 10^{24}$ atoms/cm³, and $\rho_{al}/\rho_{oal} = 0.99595$.)

The values of the group constants for solid aluminum at 77°C are

$$\tau = \tau_o \left(\frac{\rho_{oal}}{\rho_{al}} \right)^2 = 9486.3 \text{ cm}^2, \quad D_f = D_{fo} \left(\frac{\rho_{oal}}{\rho_{al}} \right) = 3.3043 \text{ cm},$$

$$D_s = D_{so} \left(\frac{\rho_{oal}}{\rho_{al}} \right) = 4.0668 \text{ cm}, \quad \Sigma_a = 0.0111971 \text{ cm}^{-1} \quad \text{and}$$

$$\Sigma_1 = \frac{D_{fo}}{\tau_o} \left(\frac{\rho_{al}}{\rho_{oal}} \right) = 0.00034832 \text{ cm}^{-1}.$$

The constants for the Al-Air mixture are computed from those above by simple correction for the change in atomic density produced by the voids. The total removal cross sections are obtained by adding $D_1 B_h^2$ to Σ_1 and $D_2 B_h^2$ to Σ_a for the fast and thermal neutron groups, respectively, where the constants D_1 and D_2 are identical to D_f and D_s because there is no geometry correction necessary.

(B_h^2 is the same as for Region 1.)

The group constants are summarized below for the Al-Air material of the second region.

$$\begin{aligned}
 D_1 &= 5.0179 \text{ cm} & \Sigma_{1R} &= 0.00451967 \text{ cm}^{-1} \\
 D_2 &= 6.1758 \text{ cm} & \Sigma_{2R} &= 0.0126536 \text{ cm}^{-1} \\
 D_1 B_h^2 &= 0.00429032 \text{ cm}^{-1} & \Sigma_1 &= 0.00022937 \text{ cm}^{-1} \\
 D_2 B_h^2 &= 0.0052803 \text{ cm}^{-1} & \Sigma_a &= 0.0073733 \text{ cm}^{-1} \\
 T &= 77^\circ\text{C} & \Delta R_2 &= 4.077676 \text{ cm}
 \end{aligned}$$

The Group Constants for Al and Water, Region 3

This region is considered to be a homogeneous mixture of the alternate layers of water and aluminum between the Al-Air region and the stainless steel blanket container. The assumption is that this region is alternately 1/16-inch of water, 1/2-inch of Al (pressure vessel), and 1/8-inch of water; hence $\Delta R_3 = 1.74626 \text{ cm}$.

The fast group constants are computed by means of the same formulae used to obtain the core constants for aluminum-water mixtures. The metal-to-water ratio, γ , of the region is 2.63854, and the effective γ' is 2.68239. The effective void fraction, f_v , is 0.0153. Again, B_h^2 is identical to that of the core. The neutron group constants are listed below.

$$\begin{aligned}
 D_1 &= 1.68983 \text{ cm} & \Sigma_{1R} &= 0.01068977 \text{ cm}^{-1} \\
 D_2 &= 0.56919 \text{ cm} & \Sigma_{2R} &= 0.01339566 \text{ cm}^{-1} \\
 D_1 B_h^2 &= 0.0014481 \text{ cm}^{-1} & \Sigma_1 &= 0.00924496 \text{ cm}^{-1} \\
 D_2 B_h^2 &= 0.00048666 \text{ cm}^{-1} & \Sigma_a &= 0.012909 \text{ cm}^{-1} \\
 T &= 77^\circ\text{C} & \Delta R_3 &= 1.74626 \text{ cm}
 \end{aligned}$$

The Group Constants for Iron, Region 4

The fourth reactor region is a 1/8 inch layer of stainless steel, which comprises the inner wall of the blanket container. The group constants used for this region are those of pure iron, which is almost identical to stainless steel in neutron diffusion properties. A single exception is that the microscopic absorption cross section, σ_{aFe} , used is one based on a measured value (for type 347 stainless steel) such that when σ_{aFe} is multiplied by the atomic density

of iron (of density $\rho = 7.85 \text{ gms/cm}^3$ at 20°C) the correct macroscopic cross section is obtained. ($\sigma_{a\text{Fe}} = 2.8 \text{ barns.}$)

The fast neutron group constants are obtained in the same manner as was employed to obtain the aluminum constants discussed earlier. (D_1 is averaged with respect to an MTR fast flux spectrum, and $\Sigma_1 = D_1/\gamma$). The atomic density of the iron is $N = 0.08467 \times 10^{24} \text{ atoms/cm}^3$ at temperature.

The region temperature is 77°C and the effective neutron removal cross sections are calculated as usual with B_h^2 equal to that of the core for $H = 3.0 \text{ feet}$.

The neutron group constants are:

$$\begin{array}{ll} D_1 = 0.9077 \text{ cm} & \Sigma_{1R} = 0.00181882 \text{ cm}^{-1} \\ D_2 = 0.3563 \text{ cm} & \Sigma_{2R} = 0.19250 \text{ cm}^{-1} \\ D_1 B_h^2 = 0.00077609 \text{ cm}^{-1} & \Sigma_1 = 0.00104273 \text{ cm}^{-1} \\ D_2 B_h^2 = 0.00030 \text{ cm}^{-1} & \Sigma_a = 0.1992 \text{ cm}^{-1} \\ T = 77^\circ\text{C} & \Delta R_4 = 0.3175 \text{ cm} \end{array}$$

The Group Constants For the Blanket, Region 5

For the purposes of the core parameter study, a blanket region six inches thick is assumed. The concentration of indium sulfate, $\text{In}_2(\text{SO}_4)_3$, in the water of the blanket is 400 grams per liter at 20°C . This concentration corresponds to a solution density of 1.342 gms/cm^3 at 20°C . The density of the solution is assumed to vary with temperature as does that for clean water so that at 77°C the blanket density is 1.3089 gms/cm^3 . If the grams of indium sulfate per gram of solution do not change, then the 77°C solution contains 0.39013 grams of indium sulfate per cm^3 . This figure is called ρ_{In} . The water density at 77°C , ρ_m , is therefore equal to $\rho_m = 0.91877 \text{ gms/cm}^3$.

It is conservatively assumed that the fast neutron diffusion area, γ , is equal to that for pure water of density ρ_m . Hence,

$$\gamma = (31.4) \left(\frac{\rho_{\text{om}}}{\rho_m} \right)^2 \quad \text{where } \rho_{\text{om}} \text{ is equal to } 0.942 \text{ gms/cm}^3$$

at 20°C for a solution density of 1.342 gms/cm^3 . D_1 and D_2 are estimated on the same basis, so that $D_1 = 1.19(\rho_{\text{om}}/\rho_m)$

$D_2 = 0.16 (\rho_{om}/\rho_m)$. $\Sigma_1 = D_1/\gamma$, as usual. (These values are obtained from the formulae given for the core when γ' is equal to zero.)

The effective thermal neutron absorption cross section of indium is corrected for an assumed cadmium ratio of 3.26 in the blanket. That is, the ratio of thermal plus resonance neutron absorptions to thermal absorptions is $CR/(CR-1) = 1.44$, where CR = the cadmium ratio.

At 20°C, the absorption cross section of In^{113} is 56 barns for the isomer of 49 day half-life and 2 barns for the isomer of 72 second half-life, and since In^{113} comprises 4.23% of the atoms in naturally occurring indium, the In^{113} contribution to the microscopic absorption cross section of the element is $(0.0423)(56 + 2) = 2.45$ barns. The cross sections of the 13 second half-life isomer and the 54 minute half-life isomer of In^{115} are, respectively, 52 barns and 145 barns at 20°C. Hence, the absorption cross section of In^{115} is $(0.9577)(52 + 145) = 188.67$ barns. The 20°C cross section is, therefore, $(\sigma_{aIn})_0 = 191.12$ barns. At 77°C the average thermal absorption cross section, σ_{aIn} , is 154.97 barns, if it is assumed that the cross section varies as $1/v$ and the thermal neutron flux distribution in energy is Maxwell Boltzmann in nature. Corrected to include the resonance contribution, this cross section becomes the effective absorption cross section of elemental indium in the blanket. That is, $(\sigma_{aIn})_{eff} = (1.44)(154.97) = 223.16$ barns. The atomic density of indium in the blanket, N_{In} , is given by the formula,

$$N_{In} = \frac{2(0.6024 \times 10^{24})}{M} \rho_{In} \text{ atoms/cm}^3 \text{ where } M \text{ is the}$$

molecular weight of $In_2(SO_4)_3$. $M = 517.70$ gms/mol, so that for $\rho_{In} = 0.39013$ gms/cm³ it follows that $N_{In} = 0.00090791$ atoms/cm³. Hence,

$$\sigma_{aIn} = N_{In}(\sigma_{aIn})_{eff} = 0.202609 \text{ cm}^{-1}.$$

The absorption of the sulfate component of the molecule may be neglected, and the water absorption cross section, Σ_{am} , is equal to $0.0160441 \text{ cm}^{-1}$.

The effective macroscopic absorption cross section, Σ_a , for thermal neutrons in the blanket is $\Sigma_{aIn} + \Sigma_{am} = 0.2186531 \text{ cm}^{-1}$. It is noted that absorptions in indium comprise $\Sigma_{aIn}/\Sigma_a = 0.926623$ of the total.

B_h^2 is assumed to be the same as that of the reactor core, i.e., $B_h^2 = 0.00085500 \text{ cm}^{-2}$, and the effective removal cross section due to axial leakage is based on the products $D_1 B_h^2$ and $D_2 B_h^2$ as before.

$$\begin{aligned} D_1 &= 1.29316 \text{ cm} & \Sigma_{1R} &= 0.0359804 \text{ cm}^{-1} \\ D_2 &= 0.17387 \text{ cm} & \Sigma_{2R} &= 0.21880176 \text{ cm}^{-1} \\ D_1 B_h^2 &= 0.0011057 \text{ cm}^{-1} & \Sigma_1 &= 0.0348747 \text{ cm}^{-1} \\ D_2 B_h^2 &= 0.00014866 \text{ cm}^{-1} & \Sigma_a &= 0.2186531 \text{ cm}^{-1} \\ T &= 20^\circ\text{C} & \Delta R_5 &= 15.24 \text{ cm} \end{aligned}$$

The Group Constants for the Thermal Shield, Region 6

The thermal shield region is considered to be the alternate layers of 1/8 inch stainless steel (blanket container), 1/8 inch water, 3 inches iron, 1/8 inch water, and 1/4 inch iron between the blanket, itself, and the water of the pool. This region is approximately represented by a nominal 3 inches of iron so that $\Delta R_6 = 7.62 \text{ cm}$.

The neutron group constants are identical to those of region 4, which is also iron. The temperature of the region is 77°C .

The Group Constants for the Water Pool, Region 7

The water pool is clean water at 20°C so that the value of $\gamma = 31.4 \text{ cm}^2$, D_1 is 1.19 cm, and D_2 is 0.16 cm. Hence $\Sigma_1 = D_1/\gamma = 0.0378981 \text{ cm}^{-1}$, and $\Sigma_a = 0.0195267 \text{ cm}^{-1}$.

(See the constants and formulae for water given in the section related to the core constants.) For the sake of consistency rather than relevancy, B_h^2 is assumed to be the same as for the other regions. (At this distance from the core, two group theory has little relevancy because the neutron flux properties become chiefly dependent upon direct flight fast neutrons, which two group diffusion theory neglects entirely.) The thickness of this region is taken to be 40 cm in order to effectively represent an infinite water pool. The group constants are:

$$\begin{aligned} D_1 &= 1.19 \text{ cm} & \Sigma_{1R} &= 0.0389156 \text{ cm}^{-1} \\ D_2 &= 0.16 \text{ cm} & \Sigma_{2R} &= 0.0196635 \text{ cm}^{-1} \\ D_1 B_h^2 &= 0.0010175 \text{ cm}^{-1} & \Sigma_1 &= 0.0378981 \text{ cm}^{-1} \end{aligned}$$

$$D_2 B_h^2 = 0.0001368 \text{ cm}^{-1}$$

$$\Sigma_a = 0.0195267 \text{ cm}^{-1}$$

$$T = 20^\circ\text{C}$$

$$\Delta R_7 = 40.0 \text{ cm}$$

A2.2.2 The Reactor Constants for H = 2.5 feet and H = 2.0 feet

Aside from changes in Σ_{a25} due to concentrating a given M_0 in a smaller volume, the group constants for all regions are altered only by the change in B_h^2 when the reactor height is reduced. Hence, only the removal cross sections, Σ_{1R} and Σ_{2R} , are different from the constants derived in Section A2.2.1 for the active core height of 3.0 feet.

When $H = 2.5$ feet, the axial reflector savings, δ_h is assumed to be 7.5 cm; and when $H = 2.0$ feet, δ_h is assumed to be 7.0 cm. B_h^2 is defined by $(\pi/H + 2\delta_h)^2$, so that B_h^2 is equal to $0.00118662 \text{ cm}^{-2}$ and $0.00175647 \text{ cm}^{-2}$ for $H = 2.5$ feet and $H = 2.0$ feet, respectively. The change in height occurs for the reactor core characterized by $d = 2.5$ inches and $\gamma = 0.7$.

Summarized below, by regions, are the altered total neutron removal cross sections corresponding to the two reduced reactor core heights.

The Core, Region 1 ($d = 2.5$ inches, $\gamma = 0.7$)

H, ft.	$\Sigma_{1R}, \text{cm}^{-1}$	$(\Sigma_f = cM_0)$	$(*\Sigma_{2R} = aM_0G(M_0) + b)$	
		c, cm^{-1}/kg	a, cm^{-1}/kg	b, cm^{-1}
2.5	0.02272777	0.01014543	0.01201219	0.01740443
2.0	0.02349019	0.01268179	0.01501524	0.01755891

$$*G(M_0) = \frac{(1.094294 + 0.013567 M_0)}{(1 + 0.013074 M_0)}$$

The Al-Air, Region 2

H, ft	$\Sigma_{1R}, \text{cm}^{-1}$	$\Sigma_{2R}, \text{cm}^{-1}$
2.5	0.00618370	0.0147016
2.0	0.00904315	0.0182209

The Al-Water, Region 3

<u>H, ft</u>	<u>$\Sigma_{1R}, \text{cm}^{-1}$</u>	<u>$\Sigma_{2R}, \text{cm}^{-1}$</u>
2.5	0.01125014	0.01358441
2.0	0.01221309	0.01390876

The Iron, Region 4

<u>H, ft</u>	<u>$\Sigma_{1R}, \text{cm}^{-1}$</u>	<u>$\Sigma_{2R}, \text{cm}^{-1}$</u>
2.5	0.00211982	0.19262
2.0	0.00263708	0.19283

The Blanket, Region 5

<u>H, ft.</u>	<u>$\Sigma_{1R}, \text{cm}^{-1}$</u>	<u>$\Sigma_{2R}, \text{cm}^{-1}$</u>
2.5	0.0364092	0.2188594
2.0	0.0371461	0.2189585

The Thermal Shield Iron, Region 6

(Same as Region 4)

The Water Pool, Region 7

<u>H, ft</u>	<u>$\Sigma_{1R}, \text{cm}^{-1}$</u>	<u>$\Sigma_{2R}, \text{cm}^{-1}$</u>
2.5	0.0393102	0.0197166
2.0	0.0399883	0.0198077

A2.2.3 The Constants for Replacement of Region 2 by Solid Aluminum and by Beryllium

In the cases in which the usual Al-Air of region 2 is replaced by aluminum or beryllium, the reactor core height is equal to 3.0 feet, so that $B_h^2 = 0.00085500 \text{ cm}^{-2}$.

The basic neutron group constants for solid aluminum are presented in Section A2.2.1 in the derivation of the constants for Al-Air, so that the group constants for solid aluminum may be written directly.

Neutron Group Constants for Solid Aluminum, H = 3.0 feet

$$\begin{aligned}
 D_1 &= 3.3043 \text{ cm} & \Sigma_{1R} &= 0.00317342 \text{ cm}^{-1} \\
 D_2 &= 4.0668 \text{ cm} & \Sigma_{2R} &= 0.0146742 \text{ cm}^{-1} \\
 D_1 B_h^2 &= 0.00282519 \text{ cm}^{-1} & \Sigma_1 &= 0.00034832 \text{ cm}^{-1} \\
 D_2 B_h^2 &= 0.0034771 \text{ cm}^{-1} & \Sigma_a &= 0.0111971 \text{ cm}^{-1} \\
 T &= 77^\circ\text{C} & \Delta R_2 &= 4.077676 \text{ cm}
 \end{aligned}$$

In calculating the neutron group constants for beryllium, the following properties are taken from AECD-3645, Chapter 1.5:

Beryllium density, $\rho = 1.85 \text{ gms/cm}^3$

Fast neutron diffusion area, $\tau = 97.2 \text{ cm}^2$

Thermal neutron diffusion area, $L^2 = (20.8)^2 = 432.64 \text{ cm}^2$

Thermal λ (transport) = 1.43 cm

The average microscopic absorption cross section, σ_{ao} , (assumed for a Maxwell Boltzmann distribution of thermal neutrons) at 20°C is therefore

$$\sigma_{ao} = \frac{\lambda}{N_3 L^2} = 0.00892 \text{ barns, where } N \text{ is the atomic density}$$

corresponding to $\rho = 1.85 \text{ gms/cm}^3$. ($\lambda/3$ is equal to D_2 .)
($N = 0.1236 \times 10^{24} \text{ atoms/cm}^3$.) Corrected to 77°C , the σ_a is given by

$$\sigma_a = \sigma_{ao} \sqrt{\frac{(273+20)^\circ\text{K}}{(273+77)^\circ\text{K}}} = 0.00816 \text{ barns}$$

Hence, $\Sigma_a = N \sigma_a = 0.00101 \text{ cm}^{-1}$.

D_1 is obtained by averaging the λ_{tr} of beryllium with respect to the fast flux spectrum of an MTR type reactor. The D_1 found in this manner is $D_1 = 0.76 \text{ cm}$.

The neutron group constants are summarized below:

$$\begin{aligned}
 D_1 &= 0.76 \text{ cm} & \Sigma_{1R} &= 0.00844 \text{ cm}^{-1} \\
 D_2 &= 0.48 \text{ cm} & \Sigma_{2R} &= 0.00142 \text{ cm}^{-1}
 \end{aligned}$$

$$D_1Bh^2 = 0.00065 \text{ cm}^{-1}$$

$$D_2Bh^2 = 0.00041 \text{ cm}^{-1}$$

$$T = 77^\circ\text{C}$$

$$\Sigma_1 = 0.00779 \text{ cm}^{-1}$$

$$\Sigma_a = 0.00101 \text{ cm}^{-1}$$

$$\Delta R_2 = 4.077676 \text{ cm}$$

Appendix 3.0 Method for Reactor Core
Heat-Transfer Calculations

Heat-transfer conditions are analyzed assuming a cosine distribution of the axial heat flux, with a maximum-to-average ratio of 1.5. The radial maximum-to-average is 1.56. Table A3.0 shows the basic data:

Table A3.0 Physical Data on Core (for Heat
Transfer Calculations)

Power, MW	20
Flow velocity, ft/sec	7.5
Heated length, in.	36
Core heat-transfer area, ft ²	407
Flow channel, in.	0.106 x 2.26
Plate heat-transfer area, ft ² /in of length	0.0294

The average heat flux in the core is

$$q_{av} = \frac{20 \text{ MW} \times 3.413 \times 10^3 \text{ Btu/hr}}{407 \text{ ft}^2 \text{ MW}} = 1.68 \times 10^5 \text{ Btu/hr-ft}^2$$

The maximum heat flux in the core is

$$q_{max} = 3.92 \times 10^5 \text{ Btu/hr-ft}^2$$

The heat flux at a position (x) along the fuel plate is

$$q_x = 3.92 \times 10^5 \cos \frac{\pi X}{L} \quad (1)$$

where q_x = heat generation at point X

X = location of point, $-18 \leq X \leq +18$ in.

L = extrapolation length = 37.8 in.

Local water temperature is found by dividing the heat generated up to that point, by the heat capacity, or

$$t_1 = 3.92 \times 10^5 \int_{-18}^X \cos \frac{\pi X}{37.8} dX \times \frac{1}{WC} + t_i \quad (2)$$

$$= \frac{3.92 \times 10^5 \times 0.0294}{2770} \int_{-18}^X \cos \frac{\pi X}{37.8} dX + t_i$$

where t_1 = bulk water temperature at point X, °F

t_i = inlet bulk water temperature, °F

W = mass flow rate, lb/hr

C = specific heat, Btu/lb-°F

Integrating

$$t_1 = 50 \sin \frac{\pi X}{37.8} + 49.9 + t_i \quad (3)$$

To determine the surface temperature of the fuel element

$$q_x = h_x \Delta t_x = h_x(t_s - t_1)$$

where h_x = heat-transfer coefficient at X = Btu/hr-ft²-°F

$$t_s = \frac{q_x}{h_x} + t_1 \quad (4)$$

Substituting

$$t_s = \frac{3.92 \times 10^5}{h_x} \cos \frac{\pi X}{37.8} + 50 \sin \frac{\pi X}{37.8} + 49.9 + t_i \quad (5)$$

To find the maximum temperature, differentiate and set equal to zero.

$$\frac{dt_s}{dX} = \frac{3.92 \times 10^5}{h_x} \left(\frac{37.8}{\pi} \right) \sin \frac{\pi X}{37.8} - 50 \left(\frac{37.8}{\pi} \right) \cos \frac{\pi X}{37.8} = 0$$

$$\tan \frac{\pi X}{37.8} = \frac{50 h_x}{3.92 \times 10^5} \quad (6)$$

h_x is a function of local bulk-water temperature and velocity given by

$$150 (1 + 0.011t) \frac{v^{0.8}}{v^{0.2}}$$

For this case, velocity is held constant. To determine maximum fuel-plate surface temperature, t_1 is assumed, and h_x calculated. Equation 6 is then solved, which allows calculation of t_s and t_1 . If needed, a new t_1 is assumed, and t_s and t_1 are re-calculated.

SECTION A-A
SCALE: 1:1

TYPICAL END VIEW

TYPICAL SECTION OF ELEMENT
SCALE: 2:1

END BOX
2 REQD PER ASSEMBLY
SCALE: 1:1

FUEL PLATE
16 REQD.
SCALE: 1:1

ENLARGED VIEW 'A'
SCALE: 10:1

SIDE PLATE
2 R. 30 PER ASSEMBLY
SCALE: 1:1

ENLARGED VIEW 'B'
SCALE: 10:1

SCALE: 1" = 1'-0"

INITIALS	DATE	FOOD IRRADIATION REACTOR
DRAWN BY	MJK	FUEL ASSEMBLY
CHECKED BY		INTERNUCLEAR COMPANY
APPROVED		CLAYTON, MO.
APPROVED		SCALE: NOTED
		BRANING, INC.
		FIGURE 3.C

INITIALS	DATE	FIGURE 3.D	REV.
DRAWN BY	M.J.R.	SCALE 1/2	
CHECKED BY	F.B.S.	FIGURE 3.D	
APPROVED		FIGURE 3.D	
APPROVED		FIGURE 3.D	

FIG. 3.E

NUCLEAR INSTRUMENTATION AND CONTROL-BLOCK DIAGRAM

FIG. 3.E

1. LOG CRM RECORDER	1. PERIOD RCDR	1. PWR LVL SERVO DRIVE	1. PWR LVL RECORDER	1. PWR LVL RECORDER
2. SPKR. AUD. CR	2. LOG N. RCDR	2. VR ELECTROMETER	2. VR ELECTROMETER	2. VR ELECTROMETER
3. SCALER	3. LOG N-MICRO-MICRO	3. SERVO AMP	3. GALVANOMETER	3. ROD POSITION INDICATORS
4. LOG CRM	4. PERIOD AMP	4. CONTROL SWITCH PANEL		
5. A-1 AMPLIFIER	5. CIC-HV PWR LVL. SERVO			
6. CIC-HV LOG N	6. CIC-HV PWR LVL.			
7. CIC-HV PWR LVL.				

1. PWR-C-1 ΔT-FLOW	1. GMRA-1 FUEL RUPTURE GAMMA MONITOR RECORDER	1. ANNUNCIATOR
2. ΔT-1 REAG. ΔT	2. FR-3 PRIMARY FLOW	2. RADIATION ECDR.
3. CR CONDUCT-PHR	3. FUEL RUPTURE CRM	3. FR-1 & FR-2
4. AC LINE VOLTAGE REGULATOR	4. MAGNET AMP	4. MAGNET AMP
	5. MAGNET AMP	5. GALVANOMETER
	6. MAGNET AMPLIFIERS	6. CIC-HV (BATTERIES)

1. PI-12	PI-13	1. TR-1
PI-14	PI-15	2. TR-2-TRA-2(IL)
PRC-1	ΔT-2	3. RRA-1
LRCA-1	FR-5	RRA-2
FR-3	FR-4	RRA-3
PR-1(IL)/FR-5(IL)		
2. MAGNET AMP		
PWR SUPPLY		

CONTROL RELAY PANEL

(IL) Indium Heat Exchanger Loop
[Other process instrumentation from
cooling water systems]

FIG. 3.F CONTROL ROOM CONSOLE-LAYOUT

FIG. 3.F

SEE RIGHT
SIDE OF DRAWING
FOR VENT LINE

FOOD IRRADIATION REACTOR INDIUM SOLUTION SYSTEM FLOW DIAGRAM			
INTERNUCLEAR COMPANY			
DESIGNED BY	DATE	SCALE	REV.
CHECKED BY	12/2	NONE	1
APPROVED		DRAWING NO.	
APPROVED		SHEET	FIGURE 4.A

SECTION A-A
SCALE 3/4"=1"

INITIALS	DATE	FOOD IRRADIATION REACTOR
DATE	DATE	NO. 2 CAN RADIATOR ASSEMBLY
DRAWN BY	MJK	INTERNUCLEAR COMPANY
CHECKED BY		CLAYTON, MS.
APPROVED		SCALE: NOTED
APPROVED		DRAWING NO. REV
		SHEET FIGURE 4.B

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INITIALS	DATE	FOOD IRRADIATION REACTOR		
DRAWN BY	MJK	TYPICAL DRUM RADIATOR		
CHECKED BY		INSTALLATION		
APPROVED		INTERNUCLEAR COMPANY		
APPROVED		CLAYTON, MO.		
APPROVED		SCALE: 1"=1'	DRAWING N°	REV.
		SHEET	FIGURE 4.D

ENLARGED VIEW "A"
SCALE: HALF SIZE

ENLARGED VIEW "B"
SCALE: HALF SIZE

ENLARGED VIEW "C"
SCALE: HALF SIZE

TOP VIEW (TYPICAL)
WITHOUT PLATE

VIEW "E"
FULL SIZE

VIEW "D"
FULL SIZE

NOTE:
U PLATE WITH 1" SLOT AT
TOP WILL HAVE 1/2" SLOT AT
BOTTOM, TO BE ALTERNATED
THRU OUT RADIATOR WITH
U PLATE HAVING 1/2" SLOT
AT TOP AND 1" SLOT AT
BOTTOM.

MULTIPLE-SLAB RADIATOR
SCALE: 1" = 1'-0"

SINGLE-SLAB RADIATOR
SCALE: 1" = 1'-0"

INITIALS		DATE		FOOD IRRADIATION REACTOR	
DRAWN BY		DATE		SLAB - RADIATORS	
CHECKED BY		DATE		INTERNUCLEAR COMPANY	
APPROVED		DATE		CLAYTON, MO.	
APPROVED		DATE		SCALE: NOTED	
APPROVED		DATE		DRAWING NO.	
APPROVED		DATE		SHEET	
APPROVED		DATE		FIGURE 4.E	

Figure 4.F Indium Sulfate Solution Viscosity versus Absolute Temperature

FIG. 4.F

Figure 5.B Maximum Fuel Plate Surface Temperature versus Core Water Velocity

Fig. 5.B

Fig. 5.C

Fig. 5.D

Figure 5.E Primary Heat Exchanger Service Coefficient

Fig. 5.E

<u>Region</u>	<u>Composition</u>	<u>Thickness, cm</u>
1	Reactor Core	18.92 radius
2	Al + Air	4.08
3	Al + Water	1.75
4	SS	0.32
5	In ₂ (SO ₄) ₃ Sol.	15.24
6	Steel	7.62
7	Water	40.0

Fig. 6.A

Fig. 6.B

Figure 6.0 Neutron Multiplication Constant, K versus
Metal to Water Ratio, γ

($H = 3$ ft., $M_0 = 9.47$ Kg)

d = center to center spacing of fuel assemblies

Fig. 6.C

Fig. 6.D

Fig. 6.E

Figure 6.F Approximate Weight Percentage of Uranium in Fuel Plate Meat versus Uranium Loading, M_0 (Kg)

Reactor Height, $H = 3.0$ feet

Fig. 6.F

Fig. 6.G

Fig. 7.A

Figure 7.B Linear Gamma Absorption Coefficients for Indium Sulfate Solutions in Light Water

Fig. 7.B

Fig. 7.C

Fig. 7.E

Fig. 7.F

Fig. 8.A

Figure 8.B Variation of E_2 (Core-Blanket Efficiency Factor)
with Indium Sulfate Solution Density

$E_2 = (\text{Indium Absorptions per } ^{235}\text{U Absorption})$

Based on Figure 4.H INTERNUC-3

6" Blanket Thickness

Reactor Core - twenty-four 3"x3"x3" fuel assemblies

Fig. 8.B

Fig. 8.C

Fig. 8.D

Fig. 8.E

FIGURE 8.F E_3 (Volume-Circulation Efficiency Factor)
for Slug Flow in Both Blanket and Radiator - Case I

$$E_3 = \frac{\text{Disintegrations in Radiator}}{\text{Total Disintegrations}}$$

$$\omega = \frac{\text{Volume of Radiator}}{\text{Total Loop Volume}}$$

$$a = \frac{\text{Loop Cycle Time}}{\text{Mean Life of Radioactive Isotope}}$$

Fig. 8.F

Figure 8.G E_3 (Volume-Circulation Efficiency Factor) for
 Slug Flow in Blanket and Perfect Mixing in
 Radiator - Case II

$$E_3 = \frac{\text{Disintegrations in Radiator}}{\text{Total Disintegrations}}$$

$$\omega = \frac{\text{Volume of Radiator}}{\text{Total Loop Volume}}$$

$$a = \frac{\text{Loop Cycle Time}}{\text{Mean Life of Radioactive Isotope}}$$

Fig. 8.G

Figure 8.H E_3 (Volume-Circulation Efficiency Factor)
for Perfect Mixing in Blanket and Slug
Flow in Radiator - Case III

$$E_3 = \frac{\text{Disintegrations in Radiator}}{\text{Total Disintegrations}}$$

$$\omega = \frac{\text{Volume of Radiator}}{\text{Total Loop Volume}}$$

$$a = \frac{\text{Loop Cycle Time}}{\text{Mean Life of Radioactive Isotope}}$$

Fig. 8.H

Figure 8.1 E_3 (Volume-Circulation Efficiency Factor) for Perfect Mixing in Both Blanket and Radiator - Case IV

$$E_3 = \frac{\text{Disintegrations in Radiator}}{\text{Total Disintegrations}}$$

$$\omega = \frac{\text{Volume of Radiator}}{\text{Total Loop Volume}}$$

$$a = \frac{\text{Loop Cycle Time}}{\text{Mean Life of Radioactive Isotope}}$$

Fig. 8.1

Figure 8.J Effect of Vertically Directed Flow in Blanket on E_3

Fig. 8.J

Figure 8.K Effect of Radially Directed Flow in Blanket on E_3

Neutron flux distribution in radial direction given by

$$\phi(x) = e^{-0.19x}$$

where

inner radius of blanket, $x = 0$

outer radius of blanket, $x = 15.2$ cm

blanket thickness, 6 inches

E_3 (for calculated neutron flux distribution in blanket)

$\beta = \frac{E_3(\text{for uniform neutron flux in blanket})}{E_3(\text{for uniform neutron flux in blanket})}$

Fig. 8.K

Figure 8.1 E_{42} (Mass Absorption Efficiency Factor)
for No. 2 and No. 10 Can Radiators

Fig. 8.1

Figure 8.M Fraction of γ Energy Absorbed in Components and Contents of No. 2 Car Radiator

Indium Solution Density = 1.4 gms/cc

Absorption based on masses of materials and absorption coefficients only. (No correction for source self-absorption)

Fig. 8.M

Figure 8.N E_{4b} (Source Self-Absorption Correction Factor)
for No. 2 and No. 10 Can Radiators

Fig. 8.N

Figure 8.0 E_{4c} (Radial Self-Absorption Factor) for No. 2 and No. 10 Can Radiators

ρ = indium solution density, gms/cc

Fig. 8.0

Figure 8.P $E_{4\beta}$ (Axial Self-Absorption Factor) for No. 2 and No. 10 Can Radiators

Fig. 8.P

Figure 8.Q E_d (Radiator Efficiency Factor) for
No. 2 Can Radiator

ρ = indium solution density, gms/cc

Fig. 8.Q

Fig. 8.R

Figure 8.S E_3 (Volume-Circulation Efficiency Factor) for
No. 2 Can Radiator

Fig. 8.S

Fig. 8.T

Fig. 8.U

Fig. 8.V

Fig. 8.W

Fig. 8.X

Figure 8.Y $E_3 \times E_4$ for No. 10 Car Radiator - Indium
Solution Density of 1.4 grs/cc

Fig. 8.Y

Figure 8.2 $E_3 \times E_4$ for No. 10 Can Radiator - Indium Solution
Density of 1.6 gms/cc

Fig. 8.2

Fig. 8.AA

Fig. 8.BB

Figure 8.CC Reactor Power for No. 2 Can Radiator

Space between drum tubes, 0.25 in.

Reactor blanket thickness, 4 in.

Energy absorbed in product - 3 ton megarep/hr.

Fig. 8.CC

Fig. 8.DD

Figure 8.EE Reactor Power for No. 10 Can Radiator

Fig. 8.EE

FIGURE 8.FF
ARRANGEMENT FOR SLAB RADIATOR
STUDIES

Figure 8.GG $E_{\infty p}$ (Infinite Slab Efficiency Factor) for
Slab Sources of Various Thicknesses

Fig. 8.GG

Figure 8.HH E_{∞} (Infinite Slab Efficiency Factor) for Slab Sources with Steel Webs

Fig. 8.HH

Figure 8.II E_4 (Radiator Efficiency Factor) for Multiple-Unit Slab Source

Fig. 8.II

Fig. 8.JJ

Figure 8.KK E_3 (Volume-Circulation Efficiency Factor) for Two-Unit Slab Radiator

Fig. 8.KK

Figure 8.LL E_3 (Volume Circulation Efficiency Factor) for
Four-Unit Slab Radiator

Fig. 8.LL

Figure 8.MM $E_3 \times E_4$ for One-Unit Slab Radiator -
Indium Solution Density of 1.4 gms/cc

E_3 = volume-circulation efficiency factor

E_4 = radiator efficiency factor

Fig. 8.MM

Figure 8.NN $E_3 \times E_4$ for Two-Unit Slab Radiator - Indium
Solution Density of 1.4 gms/cc

E_3 = volume-circulation efficiency factor

E_4 = radiator efficiency factor

Fig. 8.NN

Figure 8.00 $E_3 \times E_4$ for Four-Unit Slab Radiator - Indium
Solution Density of 1.4 gms/cc

Fig. 8.00

Fig. 8.PP

Figure 8.QQ $E_2 \times E_3 \times E_4$ versus Indium Solution Density
for Two-Unit Slab Radiator

$$V_B + V_{PP} = 8 \text{ ft}^3$$

E_2 = core-blanket efficiency factor
 E_3 = volume-circulation efficiency factor
 E_4 = radiator efficiency factor

$E_2 \times E_3 \times E_4$

0.15

0.10

0.05

0

1.2

1.4

1.6

Indium Solution Density, ρ , gms/cc

Fig. 8.QQ

Figure 8.RR Production x Dose versus Reactor Power for
Slab and Drum Radiators

Fig. 8.RR

Fig. 8.SS

Fig. 8.TT

SECTION C-C
SEE FIG. 9.B

DRAWN BY		DATE		FOR	
CHECKED BY		DATE		FOR	
APPROVED		DATE		FOR	
APPROVED		DATE		FOR	
DRAWING NO.		SCALE		SHEET	
DRAWING NO.		SCALE		SHEET	
DRAWING NO.		SCALE		SHEET	
DRAWING NO.		SCALE		SHEET	

FOOD IRRADIATION REACTOR
REACTOR & RADIATOR BUILDINGS
VERTICAL SECTION C-C

INTERNUCLEAR COMPANY
CLAYTON, MO.

SCALE: 1/4" = 1'-0" DRAWING NO. 9.A

SHEET 1 OF 1

INITIALS	DATE	BY	DATE	BY
DRAWN BY	MJK			
CHECKED BY				
APPROVED				
APPROVED				
APPROVED				

SCALE	1/4" = 1'-0"	DRWING NO.	FIGURE 9.B	REV.
SHEET

SECTION F-F
SEE FIG. 9.C

SECTION G-G

SECTION H-H
SEE FIGURE 9.C

ALSO SEE FIGURE 9.A
FOR COOLING EQUIPMENT

INITIALS	DATE	REVISION
DRAWN BY		
CHECKED BY		
APPROVED		
APPROVED		

FOOD IRRADIATION REACTOR
COOLING EQUIPMENT LAYOUT
SECTIONS

INTERNUCLEAR COMPANY
CLAYTON, MO.

SCALE: 1/4" = 1'-0"
DRAWING NO. 9.D
SHEET

INITIALS	DATE	FIGURE 9.E
DRAWN BY	DATE	FIGURE 9.E
CHECKED BY	DATE	FIGURE 9.E
APPROVED	DATE	FIGURE 9.E
APPROVED	DATE	FIGURE 9.E
SCALE: 1/4" = 1'-0"	DRAWING NO.	REV.
SHEET	FIGURE 9.E	

	INITIALS	FOOD IRRADIATION REACTOR	
	DATE	RADIATOR BLDG.	
	MM/YY	VERTICAL SECTION B-B	
DRAWN BY		INTERNUCLEAR COMPANY	
CHECKED BY		CLAYTON, MO.	
APPROVED		SCALE: 1/4" = 1'-0"	DRAWING NO.
APPROVED		SHEET	FIGURE 9.F
			REV

Fig. 11.A

Fig. 11.B

Fig. 11.C

Fig. 11.D

Figure 11.E Time Required to Increase Indium Activity from Saturated Activity Corresponding to Power P_1 and Volume V_1 to Given Fraction of Saturated Activity Corresponding to Power P_0 and Volume V_0 - - By Operating at Power P_0

Fig. 11.E

Figure 11.F Time Required to Increase Indium Activity from Saturated Activity Corresponding to Power P_1 and Volume V_1 to Saturated Activity Corresponding to Power P_0 and Volume V_0 -- By Operating at Power P_2

Fig. 11.F

Fig. 11.G

Fig. 11.H

Figure 11.I Reactor Core-Fuel Assembly
Position Identification

Fig. 11.I

Figure A2.A Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 3$ in., $\gamma = 0.7$

Fig. A2.A

Figure A2.B Midplane Radial Thermal Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 3$ in., $\gamma = 0.7$

Fig. A2.B

Figure A2.C

Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 3$ in., $\gamma = 0.6$

Fig. A2.C

Figure A2.D Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 3$ in., $\gamma = 0.6$

Fig. A2.D

Figure A2.E Midplane Radial Fast Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 3$ in., $\gamma = 0.5$

Fig. A2.E

Figure A2.F

Midplane Radial Thermal Neutron Flux Dis-
tributionReactor Conditions: $H = 3 \text{ ft.}$, $d = 3 \text{ in.}$, $\gamma = 0.5$

336

Fig. A2.F

Figure A2.G Midplane Radial Fast Neutron Flux Distribution

337

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

(reference reactor)

Fig. A2.G

Figure A2.H Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in.,
 $\gamma = 0.7$ (reference reactor)

Fig. A2.H

Figure A2.I Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.6$

Fig. A2.I

Figure A2.J Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.6$

Fig. A2.J

Figure A2.K Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.5$

Fig. A2.K

Fig. A2.L Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.5$

Fig. A2.L

Figure A2.M Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 2.5$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig.A2.M

Figure A2.N

Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 2.5$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.N

Figure A2.0 Midplane Radial Fast Neutron Flux Distribution
 Reactor Conditions: $H = 2 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.0

Figure A2.P Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 2$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.P

Figure A2.Q Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.Q

Figure A2.R Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Figure A2.S Midplane Radial Fast Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 3 \text{ in.}$, $\gamma = 0.7$

Fig. A2.S

Figure A2.T Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.T

Figure A2.U Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.U

Figure A2.V Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.V

Figure A2.W

Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$ (Reference Reactor
for control rod
calculations)

Fig. A2.W

Figure A2.X Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$
 (Reference Reactor for Control Rod Calculations)

Fig. A2.X

Figure A2.Y Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.Y

Figure A2.Z Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.Z

Figure A2.AA

Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.AA

Figure A2.BB Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.BB

Figure A2.CC

Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.CC

Figure A2.DD Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.DD

Figure A2.EE Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.EE

Figure A2.FF

Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.FF

Figure A2.GG

Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.GG

Figure A2.HH Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Figure A2.II Midplane Radial Fast Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.II

Figure A2.JJ Midplane Radial Thermal Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 3 \text{ in.}$, $\gamma = 0.7$

Fig. A2.JJ

Figure A2.KK Midplane Radial Fast Neutron Flux Distribution
 Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.KK

Figure A2.LL

Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.LL

Figure A2.MM Midplane Radial Fast Neutron Flux Distribution
 Reactor Conditions: $H = 3 \text{ ft.}$, $d = 2.5 \text{ in.}$, $\gamma = 0.7$

Fig. A2.MM

Figure A2.NN

Midplane Radial Thermal Neutron Flux Distribution

Reactor Conditions: $H = 3$ ft., $d = 2.5$ in., $\gamma = 0.7$

Fig. A2.NN