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AEC RESEARCH AND DEVELOPMENT REPORT

DESIGN STUDY OF A NUCLEAR POWER PLANT FOR 100-KW ELECTRIC AND 400-KW HEAT CAPACITY

by

M. Treshow, A. R. Snider, and D. H. Shaftman

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ABSTRACT

A conceptional design study has been made of a low-power "package" reactor plant for the production of 100 kw of electrical power and 400 kw of heat at remote Arctic installations.

The power plant steam generator is proposed to be an unmanned, heterogeneous, boiling-type reactor capable of continuous operation for extended periods. The design is based on data derived from experiments with boiling-type reactors conducted by Argonne at the Reactor Testing Station, Arco, Idaho.

I. INTRODUCTION

The Army Reactors Branch has been apprised of a requirement for nuclear power plant systems capable of producing 100 kw of net electrical power and 400 kw of heat at remote Arctic installations. It was requested that a study be made at Argonne to determine the type of heterogeneous, boiling-reactor plant most feasible for this application.

The specification that "the power plant be capable of unmanned operation for as extended a period as can be achieved" was considered to be of major importance throughout this study. Therefore, the basic design principle was for simplicity of equipment and operation to ensure dependability and inherent safety of the unmanned plant.

The operating characteristics of a heterogeneous boiling reactor are being determined by the BORAX Experiments at Arco, Idaho.[1] The information derived from these experiments was used as a basis for the design presented in this report.

II. SUMMARY AND CONCLUSIONS

The unmanned nuclear power plant, as proposed in this report, should be capable of unattended operation for one to three years after initial startup. The duration of operation is dependent mainly upon metallurgical corrosion problems and the degree of reactivity control effected by the automatic control system.

The development and improvement of fuel alloys and structural materials to meet such basic requirements as corrosion resistance, adequate strength, and dimensional stability has been the subject of ever-continuing, concerted research. The advancements in fuel element technology which have evolved permit an optimistic prediction of a core lifetime of approximately three years.

Alternate methods of reactivity control have also been investigated. Physics calculations indicate that complete removal of the top fluid reflector is worth approximately 3% k in the operating reactor; therefore, up to this much control is made available by varying the level of the fluid above the reactor core. In the unmanned reactor, the fluid level could be varied by automatic control of the reactor feedwater. Such a control method is used in principle for the horizontal boiling reactor proposed in ANL-5327.(2) Another method which, according to physics estimates, could produce a 3% variation in k is the automatic variation of the water level in a vertical, annular tank which forms part of the reflector and which is immediately adjacent to the core. A control system worth 3 to 4% k is thought to be adequate for 3 years of operation of a reactor which has burnable poison, e.g., B^{10}, in the fuel plates and shim rods for the initial startup.

There are essentially two factors which will tend to disturb the balance. The first is the short-range influence of a variable electric load on the system; this would be effected by reactor power variation from 120% of normal load as an upper limit to 75% of the normal load as a lower limit. This reactor power variation corresponds to about 1/2% reactivity. A second and more important factor is the long-range influence of reactivity changes due to fuel burnup. This influence is, to a large degree, limited by the addition of burnable poison to the fuel; the maximum reactivity change is estimated to be of the order of 3% k during a three-year operating cycle. Such a change, if left unchecked, could produce great changes in the steam production amounting to several hundred per cent of normal load which could not be permitted outside of a brief period of emergency.

For these reasons, unattended reactor operation will require automatic means of control to supplement the control effected by the burnable poison alone.

It is the purpose of the present design to provide such automatic means outside of the reactor by utilizing simple inherent qualities of the reactor and steam cycle, rather than by relying upon automatic movement of control rods or of any other moving parts within the reactor proper. Such means have been found in the feedwater control systems which will be described in this report. A control capacity of at least 3% reactivity has been estimated to be available.

It is significant to note that if, for some reason, the automatic control system should fail to regulate the steam output in accordance with the variation of reactivity, the resulting increase of steam production and steam voids in the reactor core will maintain criticality. Excess steam not used in the turbine will be by-passed to the condenser. At no time during the operating cycle will the reactor have to be completely shut down because of temporary cessation of power requirements.

The turbine will operate with 295-psia, dry, saturated steam at the throttle and with atmospheric exhaust pressure. Two advantages are gained by exhausting to atmospheric pressure: (1) a vacuum system with its associated seal problem is avoided, and (2) the condenser coolant will have a sufficiently high temperature to be utilized directly in the space-heating system.

A dry-type, fluid cooler will provide the condenser with coolant. This feature is added to improve the versatility of the power plant at sites which are devoid of natural coolant water reservoirs.

In order to avoid expensive transportation of shielding materials an extensive use of the local soil or gravel material is proposed as the main ingredient in the biological shielding.

The reactor will be refueled at intervals of one to three years. The "used" core will be handled and shipped in a lead "coffin" as one complete unit. The coffin unit complies with all weight and size requirements.

The more important design features which evolved from this study are:

1. The plant is designed for totally unmanned operation.
2. No extensive development program is required before the construction of the prototype can begin.
3. There is simplicity of instrumentation.
4. The reactor tank pressure and temperature are low.
5. There is no heat exchanger.
6. There are no scram circuits owing to the inherent reactor safety against power excursions.
7. There is no pressurizer or high-pressure piping.

8. The pumping capacity required is low.
9. The heat flux and the neutron flux are low.
10. The fuel elements are of aluminum-nickel alloy, which results in inexpensive fabrication.
11. There are no moving control rods required during unmanned operation.
12. The condenser coolant supplies the heat to the space-heating system.
13. There is no water "make-up" system.
14. The plant is not dependent upon natural coolant water reservoirs.
15. The simplicity of the refueling procedure is realized by removing the core as a unit.

III. POWER PLANT DESIGN CHARACTERISTICS AND LOGISTICS

Figure 1 is the authors' conception of the reactor power plant installation. The components and operating characteristics of the plant system are given in the following tabulation.

A. Reactor

1. Performance

Power Level, mw	1.5
Av. Power Density (referring to water volume), kw/liter	14
Steam Pressure, psia	300
Steam Temperature, F	417
Steam Production (normal), lb/hr	5,030
Water Recirculation Rate, lb/lb	90-120
Av. Density Reduction Due to Boiling, %	8- 12

2. Core

Over-all Length, in.	23
Over-all Width, in.	24
Active Height, in.	23-5/8
Fuel Elements (Borax-type with aluminum-nickel alloy)	
Geometry, in.	3 x 3-1/4
Number of Elements	45
Number of Fuel Plates per Element	12
Total Thickness of Plates, in. (0.030-in. "meat," 0.020-Al-Ni clad)	0.070
Water Channel Gap, in.	0.20
Heat Transfer Area, sq ft	470
Fuel per Element (one-year reactor), gm U^{235}	191
Av. Heat Flux, Btu/(hr)(sq ft)	11,000
Av. Thermal Neutron Flux in Fuel Plates, n/(cm^2)(sec)	$\sim 7 \times 10^{12}$

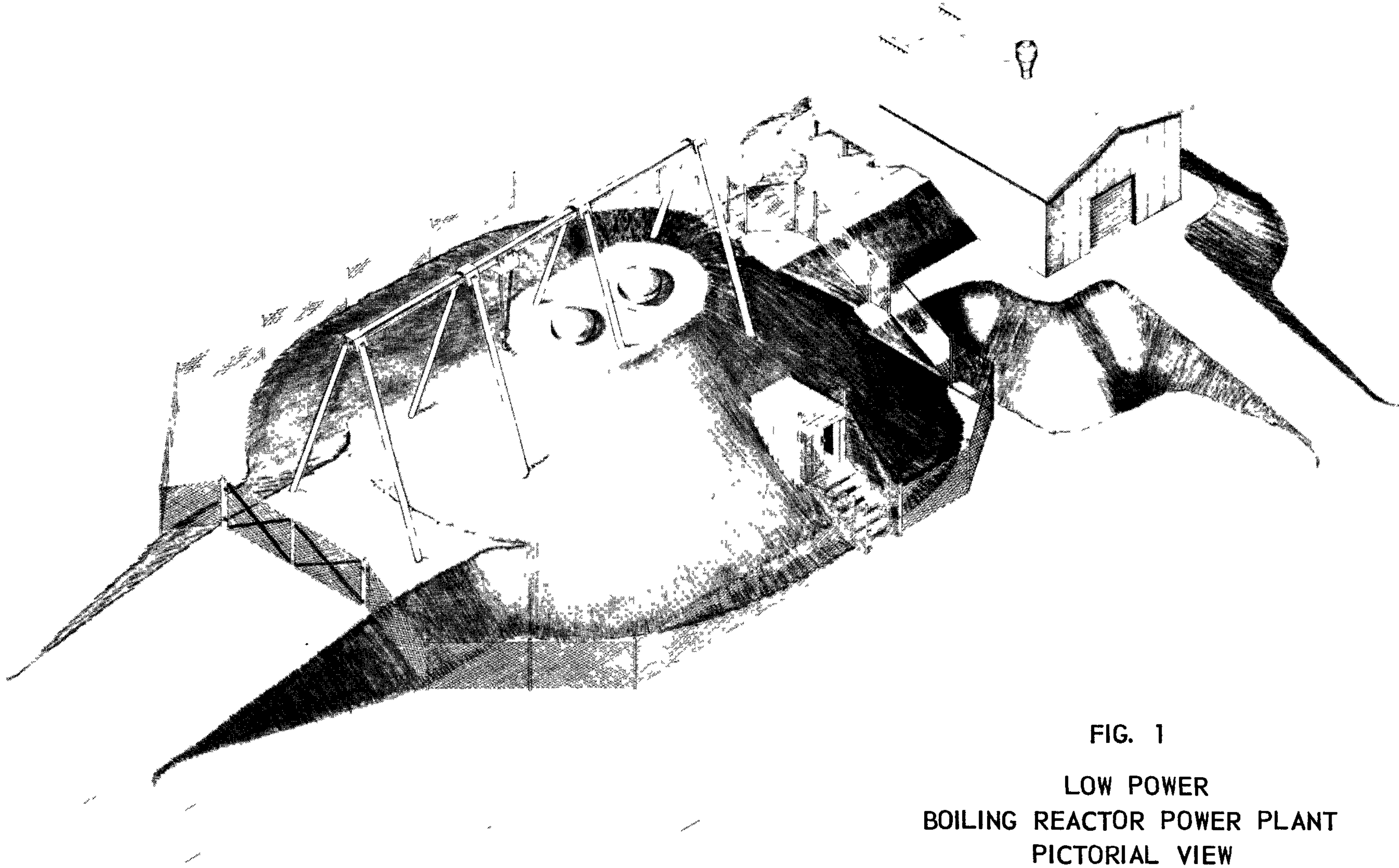

FIG. 1

LOW POWER
BOILING REACTOR POWER PLANT
PICTORIAL VIEW

Cold Metal-to-Water Volume Ratio (total core)	0.55
Total U^{235} Fuel Content (one-year reactor), kg	8.6
k_{eff}, Cold, Virgin Reactor (no xenon or samarium)	~1.10
k_{eff}, Operating Virgin Reactor (no steam voids; equilibrium xenon and samarium)	~1.03

3. Pressure Vessel

Material	Type 304 stainless steel
Tank ID, ft	4.0
Over-all Height, ft	11-1/2
Wall Thickness, in.	3/4
Insulation (Magnesia or Fiberglass) Thickness, in.	3

4. Control Rods (For startup only)

Composition — Cadmium, clad with aluminum-nickel alloy

Geometry

Blades arranged in 4 curtains.

Blade cross section: 4-1/2 in. wide x 1/8 in. thick

Number — 16

B. Turbine Building

Butler Type Construction

Dimensions, ft	18 x 24 x 30
Door Size, ft	8 x 11
Reinforced Concrete Floor Slab Thickness, in.	5

C. Turbine Generator

Steam Flow, lb/hr	4,930
Plant Electrical Operating Factor, %	75
Generator Output (Power Factor = 0.8), kw	150

Auxiliary Electrical Power, kw	33
Throttle Pressure, psia	295
Exhaust Pressure, psia	14.7

D. Condenser

Type	Shell and Tube
Tube Material	6063 T831 Aluminum Alloy
Shell Material	ASTM-A285 stainless
Tube Size	3/4 in. ID x 10 ft long
Surface Area, sq ft	350
Heat Capacity, mw	2.0
Operating Pressure, psia	14.7
Design Pressure, psia	50.0
Oxygen Removal, cc/liter	
Guaranteed	0.03
Expected	0.01
Coolant, wt-%	60% ethylene glycol, 40% water
Flow, gpm	245
Inlet Temperature, F	140
Outlet Temperature, F	180
Flash Point, F	240
Fire Point, F	245
Freezing Point, F	-58
Sp. Heat at 158F, Btu/(lb)(F)	0.824

E. Pumps

1. Condenser Feedwater Pump

Standby Capacity, %	100
Stages	6
Flow, gpm	20
Total Head, ft	950
Up and Down Thrust, lb	660

Net Positive Suction Head, ft	4
Pump Speed, rpm	3,500
Efficiency, %	31
Pump Brake Horsepower (per pump)	14.5
Driver, hp	15
Driver Elec. Requirements: 220-440, 3-phase 60-cycle	

2. Coolant Circulating Pump

Standby Capacity, %	100
Flow, gpm	300
Total Head, ft	40
Pump Brake Horsepower (per pump)	7.5
Driver Power, hp	10.0
Net Positive Suction Head, ft	10
Pump Speed, rpm	1,750
Motor Power Requirements: 440, 3-phase, 60-cycle	

F. Dry-Type Fluid Cooler

Design Ambient Air Temperature, F	90
Elevation Above Sea Level	0
Heat Load, Btu/hr	5,000,000
Coolant (by weight)	60% ethylene glycol, 40% water
Flow Required, gpm	245
Temperature to Cooler, F	180
Temperature from Cooler, F	140
Pressure Drop through Cooler, psi	1.0
Fans per Cooler	2
Fans	
Std. Air Delivered per Fan, cfm	63,750
Required Power per Fan, hp	8.5
Type of Fan	Adjustable Pitch

Motor Furnished, hp	10.0
Blade Diameter, in.	108

Controls

Automatic Shutter Controls

Adjustable Pitch Fan Blades

G. Heating System

Heat Required, kw	400
Plant Heat Operating Factor, %	50
Temperature of Heating Fluid, F	180

H. Construction Schedule

The construction time of the prototype plant should not exceed two months from date of delivery of equipment at the reactor site. Industrial estimates on delivery dates indicate that under normal conditions all of the power plant equipment would be available for erection within 10 months after the letting of the final contract. On-the-site erection time for future plants should not exceed one and one-half months.

The plant is designed for a minimum of construction at the site. All of the power plant components will be assembled for shipment in packages 7.5 ft high x 9 ft wide x 20 ft long, not exceeding 10 short tons. The turbine-generator set will be shipped as a unit. The reactor will, likewise, be shipped fully assembled.

I. Estimated Weight of Reactor Plant

The following table is a summary of the weights of the plant equipment which must be transported to the reactor power plant site. It should be noted that the over-all, erected size of the building and Dry-Type Fluid Cooler is of no consequence since they are both disassembled and shipped as a packaged units. Only the sizes of the larger components are listed. The reactor shielding and the plant foundation pad are excluded, as these materials are assumed to be available locally. The total plant equipment weighs approximately 89 tons.

Reactor	Weight, Pounds
Reactor Vessel	8,500
Pressure Vessel Lid	1,500
Core Lattice Structure	300

Reactor	Weight, Pounds	
Control Rods	150	
Control Rod Drive System	350	
Feedwater Spray System	100	
Reactor Vessel Insulation	500	
Steam Separators	800	
Fuel Elements	500	
Reactor Instruments	500	
Fuel Element Boxes	50	
Miscellaneous	750	
		14,000

Power and Heating Component		
Turbine Generator Set (4-1/2 ft x 4-1/2 ft x 12-1/2 ft)	16,000	
Deaerating Condenser (4 ft x 6 ft x 13 ft)	4,000	
Pumps		
Condenser Feedwater	3,300	
Coolant Circulating	1,400	
Dry Type Fluid Cooler	17,400	
Valves	2,400	
Piping	2,600	
Ethylene Glycol Coolant	7,200	
Glycol Expansion Tank	60	
Glycol Storage Tank (4 ft dia x 4 ft high)	1,000	
Steam Ejector	60	
Instrumentation	500	
		55,920

Water Purification		
Ion Exchange Unit and Resins	700	
Filter and Filter Unit	110	
Cooler		
Counter Flow Type	110	
One Pass Type	60	
Storage Tank (7 ft dia x 7 ft high)	2,000	
		2,980
Core Handling Facilities		
Coffin	18,400	
Unloading Tools	500	
Coffin Pit Cylinder	2,000	
		20,900
Building		12,800
Cement & Reinforcement for Floor Slab		8,000
Miscellaneous	**Weight, Pounds**	
7-1/2 Ton (overhead) Crane	13,000	
10-Ton Crane	4,500	
Building Crane Structure	1,000	
A-Frame Crane Structure	4,000	
Plant Foundation Vents	10,000	
I-Beams for Foundations	15,000	
Steam Line Culvert	500	
Pressure Vessel Shield Cylinder	2,000	
Control Cave	4,000	
Cave Shielding Wall	8,000	
Miscellaneous	2,200	
		64,200
Total Weight of Plant Equipment		178,000 pounds
	or	89 tons

IV. DESCRIPTION OF THE REACTOR AND THE REACTOR FACILITIES

The reactor, shown in Figs. 2 and 3 is similar to the BORAX Reactors. The reactor core is assembled from BORAX-type fuel elements and is located in a vertical pressure vessel.

A. Reactor Core

The core consists of 45 BORAX-type fuel assemblies (Fig. 4). Each assembly contains 12 fuel plates (0.03 in. "meat," 0.020 in. clad) with a coolant channel gap of 0.20 in. between plates. Each element is expected to contain 191 gm of U^{235}, which is equivalent to 18% by weight or about 2-1/4% by volume.

The fuel elements are similar to those which have been tested in the BORAX reactor. The experiments have indicated the need for greater rigidity and mechanical strength. A new type of element is being fabricated for future application in BORAX. If the elements perform satisfactorily, the core shown in Fig. 4 will be modified accordingly. It should be noted that the metallurgical requirements for the unmanned reactor core are less severe than specified for the BORAX reactors; the power density and heat flux is lower by a factor of 2 or 3.

Recent corrosion tests at Argonne(20) have shown that small additions of nickel have substantially improved the corrosion resistance of aluminum. Therefore, it is considered that all of the aluminum in the core be alloyed with about 1/2 to 1-1/2 wt-% nickel. (Physics calculations are based on the use of 1/2% nickel by volume, which is approximately 1-1/2% by weight.)

The core is mounted permanently on a lead plate which is cast in the bottom of an aluminum lifting shroud. The cylindrical sidewalls of the shroud extend up to the top level of the core. Relatively pure lead is used to avoid induced radiation. The lead plate also forms the bottom closure of the coffin into which the spent core is removed for transportation to the fuel re-processing plant.

Subcooled feedwater circulates down between the lifting shroud and the vessel wall and up (by natural convection) through stainless steel tubes which perforate the lead mounting plate. The flow continues upward through the core and discharges into the bulk of the circulating fluid.

B. Pressure Vessel

The vessel is designed for a pressure of 400 psi, in accordance with the ASME Code for Unfired Pressure Vessels (1952). The shell material is Type 304 stainless steel plate 3/4 in. thick.

The vessel is 11-1/2 ft high with an inside diameter of 4 ft. The top end of the shell is equipped with a steel flange (5-1/2 in. thick) to accommodate a bolted steel cover plate. The interfaces of the cover plate and flange are lined with Type 304 stainless steel to prevent corrosion and sealed with a Flexi-tallic or equivalent type gasket.

The inlet and outlet openings for the water and steam connections are located at or near the pressure vessel head. The steam separators are welded to the cover plate in such a manner that they will be removed together with the cover.

The "Multidome" type moisture separator consists of 15 small stainless steel "Cyclone" cylinders mounted on a steam manifold which is bolted to the underside of the cover plate. The structure also contains radial "spokes" which support the central guide bearing for the control assembly.

Two feedwater spray rings are used to control the degree of preheating or subcooling of the reactor inlet coolant water. The rings are supported by stainless steel legs connected to the cover plate. The rings are connected to two feedwater inlets drilled into the edge of the cover plate between adjacent head bolt holes. The stainless steel legs also support the control assembly guide bearing.

A layer of magnesia or glass fiber insulation is used to limit the pressure vessel heat loss to 10 kw of equivalent heat.

The vessel is suspended (by the closure flange) inside a cylindrical steel structure which forms a part of the primary shielding. This method of support, along with the absence of any piping far below the flange, allows the pressure vessel to expand and contract freely.

The steel cylinder consists of two half-sheets bolted together (at the site). The assembled cylinder, in turn, is bolted to structural steel beams placed in the foundation bed or slab. Shielding soil or gravel material is piled up against the outer surface of the cylinder.

If a gravel foundation bed is used over the "Permafrost" surface, a number of 12-in. corrugated vent pipes are placed at the bottom level of the gravel to prevent thawing of the subsoil. The cooling effect of these ducts can be enhanced by connecting six of them to the annular air space between the reactor vessel and the cylindrical shield. The upper end of the shield cylinder will have vent openings so that a "chimney effect" is provided due to heating of the air in the reactor pit.

C. Reactor Control and Instrumentation

As mentioned previously, the plant is designed for unmanned operation and requires a minimum of delicate mechanisms. It is intended to avoid any automatic moving devices in connection with the reactor proper, particularly inside or through the wall of the pressure vessel.

The control rods are only intended to be used for the initial startup of the reactor. Actually, they can be used as shim rods if at any time it is desirable to change or re-adjust the normal power level.

Figure 2 shows the control blades in their "in" position. The vertical drive spindle, the bevel gear drive and the horizontal drive shaft are also shown. The drive shaft is located in a tubular housing which extends up through the pressure vessel cover. The opening in the cover plate and in the drive shaft housing is sealed with pressure-tight packing.

The control blades proper are made of cadmium with a cladding of aluminum-nickel alloy. The cadmium strips are 2 ft long. The casings extend at both ends to reach a total length of about 3 ft. When the controls are all the way out the inert ends of the blades still project at least 8 in. into the control blade channels in the core.

While the vertical position of the drive spindle is maintained by means of a thrust bearing in the bevel gear box, the spindle is guided in a chrome-coated bushing or bearing located about 2 ft above the top end of the control assembly. The bearing is mounted in the center of a "spider" connecting the vertical legs of the structure which extends down from the pressure vessel cover plate.

The spindle is 2 in. in diameter and is equipped with a coarse square thread. The control assembly top frame has a tubular extension with an inside thread 6 in. long. In its bottom position the tubular extension is held firmly against the lower surface of the guide bearing.

It is important that the control blades be prevented from rattling or vibrating against the walls of the core channel.

The rigidity of the top of the control blades is assured by mounting of all the blades to a common assembly frame. The guide bearing, which is particularly effective when the control blades are in the "all out" position, steadies the upper end of the control assembly.

The control blades should be rolled to a slightly curved cross section so that a slight pressure will be exerted by the blades on both walls of the control blade channels to prevent the lower end of the control blades from vibrating. Since the control assembly is to be moved only two or three times during the core lifetime, no surface galling problems are anticipated. In the normal operating position (all out) only the aluminum-nickel control blade extensions will be in contact with channel walls, which are also of aluminum-nickel material. The reactor will be installed with the control rods fully in the core. The rods are designed to be nearly out of the core at the end of the startup period; they will remain in this position until the core is ready to be exchanged.

If the operating cycle is to last over several years, it is felt that the rods may be of benefit for an occasional readjustment of the reactor power level. The control rods may be positioned, either manually (from control core) or by a motor (turbine building). The drive system will be designed so that under no circumstances will an operator be able to remove the rods at a rate fast enough to place the reactor on a dangerously short period.

The maintenance of reactivity during operation is accomplished by means of automatic feedwater control. The operation of this system is described in detail in Chapter VI.

The necessary instrumentation and mechanisms for reactor startup are housed in a control cave which is located in the gravel shielding 10 ft from the reactor shield cylinder. The gravel shielding between the cave and the cylinder is augmented by Boral and metal shielding. The instrumentation and mechanisms include:

1. The hand-operated drive gear for the shim control rod assembly as well as the control rod drive motor used for positioning the control rods from the turbine building.

2. The necessary ion and fission chambers. (The instruments connected to the chambers may be installed in the turbine building or they may be portable instruments brought by the special personnel servicing the plant during the startup and periodic preventive maintenance inspection.)

3. A control rod position indicator which also can be read in the turbine building.

4. A reactor water level indicator.

5. Portable "survey meters" to monitor the radiation intensity. Similar meters will also be located in the turbine building.

Access to the control cave is permitted at all times during reactor operation.

D. Shielding

The basic shield design principle to follow when considering a remotely located nuclear power plant is to provide safety from radiation with a material which will require a minimum of transportation weight and bulk. Therefore, a concrete-type shield is not feasible. The more desirable materials appear to be either a metal and water shield or a gravel shield built from suitable materials available near the reactor site. For the purpose of this report, the gravel-type shield was selected. It is to be recognized that the final analysis of the shield design will be based on the conditions at the individual sites.

Order of magnitude shielding calculations indicate that the required radiation levels (0.3 rem/week, assuming a 24-hour day at the plant) could be maintained with the use of a 11-1/2 ft thick ordinary concrete (150 lb/cu ft) shield placed around the reactor pressure vessel. The concrete can be replaced by dirt or gravel in thicknesses varying inversely as their density or by lead in the ratio of 9:1 (up to 4 in. lead). This is due to the fact that, in general, a concrete shield is determined by gamma-ray attenuation. The same reasoning indicates that 2-1/2 in. of steel or 1-1/2 in. of lead is equivalent to 13-1/2 in. (1.1 ft) of concrete. The gravel shield material used in this design is assumed to have a density of 120 lb/cu ft. The personnel outside the exclusion area are then shielded by 2-1/2 in. of steel and 13 ft of gravel.

2-1/2 in. steel	=	1.1 ft of concrete
13 ft of gravel	=	10.4 ft of concrete
		11.5 ft of concrete

As seen from Figs. 5 and 6 there is a minimum of 13 ft of gravel at an elevation of 14 ft above the ground level. Above the 14-ft level, advantage is taken of the exclusion area in reducing the radiation to tolerance levels. At least a 25-ft exclusion radius is required.

The top of the reactor is shielded by 7-1/2 in. of iron (equal to 3.3 ft of concrete) and 4.5 in. of lead (~3.3 ft of concrete). The exclusion radius is approximately 50 ft of air and is equivalent to approximately 1-1/2 ft of concrete. It is estimated that the lack of 3.4 ft of concrete on the top shield plus the fact that the capture gamma-ray source has been changed will not create a radiation hazard outside of the exclusion area.

The shielding between the control cave and reactor shield cylinder is composed of 2-1/2 in. steel (1.1 ft concrete), 10 ft of dirt (8 ft of concrete), and either an 8-in. steel or a 5-in. lead shielding wall (3.7 ft concrete) with additional Boral plates. The chamber thimbles are provided with 15-in. thick lead plugs to prevent radiation streaming through the tubes and into the control cave.

Shielding requirements for the reactor after shutdown are presented in terms of exclusion distance and cooling time in Fig. 7. For example, after one-day shutdown and at a distance of 40 ft from the bare reactor core, the radiation level is 300 r/hr. It is estimated that even after 100 days cooling the radiation level would be 600 mr/hr at a distance of 200 ft. The amount of shielding to be provided by the coffin may be determined from Fig. 7 and a knowledge that the radiation will be reduced by a factor of 10 by 2 in. of lead. The induced activity due to the steel reactor vessel lid will be a factor of 10^4 less than the core activity; therefore, it does not present a hazard during the fuel reloading procedure.

It is assumed that the fuel reloading will commence 8 hrs after shutdown and that the reactor vessel has been filled with water to the steam exit pipe height. It is also assumed that each member of the refueling crew may be subject to not more than 100 mr/day for 3 days.

The unbolting and clearing of the top end of the reactor pressure vessel should not take more than 45 min. The shielding provided during shutdown maintains a 50 mr/hr level for this operation.

The coffin (7 in. thick lead) is so designed as to attenuate the 150 r/hr core activity to 50 mr/hr at a 60-ft distance from the coffin surface (8 hrs after reactor shutdown). The coffin will be moved by the overhead crane to the coffin storage pit will all operating personnel outside the exclusion fence. The actual transfer of the coffin from the reactor pit to the storage pit should not take more than 5 min. However, the men could be exposed for 3 hrs with the coffin in the air and not exceed 150 mr/hr in that time.

Fifty days after shutdown the activity at 20 ft from the coffin is 50 mr/hr. This would allow a pilot seated 20 ft from the coffin to fly for a period of 6 hrs and not exceed 300 mr/week tolerance level. Approximately 3 hrs flying time is required to transfer the coffin to the nearest railhead (750 mi.). Therefore, the same pilot could make two 3-hr trips a week or one 6-hr trip per week.

V. DESCRIPTION OF POWER PLANT

A. Building Facilities

Figures 8, 9, and 10 show the floor plan view, the elevation view, and the cross-sectional view of the power plant layout.

The power plant is housed in a Butler-type steel building insulated for a minus 60-degree Fahrenheit temperature and provided with one 8 ft by 11 ft access door and one stationary ventilator. The building is reinforced to withstand 100-knot wind gusts and to provide support for an overhead crane. The floor is a reinforced concrete slab (30-ft long, 24-ft wide, and 5-in. thick).

A motor-driven, overhead crane (7-1/2 ton) traverses the power plant building. The plant layout allows any single piece of equipment except the condenser to be removed or positioned in the building with the assistance of the crane. The condenser is located above the turbine to provide the required net positive suction head (NPSH) for the feedwater pump and is located in a position with respect to the width of the building to allow for tube repair without moving the condenser.

B. Turbine-Generator Set

The turbine is a 150-kw, 5000-rpm, non-condensing, single-stage, steam turbine. There is provided a 187.5-kva, 150-kw, at 80% power factor, three-phase, 60-cycle, 440-volt, 1200-rpm alternating current generator with directly connected 125-volt exciter. A double helical, single-reduction gear is provided to reduce the turbine speed of 5000 rpm to the generator speed at 1200 rpm. Flexible couplings are used for connecting the turbine to the gear and the gear to the generator.

The turbo-generator is mounted as a complete unit on a structural steel baseplate. The turbo-generator is provided with a constant speed oil relay governor, emergency overspeed governor, trip and throttle valve, motor and hand-operated synchronizing devices, vibrating-type tachometer, complete oil lubrication system gauges and gauge board, 4-in. steam inlet opening, and an 8-in. side exhaust opening.

A basic design principle for the steam cycle equipment is that there will be essentially no leakage from the system. The turbine seals are designed so as to allow any leakage to be recovered by a suction recovery system. A single-stage turbine was selected to simplify the turbine seal problem. The turbine-generator is designed for one-year unattended operation.

The turbine impeller is made of a 13% chrome-low-carbon steel. The corrosion-erosion properties of this metal are compatible with the use of stainless steel throughout the steam cycle system.

C. Recovery System

A steam-jet air ejector is connected to the steam line to provide a 2-in. H_2O suction to the turbine and condenser feedwater pump seals. The residue picked up in the recovery system is deposited in the condenser where any entrapped air is removed from the recovered steam.

D. Condenser

The condenser is a four-pass shell and tube-type unit designed to absorb 2.0 mw of heat. It is structurally designed for 50 psia steam pressure and will operate at atmospheric pressure. The coolant is pumped through aluminum tubes (3/4 in. by 10 ft long). Aluminum was selected as the tube material instead of Admiralty Metal to eliminate copper from the steam system. The condenser is equipped with a deaerating outlet section to control the oxygen in the condensate to a value not in excess of 0.03 cc O_2/liter; a value as low as 0.01 cc O_2/liter can be expected.

An air-relief valve has been placed on the condenser to allow the periodic escape of the gases that are removed from the condensate to the atmosphere with only a minimum loss of water.

A safety relief valve set at 40 psia is also located on the condenser to protect the equipment from excessive pressure in the event of an emergency.

E. Dry-Type Fluid Cooler

The condenser coolant, bearing lubrication coolant, and the reactor cleanup system coolant will be supplied by a dry-type fluid-cooler unit. The coolant has been designed to remove 5,000,000 Btu/hr of heat with 90F air from a 60% ethylene glycol-water solution. Two hundred and forty-five gallons per minute (245 gpm) of the "antifreeze" solution will flow through the cooler and the condenser. The antifreeze will be cooled from 180F to 140F.

The cooler has a vertical air discharge to prevent cross-wind interference and blanketed core surfaces. The two fans are mounted beneath the coil sections on separate tripods for vibration-free operation. The cooler is provided with interlocking, heavy-gauge, fabricated steel blade shutters which are automatically adjusted by a temperature controller to meet load requirements. Adjustable pitch blades are also utilized to automatically control the coolant exit temperature. As the coolant exit temperature decreases, the pitch on the fan blades adjust to a new position. When the adjustable pitch fan blades can no longer control the exit temperature, the shutters commence to close. The fan-drive motors are protected from the weather by metal enclosures and provide the required 8.5 hp for each fan.

F. Cooling Medium

The antifreeze coolant (60% ethylene glycol-40% water) is used to protect the dry-type fluid cooler, the coolant piping, coolant pumps, and the condenser tubes from "freezing up" in the event of an accidental shutdown of the reactor.

Instead of having a sharp freezing point, the antifreeze becomes a slush due to the formation of ice crystals at -58F; and as the temperature decreases, the solution becomes more viscous and eventually fails to flow. Therefore, no damage to the coolant equipment is expected at a -60F temperature.

No storage or handling problem is anticipated since the fire point of the solution is above the boiling point of water.

Coupled samples of aluminum and steel were tested in pure ethylene glycol by the Dow Chemical Company. The reported weight change rate of the aluminum was 0.0096 mg/(cm^2)(day). This is a relatively low corrosion rate and provides an indication that no serious corrosion problem will arise from the use of the antifreeze solution in an aluminum tube condenser. A basic solution should be maintained by the addition of a small quantity of borax to the antifreeze before reactor startup. An anti-foaming agent and an anti-creep agent will be added in very small quantities to maintain favorable heat-transer properties and to assist with the sealing of the coolant system.

G. Pumps

One hundred per cent standby capacity is provided for the condensate feedwater pump and for the coolant circulating pump.

The feedwater pumps are provided with stainless steel impellers. The standby capacity is automatically controlled by a level indicator located in the condenser hot well.

The coolant circulating pump standby capacity is automatically controlled by a temperature control on the "dry-cooler" exit line.

H. Ion Exchangers

Two mixed-bed-type ion exchangers are provided for the startup water purification system and for the reactor cleanup system. A quality-control instrument is provided. All piping in the reactor installation is stainless steel.

The recommended strong cation resin may be HCR (Dow) or IR-120 (Rohm and Haas) in the (H) acid-generated form, having not less than 40,000 equivalent grams of $CaCO_3$ (2 gm-eq/liter) total titrated ion exchange capacity/cu ft of resin.

The recommended basic anion resin may be SBR (Dowex-1) or IRH-406 (Rohm and Haas) in the (OH-Fill base) caustic generated from having not less than 14,000 equivalent grams as $CaCO_3$ (1 gm-eq/liter) total titrated ion exchange capacity/cu ft of resin.

The recommended ratio of anion to cation resin is 2 to 1. Each resin should be sufficiently rinsed (by the supplier) with distilled water to remove any existing quantity of generant.

I. Storage Tanks

One 1000-gallon stainless steel tank is provided for storage of the distilled water to be used in filling the reactor upon startup and after emergency shutdowns. This tank also provides water for the 500-gallon stainless steel antifreeze mixing and storage tank.

J. Feedwater-Control System

The pre-heat feedwater-control system in shown in Fig. 11. The components of the system are:

1. A turbine trip and throttle valve which meters the amount of steam required by the turbine to meet the electric load demands.

2. A pressure-operated, regulating valve which maintains the pressure in the steam line to a predetermined value by by-passing the steam produced by the reactor in excess of that required by the turbine.

3. A three-way pressure regulated feedwater valve which controls the per cent saturated or the per cent subcooled condition of the feedwater returning from the condenser hot well to the reactor.

4. An orifice-type flow meter which controls the position of the three-way feedwater regulating valve. The orifice is designed to produce a sufficient pressure signal when 100 lb of steam per hour is flowing through the by-pass line to commence the positioning of the feedwater regulating valve. The 100-lb of steam per hour flow was selected as the minimum flow consistent with good orifice design practice.

Figure 11a shows a feedwater-control system which operates on the principle of varying the level of the top reflector fluid. The water level is varied to maintain criticality in spite of variable steam demand or, more importantly, the long-term reactivity changes caused by the differential burnup of fuel and boron.

It is expected that 3% k can be controlled in this manner. Indications are that this will be sufficient control capacity to effectively match reactor power with turbine steam demands over a three-year operating period without necessitiating the by-pass of excessive amounts of steam to the condenser.

The items required for this type of operation are:

1. A turbine trip and throttle valve which meters the amount of steam required by the turbine to meet the electric load demands.

2. A steam pressure-operated regulating valve (Fig. 11a) which will by-pass feedwater from the feedwater pump to the condenser hot well.

3. A condenser hot well with sufficient water capacity to: (1) compensate for system leakage and (2) provide storage for the feedwater which the top reflector control system does not return directly to the reactor.

K. Instrumentation

The simplicity and the inherent self-regulating characteristics of the reactor design has effected a substantial reduction in elaborate and delicate nuclear instrumentation.

The turbine-generator instrumentation has been described previously. Pressure and temperature gauges will be located at points in the steam-cycle system to register pertinent operating data.

An electrical power meter is required for the reactor startup and for the reactor rod calibration.

A water-level indicator is located in the condenser hot well to provide the necessary automatic controls for the standby condensate feedwater pump.

A temperature controller is required to provide the necessary automatic control for the standby coolant circulating pump.

If it should prove to be desirable to provide a simple system of monitoring the reactor power from a central control station, it is expected that such a system could be easily and inexpensively designed.

L. Space Heating System

The 400-kw space heating system is operated with 180F, anti-freeze solution supplied from the condenser coolant exit.

It is realized that the "Dry-Cooler" could be dispensed with if sufficient quantities of water for condenser coolant are available at a reactor site. In this case the cooling water will be pumped through the condenser and the space heating system when required, and returned to the reservoir.

VI. PLANT OPERATION

The flow diagram for the combined power and heating plant is presented in Fig. 11.

The unmanned package reactor power plant will allow the maximum specified electrical overload conditions to be met at any time during the operating cycle. Unmanned reactor operation will impose a slight lowering of the over-all plant efficiency since there will be periods of low power demand when the reactor will produce steam in excess of the turbine demands. A careful investigation of the over-all plant operating characteristics shows that when operating at a 75% electrical load factor, there will be no substantial financial penalty for the periodical wasting of small quantities of steam.

Fuel element metallurgical considerations indicate that a reactor cleanup system, as provided, is desirable to maintain water conditions in the reactor core which are favorable to longer fuel element lifetime. The "reactor cleanup" system purges 2 gpm of the water from the top of the reactor core through an ion exchanger and returns the purged water back to the steam cycle. Coolers are located between the reactor and the ion exchanger to protect the ion exchange resins from high-temperature water.

For economy reasons the majority of the heat loss from the reactor to the "cleanup" system water is recovered in a feedwater, counterflow-type, heater. Sufficient heat is then extracted by a coolant supplied from a separate loop in the Dry Cooler to maintain a temperature of the water reaching the ion exchanger of not greater than 100F. As can be seen from the flow diagram, the reactor cleanup system can be completely isolated to facilitate preventive maintenance without interrupting the reactor operation by "by-passing" the feedwater heater.

The condenser feedwater pump is used to initially fill the system with the water from the water storage tank. After the water level in the reactor pressure vessel has reached the desired height, the water is continuously circulated through the reactor cleanup system until the desired purity is obtained. Although the reactor cleanup system can be used as a purifier for the reactor water, every effort should be made to obtain pure water in the storage tank before introducing the water into the reactor system. During "reactor startup" and for the first week of operation, the condenser feedwater is returned to the reactor through the filter provided. However, since the filter has a relatively poor efficiency after only a few operating days and since the construction residue would be largely cleared from the system during the first few days of operation, the filter is isolated from the main steam cycle during the unmanned portion of the operating cycle.

The system has been designed so as to allow the very minimum of leakage (less than 1/2 lb of water per day). The leakage requirement is thought to be realistic since, after startup, there are only three operating valves, and all three operating valves, the condensate feedwater pump and the turbine seals, are provided with a simple but effective suction-type recovery system. An additional safety margin against leakage is provided over that of a standard seal-type valve by utilizing bellows-type seals on all of the isolation valves. The major quantity of moisture that could escape from the system with the release of entrapped gases in the condenser is salvaged by an air-release valve.

The reactor power plant system, therefore, requires neither an elaborate make-up water system, nor the normal abundant cooling water supply.

The following general discussion is presented to indicate in a qualitative manner how an unmanned reactor plant of the type presented in this report will operate. The quantitative values are indicative only and the precise performance characteristics must, of course, be proven by actual experimentation with a prototype of the proposed reactor.

A. Reactor Startup and Operation

Complete advantage of the inherent safeties of boiling reactors is taken throughout the instrumentation program to reduce the requirements for delicate instruments to a minimum. The reactor does not require any scram-type control rod circuits. Reactor start-up safety is assured simply by designing the control rod drive system in such a way that under no circumstances could the control rods be withdrawn from the reactor core at a rate which would place the reactor on an excessively short period.

There will be provided, for start-up purposes, a "conventional" fission chamber circuit and compensated ion chamber circuit to provide a count rate recording and a flux period and power level indication. In the prototype plant a start-up neutron source will be built into the reactor core. The requirement for a start-up source in future plants modeled from the prototype reactor might be eliminated when experimentation indicates to what degree the future plants will react in the same manner as the prototype.

The counting chamber tubes extend from the control cave to the reactor pressure vessel. The chamber circuit instruments are located in the control cave. The chambers will be normally removed from the chamber tubes during the unmanned operating cycle and must be checked before usage to insure operability.

The count rate recorder and the flux power level and period indicator can be located in the turbine building along with the control drive motor switch and control rod position indicator.

The initial fuel loading method for this prototype reactor is essentially not different from those of any "new reactor" (such as the APPR-I).

The experience gained from the prototype plant will be used in building cores for reactors modeled from the prototype and the lengthy initial fuel loading procedure used on "new" reactors will have been eliminated.

The start-up procedure is basically to fill the reactor with water, purify the water and commence the withdrawal of the control rod curtain until the reactor has reached criticality. The control rods will have to be adjusted to maintain criticality until maximum xenon has built up in the reactor. The control rods then are set for a specified reactor power level with the feedwater being returned to the reactor core 100% subcooled. At this point the reactor can be left to unmanned operation.

The inherent characteristics of a heterogeneous-type, boiling reactor will permit a limited degree of variation in the reactor reactivity without movement of the reactor control rods by controlling the degree of feedwater return preheating. The ability of the "feedwater control" to vary the reactor power level is discussed in Appendix A. As discussed in Appendix B, the characteristics of a burnable poison fuel depict a change in the reactor reactivity over the reactor operating cycle due to the combined effect of differential burnup of the boron and the fuel and to the buildup of xenon, samarium, and other fission products (see Fig. 15). With the knowledge of this burnup reactivity change, knowing the amount of available feedwater preheating, and knowing the percentage change of reactivity necessary to obtain a specified reactor power level (Fig. 14), the available range of the reactor power level during the operating cycle between no preheating and maximum preheating can be found as shown in Fig. 16.

The turbine steam requirements (Fig. 19) to produce a specified electrical load is determined from the turbine performance characteristics. The reactor power level to effect a specified steam production is shown in Fig. 18.

With reference to the flow diagram (Fig. 11), the feedwater can be returned to the reactor through either the upper saturated return line or the lower subcooled return line. The term saturated signifies that the feedwater is preheated by condensing the steam in the reactor pressure vessel before it enters the core. If all of the feedwater were returned through the saturated line, the reactor would be operating with the feedwater "100% saturated." The feedwater returned directly to the reactor moderator is essentially in the same heated condition as the water in the condenser hot well. When the feedwater is returned entirely through the subcooled line the reactor would be operating with the feedwater "100% subcooled."

As can be seen from Fig. 16, the reactor power level can be varied, any given day, about 0.8 mw by changing the feedwater from 100% saturated to 100% subcooled.

With the knowledge of the reactor power level at any given time (Fig. 16) and the amount of steam produced for a given reactor power level (Fig. 18), the reactor steam production can be found at any given time for either 100% saturation or 100% subcooled feedwater return (Fig. 17).

From the previously mentioned turbine performance curve, the amount of steam required to produce a specified amount of useful electrical power can be plotted as shown in Fig. 17.

If there were no regulating controls on the reactor to match the reactor power with the electrical load demand, the reactor control rods would have to be set so that the reactor would be capable of meeting the maximum load requirements at any given time. The steam produced by the reactor, and not needed by the turbine, would be by-passed into the condenser as waste heat. This type of operation is economically unattractive. Therefore, a method of regulating the reactor power level is required.

The moving, regulating control rod is the commonly used and proven method of controlling the reactor power level to the desired value. As previously indicated, the heterogeneous boiling reactor power level can be varied within specified limits by returning the feedwater to the reactor core in varying degrees of preheat. The feedwater control system is attractive for an unmanned reactor plant due to its relative simplicity and avoidance of moving parts in the reactor and due to its reliability and ease of maintenance.

The functional components of the feedwater control system are a steam-pressure regulating control valve, an orifice-type flowmeter, a three-way, pressure-operated, feedwater regulating valve, a saturated feedwater return line, and a subcooled feedwater return line. A general sequence of the equipment and its operation follows:

The turbine trip and throttle valve meters to the turbine the required amount of steam. A decrease in the turbine steam demand will in effect create a rise in the steam line pressure. The steam pressure regulating valve senses the pressure rise and by-passes steam directly into the condenser in order to maintain the desired steam pressure. When the quantity of by-passed steam exceeds 100 lb/hr, the feedwater regulating valve commences operation and allows a portion of the feedwater to return through the saturated feedwater pipe which will lower the reactor power level. The reactor power level will fall until not more than 100 lb of steam is being by-passed into the condenser.

If the turbine steam requirement increases, the steam line pressure falls and the steam by-pass valve starts to close. The decrease in the quantity of by-passed steam effects a repositioning of the feedwater valve toward the 100% subcooled region and the reactor power rises until the increased steam demand is satisfied and there is once again 100 lb of steam per hour being by-passed into the condenser.

A study of the reactor power level and steam production during the operating cycle (Fig. 17) reveals that at startup the reactor control rods must be set so that 2 mw of reactor heat is being produced with the 100% subcooled feedwater or 1.2 mw at 100% saturated. This initial setting of the control rods will allow the reactor to produce at any time during the operating cycle, upon demand, sufficient steam to meet the turbine demands for the maximum overload conditions.

The manner of operation of the unmanned reactor will probably best be clarified by tracing a possible daily operating cycle. Assume that the reactor has been in continuous operation for five months and that the operating load is varied from 75 kw to 125 kw to 50 kw and back to 75 kw during a 24-hour period. Perhaps it would be well to emphasize the fact that the electrical power requirements refer to a net electrical output and that the gross electrical output can be found by adding the essentially constant auxiliary load of 33 kw.

On the particular day under consideration, the feedwater control has the ability to vary the reactor power level from 1.3 mw to 2.1 mw (Fig. 16). At the start of the above-mentioned cycle the feedwater is being returned 96% saturated to maintain the desired net electrical load of 75 kw. With the demand increased to 125 kw, the feedwater control valve adjusts to the 38% saturated position and the reactor power rises to a corresponding 1.83 mw. When the overload is removed and only 50 kw electricity is needed, the feedwater control valve adjusts until it reaches the 100% saturation point and the reactor power level has decreased to 1.3 mw. Since only 1.2 mw of reactor heat would be sufficient to produce the desired 50 kw, the reactor is producing 300 lb of steam per hour more than the turbine requires. This steam is by-passed directly into the condenser by the steam line by-pass valve. With the increase in electrical demand to 75 kw, the steam by-pass valve closes until the by-passed steam reaches the value 100 lb of steam per hour. At this point the feedwater control valve begins to adjust to a value which will allow the turbine load requirements to be met and also provide the 100 lb/hr steam by-pass. The feedwater has now been adjusted back to the original 96% feedwater saturation point.

It should be apparent that if the turbine generator load requirements are lower than what can be handled by the feedwater control system, the only effect on the power plant is the lowering of the plant-operating efficiency.

B. Reactor Shutdown

It is intended to shut down the reactor by returning the control rods to the fully-in position.

If for some reason it is desirable, the reactor can be shut down by discontinuing the feedwater flow to the reactor.

C. Heat Balance

Net Electrical Load		0		1/2		3/4		Full		1-1/4	
Steam Rates, lb/kw		93.5		48		39.5		37.1		37.5	
Steam lb/hr	Turbine	3090		3980		4720		4930		5930	
	By-pass	100		100		100		100		100	
	Total	3190		4080		4820		5030		6030	
Heat Absorbed, Btu/hr.	Turbine	670,000		867,000		932,000		1,070,000		1,290,000	
	Condenser	2,512,000		3,222,000		3,462,000		3,982,000		4,802,000	
	Pressure Vessel	34,140		34,140		34,140		34,140		34,140	
	Piping	34,140		34,140		34,140		34,140		34,140	
	Clean-up Cooler	99,000		99,000		99,000		99,000		99,000	
	TOTAL	3,349,280		4,256,280		4,561,280		5,219,280		6,259,280	
Reactor Heat, Btu/hr		3,349,280		4,256,280		4,561,280		5,219,280		6,259,280	
Electric Power Output, kw	Net	0		50		75		100		125	
	Gross	33		83		108		133		158	
Space Heating, kw		0	400	0	400	0	400	0	400	0	400
Dry-cooler Btu/hr x 10^{-6}		2.611	1.245	3.321	1.955	3.561	2.195	4.081	2.715	4.901	3.635

VII. REACTOR SAFETY

A. Operational Safety

All conventional power-producing plants have a certain engineering probability of an occurrence of a mechanical or electrical failure which ultimately creates an unscheduled plant shutdown. The same probability of failure exists in any reactor power-producing plant. There is, however, the possibility that such a failure might result in more extensive loss of plant equipment and injury to personnel. This difference is mainly due to the radiation hazards which are associated with the reactor plant. The primary consideration of reactor safety is, therefore, not one of the temporary interruption of power but one of protecting human lives and the power plant equipment.

In an unmanned type of operation it is necessary to have the 100% Diesel standby capacity to automatically supply the power requirements in the event of failure of any of the main reactor components. A signal relayed to a central station would notify a maintenance crew that the reactor plant needs attention.

In making an appraisal of the possible reactor equipment failures, "unlikely" incidents, such as a pipe breaking, have not been considered. This apparently hasty assumption is very realistic considering that the operating steam temperature is only 417F and that the operating pressure is 300 psia. The steam system is also protected by pressure relief valves located on the condenser and on the steam line leading from the reactor to the turbine.

The investigation of the reactor power system reveals two prominent possibilities for an equipment failure: (1) pump failure, and (2) malfunction of the pressure relief valves or a pressure-regulating valve.

1. Pump Failure

The failure of either the condensate feedwater pump or the coolant circulating pump at any one time or at the same time will ultimately have the same effect on the reactor. To allow for the possibility of a pump failure, 100% standby capacity, automatically controlled, has been provided. The probability of two pumps failing in any one given operating cycle is considered to be remote; therefore, the following discussion applies only in the event of failure of both pumps.

At this time, it would be well to emphasize an inherent operating characteristic of an heterogeneous-type boiling reactor which has been affirmed by a Borax-II experiment. If the water level in the reactor falls, the reactor power level drops and ultimately "shuts down" completely when the reactor water level falls below the top of the core. The "shutting-down" effect is thought to be due to a joint contribution of the loss of the top reflector and the loss of natural circulation through the reactor core which effects an increase in the steam void. Both effects can essentially be categorized as an increase in neutron leakage from the core.

Consider first that the coolant circulating pump and its standby both fail. The pump failure will lead to a steam buildup in the condenser with a corresponding rise in temperature and pressure. The exhaust pressure relief valve will periodically open in order to control the condenser pressure at the 40 psia maximum value. The water level in the pressure vessel drops due to the loss of water from the system. When a sufficient quantity of steam has been lost from the system the water level falls below the top of the core and the reactor is shutdown with a new equilibrium pressure at 40 psia throughout the steam cycle system. The steam cycle pressure will gradually fall to atmospheric pressure in time as the decay heat after shutdown decreases and is dissipated as a heat loss through the reactor pressure vessel and as steam evaporated from the core.

Secondly, if the condensate feedwater pump and its standby both fail, the reactor water level immediately falls and the water level in the hotwell rises. The reactor core level has been reached and the reactor is finally shutdown. The loss of the reactor power drops the steam production and pressure to a point where the turbine generator can no longer handle the electric load and the coolant pumps fail to operate. The sequence of events which follow are now the same as when the coolant circulating pumps fail.

After the reactor has shut itself down, the problem is to prevent the core from melting by removing the shutdown heat generated by the core. Figure 20 shows the shutdown heat generation for an irradiated U^{235} core.(21) During the twenty hours immediately following the reactor shutdown the decay heat is lost from the reactor core by evaporating steam from the core and by heat being dissipated through the reactor pressure vessel. This pressure vessel is insulated so as to allow approximately 10 kw of heat loss through the pressure vessel with a temperature drop of 200F through the pressure vessel walls and insulation. Figure 20 shows that, after 20 hr, all of the heat generation in the core will be lost through the pressure vessel wall and no more water will be evaporated from the core. A mathematical integration of Fig. 20 shows that up to the 20-hr equilibrium point 269 lb of steam were evaporated from the core. This is equivalent to 4.55 cu ft of water, or a lowering of the water level 4.55 in. below the top level of the core.

To provide a conservative basis on which to determine the exposed fuel element temperature it was assumed that the water level had fallen 8 in. below the top of the core, and that the entire 10/3 kw heat generated in the upper 1/3 of the core must be transferred by pure conduction from the top of the reactor core through only the aluminum fuel elements into the water covering the lower 2/3 of the reactor core. A temperature rise of 93F was calculated using these assumptions. The maximum fuel element temperature under these conditions appears to be 363F which is well below the melting point of standard aluminum, commonly stated to be 1220F. Therefore, the core will not melt and no auxiliary provisions to protect the reactor core are necessary. The entire reactor plant will "look after itself" in the unlikely event that both the operating and the standby pump fail in either the steam cycle system or the coolant cycle system.

2. Valve Failure

The possibility of a valve failure to create a hazardous reactor incident appears very remote. The reliability of pressure-relief valves has been well substantiated by industrial experience. However, as an added precautionary measure against valve failure, blowout disks could be used in the final design of the pressure vessel and the condenser.

Should the 3-way, pressure-operated, feedwater-regulating valve fail to operate, the reactor power plant will continue to operate normally but less efficiently.

Should the pressure-operated, steam by-pass valve fail to open, the steam pressure will build up in the reactor to 400 psia at which time the safety valve releases the steam to the atmosphere and the core water level is lowered. The "blowdown" process will continue until the reactor has shut itself down.

B. Personnel Safety

The health physics safeguard requirements necessary to protect human life from injury is prevalent in any type of reactor operation. The reactor is located in a fenced-off exclusion area and housed in an earth-type biological shield. The reactor shielding requirements have been discussed in Section IV. There are, however, certain radioactive radiation health hazards in small heterogeneous boiling reactors which must be considered (see Appendix C). It should be noted that these hazards do not directly influence the normal operating schedule of an unmanned-type power plant.

The radioactive radiation that is associated with this reactor system can be considered in three separate stages because of the differences in intensity and differences in the decay rates of the various activated isotopes:

(1) N^{16} and N^{17} activity from irradiated H_2O; $O^{16}(n,p)\ N^{16}$ and $O^{17}\ (n,p)\ N^{17}$

(2) Activation of corrosion-erosion products

(3) Release of fission products from defective fuel elements.

During normal operation the activated isotopes that will generate the greatest amount of radiation will be N^{16}, which decays and releases a 7-mev gamma with the short half-life of 7.35 seconds, and N^{17}, which releases a 1-mev neutron with a shorter half-life of 4.14 seconds. The calculations indicate that none of the water or steam piping will require shielding to protect personnel from these particular radiations, although a distance of two feet should be maintained from all steam pipes by personnel exposed for several hours. The hot well and the ion exchangers will radiate a much greater amount than the steam pipes because of the greater quantity of water contained, and a distance of ten feet should be safe for long exposures. For short periods of time, men can approach these containers for any necessary inspections or servicing; however, radiation measurements should be made at each location.

It should be noticed that the water which leaves the hot well has already decayed for several minutes and practically no N^{16} activity remains.

The evaluation of the corrosion-erosion activity can be obtained by directly applying the results of a study for the BER cooling water. This study states that apparently no shielding is necessary for the crud activity in the cooling water if the crud does not get an opportunity to cycle through the reactor more than once and if the entrainment factor is assumed to be 10^{-3} or less. The activity due to a total accumulation of crud at some point is treated as though it is a point source and converted to mr/hr at 3 ft. Although these conditions are not altogether realistic, they provide a means of indicating the maximum value of activity that may collect. The activity thus calculated amounts to 0.2 mr/hr, which would indicate that there is no problem in shielding the accumulated crud.

The amount of fission products that may be released to the water system by failure of the fuel elements is a matter of statistical probability that can only be determined by actual long-term experience. Recent experiments in the corrosion properties of a new aluminum-nickel alloy by Argonne National Laboratory indicate that a favorable fuel element lifetime under the present reactor design conditions can be expected. The amount of fission products generated is roughly 1 gram per megawatt day of operation. Any attempt to determine the amount released to the water system by the rupturing of a fuel element would be very arbitrary. Rather a safe approach in the final design of the plant layout would be to include two or more ion exchange tanks in parallel and located in isolated pits or cells to allow alternate use and recharging. Experience has indicated that ion exchange resins are very effective for cleanup of dissolved or colloidal fission products, and that the use of "loaded" resins may be the most efficient way of handling and disposing of active particles. Should a fuel element break up into larger than colloidal particles, the particles would possibly be lodged in the filters or in the bottom of the reactor vessel. In this event, the most serious health hazard would be from contamination during the times of maintenance of these various pieces of equipment. Again the ion exchangers would tend to clean up the system with the passing of operating time.

C. Precautions Against "Freeze-up"

A preliminary survey of the possible damage due to freezing of the reactor components during an unattended and unscheduled shutdown indicates that no extensive permanent damage will occur. In connection with the freezing problem it should be noted that:

1. Any distortion of the used reactor core in the coffin storage pit is of no consequence in that it will never be returned to the reactor.

2. The reactor pressure vessel is designed to withstand any stresses that may result from freezing.

3. No ice should form in close contact with the fuel plates, i.e., the water in the reactor core channels will not freeze.

4. Ethylene glycol is used as the condenser coolant and in the space heating system as a precaution against freezing.

5. The turbine is, to a degree, protected since the steam condensate would freeze in the low point of the steam system which is the steam line preceding the turbine.

6. The extreme freezing conditions for the reactor plant will not occur unless the 100% standby Diesel-generator fails to function properly.

VIII. REACTOR REFUELING

The exchange of the fuel and the reactor core is to be made once a year, although future considerations may ultimately increase the reloading cycle to two or even three years. The installation power requirements during the refueling shutdown will be supplied by the Diesel standby unit. The refueling shutdown is the ideal time for performing a routine check of the power plant equipment. The time required for the refueling operation should not exceed twenty-four hours when done by a trained "refueling crew," but the actual refueling shutdown time will be governed by the equipment repairs required.

The main features of the refueling equipment and the refueling procedure are safety, simplicity, and reliability. The core will be completely enclosed and removed from the reactor pressure vessel in a coffin with the assistance of a motor-driven, overhead crane. The radioactive coffin and core will be stored in a pit twelve feet from the reactor vessel pending transportation to a fuel re-processing plant. The transportation size and weight limitations are met.

A. Equipment

The only special tool required for the refueling procedure is a simple socket wrench located on the end of a 9-foot aluminum handle. This tool is used to tighten the coffin pickup head safety bolts. An air-operated impact wrench is recommended to expedite the removal of the pressure vessel lid bolts.

The schematic view of the coffin bottom, the coffin, the coffin-lifting ring, and the coffin-pickup head are shown in Fig. 21. The coffin-lifting ring and the coffin bottom are located in the reactor pressure vessel, as shown in Fig. 2. The coffin pit is located in the biological shield (Fig. 5).

B. Procedure

The refueling operation begins when the reactor has been shut down and the water level in the reactor pressure vessel has been raised to the steam pipe exit level. The water depth between the core and the top of the reactor pressure vessel and a 4-1/2-inch thick lead shielding plate located on top of the reactor pressure vessel lid provides sufficient biological shielding at the top of the reactor to allow the "refueling crew" to work for two hours without exceeding tolerance. Two hours is thought to be more than adequate time to remove the protective cover plate and the control rod drive transmission, and unscrew the control rod drive shaft and pressure vessel stud bolts and disconnect the feedwater pipes.

The motor-driven crane is first used to remove the coffin-pit cover and is then fastened to the pressure vessel lid. The crane lifts the pressure vessel lid, the feedwater return pipes, and the control rod drive shaft clear of the reactor pressure vessel and locates these units on the edge of the shielding deck away from the "clean" equipment. The top of the reactor vessel is now clear and ready to receive the coffin. The crane is used to position the coffin in the pressure vessel (Fig. 21). No personnel should approach the reactor beyond the bottom of the biological shield from the time the pressure vessel lid has been removed to the time when the coffin has been positioned over the core. When the coffin is in place in the reactor pressure vessel, two members of the refueling crew position the coffin pickup head so that it grasps under the edge of the coffin-lifting ring. The "pickup head" and the "lifting ring" have projecting edges which will be placed clear of each other when the coffin is being placed in the reactor tank. The projectings will grasp under each other when the pickup head is turned a certain angle.

Three positions are shown in Fig. 21a: first, the pickup head just lowered to its place below the lifting ring. The next position shows the pickup head being rotated by means of a portable tool which consists of a pinion with a long shaft and handle which can be turned from a position above the tank. The pinion is supported in a bushing located in the coffin top and it engages a gear ring cut in the inside opening in the pickup head as shown in the plan view. The pickup head is guided during the rotations because its diameter fits with a reasonable clearance inside the diameter of the cylindrical "coffin-lifting ring" which will be centered by means of a few lugs inside the pressure vessel. The third view shows the pickup head firmly locked in position. The coffin-pickup head safety bolts are then tightened, pressing the bottom section against the cylinder to assure that the coffin will not disassemble or leak during the core removal operation. The coffin is then raised clear of the pressure vessel, moved to the coffin pit, and lowered into the pit. The coffin pit lid is then positioned. The transfer of the coffin from the reactor to the coffin pit should not take more than five minutes; however, the coffin shielding will allow personnel to stay at a distance of 60 ft from the coffin for a period of 3 hr. After a 50-day cooling period, the coffin can be approached up to the distance of 20 ft for a period of 6 hr. The coffin could then be moved by plane to a distance not to exceed 6 hours flying time, provided the pilot is 20 ft away from the side of the coffin. Should a longer flying time be desirable, a one-inch thick lead shield placed between the pilot and the coffin would allow an 18-hour exposure.

The new core and coffin bottom are placed in the reactor after the water has been removed from the reactor vessel and a new "Flexi-tallic" gasket is installed. The pressure vessel lid, feedwater return pipes, and control rod drive shaft are replaced as a unit. The reactor is again ready for startup after the pressure head bolts, control rod transmission, and the protective cover plates have been replaced.

IX. PLANT COST ESTIMATE

The following cost estimate is based on several arbitrary assumptions. The actual cost will depend on the circumstances and policies which may vary in individual cases.

1. Development Work

The reactor installation as presented in this report utilized the experience gained from the operating reactor of the same type, BORAX, and is designed to avail itself for immediate prototype construction.

The power-plant facilities are comprised of "off-the-shelf" items and the plant costs are based on industrial quotations on equipment suitable for doing the required job. Based on the above, no development costs are included.

2. Plant Location

The plant is designed for a minimum of construction work at an Arctic site. Components will be assembled for shipping in packages not to exceed 10 short tons and 7.5 ft high x 9 ft wide x 20 ft long.

The erection cost includes the shipping of 50 tons of construction equipment (bulldozers, trucks, power shovels, etc.) to the site but does not include the cost of the construction equipment.

There is no allowance made for the cost of the building site.

3. Instrumentation

It is expected that the reactor is inherently safe against power surges, excessive steam pressure, pump failures, and that the steam system is substantially self-regulating. The regulating control system in the form of feedwater control and the forementioned circumstances simplify the reactor and help to lower the expenditure for elaborate and delicate instrumentation.

4. Operating Data

Net electric power output, kw	100
Available energy for heating system, kw	400
Reactor power, kw	1,500
Fuel burnup per mwd, gm	1.25
Plant operating factors, %	
Electrical	75
Heating	50

5. Net Power Production

100 kw at 75% operating rate, av kw	75
Net electrical power output/year, kwhr	657,000

6. Fuel Burnup

Reactor power to produce 75 kw net electric power, mw	1.3
Fission power produced in one year at 75% operating rate (1.3 mw x 365), mwd/yr	475
Fuel consumption (475 x 1.25), gm/yr	594

7. Credit for Heating System

Heating plant average output (50% x 400), kw	200
Useful heat output per year, kwhr	1,750,000 or 6×10^9 Btu/yr

(This heat might have been produced by 3.4×10^5 lb of fuel oil at 17,500 Btu/lb useful heat value in the boiler. Assuming the cost of oil is 20 cents per gal, or 2.6 cents per lb ($8.40 per barrel at 42 gal), the cost for an equivalent amount of heat would be $8,850.)

8 Investment and Capital Charges

The plant investment includes building and all power and heat producing equipment. It does not include the cost of the switchgear or any electrical equipment beyond the generator output terminals. It does not include pipe lines and radiators for the buildings which use the produced heat or power. The plant investment is charged at 7% per year.

9. Reactor Power Plant Cost Estimate

Reactor Components

Reactor Vessel (1)	$6,000
Pressure Vessel Lid (1)	1,000
Core Lattice Structure (1)	3,000*
Control Rods (16)	300*
Control Rod Drive System	1,600

*Total core cost = $13,470 (excluding U^{235})

Reactor Components (Contd.)		
Feedwater Spray System	500	
Reactor Vessel Insulation	1,500	
Steam Separators	1,000	
Fuel Element Fabrication	9,720*	
Reactor Instruments	5,000	
Fuel Element Boxes	450*	
Miscellaneous	937	
		$31,007
Power and Heating Component		
Turbine Generator Set	$20,000	
Deaerating Condenser	4,000	
Pumps		
Condenser Feedwater (2)	4,000	
Coolant Circulating (2)	760	
Dry Type Fluid Cooler (1)	10,000	
Steam By-Pass Valve (1)	1,000	
Feedwater Regulating Valve (1)	1,000	
Pressure Relief Valves (2)	400	
Miscellaneous Valves (30)	1,550	
Piping (Stainless Steel)	3,018	
Ethylene Glycol Coolant	865	
Glycol Expansion Tank (1)	500	
Glycol Storage Tank (1)	2,000	
Steam Ejector (1)	200	
Instrumentation	5,000	
		$54,293

*Total core cost = $13,470 (excluding U^{235})

Water Purification System		
Ion Exchange Unit (2)	$2,000	
Ion Exchange Resins	872	
Filter and Filter Unit (1)	200	
Coolers		
Counter-Flow Type (1)	125	
One Pass-Type (1)	100	
Storage Tank - 1000 gal (1)	4,000	
		$7,297

Core Handling and Replacement Equipment		
Coffin (1)	9,000	
Unloading Tools	500	
Coffin-Pit Cylinder (1)	2,000	
		$11,500

Miscellaneous Charges		
Building (includes door, insulation, and building vents)	$4,053	
Building Crane (7-1/2 ton), motor driven (1)	5,000	
A-Frame Crane (10 ton), motor driven (1)	4,000	
Building Crane Structure	1,000	
A-Frame Crane Structure	2,000	
Plant Foundation Vents	2,000	
Building-Foundation Pad (gravel)	1,800	
I-Beams for Foundation	1,200	
Steam Line Duct	250	
Shield Cylinder	2,000	
Control Cave	1,000	
Control Cave Facilities	3,600	
Total Miscellaneous Charges		$ 27,903

Total Miscellaneous Charges		$ 27,903
Total Equipment Costs		132,000
Shield (Dirt and Gravel at $3/cu yd)		7,000
Transportation Cost		30,000
Total Materials Cost		169,000
Erection Cost		130,000
Sub-Total		299,000
Contingencies (15%)		44,850
Contractor's Fee (20%)		59,800
Total Plant Investment		$403,650

10. Yearly Costs (One-Year Reactor)

Capital Charges		
7% of capital investment		$ 28,256
Operating and Maintenance Cost		
Manpower	$ 10,000	
Materials	10,000	
Total Operating Cost		20,000
Fuel Charges		
Core	13,470	
Fuel (8,600 gm at $16/gm)	137,600	
Total Fuel Charges		151,070
Total Yearly Costs		199,326
Chargeable to Heating System (1,750,000 kwhr at 5 mills/kwhr)		8,750
Chargeable to Electrical Power		190,576
Cost of Electric Power (657,000 kwhr)	$0.29/kwhr	
Cost of Heat	5 mills/kwhr	

The above estimate is based on the specification that "recovery of fuel from spent elements is uneconomical" and that the reactor core is replaced after one year of operation without credit for the unused fuel.

11. Alternate Cost Estimate

The following yearly cost estimate for the "one-year" reactor is perhaps more realistic. The changes are: (1) the fuel remaining after each year of operation will be reprocessed and recovered at $4 per gram, and (2) the credit for space heat is estimated on the basis of an equivalent cost of oil at $42 per barrel delivered to the Arctic. This corresponds to 25 mills/kwhr of heat.

Capital Charges		
7% of capital investment		$28,256
Operating and Maintenance Cost		20,000
Fuel Charges		
Core	$13,470	
Fuel Burnup (594 gm at $16/gm)	9,504	
Fuel Reprocessing (7.006 gm at $4/gm)	28,024	
Total Fuel Charges		50,998
Total Yearly Cost		99,254
Chargeable to Heat (1,750 kwhr at 25 mills/kwhr)		43,750
Chargeable to Electric Power		55,504
Cost of Electric Power (657,000 kwhr)		84.5 mills/kwhr

re [illegible] using [illegible] costs

X. THREE-YEAR REACTOR COST ESTIMATE

The following cost estimate is based on the assumption that the reactor is so designed and operated that the core will need to be exchanged only after three years of operation.

Physics calculations have indicated that the "three-year reactor" will require a fuel charge of 10.1 kg U^{235}. With a burnup of 594 gm/year, 8,318 gm of used fuel will be reprocessed after each three-year period. Credit for heat used in the space heating system is again taken to be 25 mills/kwhr.

Yearly Costs

Capital Charges		
7% of capital investment		$28,256
Operation and Maintenance Cost		
Manpower	$10,000	
Materials	10,000	
Total Operating Cost		20,000
Fuel Charges		
Core ($13,470/3)	$ 4,490	
Fuel Burnup (594 gm/yr at $16/gm)	9,504	
Fuel Reprocessing (8318 gm at $4/gm)/3	11,090	
Total Fuel Charges		$25,084
Total Yearly Costs		73,340
Chargeable to Heating System		43,750
Chargeable to Electrical Power		$29,590
Cost of Heat		25 mills/kwhr
Cost of Electrical Power (657,000 kwhr)		45 mills/kwhr

A comparison of the cost estimate of electrical power based on the realistic one-year reactor with the three-year reactor indicates a savings of 39.5 mills/kwhr.

XI. COST OF ADDITIONAL PLANTS

The cost of the prototype plant erected in the Arctic is expected to be $403,650 (299,000 + 15% contingencies + 20% contractor's fee).

The price per installation for 12 additional plants will be effected in the following manner.

1. The $169,000 total materials cost will be essentially unaffected. The mass production discounts will not substantially change the discounts already quoted on the stock items from which the plant is assembled.

2. The $130,000 construction and erection cost will be reduced by $50,000 since the same construction crew can build two plants during the construction season. This figure will vary according to the individual circumstances arising from the site location, military participation, and civilian participation; no attempt will be made to estimate these conditions.

3. The 20% contractor's fee will, by its nature, remain fixed unless the additional plants are supervised and constructed entirely from military personnel. There is again no basis for a sound estimate change.

4. The 15% for contingencies is essentially an overestimate on the prototype plant which theoretically could be removed if the estimate on the prototype plant is reasonably accurate.

The cost of future installations would therefore be (as seen from the prototype cost estimate):

Equipment Cost	$169,000
Erection Cost	80,000
	$249,000
Contractor's Fee (20%)	49,800
Total Plant Investment	$298,800

This reduction in total plant investment will reduce the cost of electric power to 34 mills/kwhr as based on the alternate "three-year" cost estimate.

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APPENDIX A

BOILING HEAT TRANSFER CHARACTERISTICS

The amount of heat energy which is transferred to the boiling water can be referred to as average heat flux, Btu/(sq ft)(hr), or in terms of power density, kw per liter of channel volume. The first measure, 11,000 Btu/(sq ft)(hr), indicates a very high degree of safety against burn-out of fuel elements; the other, 14 kw/liter, indicates that the contemplated steam production rate is quite moderate compared with rates achieved by operation of the BORAX reactor.

Inasmuch as the control of the reactor is inherently associated with the steam voids in the reactor core, it is important to predict this average moderator density loss for different operating conditions. The degree of accuracy of such calculations is obviously limited, but the assumption is that the relative values can be estimated sufficiently well to allow reasonable predictions when operating conditions are varied; for instance, from one power rate to another or from subcooled to preheated feedwater.

The experimental evidence which tends to substantiate the estimates is provided by a series of mock-up tests conducted at Argonne with electrically heated elements where steam voids were measured by attenuation of gamma rays.

The general principle of the two-phase fluid-flow calculations for steady-state conditions is that the drive pressure must be equal to the friction losses plus acceleration losses in the flow channels. In case of natural convection the drive pressure is equal to the average density loss in the fuel channels due to boiling multiplied by the channel height. The friction loss is estimated on the basis of the average liquid water velocity in the channel. The coefficient of friction is related to the hydraulic radius, viscosity, and Reynolds Number. The acceleration loss is based on the maximum kinetic energy attained by the water at the channel exits, assuming that this energy is not recovered.

The following nomenclature and equations were used to determine the moderator density and its variation in relation to circulating flow rate, steam pressure, subcooling, and slip ratio:

G_s Flow rate of steam, lb/sec

G_w Flow rate of liquid, lb/sec

G_t Total flow rate, lb/sec

Y Steam quality, G_s/G_t

X Recirculation factor, (G_w/G_s), lb liquid/lb steam at channel discharge point

X_ℓ	Recirculation factor at intermittent point of channel
p	Steam pressure, psia
v_s	Steam velocity, fps
v_w	Liquid water velocity, fps
r	Slip ratio, v_s/v_w
A	Channel flow area, sq ft
ρ	Density of two-phase fluid, lb/cu ft
ρ_s	Steam density at existing conditions, lb/cu ft
ρ_w	Saturated liquid density at existing conditions, lb/cu ft
$\bar{\rho}_b$	Average fluid density in boiling section of channel, lb/cu ft
$\bar{\rho}_t$	Average density of all fluid in both subcooled and boiling channel sections, lb/cu ft
V	Specific volume (same subscripts as above), cu ft/lb
R_V	Steam void ratio, steam flow area/total channel flow area
P	Power generated in channel, Btu/sec
L or L_t	Length of channel, ft
L_1	Length of subcooled channel section, ft
L_b	Length of net boiling channel section, ft
ℓ	Length of channel from point of initial boiling to intermittent point, ft
e	Heat transferred per linear foot of channel, Btu/ft (this value is assumed constant over whole length)
H	Enthalpy of steam, Btu/lb
ΔH	Heat of vaporization for water, Btu/lb
h	Enthalpy of liquid, Btu/lb
h_{sl}	Enthalpy of saturated liquid, Btu/lb
h_o	Enthalpy of total liquid entering channel, Btu/lb
Δh	Subcooling of liquid, $(h_{sl}-h_o)$, Btu/lb
m	Ratio: $(\rho_w - r\rho_s)/r\rho_s$
m^*	Ratio: $m^2 r \rho_s/(\rho_w - \rho_s)$

Two phase flow velocities:

$$v_s = (V_s + Xr\ V_w)\ G_s/A$$

$$v_w = v_s/r$$

Exit fluid density:

$$\rho = (1 + Xr)/(V_s + Xr\ V_w)$$

Exit steam void:

$$R_V = \frac{1}{(1 + Xr)\ \rho_s/\rho_w}$$

Average fluid density in net boiling section:

$$\overline{\rho}_b = \frac{\rho_w}{m^*\ Y} \log\ (1 + m\ Y)$$

where

$$Y = 1/(1 + X) = \text{steam quality}$$

Ratio of subcooled distance to total channel length:

$$L_1/L_t = \Delta h/(\Delta h + Y\Delta H)$$

The above equations were based on the approximation that the heat generation would be constant at all points of the channels.

General steam data used in present project:

Steam pressure	300 psia
Saturation temperature	417F
Enthalpy	1203 Btu/lb
Heat of vaporization, ΔH	810 Btu/lb
Steam density	0.65 lb/cu ft
Density of saturated water	53 lb/cu ft
Velocity slip ratio in channels (r)	2.5
m (Ratio as defined above)	31.5
m* (Ratio as defined above)	31.1

The curve presented in Fig. 12 shows the relation between the recirculation factor X and the average density loss of the moderator inside the core calculated from the above equation for $\overline{\rho}_b$ applied to the present steam constants.

Other constants used in the following estimates are:

Total channel cross section, A	1.95 sq ft
Equivalent flow channel diameter, D_e	0.033 ft (water gap = 0.2 in.)
Fuel channel length, L	2.0 ft
Fluid viscosity, μ	0.31 lb/(ft)(hr)
Reynolds Number for an average velocity of 2 fps ($R_e = D_e v\rho/\mu$)	42,000

The coefficient of friction for rough surfaces and for a Reynolds Number of 42,000 is 0.006.(3) Due to the surface boiling, the coefficient was multiplied by 1.5; therefore, the average coefficient of friction is assumed to be f = 0.009. In the present case, the friction loss through the core becomes:

$$\Delta F = (2fv^2/gD_e)\ (L)\ (\rho_w) = 1.8\ (\bar{v})^2\ \text{lb/sq ft}$$

The acceleration loss is based on the equation:

$$\Delta\rho_a = \left[\frac{(1.5)(v_w)^2}{2g}\right]\ (\rho_w) = 1.24\ (v_w)^2\ \text{lb/sq ft}$$

where $\overline{v}$ is the water velocity averaged between point of inlet and exit and v_w is the exit velocity of the water. The factor 1.5 was arbitrarily introduced to take care of inefficiency losses in connection with the flow.

The drive pressure (or syphon drive pressure) is estimated as

$$D = (\Delta\rho_t)\ (\text{total core height})$$
$$= (2.33)\ (\Delta\overline{\rho}_t)\ \text{lb/sq ft.}$$

The core height includes the 4-in. extensions of the frame beyond the fuel plates.

A calculation of flow conditions for five different operation cases is shown in Table I. In each case an equilibrium has been found where the total flow pressure loss equals the syphon drive pressure.

Case No. 1 is the normal reactor power level of 1.5 mw obtained with the feedwater entering the core channels preheated to the full saturation temperature. This means that the feedwater is introduced in the steam dome where it is heated by the condensation of about 20% of the steam from the core. The total steam flow at the core exit will therefore be 6,250 lb/hr while the net steam production to the steam pipe is 5000 lb/hr.

It is found that the average moderator density loss in the core will be 11-1/2% of the saturated water density. The net electric power output is 100 kw.

Case No. 2 deals with the same power level but refers to the conditions where all the feedwater is returned to the down-going water current which will be subcooled accordingly. The net boiling will occur only after the up-going water has received about 20% of the reactor energy, which is the ratio of feedwater preheat to the total preheat plus vaporization heat. The recirculated water has, of course, already reached its saturation heat before entering the downcomer area. Since no steam is condensed in the steam dome, no excess steam is produced in the core. Therefore, all of the steam produced in the core will leave through the steam pipe.

In this case the total average moderator density loss will be only 8.6% of liquid water density.

Case No. 3 is again operation with subcooled feedwater while the power level is increased to 1.8 mw, corresponding to a net electric output of 125 kw. A density loss of 9.5% is found in this case.

In Case No. 4, the power level is increased to 2.0 mw in connection with subcooled feedwater operation. The density loss is now 10.2%.

Case No. 5 was studied in order to find the power level if the Case No. 4 operation were to be changed to fully preheated feedwater. The equilibrium condition is found at 1.2 mw, which would produce a net electric power of 49 kw. Since the reactivity is left unchanged, the average steam void or density loss is the same as in Case No. 4.

The reactivity losses resulting from the respective total average density losses due to steam voids are obtained directly by comparison of the total density losses with the reactivity curve on Fig. 13.

Finally, the results from Table I are plotted in Fig. 14. Two sets of curves are shown. One set shows the relation between the reactor power and the moderator density losses due to boiling in case of subcooled or saturated (preheated) feedwater. The other set of curves shows the corresponding reactivity variations from a zero point which was chosen to be where the reactor produces 2 mw of power with fully subcooled operation. This is the same point at which 1.2 mw of reactor heat can be produced with fully saturated feedwater.

Table I

CASE STUDIES OF BOILING HEAT TRANSFER CHARACTERISTICS

	Non-Boiling (417F)	Case 1	Case 2	Case 3	Case 4	Case 5
Net Electrical Load, kw	0	100	100	125	125+	49
Reactor Load, mw		1.5	1.5	1.8	2.0	1.2
Net Steam Produced, lb/hr	0	5000	5000	6000	6600	4000
Feedwater Condition		sat.	←	subcooled	→	sat.
Steam from Core, lb/hr		6250	5000	6000	6600	5000
$\frac{G_s}{A}$ from Core, lb/(sec)(ft^2)	0	0.89	0.715	0.86	0.95	0.715
Av Density Loss in Core ($\Delta\bar{\rho}_t$), lb/ft^3		6.1	4.55	5.0	5.4	5.4
($\Delta\bar{\rho}_t/\rho_w$), %	0	11.5	8.6	9.5	10.2	10.2
Av Density Loss in Boiling Zone ($\Delta\bar{\rho}_b$), lb/ft^3		6.1	5.7	6.3	6.75	5.4
Av Density in Boiling Zone ($\bar{\rho}_b$), lb/ft^3		46.9	47.3	46.7	46.25	47.6
Circulation Factor (G_w/G_s),		104	113	99	91	125
Total Flow Rate through Core (G_t/A), lb/(sec)(ft^2)		93.5	81	86	87.5	90
Inlet Water Velocity, fps		1.76	1.53	1.62	1.65	1.7
Exit Steam Velocity, fps		5.7	4.95	5.3	5.55	5.4
Exit Water Velocity, fps		2.3	2.0	2.12	2.22	2.16
Av Water Velocity in Core, fps		2.03	1.76	1.86	1.93	1.93
Core Friction Loss (Δf), psf		7.4	5.6	6.2	6.7	6.7
Acceleration Loss (Δp_a), psf		6.6	4.9	5.6	6.1	5.8
Total Pressure Loss (Δp), psf	0	14.0	10.5	11.8	12.8	12.5
Syphon Drive Pressure (D), psf	0	14.2	10.6	11.7	12.6	12.6
Reactivity Above Normal Operation (1.5 mw sat.)	3.0	0	0.82	0.59	0.40	0.40
Reactivity above zero if $\Delta k \equiv 0$ for 2-mw subcooled operation.	2.6	-0.4	0.42	0.19	0	0

APPENDIX B

REACTOR PHYSICS ANALYSIS

INTRODUCTION

The basic concept underlying the physics approach to the reactor design may be stated concisely. It is the idea of countering the negative reactivity coefficient of fuel consumption with the positive coefficient inherent in the burnup of parasitic absorbers of sufficiently high microscopic absorption cross section.

To some extent the concept is common to several reactors that have been proposed; for example: APPR(4) utilizes B^{10} as an adjunct of control by absorbing tubes. Likewise, in the Horizontal Package Reactor proposal (HPR),(2) B^{10} was suggested as a means of reducing the over-all control requirements to a level easily attainable by conventional absorbing blades. In the present design it is proposed to use one or more burnable poisons to control all but a relatively small fraction of the reactivity loss engendered by depletion of the fuel mass during reactor operation. In fact, when B^{10} or Hg^{199} (e.g., Hg^{199} in a natural mixture of isotopes) is used, the reactivity gain in burnup of that parasitic absorber more than compensates the loss of fuel until much of the parasite has been consumed. At this time the slope of the reactivity versus time curve changes sign and reactivity drops.

The small net variation in reactivity is to be compensated mechanically, for example: by regulating the degree of preheating of the fluid entering the core (hereafter referred to as "feedwater control") or by regulating the level of the top reflector fluid ("top reflector control"), or by a combination of these two schemes.

The goal of the early physics effort was the design of a reactor which would operate continuously for an integral number of years at one hundred per cent of rated power. Provision was to be made for twenty-five per cent more electrical power, at any time, for an aggregate five per cent of the core life. Several burnable poisons including Li^6, Hg^{199} and B^{10}, acting separately or in combination, were examined. Because of its relatively low thermal neutron absorption cross section, when associated with mercury or with boron, Li^6 is useful in reducing the reactivity variation over the operating cycle. The price to be paid, a sharply increased fuel mass requirement, arises from the very nature of this utility, for a large fraction of the original lithium is present at the end of the core cycle. A similar difficulty is encountered when mercury alone is used, though to a smaller degree. Furthermore, there may be metallurgical problems associated with the dispersion of sufficient mercury in the fuel alloy, or with its alloying with the fuel and aluminum structural material. These are problems of degree rather than of kind, for with presently available techniques it may be possible to alloy as much as four per cent by weight of mercury in the meat of the fuel plate. For the core structure designed perhaps six per cent would be required for two years of full power operation.

Of course, if the total fuel burnup could be reduced by relaxing the requirement of continuous full-power operation, the core life would be prolonged with a relatively small effect on the reactivity variation. (One difference arises from the interplay of burnup with the slow buildup of samarium at the beginning of the cycle.) However, the reduction in fuel burnup is not directly proportional to the reduction in average load power, since the auxiliary equipment power requirement is fixed.

As mentioned above, the idea of continuous full power operation was suggested early in the study by its relative simplicity. It appears possible, however, to incorporate at least a partial adjustment of core power to the power demand; this modification is discussed in the body of the report.

FEEDWATER CONTROL

A substantial part of the study program has been devoted to investigating the feasibility of regulating automatically this net variation by feedwater control <u>alone</u>, for all but a very small fraction of the operating cycle. (Once the reactor is brought up to power with the equilibrium level of xenon it would operate unmanned.)

It is estimated that the reactivity variation controllable by feedwater preheating is 0.8%, possibly more.

With the limitation of 0.8%, the result of theoretical investigation is that it appears feasible to construct a reactor which will operate continuously for at least one year at full-rated power (1.5 mw) plus the specified overload. This reactor utilizes B^{10} as a burnable poison in the fuel meat. (There is no difficulty anticipated in alloying the required mass of this isotope with the other constituents of the meat, even for a three-year reactor.)

The theory implies a sufficiently large margin of control so that relatively large deviations from the calculated graph of reactivity versus time would still permit the statement of feasibility for a one-year reactor. Since the worth of the feedwater control is not affected strongly by the fuel mass present, whereas the longer the life of the reactor at rated power the larger the reactivity variation, this control margin is considerably reduced for the case of longer operation. Thus, though the theory implies the possibility of such full power operation for two years, using B^{10}, this should be considered only a marginal possibility, at present.

It may be possible to control a reactor over a two-year operating cycle by feedwater preheating alone, when using Hg^{199} as the burnable poison. The $\frac{\sin \alpha r}{r}$ - Perturbation Method used to calculate the time-dependent reactivity effects in the one-year reactor may be seen (Fig. 26) to be reasonably accurate. Its extension to a two-year reactor, though probably less accurate, indicates that, with the proper blending of Hg^{199} and fuel in the core, continuous full power operation for two years or more may be within the range of feedwater control (Fig. 25). (As mentioned previously, there is a price - perhaps two to three kilograms more fuel than in the case of B^{10}. Also, there may be metallurgical obstacles.)

(Details of the $\frac{\sin \alpha r}{r}$ - Perturbation Method, and of more nearly accurate methods are presented later in the Appendix, under "Time-Dependent Reactivity Variation.")

OTHER TYPES OF AUTOMATIC CONTROL

It may be possible to control a two-year reactor by feedwater preheating. And perhaps feedwater control is worth more than 0.8% in reactivity. However, at least two alternate methods of automatic control, each worth three per cent or more, appear worthy of investigation to replace or to supplement feedwater control for unmanned operation.

Calculations of reactivity variation for three-years operation at full power indicate that it may be possible to build a three-year reactor, using B^{10} as a burnable poison, with a net reactivity variation of perhaps three per cent (Fig. 28) - hence the interest in an automatic control system worth three to four per cent.

A. Top-Reflector Control

One scheme is that of varying the height of the reflector fluid directly above the reactor core. This method has already been examined for a similar reactor;[2] for the present reactor it is estimated that a three per cent reduction in reactivity would result from the complete removal of the top reflector, that is, from the reduction of the fluid level to the top of the fuel elements proper. At present there is insufficient experimental indication of reactor stability under such operation to permit a firm claim of feasibility. An encouraging result from BORAX, at NRTS in Idaho, is that in an experiment where the feedwater was shut off the reactor continued to produce steam until the water level dropped approximately to the top of the fuel elements. The transition from the operating condition was smooth.

B. Side-Reflector Control

A second possibility is that of varying the water level in an annular tank adjacent to the core and in the reflector. It is estimated that at least three per cent in reactivity is inherent in a gap thickness of four inches for the type of reactor visualized here. It is believed that this method also merits a more detailed examination. The water-level control appears to be adequate for the purpose of controlling the net reactivity variation although the "downcomer" volume requirement and reactor tank diameter also limit the size of the gap and therefore its effectiveness.

MATHEMATICAL MODEL

The major aspect of physics analysis for this reactor is the determination of the functional connection between reactivity variation and the time-integrated reactor heat output. The thermal neutron flux spectrum is of primary importance; epithermal events are believed to be of second order importance for this problem in this reactor.

The heterogeneous boiling reactor with circulation by natural convection is highly nonuniform. A detailed analysis of reactivity variation with burnup in such a reactor would depend on a knowledge of the pattern of steam void formation in the core; to date there is little experimental information of this type. Of course, the void pattern influences and is influenced by the spatial variation and energy spectrum of the neutron flux, and therefore also changes with burnup.

The program of design for this package reactor did not provide sufficient time to attempt such a complex analysis. Therefore, the physics analysis was simplified in several ways. For the calculation of reactivity variation and of fuel requirements, two-group diffusion theory was used. The scattering and slowing down parameters in both groups were calculated for the core homogenized by volume averaging the components. The volume-average steam void in the core fluid was determined by a heat transfer and flow rate analysis. Allowance was made for fuel self-shielding in computing the effective absorption in the thermal group.

In the operating reactor the vertical variation of thermal neutron flux is associated with the vertical distribution of steam voids. For the Horizontal Package Reactor, a brief UNIVAC study indicated that the vertical accumulation of steam in fuel channels leads to a shift in the position of the peak flux from vertical center (as in a vertically uniform core) toward the region of higher fluid density. A reasonable extrapolation from this result for a similar reactor leads to the conclusion that the smaller is the degree of preheating of the inlet feedwater, the more pronounced is this shift in this reactor. Consequently, the maximum burnup rate of fuel and of parasitic absorber occurs off vertical center, and its location varies with time as the strong neutron absorbers are depleted during operation.

Analogously, in the horizontal plane there is a tendency toward flattening of the thermal neutron flux, since the region of greatest heat production rate is the region of greatest reduction in fluid density.

It is believed that these deviations from the flux pattern calculated for the homogenized core do not lead to large errors in the homogenized core analysis of reactivity variation. They were not taken into account in the present analysis.

The two-group diffusion theory results are believed to be highly indicative of the nature of the reactivity variation. Probably, though not certainly (in view of the lack of experimental knowledge of steam formation in the fuel channels), more accurate quantitative results could be obtained from the application of multigroup slowing down models (e.g., Goertzel - Selengut) to the calculation of fuel requirement and of reactivity variation. Such studies are large consumers of time, even on such fast machines as the UNIVAC, especially if the subtleties of two-dimensional analysis are considered. The final word, of course, would come from operation of a prototype reactor.

The two-group diffusion theory equations used are the customary ones. (They were presented, in the form used in this analysis, in the Physics Appendix of the study on the Horizontal Package Reactor.[2] For convenience, that part of the appendix is reproduced below, with the typographical errors corrected.)

$$D_f \nabla^2 \phi_f - \frac{D_f}{\tau} \phi_f + k_\infty \overline{\Sigma}_{a_s} \phi_s = 0 \tag{1}$$

$$D_s \nabla^2 \phi_s - \overline{\Sigma}_{a_s} \phi_s + \frac{D_f}{\tau} \phi_f = 0 \quad , \tag{2}$$

where

ϕ_f = epithermal, or fast neutron flux (neutron cm/cm^{3}sec)

ϕ_s = thermal neutron flux (neutron cm/cm^{3}sec)

D_f = fast diffusion coefficient (cm)

D_s = thermal diffusion coefficient (cm)

τ = two-group age of fast neutrons (cm^2)

$k_\infty = \dfrac{\eta^{fuel} \overline{\Sigma}_{a_s}^{fuel}}{\overline{\Sigma}_{a_s}}$ = infinite multiplication constant ($p\epsilon$ = 1) (average number of fission neutrons produced per thermal neutron absorbed in an infinite medium)

$\overline{\Sigma}_{a_s}^{fuel}$ = average thermal macroscopic absorption cross section of the fuel (U^{235}) in the region (probability of absorption in fuel per cm of travel of thermal neutron)

$\overline{\Sigma}_{a_s}$ = total average thermal macroscopic absorption cross section in the region (probability of absorption per cm of travel of thermal neutron)

η^{fuel} = average number of fission neutrons produced per thermal neutron absorption in fuel.

The coupled differential equations were solved by assuming that both the thermal and fast neutron fluxes satisfied the wave equation

$$\nabla^2\phi + B^2\phi = 0 \quad . \tag{3}$$

The actual reactor core is neither sphere nor rectangular parallelopiped nor right circular cylinder. However, it is not far from any one of these geometries; for various aspects of the physics analyses the choice of geometry was determined by its convenience for the calculation. For example, the analyses of time-dependent reactivity variation were based on a spherical core; the worth of the annular gap control method was estimated for a right circular cylinder; and the worth of the absorbing "curtains" was calculated for an appropriate rectangular parallelopiped, as were the critical fuel mass and the reactivity implicit in the steam void. The advantage of the spherical model is that the fluxes are assumed to be functions of only one variable. This model suffers from the disadvantage that the operating core is not symmetric; the lack of symmetry applies to the cylindrical model, as well, although to a lesser extent. The usual assumption made for the other two models, however, is that the flux may be written as a sum of products of functions of a single variable:

$$\psi(r, z) = R(r)\ Z(z)$$

and

$$\psi(x, y, z) = X(x)\ Y(y)\ Z(z) \quad .$$

In fact, the iteration procedure followed[5] yields the fluxes as single products

$$\phi_f(r, z) = R_1(r)\ Z_1(z)$$

$$\phi_s(r, z) = R_2(r)\ Z_2(z)$$

and

$$\phi_f(x, y, z) = X_3(x)\ Y_3(y)\ Z_3(z)$$

$$\phi_s(x, y, z) = X_4(x)\ Y_4(y)\ Z_4(z) \quad .$$

That is, the fluxes were determined by a sequence of one-dimensional calculations; at each step in the sequence the reactor core was assumed to be unreflected in the other directions (these core dimensions being augmented by the estimated reflector savings) and the reflector savings was determined for the remaining direction. The method was used only to determine fuel requirements and the reactivity worth of the various control schemes, where no serious errors result. The unpleasant alternative to these approximations is to solve the wave equation in two or three dimensions, a formidable task even for the high-speed computing machines available today.

CONTROL REQUIREMENTS: STARTUP AND SHUTDOWN

Assuming criticality for the virgin reactor at operating temperature, with 11.5% volume-average steam void in the core fluid, and with equilibrium xenon and samarium present, the cold virgin reactor without fission products would be approximately 9 to 10% supercritical (for one-year to three-year reactors).

In the core design discussed in the other chapters of this report, sixteen (curved) blade absorbers (clad boron or cadmium alloys) are included, distributed in four parallel vertical sections. The actual total spread of the blades is only two-thirds of the full section width. These sections were calculated as four solid curtains of zero thickness transparent to fast neutrons and black to thermal neutrons. The logarithmic derivative boundary condition

$$\frac{\phi'_s}{\phi_s} = \pm \frac{1}{0.71\lambda_{t_s}}$$

was applied to the thermal neutron flux ϕ_s at the curtains, where λ_{t_s} is the total mean free path, in centimeters, for thermal neutrons in the core; the choice of "+" or "-" was made so that the flux always decreased as a curtain was approached. The result of this calculation was that the difference in reactivity between full removal and full insertion of these four curtains is

$$\frac{\Delta k}{k} \approx -0.22$$

in the cold virgin one-year reactor. Allowing a liberal reduction for the fact that the blades do not constitute four solid curtains it is reasonable to expect adequate control of the cold virgin reactor.

In an earlier design of this reactor there was no provision for control blades. Control was to be obtained by the vertical motion of a central bundle of fuel assemblies, the gap to be filled by fluid. Questions arose as to what would happen, at various levels of fuel removal, if a sudden reactor power excursion were to blow some of this fluid out of the water hole; that is, since a large part of the control inherent to this scheme arises from the slowing down and absorption of neutrons in the water hole, could such a displacement of fluid lead to a positive reactivity coefficient? This reactivity gain would be compensated at least partially by the simultaneous ejection of fluid from the central fuel bundle itself. It is not known, however, whether the net change in reactivity would always be negative. Since such control would be needed only at startup and (possibly) at shutdown in this design, the actual requirements are probably less stringent than for the prototype reactor where personnel training (and probably additional experiments) would be involved. If time permitted, it would be advantageous to test such fuel motion in the prototype reactor; one advantage of this control method is that, once the reactor has been properly started, the fuel bundle would be in the core, leaving a minimum of water holes in the core and thereby reducing fuel requirements.

It was estimated that for a core with forty-five fuel assemblies of the type discussed in this report the removal of a central fuel bundle of nine to thirteen assemblies would provide quite adequate control of the cold reactor. It would not be necessary to move a thirteen-assembly bundle entirely away from the remaining fuel assemblies.

If such a control method were adopted it would be advisable to include a provision for extra absorption, e.g., the injection of a soluble poison in the fluid, at the time of core replacement. In order to reduce the size of the coffin it would be preferable at this time to have the central fuel bundle in line with the remaining fuel assemblies.

METHODS OF CONTROLLING THE TIME - DEPENDENT REACTIVITY VARIATION

In this section are presented analyses of the three schemes described previously for controlling the net reactivity variation inherent to the use of burnable poisons in the fuel meat.

A. Feedwater Control

To determine the reactivity control afforded by the spectrum of preheating of the inlet feedwater, from saturation to 100% subcooling, an analysis was made of the relationship between $\Delta k/k$ and the volume-average steam void in the core fluid. The calculation of reflector savings versus steam void was made for a homogenized cylindrical core (of circular horizontal cross section) completely surrounded by saturated fluid. Because of the low density of the fluid above the core, and on the basis of UNIVAC results for the Horizontal Package Reactor, approximately 1.5 centimeters was subtracted from the total vertical reflector savings for the operating reactor (11.5% steam void). For other percentages of steam void the total vertical reflector savings was reduced by an amount proportional to the steam void. The resulting curve of $\Delta k/k$ versus steam void is presented as Fig. 13. It is estimated that full power corresponds to 11.5% void with saturated entering feedwater. This power is maintained by valving to not more than 8.5% steam void in the core fluid with 100% subcooled feedwater. From Fig. 13 we then may obtain the result that feedwater regulation is worth approximately 0.8% in reactivity.

(As indicated in HPR it is believed that reactivity calculations based on volume average void are good approximations to reactivity calculations based on spatially distributed voids - at least for the virgin reactor, where the effective distribution of the strong absorbers is essentially uniform.)

It is not expected that $\Delta k/k$ (feedwater) would vary greatly over an operating cycle of one or two years.

B. Varying the Level of the Top-Reflector Fluid

The $\Delta k/k$ attributable to the top reflector may be determined easily by calculating the total geometric buckling, B^2, for the situation where there is no top reflector; from this there is obtained

$$k_{effective} = \frac{k_\infty}{(1 + L^2B^2)(1 + \tau B^2)} \tag{4}$$

and

$$\frac{\Delta k}{k} = \frac{k_{eff} - 1}{k_{eff}} \tag{5}$$

Having assumed a total vertical reflector savings 1.5 centimeters smaller than that obtained for the operating core completely reflected by saturated fluid, the top reflector was estimated to be worth approximately 9.2 centimeters for the operating reactor with 11.5% void, and the removal of this top reflector is estimated to reduce the reactivity by 3%. It is expected that this is an underestimate of worth, for the accompanying change in steam void distribution probably increases this worth.

C. Varying the Level of Fluid in an Annular Tank in the Reflector

As mentioned earlier, the diameter of the reactor tank must conform to the ten-ton unit load limitation. There must also be adequate volume available for circulation of fluid. As a result this annular tank cannot be more than four to six inches thick (OD) in the present design. However, it is estimated that such a gap would be worth at least 3% in reactivity for the operating reactor.

The model selected for this calculation was a right circular cylinder with height augmented by the (operating) reflector savings and with a four-inch thick annular air gap adjacent to the core. The remaining reflector consisted of a six-inch thick annulus of saturated fluid (including an estimated one-inch thickness for the steel wall of the tank). Because of the air gap the continuity conditions usually applied to the fluxes and currents at the interface of core and reflector must be modified. Ignoring streaming effects, the boundary conditions on the fast and slow neutron currents were modified to continuity of the functions: radius times current. If the air gap may be considered as the limiting condition of material dilution, and if the solutions of the wave equation

$$\nabla^2\phi + B^2\phi = 0$$

are applied to the dilute material, the limiting conditions of constant fast and slow flux in the gap are implied, i.e , the fluxes are continuous across the gap.

It is believed that an underestimate of the gap worth is obtained in this analysis.

FUEL (U^{235}) MASS REQUIREMENTS

In this section, analyses of fuel requirements for "one" and three-year reactors are presented together with some discussion of the uncertainties in the critical parameters. Details of the fuel burnup are not included; they are to be found in the section entitled "Time-Dependent Reactivity Variation."

A. "One"–Year Reactor

The homogeneous virgin reactor at operating temperature (417F), with an 11.5% volume-average steam void in the core fluid, and with equilibrium xenon and samarium, is estimated to require 5.11 kilograms of U^{235} for criticality. (This is based on a total core volume of 1.9556 x 10^5 cm^3, including the volume of the control sections.) However, no allowance is included for the self-shielding of the fuel, for burnup of fuel during operation, for control of the fission product buildup (other than xenon and samarium), for the possibly higher effective neutron "temperature" in the reactor, or for compensation of the burnable poison (B^{10}) remaining in the reactor at the end of the operational cycle. Approximately 50% of the initial B^{10} is present at that time because of its low $\int \phi_s(t)dt$ exposure. Table II lists the estimated fuel mass additions required to control these deviations from the homogeneous virgin operating core.

TABLE II

"ONE"-YEAR REACTOR: FUEL REQUIREMENTS

Homogeneous virgin reactor		5.107 kg U^{235}
Self-shielding of U^{235}		0.175 kg U^{235}
Fuel burnup	corresponding to 1.25 years of operation	0.881 kg U^{235}
Fission products		0.312 kg U^{235}
B^{10} remaining at end of cycle	$\left(\frac{\sin \alpha r}{r}\right.$ - Perturbation Method$\left.\right)$	1.65 kg U^{235}
(Possibly) higher effective neutron "temperature"		Factor≈1.05
	Estimated total	8.6 kg U^{235}
B^{10} at beginning of cycle		9.13 grams

SELF-SHIELDING OF U^{235}

Since the U^{235} is distributed in a heterogeneous or "lumped" fashion in the core, in thin plates (0.03-in. fuel meat, 0.02-in. clad on each side) with a 0.20-in. channel spacing, there is some self-shielding. A factor of approximately 1.06 was applied to the thermal macroscopic absorption cross section of the core fluid because of this effect. This factor is based on a combined diffusion theory-transport theory model(6) for an infinite lattice of alternating infinite slabs of fuel and of moderator fluid. The diffusion theory aspect is that the thermal flux in the fluid was assumed to satisfy the one-group diffusion equation:

$$D_s \nabla^2 \phi_s - \bar{\Sigma}_{a_s} \phi_s + S = 0 \quad , \tag{6}$$

where S is the source of thermal neutrons from slowing down of fast neutrons; S is assumed to be constant in the moderator. The transport theory aspect is that the flux in the fuel, $\phi_s(y)$, was determined by the integral condition:(7)

$$\phi_s(y) = \int_{-\infty}^{\infty} dx \;\; E_1(|x - y|) \left[\frac{1 - a(x)}{2} \, \phi_s(x) + \frac{Q(x)}{2} \right] \tag{7}$$

where

$$a(x) = \begin{cases} 1 & \text{, in fuel} \\ \dfrac{\bar{\Sigma}_{a_s}^{\text{fluid}}}{\bar{\Sigma}_{t_s}^{\text{fluid}}} & \text{, in fluid} \end{cases}$$

and

$$Q(x) = \begin{cases} 0 & \text{, in fuel} \\ 2 & \text{, in fluid} \end{cases} \quad ;$$

distances are measured in units of total mean free path, and $E_1(u)$ is the exponential integral $E_1(u) = \int_u^\infty \frac{\exp(-z)}{z} dz$. (Since it has been assumed that $Q(X) = 0$ in the fuel slab, the normalization of S may be chosen so that $Q(X) = 2$ in the fluid.) The cross sections used in this problem were essentially those for an averaged operating virgin one-year reactor. It is expected that for the longer lived reactors a somewhat larger factor should be applied to the fluid thermal neutron absorption cross section, although in the present analysis this same factor was used for all reactors considered.

FUEL BURNUP

It has been assumed that the fuel burnup is determined by the number of years of continuous operation at full rated power, which corresponds to 1.5 mw produced by fissions in the core. Added to this is 0.05 mw for the occasional electrical overload required. Assuming 200 mev per fission, one mwd requires the fissioning of 1.0535 grams of U^{235}. If all the fissions occurred thermally, and if the assumption is made that $\frac{\bar{\sigma}^{U^{235}}_{a_s}}{\bar{\sigma}^{U^{235}}_{t_s}} = 1.183$, one mwd would require the burnup of 1.2463 grams of U^{235}.

An estimate was made of the fraction of total fissions occurring epithermally. The epithermal flux, ψ_{epi}, was assumed to be given by

$$\psi_{epi}(E)\ dE = \frac{q(E)dE}{\xi\Sigma_s(E)E} \quad , \tag{8}$$

where $q(E)$ is the slowing down density and ξ is the average logarithmic energy decrement; the thermal neutron flux $\phi_s(E)$ was assumed to be distributed as a (normalized) Maxwellian

$$\phi_s(E)\ dE = \frac{E}{(kT)^2} \exp[-E/kT]\ dE$$

with mode $kT = 0.042$ ev corresponding to the moderator temperature of the operating core. The denominator, $\xi\Sigma_s(E)\ E$, in (8) was approximated by $\Sigma_s^H(\text{core};\ E)E$, implicit here is the assumption that the energy E_0 lies above the first vibrational level (≈ 0.2 ev) of a proton in a water molecule and, as a result, epithermal scattering is by unbound protons, and $\xi = 1$. Scattering by other materials was ignored. The energy, E_0, at the point of intersection was determined by the condition that $\phi_s(E_0) = \psi_{epi}(E_0)$; then $q(E_0) \approx \overline{\Sigma}_{a_s}$, the effective total macroscopic absorption cross section for thermal neutrons in the homogenized core (where $\overline{\Sigma}_{a_s}$ is the average with respect to the Maxwellian distribution). The total number of epithermal fissions was computed and the epithermal fission contribution was estimated to be eight per cent of the total fissions. On the basis of the information in BNL-250,(8) it appears that the ratio of fission events to absorption events in the fuel is larger in the epithermal region than in the thermal region. Thus, the actual fuel burnup rate is somewhat lower than 1.2463 grams per mwd. However, the assumption of 200 mev per fission may be somewhat optimistic, and the rate of 1.2463 gm/mwd seems to be a reasonable compromise.

Of course, as discussed previously, it may be possible to operate at a smaller average power, thus prolonging the core life. The 1.25 years of continuous operation may then mean approximately 1.5 years at slightly reduced power.

FISSION PRODUCTS

In view of the continuing controversy about the effective neutron absorption cross section of fission products in thermal reactors it was decided to adopt what is considered to be a conservative estimate: linear buildup of fission product absorption at the rate of 80 barns per fission.

BURNABLE POISON REMAINING AT END OF OPERATIONAL CYCLE

Tables II and III list the fuel mass requirements for the B^{10} remaining at the end of the cycle for the "one"-year and three-year reactors, respectively. An interesting observation is that the three-year reactor requirement is smaller (in this approximation) than for the "one"-year. The explanation seems to be simple: 50% of the B^{10} burns out in "one"-year whereas perhaps only 30% remains after three years. At the same time only 50% more B^{10} is required for the virgin three-year reactor; thus, perhaps fewer grams of B^{10} remain after three years than after one year. As a result, less than two extra kilograms of fuel suffices to extend the reactor core life to three years.

XENON AND SAMARIUM

Because of the low power density in the reactor the average thermal neutron flux in the fuel is low, of the order of 6×10^{12} neutron cm/cm^3 sec in the virgin one-year reactor. Therefore the equilibrium concentration of Xe^{135} is low, and also there is an insignificant difference between the equilibrium concentration and the maximum level attained after complete shutdown. It was assumed that the total fission yield of xenon is 0.067, of which 0.003 is direct. The reactivity effect of the equilibrium xenon and samarium is $\Delta k/k \approx -0.025$; $\Delta k/k \approx -0.027$ for the maximum level of xenon and samarium attained after complete instantaneous shutdown of the virgin one-year reactor.

This maximum occurs four to five hours after the shutdown. Analogous results for the three-year reactor are very close to these results. In the present analysis it was assumed that the (statistical) ratio $\frac{\bar{\Sigma}_{a_s}^{Xe + Sm}}{\bar{\Sigma}_{a_s}^{U^{235}}}$ was 0.037 for all reactor cores discussed. This ratio was calculated for a "cut-off" $\frac{\sin \alpha r}{r}$ thermal flux variation in a bare equivalent sphere, the cut-off being made at the boundary corresponding to the core volume proper. Applying two-group perturbation theory:

$$\frac{\Delta k}{k}(\text{equilibrium Xe}) = -\frac{0.067}{1.183}\, \frac{\int_{core} \left[\frac{\overline{\phi_s(r)\,\sigma_{a_s}^{Xe}}}{\lambda^{Xe} + \overline{\phi_s(r)\,\sigma_{a_s}^{Xe}}} \right] \phi_s(r) \phi_s^*(r)\, r^2 dr}{\eta^{U^{235}} \int_{core} \phi_s(r)\, \phi_f^*(r)\, r^2 dr} \tag{9}$$

where the fluxes have been normalized to give the correct power in the core itself, and $\phi_s^*(r)$, $\phi_f^*(r)$ are respectively the adjoint thermal and adjoint fast neutron fluxes of the two-group perturbation theory. Then

$$\frac{\bar{\Sigma}_{a_s}^{Xe}}{\bar{\Sigma}_{a_s}^{U^{235}}}(\text{equilibrium}) = -\frac{\phi_f^*(o)}{\phi_s^*(o)}\, \eta^{U^{235}} \frac{\Delta k}{k}(\text{equilibrium Xe}) \tag{10}$$

or

$$\frac{\bar{\Sigma}_{a_s}^{Xe}}{\bar{\Sigma}_{a_s}^{U^{235}}}(\text{equilibrium}) = -\frac{\bar{\Sigma}_{a_s}}{\bar{\Sigma}_{a_s}^{U^{235}}} \left[1 + L^2B^2\right] \frac{\Delta k}{k}(\text{equilibrium Xe}) \; . \tag{11}$$

To this was added $\frac{\bar{\Sigma}_{a_s}^{Sm}}{\bar{\Sigma}_{a_s}^{U^{235}}}$ (equilibrium) = 0.011.

EFFECTIVE NEUTRON "TEMPERATURE"

The thermal neutron flux spectrum has been approximated as a Maxwellian distribution:

$$\phi_s(E)\, dE = \frac{E}{(kT)^2} \exp\left[- E/kT \right] dE \tag{12}$$

with mode kT corresponding to the bulk fluid temperature in the core. It is quite likely that the actual spectrum deviates significantly from this model in such a reactor. Attempts have been made to calculate this deviation for thermal reactors moderated by light water. In particular, reference may be made to the work of Wigner and Wilkins[9] and to the modifications of this paper by Avery and Krasner.[10,11] It appears, then, that it is a reasonable approximation to treat the flux spectrum as a Maxwellian distribution but with a mode, kT, somewhat higher than that for the bulk fluid. It is believed that the method of Avery and Krasner leads, in general, to an overestimate of the adjusted kT for such reactors; an interpolation of their results implies that the factor kT (neutron)/kT (moderator) ≈ 1.4 for the operating virgin one-year reactor. Applying the higher kT in two-group calculations, and assuming that the reflector savings is unchanged, it is estimated that the U^{235} requirement would be increased by approximately 5%. (The fission product absorption cross section was assumed to be independent of the effective neutron temperature. A slight reduction of this 5% addition would result if the fission products were all 1/v absorbers.)

FLUX PEAKING IN CONTROL SECTION CHANNELS

If the startup and shutdown control is provided by control blades, in the operating reactor these blades are out of the core and their volume is replaced by fluid. There is certainly an accompanying local augmentation of the thermal neutron flux and therefore an increased loss of thermal neutrons by absorption in the fluid and adjacent structure material. This is believed to be a small effect and it was considered as being compensated by the other, pessimistic, assumptions concerning fuel requirements.

COMPUTATION OF TWO-GROUP PARAMETERS: τ, D_f, D_s

The two-group age, τ, of fast neutrons was determined for the aluminum-fluid moderators in terms of a volume-average mixture of metal and water. A graph of τ versus the volume ratio of cold water to aluminum was constructed from a composite of results which appeared in early Monsanto reports.[12,13,14] The ages for the various conditions of temperature and steam void were calculated with an inverse second power void correction:

$$\tau(\text{metal/hot fluid}) = \frac{\tau(\text{metal}/H_2O \text{ of density } 1)}{(1 - \text{core void fraction})^2} .$$

The ages used in the analyses of this report very closely correspond, as well, to the ages calculated in this same fashion but depending on the curve of the age determined (essentially) experimentally[15] for various aluminum-water mixtures.

Some work has been done on the calculation of light-water moderated reactors by other methods (e.g., Goertzel-Selengut and BTHZ (at KAPL) and the age of light water has been investigated for finite reactors. The KAPL results[16,17] indicate that the age for light water may be smaller than that obtained to date by the analysis of experimental data. Shaftman has been able to match the experimentally determined critical mass requirements for several ORNL reactors,[18] whose cores consisted of solutions of U^{235} salts in cold (light) water and which were (light) water reflected. In these (unpublished) analyses, two-group diffusion theory was used, and the required two-group age of the water in the core ranged between 29.5 cm^2 and 30.2 cm^2. Of course, it is quite an extrapolation to the age for hot mixtures of water and aluminum, but at least there is a possibility indicated that the experimental ages are larger than the appropriate two-group age in the finite reactor. However, even if the proper age should be smaller for this reason, the heterogeneous structure of the core implies the possibility of a slight increase in the effective age. As a result the choice of age is not clear and the present selection appears to be as reasonable as any.

(For the analyses of the ORNL liquid criticals, the value of $D_f = 1.143$ was used for cold water.) The fast diffusion coefficient, D_f, for aluminum-water mixtures was calculated from a curve based on a composite of results from the Monsanto reports given as references for the age, τ. The computation of D_f for the homogenized hot cores is analogous to the computation of τ except that an inverse first power void correction is made.

To determine the thermal diffusion coefficient, D_s, in the core, the basic values of $\overline{\Sigma}_{tr_s}^{H_2O}$ (0.025 ev) = 2.0988, $\overline{\Sigma}_{tr_s}^{Al}$ (0.025 ev) = 0.084, and $\overline{\Sigma}_{tr_s}^{Ni}$ (0.025 ev) = 1.55 were selected for room temperature conditions. The transport cross sections of aluminum and nickel were assumed to be invariant under temperature change but, for water, the formula

$$\overline{\Sigma}_{tr_s}^{H_2O}(kT) = \Sigma_{tr_s}^{H_2O}(0.025\text{ ev}) \frac{\sigma_s^H(kT)}{\sigma_s^H(0.025\text{ ev})}$$

was adopted, in addition to the usual density correction.

There have been attempts to adduce corrections of $[1 - \overline{\mu}(kT)]$ to determine

$$\overline{\Sigma}_{tr_s}^{H_2O}(kT) = \overline{\Sigma}_s^{H_2O}(kT)\,[1 - \overline{\mu}(kT)] \quad ,$$

since there is an effect of chemical binding of the hydrogen and oxygen in the water molecules at these low (thermal) energies. Also, the uncertainty in the effective kT influence D_s; in fact, the correction to D_s because of a kt (neutron)/kT (moderator) factor equal to 1.4 is considerably larger than the Radkowsky corrections.(19) In any event this does not appear to lead to serious under-estimates of the fuel requirements.

Table III

THREE-YEAR REACTOR: FUEL REQUIREMENTS

Homogeneous virgin reactor (including equilibrium Xe and Sm)	5.107 kg U^{235}
Self-shielding of U^{235}	0.175 kg U^{235}
Fuel burnup	2.12 kg U^{235}
Fission Products	0.75 kg U^{235}
B^{10} remaining at end of cycle ($\frac{\sin \alpha r}{r}$ - Perturbation Method)	1.43 kg U^{235}
(Possibly) higher effective neutron "temperature" factor	1.055
Estimated total	10.1 kg U^{235}
B^{10} at beginning of cycle	13.03 gm

The time-independent two-group parameters are presented in Table IV.

STABILITY; VOID COEFFICIENT; REACTOR SIZE

The choice of metal-to-water ratio in the core was governed by the criterion that at operating temperatures there should be no possibility of a positive reactivity void coefficient. The choice of forty-five assemblies was made to keep the reactivity implicit to the operating steam void, $\Delta k/k$ (operating $\rightarrow$ no steam), at approximately three per cent. These choices, and the operating temperature (417F) and pressure (300 psi), place the reactor in a favorable position with regard to stability of operation, in view of the stable operation of recent BORAX reactors with similar core characteristics.

It may be possible to reduce the volume fraction of aluminum in the core and still to achieve a negative reactivity void coefficient. The reduced metal-to-water ratio would lead to a smaller $\Delta k/k$ in operating steam void and to a somewhat smaller fuel mass requirement. (However, one advantage in the larger mass is that the burnup of fuel has a smaller reactivity effect.) Or the reactor core size may be reduced, as well, which would raise the $\Delta k/k$ in steam back to the present estimated value of approximately three per cent for all the reactors under consideration.

Table IV

TIME-INDEPENDENT TWO-GROUP PARAMETERS

(Nickel content in all aluminum is 1/2% by volume)

	Cold Core (68F; kT = 0.025 ev)		Hot Core (no steam) 417F; 300 psi kT = 0.042 ev		Operating Core (11.5% average steam void in fluid) 417F; 300 psi kT = 0.042 ev	
	Core	Reflector	Core	Reflector	Core	Reflector
Aluminum Volume fraction	0.356	0	0.356	0	0.356	0
H_2O (density 1) Volume fraction	0.644	1	0.547	0.849	0.484	0.849
D_s (cm)	0.2408	0.1588	0.3201	0.2127	0.3603	0.2127
D_f (cm)	1.225	1.143	1.375	1.346	1.490	1.346
τ (cm^2)	56	33	73.8	45.7	90	45.7
$\bar{\sigma}_{a_s}^{U^{235}}$ (barns)	598	--	448	--	448	--
$\bar{\sigma}_{a_s}^{B^{10}}$ (b)	3536	--	2744	--	--	--
σ_a^{Al} (b)	0.22	--	--	--	--	--
σ_a^{Ni} (b)	4.5	--	--	--	--	--
$\bar{\sigma}_{a_s}^{Xe}$ (b)	--	--	2.60×10^6	--	2.60×10^6	--
Reflector savings (cm)	8.05	--	9.66	--	10.78	--
Total buckling B^2(cm^{-2})	0.005502	--	0.004970	--	0.004751	--
Region net void (%)	0	0	9.7	15.1	16.0	15.1

$\eta^{U^{235}} = 2.09$ $\qquad$ $\dfrac{\bar{\sigma}_{a_s}^{U^{235}}}{\bar{\sigma}_{f_s}^{U^{235}}} = 1.183$

TIME-DEPENDENT REACTIVITY VARIATION

A. Introduction

In this section are presented details of the combined reactivity effects of the fuel burnup, the formation of fission products, and the burnup of parasitic absorbers dispersed in the fuel meat for several reactors, including the "one"-year and three-year reactors described in the previous section.

Several possible burnable poisons were considered: B^{10}, Hg^{199}, and Li^6. There has been some metallurgical investigation of alloys of U^{235}, B^{10}, and aluminum, and no difficulty is anticipated for the small B^{10} weight fractions required in these reactors. The mercury isotope is presently quite expensive and is very difficult to obtain in larger than, perhaps, one-half-gram quantities, which would be completely inadequate for these reactors. Even if the isotope were available in sufficiently large quantities and at a greatly reduced cost, it is not certain that the present technology could (relatively) simply incorporate it in the fuel meat. Finally, although Hg^{199} does appear to "flatten" the reactivity curve of burnup, an increased fuel mass is required.

When associated with B^{10} or Hg^{199}, Li^6 can be used to reduce further the reactivity variation during reactor operation. However, Li^6 has such a low absorption cross section that at the end of a three-year cycle approximately 75% would remain. Therefore a substantial increase in virgin fuel mass would be required - more than use of Li^6 warrants, in the opinion of the writer, unless there should be an urgent need for a slight reduction in reactivity variation for lack of adequate automatic control.

B. Perturbation Methods

Usually, initial and terminal reactivity conditions are set and an attempt is made to obtain that uniform combination of fuel mass and poison mass which satisfies these conditions. If a single burnable poison is used, one condition determines the ratio of macroscopic absorption cross sections of the poison and fuel; the other condition then fixes the absolute masses of these two materials. In general, it is a very difficult problem to analyze the complicated interaction effects of the burnup, of the flux perturbation caused by the control scheme (e.g., motion of absorbing blades, regulation of feedwater preheating, variation of top-reflector level) which deals with the net reactivity variation, and of the time-dependent behavior of fission product poisoning. The method suggested here is to calculate the real and adjoint neutron fluxes of the two-group diffusion theory in a model of the operating virgin reactor and then to apply the two-group perturbation theory.

1. $\frac{\sin \alpha r}{r}$ - Perturbation Method

For the sake of simplicity, a bare-equivalent sphere of radius R was selected as the model of the (almost spherical) reactor. The sphere was chosen so that its geometric buckling was equal to the total buckling of the reflected, homogenized, operating virgin, forty-five assembly reactor without control sections. A concentric subsphere was selected of radius r_c equal to the total radius minus the operating reflector savings calculated for the homogenized reactor; this was labeled the "core." In such a model all the real and adjoint neutron fluxes are proportional to the function $\sin \alpha r/r$, where $\alpha = \pi/R$, and it is easy to apply the perturbation theory to the analysis of reactivity effects of burnup in the <u>core</u> itself. (The flux in the <u>core</u> is then the so-called "cut-off" $\sin \alpha r/r$.) For convenience of calculation the real thermal neutron flux was normalized to give the correct power in the <u>core</u> for some estimated fuel mass. It was then assumed that this flux shape persisted for the life of the core, and, in fact, the actual burnup calculations were made with the assumption that the magnitude of the flux also was time-independent. (The latter assumption is solely for simplification of the calculation procedure. Having determined the fuel mass requirement, it is very easy to adjust the time scale to conform to actual power requirements. For this reason the so-called "one"-year reactor actually lasts for another quarter-year, since the "one"-year designation applies to the model reactor with a 20% smaller fuel mass.) Having estimated the fraction of fuel and of burnable poison remaining at position r in the reactor after one-quarter year of "exponential burnup" in this flux, it is easy to square these fractions and thus to obtain the fractions remaining at position r after one-half year of exposure, and so on. Applying the statistical analysis of the perturbation theory to these results, only a few simple algebraic operations are required to determine the uniformly distributed virgin masses of fuel and burnable poison which meet the pre-set reactivity conditions. The method is easily generalized to more than one type of fuel and to more than one burnable poison. Of course, in general, uniqueness of the solutions requires as many reactivity conditions as there are materials which burn up or are produced as a result of burnup. For the present analysis it was assumed that the fission product absorption was directly proportional to the total number of fissions, and a cross section of 80 barns per fission was applied; hence, the fission products did not constitute "additional materials" in this analysis, and only two conditions were necessary for the case of only one fuel (U^{235}) and only one burnable poison. It was also assumed that

$$\frac{\overline{\Sigma}_{a_s}^{Xe + Sm} \text{(equilibrium)}}{\overline{\Sigma}_{a_s}^{U^{235}}}$$

was not altered during operation.

For this spherical model the perturbation analysis leads to the formula (with B^{10} as the single burnable poison):

$$\frac{\Delta k}{k}\left(\frac{n}{4}\text{ years}\right) = \frac{1}{\eta^{U^{235}}\int_o^{r_c}\sin^2\alpha r\,dr}\left\{\begin{array}{l}\dfrac{\phi^*_s}{\phi^*_f}\displaystyle\int_o^{r_c}\frac{\Sigma^{B^{10}}_{a_s}(r,o)}{\Sigma^{U^{235}}_{a_s}(r,o)}\left[1-\exp\left(-nt_0\,\bar{\sigma}^{B^{10}}_{a_s}\,A\,\frac{\sin\alpha r}{r}\right)\right]\sin^2\alpha r\,dr\\ -F\displaystyle\int_o^{r_c}\left[1-\exp\left(-nt_0\,\bar{\sigma}^{U^{235}}_{a_s}\,A\,\frac{\sin\alpha r}{r}\right)\right]\sin^2\alpha r\,dr\end{array}\right\} \quad (13)$$

with

$$F = \eta^{U^{235}} - \frac{\phi^*_s(o)}{\phi^*_f(o)}\left(\frac{\bar{\Sigma}^{Xe+Sm}_{a_s}}{\bar{\Sigma}^{U^{235}}_{a_s}} - \frac{\bar{\Sigma}^{U^{235}}_{f_s}}{\bar{\Sigma}^{U^{235}}_{a_s}}\,\frac{\bar{\sigma}^{f.p.}_{a_s}}{\bar{\sigma}^{U^{235}}_{a_s}}\right)$$

where $\bar{\sigma}^{f.p.}_{a_s} = 80 \times 10^{-24}\ cm^2$ is the fission product absorption cross section, $t_0 = 0.7884 \times 10^7$ seconds (=0.25 years), $\phi_s(r) = A\,\frac{\sin\alpha r}{r}$ is the power-normalized thermal neutron flux, in the virgin core and $\frac{\phi^*_s(r)}{\phi^*_f(r)} = \frac{k_\infty}{1 + L^2B^2}$ is constant in the bare (equivalent) sphere.

The "remainder fraction," $R_1^{B^{10}}(r, \frac{n}{4})$, of B^{10} after n quarter-years of exposure is given by:

$$R_1^{B^{10}}(r,\tfrac{n}{4}) = \exp\left(-nt_0\,\bar{\sigma}^{B^{10}}_{a_s}\,A\,\frac{\sin\alpha r}{r}\right) = \left[\exp\left(-t_0\,\bar{\sigma}^{B^{10}}_{a_s}\,A\,\frac{\sin\alpha r}{r}\right)\right]^n . \quad (14)$$

Similarly, the "remainder fraction," $R_1^{U^{235}}(r, \frac{n}{4})$, of U^{235} is defined:

$$R_1^{U^{235}}(r,\tfrac{n}{4}) = \exp\left(-nt_0\,\bar{\sigma}^{U^{235}}_{a_s}\,A\,\frac{\sin\alpha r}{r}\right) . \quad (15)$$

Using equations (14) and (15), equation (13) may be replaced by the more compact equation:

$$\frac{\Delta k}{k}\left(\frac{n}{4}\text{years}\right) = \frac{1}{\eta^{U^{235}}\int_o^{r_c} \sin^2\alpha r\, dr} \left\{ \begin{aligned} & \frac{\phi_s^*}{\phi_f^*}\int_o^{r_c} \frac{\bar{\Sigma}_{a_s}^{B^{10}}(r,o)}{\bar{\Sigma}_{a_s}^{U^{235}}(r,o)}\left[1 - R_1^{B^{10}}\left(r, \frac{n}{4}\right)\right]\sin^2\alpha r\, dr \\ & - \left[\eta^{U^{235}} - \frac{\phi_s^*}{\phi_f^*}\left(\frac{\bar{\Sigma}_{a_s}^{Xe + Sm}}{\bar{\Sigma}_{a_s}^{U^{235}}} - \frac{\bar{\Sigma}_{f_s}^{U^{235}}}{\bar{\Sigma}_{a_s}^{U^{235}}}\frac{\bar{\sigma}_{a_s}^{f.p.}}{\bar{\sigma}_{a_s}^{U^{235}}}\right)\right]\int_o^{r_c}\left[1 - R_1^{U^{235}}\left(r, \frac{n}{4}\right)\right]\sin^2\alpha r\, dr \end{aligned} \right\} \qquad (16)$$

If, as in the present analyses, $k_{eff}(0) = c$ (constant) is desired, the relationship

$$k_{eff}(0) = \frac{k_\infty}{(1 + L^2(o)B^2)(1 + \tau B^2)}$$

implies the linear relation

$$\bar{\Sigma}_{a_s}^{U^{235}}(o) = \frac{\left[D_s B^2 + \bar{\Sigma}_{a_s}^{mod.}\right] + \bar{\Sigma}_{a_s}^{B^{10}}(o)}{\frac{1}{k_{eff}(o)}\frac{\eta^{U^{235}}}{1 + \tau B^2} - \left(1 + \frac{\bar{\Sigma}_{a_s}^{Xe + Sm}(o)}{\bar{\Sigma}_{a_s}^{U^{235}}(o)}\right)} \qquad (17)$$

The second linkage of $\bar{\Sigma}_{a_s}^{B^{10}}(o)$ and $\bar{\Sigma}_{a_s}^{U^{235}}(0)$ is obtained by setting $\frac{\Delta k}{k}\left(\frac{N}{4}\text{years}\right) = d$ (constant) at the desired N quarters.

Then $\frac{\Delta k}{k}\left(\frac{j}{4}\text{years}\right)$ may be determined for j = 1,2,3,---, N-1 to yield the curve of reactivity versus time of operation. (Of course, as mentioned earlier, "N quarters" may actually correspond to a different time because of the assumption of the constant flux of preassigned normalization.)

It is emphasized that the "perturbation method" is only an approximation to the actual situation. Furthermore, the assumption of a cut-off $\frac{\sin\alpha r}{r}$ thermal neutron flux in the core ignores the flux peaking near the core-reflector interface and therefore appears to lead to an underestimate of absorber burnout near that interface. As a result, the maximum reactivity gain during burnup is underestimated. The "perturbation-burnout" reactivity curve is relatively close to the results of a multi-region two-group theory AVIDAC calculation for a "one"-year reactor (Fig. 26). However, an AVIDAC calculation of a three-year reactor yielded a reactivity curve which was not well approximated by the $\frac{\sin\alpha r}{r}$ - Perturbation Method (Fig. 28). (Note: In the figures, the term "perturbation-burnout" denotes the approximation method described above.)

2. Virgin Flux - Perturbation Method

For spherical geometry, the correct two-group perturbation theory for $\Delta k/k$ is:

$$\frac{\Delta k}{k}\left(\frac{n}{4}\text{years}\right) = - \frac{1}{\int_{core} \phi_f^*(r, \text{VIRGIN})\phi_s\left(r, \frac{n}{4}\right) r^2\, dr} \left\{ \begin{array}{l} \int_{core} \left[1 - R^{U^{235}}\left(r, \frac{n}{4}\right)\right] \phi_f^*(r, \text{VIRGIN})\phi_s\left(r, \frac{n}{4}\right) r^2\, dr \\ \int_{core} \left\{ a\left[1 - R^{U^{235}}\left(r, \frac{n}{4}\right)\right] + b\left[1 - R^{B^{10}}\left(r, \frac{n}{4}\right)\right]\right\} \phi_s^*(r, \text{VIRGIN})\phi_s\left(r, \frac{n}{4}\right) r^2\, dr \end{array} \right\} \quad (18)$$

where

ϕ_f^* (r, VIRGIN) is the adjoint fast neutron flux in the reflected operating virgin core,

ϕ_s^* (r, VIRGIN) is the adjoint thermal neutron flux in the reflected operating virgin core,

$\phi_s(r, \frac{n}{4})$ is the real, power-normalized, thermal neutron flux in the reflected operating core after n/4 years of operation,

$$a = \frac{1}{\eta U^{235}} \left[1 + \frac{\bar{\Sigma}_{a_s}^{Xe + Sm}\left(r, \frac{n}{4}\right)}{\bar{\Sigma}_{a_s}^{U^{235}}(r, \text{VIRGIN})} - \frac{\bar{\sigma}_{a_s}^{f.p.}\, \bar{\Sigma}_{f_s}^{U^{235}}}{\bar{\sigma}_{a_s}^{U^{235}}\, \bar{\Sigma}_{a_s}^{U^{235}}} \right]$$

$$b = \frac{1}{\eta U^{235}} \frac{\bar{\Sigma}_{a_s}^{B^{10}}(r, \text{VIRGIN})}{\bar{\Sigma}_{a_s}^{U^{235}}(r, \text{VIRGIN})}$$

$$R^{U^{235}}\left(r, \frac{n}{4}\right) = \exp\left[- \int_0^{0.7884 \times 10^7 n} \phi_s(r,t)\, \bar{\sigma}_{a_s}^{U^{235}}\, dt \right], \; n \geq 0 \qquad (19)$$

$$R^{B^{10}}\left(r, \frac{n}{4}\right) = \exp\left[-\int_{0}^{0.7884 \times 10^{7} n} \phi_s(r,t)\, \bar{\sigma}_{a_s}^{B^{10}}\, dt\right], \quad n \geqq 0 \tag{20}$$

In the $\frac{\sin r}{r}$ - Perturbation Method the following substitutions were made in equation (18):

replace	with
$\phi_f^*(r, \text{VIRGIN})$	$\frac{\sin \alpha r}{r} \quad \left(\alpha = \frac{\pi}{R}\right)$
$\phi_s^*(r, \text{VIRGIN})$	$\frac{\phi_s^*(o, \text{VIRGIN})}{\phi_f^*(o, \text{VIRGIN})} \frac{\sin \alpha r}{r}$
$\phi_s\left(r, \frac{n}{4}\right)$ [equation (18)] and $\phi_s(r,t)$ [equations (19), (20)]	$\phi_s(r, \text{VIRGIN})$
$\frac{\bar{\Sigma}_{a_s}^{Xe + Sm}\left(r, \frac{n}{4}\right)}{\bar{\Sigma}_{a_s}^{U^{235}}(r, \text{VIRGIN})}$	$0.037 \left(\approx \text{average} \frac{\bar{\Sigma}_{a_s}^{Xe + Sm}(\text{VIRGIN equilibrium})}{\bar{\Sigma}_{a_s}^{U^{235}}(r, \text{VIRGIN})}\right)$

and the time scale was adjusted subsequently to correspond to the actual burnup after "n/4 years."

The Virgin Flux-Perturbation Method consists of the following table of substitutions (in equations (18), (19), (20)):

replace	with
$\phi_s\left(r, \frac{n}{4}\right)$ [equation (18)]	$\phi_s(r, \text{VIRGIN})$ (power-normalized)
$\frac{\bar{\Sigma}_{a_s}^{Xe + Sm}\left(r, \frac{n}{4}\right)}{\bar{\Sigma}_{a_s}^{U^{235}}(r, \text{VIRGIN})}$	0.037

and replace equations (19), (20) by the recursion formulas:

$$R_2^{U^{235}}\left(r, \frac{n}{4}\right) = \begin{cases} 1, \; n = 0 \\ R_2^{U^{235}}\left(r, \frac{n-1}{4}\right) \exp \left[-\overline{\overline{\phi}}_s\left(r, \frac{n-1}{4}\right) \overline{\sigma}_{a_s}^{U^{235}} \times 0.7884 \times 10^7 \right], \; n>0 \end{cases} \quad (19')$$

$$R_2^{B^{10}}\left(r, \frac{n}{4}\right) = \begin{cases} 1, \; n = 0 \\ R_2^{B^{10}}\left(r, \frac{n-1}{4}\right) \exp \left[-\overline{\overline{\phi}}_s\left(r, \frac{n-1}{4}\right) \overline{\sigma}_{a_s}^{B^{10}} \times 0.7884 \times 10^7 \right], \; n>0 \end{cases} \quad (20')$$

where $\overline{\overline{\phi}}_s\left(r, \frac{j}{4}\right)$ has been normalized to give the correct power in the core while maintaining the shape of

$$\overline{\overline{\phi}}_s(r,o) \equiv \overline{\overline{\phi}}_s(r, \text{VIRGIN}) \quad ,$$

the power-normalized thermal neutron flux in the virgin operating core.

The Virgin Flux-Perturbation Method was applied to the analysis of reactivity variation in the "one"-year reactor; the results are presented in Fig. 26. It appears to be an improvement over the $\frac{\sin \alpha r}{r}$ - Perturbation Method. (The application of this method of analysis to the three-year reactor has not yet been completed.)

As mentioned previously, the perturbation-burnout methods yield results which are in good agreement with those obtained on the AVIDAC for the "one"-year reactor. However, the AVIDAC "one"-year problem did not include the delayed effect of samarium. Figure 24 presents results of the application of the $\frac{\sin \alpha r}{r}$ - Perturbation Method to "one"-year reactors wherein the delayed effect has been considered. The reactivity variation may be observed to be confined within narrow limits.

C. Reactivity Effects of Buildup of Xenon and Samarium to Equilibrium Concentrations

As a compromise between reducing fuel mass and reducing reactivity variation it is usually considered desirable to have $k_{eff} = 1$ at both ends of the operating cycle. However, because of the low thermal neutron flux in the fuel, the samarium builds up very slowly, and this time-dependent reactivity effect must be taken into account in the analysis of net reactivity variation. In Fig. 23 the $\Delta k/k$ inherent to the buildup of xenon and samarium in the "one"-year reactor is presented, based on a slightly smaller equilibrium value than the result of a statistical, or perturbation, analysis; the buildups were calculated for a volume-average flux of $6.1 \times 10^{12} \frac{\text{neutron cm}}{\text{cm}^3 \text{ sec}}$ in the fuel whereas the equilibrium $\frac{\Delta k}{k}$ (Xe + Sm) = -0.025 was computed for the spatially varying thermal neutron flux. However, the equilibrium $\frac{\Delta k}{k}$ (Sm) = -0.0075 is correct for this reactor, since the equilibrium concentration of samarium is (essentially) independent of the flux level. The condition $k_{eff}(o) = -0.0075$ was selected in every case but for the already initiated AVIDAC calculation of the "one"-year reactor, for in the perturbation analyses it was assumed that equilibrium concentrations of xenon and samarium were present in all times; the terminal condition $k_{eff}(\frac{N}{4}) = 0$ was retained.

In calculating the buildup of xenon and samarium to equilibrium levels it was assumed that the thermal flux is a step-function of time -- zero up to time zero, and $6.1 \times 10^{12} \frac{\text{neutron cm}}{\text{cm}^3 \text{ sec}}$ for $t \geqq 0$. The resulting equations are:

$$\frac{\Delta k}{k}(\text{Xe};\, t) - \frac{\Delta k}{k}(\text{equilibrium Xe}) = \frac{\bar{\bar{\Sigma}}^{Xe}_{a_s}(t)}{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}} \frac{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}}{\bar{\bar{\Sigma}}_{a_s}} \frac{1}{1 + L^2B^2} \tag{21}$$

and

$$\frac{\bar{\bar{\Sigma}}^{Xe}_{a_s}(t)}{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}} = \frac{0.067}{1.183} \frac{\overline{\phi_s \sigma}^{Xe}_{a_s}}{\lambda^{Xe} + \overline{\phi_s \sigma}^{Xe}_{a_s}} \left[1 - \exp\left(-\left[\lambda^{Xe} + \overline{\phi_s \sigma}^{Xe}_{a_s}\right] t\right)\right] \tag{22}$$

$$+ \frac{0.064}{1.183} \frac{\overline{\phi_s \sigma}^{Xe}_{a_s}}{\lambda^{Xe} + \overline{\phi_s \sigma}^{Xe}_{a_s} - \lambda^{I}} \left[\exp\left(-\left[\lambda^{Xe} + \overline{\phi_s \sigma}^{Xe}_{a_s}\right] t\right) - \exp\left(-\lambda^{I} t\right)\right] \quad ;$$

$$\frac{\Delta k}{k}(\text{Sm};\, t) - \frac{\Delta k}{k}(\text{equilibrium Sm}) = \frac{\bar{\bar{\Sigma}}^{Sm}_{a_s}(t)}{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}} \frac{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}}{\bar{\bar{\Sigma}}_{a_s}} \frac{1}{1 + L^2B^2} \tag{23}$$

and

$$\frac{\bar{\bar{\Sigma}}^{Sm}_{a_s}(t)}{\bar{\bar{\Sigma}}^{U^{235}}_{a_s}} = \frac{0.013}{1.183} \left[1 - \frac{1}{\lambda^{Pm} - \overline{\phi_s \sigma}^{Sm}_{a_s}} \left(\lambda^{Pm} \exp\left[-\overline{\phi_s \sigma}^{Sm}_{a_s} t\right] - \overline{\phi_s \sigma}^{Sm}_{a_s} \exp\left[-\lambda^{Pm} t\right]\right)\right] \tag{24}$$

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Figure 23 is based on these equations, with the parameters:

$$\lambda^{Xe} = 2.09 \times 10^{-5}/\text{sec}$$

$$\lambda^{I} = 2.87 \times 10^{-5}/\text{sec}$$

$$\lambda^{Pm} = 4.10 \times 10^{-6}/\text{sec}$$

$$\overline{\phi_s \sigma_{a_s}^{Xe}} = 1.586 \times 10^{-5} \text{ absorptions/sec}$$

$$\overline{\phi_s \sigma_{a_s}^{Sm}} = 3.633 \times 10^{-7} \text{ absorptions/sec}$$

Of course, the actual buildup of samarium depends on the flux. Hence, it is different for the longer lived reactors, where the fuel mass is larger. This was not considered in the present analyses. (The larger is the virgin fuel mass the smaller is the average flux and the slower is the buildup to equilibrium samarium.)

D. Machine (AVIDAC) Analysis of Reactivity Variation in Reflected Spherical Reactors

An AVIDAC code is available for two-group diffusion theory calculations of multi-region, spherically symmetric reactors. Therefore, it was possible to investigate in more detail the reactivity variation and the variation of the thermal neutron flux during reactor operation.

The "AVIDAC-burnout" method consists of the following:

(1) On the AVIDAC, compute the thermal neutron flux shape $\widetilde{\phi}_s(r, 1)$ for the virgin reactor (assumed to be a reflected sphere) and determine $\Delta k/k$;

(2) Normalize this flux (getting $\phi_s(r, 1)$) so as to provide the desired fission rate in the core;

(3) Compute the "remainder fractions," $R_3^{U^{235}}(r, t_1)$ and $R_3^{B^{10}}(r, t_1)$, of the fuel and burnable poison (e.g., B^{10}) at radial position r after exposure to flux $\phi_s(r, 1)$ for time t_1 -- "exponential burnup" in constant flux;

(4) Volume-average the remainder fractions within each core region of the AVIDAC partition of the reactor core; from the U^{235} burnout fraction up to time t_1, the fission product buildup in each core region is computed;

(5) Assign the new distributions of U^{235}, B^{10}, and fission products in the core regions, in terms of the volume-averaged remainder fractions.

At this point a second AVIDAC computation is made for the fluxes and the $\Delta k/k$ of the reactor, and steps (2) through (5) are repeated; -- and so on.

The criterion for terminating the iteration process of the AVIDAC is the reproduction of the integrated fission source in each core region to within a specified error, i.e., reproduction of the $\Delta k/k$ to within a specified error. At the present time, steps (2) through (4) are performed by hand; if many such burnup problems should be desired it would be possible, and preferable, to incorporate additional machine routines. It is not necessary to begin with a uniform distribution of materials in the reactor; this spherical model requires uniformity only within each (spherically) annular region.

The computations for the remainder fractions may be summarized by the following recursion formula: $(0 \leqslant r \leqslant r_{core})$

$$R_3^M(r,k) = \begin{cases} 1, & k = 1 \\ R_3^M(r, k-1) \exp\left[-\phi_s(r,k-1)\,\bar{\sigma}_{a_s}^M\, t_{k-1}\right], & k \geqslant 2 \end{cases} \quad (25)$$

where

$\phi_s(r,j)$ is the thermal neutron flux computed by the AVIDAC for the j-th problem, normalized to yield the correct fission rate in the core;

M represents the material in question (e.g., U^{235} or B^{10}) ;

$\bar{\sigma}_{a_s}^M$ is the thermal microscopic absorption cross section of material M; and t_j is the duration, in seconds, of the j-th burnout interval $(j \geqslant 1)$.

Denoting the virgin density (atoms/cm^3) of material M in core region ℓ by $N^M(\ell,1)$, the volume-averaged remainder fraction in region ℓ for the k-th AVIDAC problem is given by:

$$\frac{N^M(\ell,k)}{N^M(\ell,1)} = \frac{3}{r_\ell^3 - r_{\ell-1}^3} \int_{\text{region } \ell} r^2 R_3^M(r,k)\, dr \quad , \quad (26)$$

$$\text{where } r_j = \begin{cases} 0, & j = 0 \\ \text{outer radius region of } \ell, & j = \ell > 0. \end{cases}$$

Of course, equations (25) and (26) are applied only when material M is present in the region.

In accordance with the assumption that the fission product absorption cross section is directly proportional to the number of fissions,

$$\bar{\Sigma}_{a_s}^{f.p.}(\ell,k) = \frac{\bar{\sigma}_{a_s}^{f.p.}}{1.183} N^{U^{235}}(\ell,1) \left[1 - \frac{N^{U^{235}}(\ell,k)}{N^{U^{235}}(\ell,1)}\right] \qquad (27)$$

The "AVIDAC-burnout" method was applied to two reactors (four core regions, two reflector regions in each case):

1. "One"-Year Reactor (1.5 mw) (Figs. 26, 27)

$\left(\frac{\Delta k}{k}(0) = 0, \text{ with equilibrium Xe and Sm.}\right)$

$$\bar{\Sigma}_{a_s}^{U^{235}}(\ell,1) = 0.045662 \qquad (\ell = 1,2,3,4)$$

$$\bar{\Sigma}_{a_s}^{B^{10}}(\ell,1) = 0.006270 \qquad (\ell = 1,2,3,4)$$

(The virgin fuel mass in this spherical model is 7.08 kg, uncorrected for possibly higher neutron kT.)

2. Three-Year Reactor (1.55 mw) (Figs. 28, 29)

$\left(\frac{\Delta k}{k}(0) = -0.0075, \text{ with equilibrium Xe and Sm}\right)$

$$\bar{\Sigma}_{a_s}^{U^{235}}(\ell,1) = 0.057130 \qquad (\ell = 1,2,3,4)$$

$$\bar{\Sigma}_{a_s}^{B^{10}}(\ell,1) = 0.011800 \qquad (\ell = 1,2,3,4)$$

(The virgin fuel mass in this spherical model is 8.86 kg, uncorrected for possibly higher kT.)

The resulting reactivity curves are expected to be qualitatively correct; because of the incorect assumptions of spherical symmetry and of the core parameters D_f, τ, D_s, and $\bar{\Sigma}_{a_s}^{moderator}$, it is not possible to ascribe quantitative exactness. However, there is a liberal margin of control still available for the feedwater control of the "one"-year reactor. For the longer lived reactor, it seems probable that an additional automatic control worth three per cent or more would suffice.

It should be noted that the volume of the core in this spherical model is approximately ten per cent less than the volume of the reactor core containing the four control sections.

NOTE: Equations (13) and (18) yield the change in $\Delta k/k$ from the "excess" reactivity, $[\Delta k/k](0)$, for the virgin reactor, assuming constant equilibrium levels for xenon and samarium. The $[\Delta k/k]$(n/r years) so computed were adjusted, to obtain $[\Delta k/k]$(n/4 years) for the reactor, by subtracting $[\Delta k/k](0)$; these results are presented in the figures.

E. Reactivity Variation for Burnup in Volume-Average Flux

A third method for estimating reactivity variation is to burn up the fuel and poison exponentially in a (spatially) constant flux which is adjusted periodically so as to approximate the increased flux requirement as fuel is depleted. Knowing the core volume, the power requirement, and the spatially constant fuel density in the virgin reactor, a volume-average flux, $\bar{\phi}_s$(VIRGIN), may be obtained.

The remainder fraction of material M after time t_1, $\bar{R}^M(t_1)$, is

$$\bar{R}^M(t_1) = \exp\left[-\bar{\phi}_s(\text{VIRGIN})\,\bar{\sigma}^M_{a_s}\,t_1\right] \quad . \tag{28}$$

In general, the following recursion formula is obtained:

$$\bar{R}^M(t_k) = \begin{cases} 1, & k = 0 \\ \bar{R}^M(t_{k-1})\exp\left[-\bar{\phi}_s(t_{k-1})\,\bar{\sigma}^M_{a_s}\,(t_k - t_{k-1})\right], & k > 0 \end{cases} \tag{29}$$

where

$$\bar{\phi}_s(t_j) = \frac{\bar{\phi}_s(\text{VIRGIN})}{\bar{R}^M(t_j)} \quad , \quad j \geqslant 0 \quad .$$

Assuming that the total buckling, B^2, is constant, the reactivity may be obtained (e.g., using U^{235} and B^{10}) as:

$$\frac{\Delta k}{k}(t_k) = \frac{k_{eff}(t_k) - k_{eff}(\text{VIRGIN})}{k_{eff}(t_k)} \quad ,$$

where

$$k_{eff}(t_k) = \frac{k_\infty(t_k)}{(1 + L^2(t_k)\,B^2)\,(1 + \tau B^2)} \quad ,$$

and

$$k_\infty(t_k) = \frac{\eta^{U^{235}} \, \bar{\Sigma}_{a_s}^{U^{235}}(t_k)}{\left[1 + \dfrac{\Sigma_{a_s}^{Xe + Sm}(t_k)}{\Sigma_{a_s}^{U^{235}}(t_k)}\right] \Sigma_{a_s}^{U^{235}}(t_k) + \bar{\Sigma}_{a_s}^{mod.} + \bar{\Sigma}_{a_s}^{B^{10}}(t_k) + \bar{\Sigma}_{a_s}^{f.p.}(t_k)} \,, \tag{30}$$

with the cross sections determined in the obvious manner.

Table V lists the "one"-year reactor U^{235} and B^{10} remainder fractions computed by volume-averaging the AVIDAC regional remainder fractions, and also as calculated by exponential burnup in the periodically adjusted volume-average flux. Although the corresponding fractions are nearly identical, the spatial variation of the remainder fractions apparently yields a significant difference in the corresponding curves of reactivity variation.

Table V

VOLUME-AVERAGE REMAINDER FRACTIONS OF B^{10} AND U^{235} FOR THE "ONE"-YEAR REACTOR

Time (year)	AVIDAC	Burnup by Volume-Average Flux	Difference
$Boron^{10}$			
0	1	1	0
0.25	0.8641	0.8628	0.0013
0.50	0.7446	0.7418	0.0028
0.75	0.6399	0.6353	0.0046
1.00	0.5477	0.5420	0.0057
$Uranium^{235}$			
0	1	1	0
0.25	0.9764	0.9762	0.0002
0.50	0.9528	0.9524	0.0004
0.75	0.9292	0.9286	0.0006
1.00	0.9054	0.9048	0.0006

APPENDIX C

RADIOACTIVE RADIATION HEALTH HAZARDS

STATEMENT OF THE PROBLEM

Determine the radioactive radiation health hazards associated with the operation, routine servicing, and periodic core replacement or maintenance of this reactor system.

SUMMARY AND CONCLUSIONS

The radioactive radiations that are associated with this reactor system can be considered in three separate stages because of the differences in intensity and differences in decay rates of the various activated isotopes. These stages are:

1. N^{16} and N^{17} activity from irradiated H_2O.
2. Activation of corrosion-erosion products.
3. Release of fission products from defective fuel elements.

During normal operation the activated isotopes that will generate the greatest amount of radiation will be the O^{16} (n,p) N^{16} that releases a 7-mev gamma but with the short half-life of 7.35 seconds, and the O^{17} (n,p) N^{17} that releases a 1-mev neutron with a shorter half-life of 4.14 seconds. The calculations indicate that none of the water or steam piping will require shielding to protect personnel from these particular radiations, although a distance of two feet should be maintained from all pipes for personnel exposed for several hours. However, this can best be determined by radiation measurements taken during the times of exposure. The hot well and the ion exchangers will radiate a much greater amount than the pipes because of the greater quantity of water contained, and a distance of 10 ft should be safe for long exposures. For short periods of time, men can approach these containers for any necessary inspections or servicing; however, radiation measurements should be made at each location.

When considering states (2) and (3), in which normal corrosion-erosion products from the materials of construction of this system are irradiated and collected in the ion exchangers or in places of low velocity such as the hot wells, plus the collection of fission products from the failure of fuel elements, some sort of temporary shielding or remote approach should be made available for these collection points.

The evaluation of the corrosion-erosion activity for the BER Cooling Water by Grotenhuis of ANL can be applied directly to this reactor because of their similarity and is summarized as follows:

"From this study it appears that no shielding is necessary for the crud activity in the cooling water under the specified conditions. It may be well to point out the conditions which may change, or be too restrictive:

a. The by-pass filter is assumed to be 100% effective.

b. The crud does not get an opportunity to cycle through the reactor more than once.

c. The entrainment factor is assumed to be 10^{-3} or less.

"The activity due to a total accumulation of crud at some point in the system may be treated as though it were a point source and converted to mr/hr at 3 ft. Although this is not realistic, it is a quantitative way of indicating the maximum value of the activity that may collect. The activity thus calculated amounts to 0.2 mr/hr which would indicate that there is no problem in shielding the accumulated crud, again, subject to the conditions listed previously."

The BER system is about 12 times larger than that of the APPR(4) (Army Package Power Reactor) in both heat power and in H_2O flow, and the above quotation covering the corrosion-erosion products for the BER should be a good indication that based on the same system conditions of ion exchange effectiveness, this reactor will contain very little radiation from these products.

The amount of fission products that may be released to the water system by failure of fuel elements is more a matter of probability that can only be determined by actual experience. The amount of fission products generated by fission is roughly one gram per megawatt day of operation, but any attempt to determine the amount released to the water system would be very arbitrary. Rather, a safe approach in the design of the plant layout would be to include two or more ion exchange tanks in parallel and located in isolated pits or cells to allow alternate use and recharging.

Discussions with Dr. H. G. Swope of the ANL Waste Disposal Facility indicates that ion exchange resins are very effective for cleanup of dissolved or colloidal fission products and that the "loaded" resins may be the most efficient way of handling and disposing of hot waste materials. Should a fuel element break up into larger than colloidal particles, the particles would possibly be lodged in the filters, in the bottom of the hot well, in the bottom of the reactor vessel, or in the recesses of the turbine casing. In this event, the most serious health hazard would be from contamination during the times of maintenance of these various pieces of equipment.

CALCULATIONS

Amount of H_2O in the System (Use 4,500 lb)

1.	Reactor Tank	=	3,990. lb
2.	10" Steam Line x 10'	=	0.024
3.	4" Steam Line x 50'	=	2.8
4.	1-1/2" H_2O Line x 120'	=	88.3
5.	2" H_2O Line x 10'	=	13.1
6.	Steam in Reactor Tank	=	32.6
7.	Condenser and Turbine	$\simeq$	1.5
8.	Hot well	$\simeq$	120.
9.	Ion Exchangers	=	63.2
10.	Heat Exchangers	$\simeq$	16

A. <u>N^{16} Activation in Cooling Water</u>

$$A = N_0 \sigma_a \phi_{HP} \frac{1 - e^{-\lambda t_r}}{1 - e^{-\lambda t_b}}$$

A, disintegrations/sec lb H_2O leaving reactor core

N_0, susceptible atoms O^{16}/lb H_2O = 1.52×10^{25} atoms/lb

σ_a, experimental cross section of O^{16} for fission neutrons = 3.8×10^{-29} cm^2

ϕ_{HP}, fission neutrons calculated from heat power = 6.54×10^{11} n/(cm^2) (sec)

P, heat power = 1500 kw

λ, decay constant for N^{16} = 0.0944/sec

V, neutron velocity assumed to fit ϕ_{HP} = 1 cm/sec

t_r, time of irradiation in core = 0.8 sec

t_b, time of recirculation in blanket = 9.6 sec

t_c, time of complete cycle = 360 sec

Note that the ϕ_{HP} is a calculated average neutron fission flux based upon the accepted constants of $3.1 \times 10^{13} \frac{\text{fissions}}{\text{sec}} = 1$ kw and 2.5 neutrons per fission. This will not agree with the fast or thermal flux determined by the core physics calculation, but is necessary to properly match the most recent experimental values of cross sections for the activation of both O^{16} and O^{17} which have a high threshold energy for activation.

$$\phi_{HP} = \frac{3.1 \times 10^{13} \text{ fissions}}{\text{kw sec}} \times \frac{1500 \text{ kw sec}}{1.78 \times 10^5 \text{ cm}^3} \times \frac{2.5 \text{ neutrons}}{\text{fissions}}$$

$$\times \frac{1 \text{ cm velocity}}{\text{sec}}$$

$$A = N_0 \, \sigma_a \, \phi \frac{1 - e^{-\lambda t_r}}{1 - e^{-\lambda t_b}}$$

$$= 1.52 \times 10^{25} \times 3.8 \times 10^{-29} \times 6.54 \times 10^{11} \; \frac{1 - e^{-0.075}}{1 - e^{-0.9}}$$

$= 4.45 \times 10^7$ disintegrations/(sec)(lb of H_2O) of 6.6 average mev γ.

Since this activity decays very rapidly, let us use a length of steam pipe (leaving the reactor) that will contain one pound of H_2O.

L, length = specific vol/area of steam pipe

$$= 1.54 \frac{\text{ft}^3}{\text{lb}} \times \frac{1}{0.087 \text{ ft}^2}$$

$$= 17.7 \text{ feet}$$

A preliminary calculation based on the assumption that the entire pound was concentrated at a point indicated a safe distance of 2 ft for 0.060 r/8-hr exposure. Then using the chart by Stephenson,[22] where a flux of 10^3 photons/ cm^2 sec of 6.6-mev gamma for an 8-hr exposure is permissible within AEC Health Standard, the reduction factor by which the activity must be decreased to arrive at a safe value $= \frac{4.45 \times 10^7 \text{ dis/lb sec}}{10^3 \text{ dis/cm}^2 \text{ sec}}$.

Assuming a spherical distribution of photons and relying entirely upon the inverse square of distance for reduction of intensity, the safe distance

$$x = \sqrt{\frac{4.45 \times 10^7}{10^3 \times 4\pi}} \text{ cm} \sim 2 \text{ ft}$$

B. N^{17} Activation in APPR Cooling Water

The N^{17} activity is calculated in the same manner as for the N^{16} except with different values as follows:

N_0, susceptible atoms O^{17}/lb H_2O	$= 5.94 \times 10^{21}$ atoms/lb
σ_a, experimental cross section of O^{17} for fission neutrons	$= 7.8 \times 10^{-28}$ cm^2
λ, for N^{17}	$= 0.167$ sec^{-1}

$$A = N_0 \, \sigma_a \, \phi_{HP} \frac{1 - e^{-\lambda t_r}}{1 - e^{-\lambda t_b}}$$

$$= 5.94 \times 10^{21} \times 7.8 \times 10^{-28} \times 6.54 \times 10^{11} \frac{(0.123)}{(0.798)}$$

$$= 4.66 \times 10^5 \text{ dis/lb sec of 1-mev neutrons.}$$

The allowable dose rate of 0.060 rem/8 hr is produced by 60 n/(cm^2)(sec) of 1-mev energy. Therefore, the safe distance is:

$$x = \sqrt{\frac{4.66 \times 10^5}{60 \times 4\pi}} \text{ cm} \sim 1 \text{ ft}$$

C. N^{16} and N^{17} Activity in Large Containers (Fig. 22)

Since the hot well is the largest single container of H_2O outside the reactor vessel, the integrated gamma and neutron doses from the entire container are calculated as follows:

$$A_T = A_E \frac{f}{\lambda} \left[1 - \exp(-\lambda m/f) \right]$$

A_T, integrated isotope radiation, disintegrations/sec

A_E, specific activity entering tank	$= 3.65 \times 10^7$ dis/lb sec
f, flow rate	$= 1.39$ lb/sec
λ, decay constant for N^{16}	$= 0.0944$/sec
m, total weight of H_2O in tank	$= 120$ lb
t, time from reactor to tank	$= 2.03$ sec

$$A_E = A_0 \; e^{-\lambda t}$$

$$= 4.55 \times 10^7 \times e^{-0.189}$$

$$= 3.65 \times 10^7 \text{ dis/lb sec}$$

$$A_T = A_E \frac{f}{\lambda} \; [\, 1 - \exp\,(-\lambda m/f)\,]$$

$$A_T = 3.65 \times 10^7 \times \frac{1.39}{0.0944}\left[1 - \exp\left(-\frac{0.094 \times 120}{1.39}\right)\right]$$

$$= 5.4 \times 10^8 \text{ dis/sec of 6.6 average mev gamma.}$$

Reduction factor required for health tolerance:

$$X^2 = \frac{5.4 \times 10^8}{10^3 \times 12.6}$$

$$= 4.3 \times 10^4 \text{ for gamma only.}$$

Considering the N^{17} activity:

$$A_E = 4.66 \times 10^5 \times 0.73 \text{ neutrons/lb sec}$$

$$\lambda = 0.167 \text{ sec}^{-1}$$

$$A_T = A_E \frac{f}{\lambda} \; [1 - \exp\,(-\lambda m/f)\,]$$

$$= 2.82 \times 10^6 \text{ dis/sec of 1-mev neutrons.}$$

Reduction factor for 1-mev neutrons only:

$$X^2 = \frac{2.82 \times 10^6}{6 \times 12.6}$$

$$= 3.74 \times 10^4$$

Safe distance for 8-hour exposure for both γ and n:

$$X = \sqrt{4.3 \times 10^4 + 3.74 \times 10^4}$$

$$= 283 \text{ cm or 9.3 ft.}$$

D. Corrosion-Erosion Products Activity

Since the analysis by Grotenhuis indicated that the corrosion-erosion products activity was not serious in a system very similar to this reactor, only two representative calculations will be presented. One calculation will be of a high intensity but short half-life element (aluminum), and one calculation of a high intensity and long half-life (cobalt). The aluminum will be present in large quantities since the fuel elements and the condenser tubes are made of this material, whereas the cobalt should only be present in trace amounts because of its limited use in valve seats and turbine blades. Assume a corrosion rate of 0.0008 in./yr of the 760 sq ft of aluminum surface which give approximately 8.5 lb aluminum per year entering the water system, and using approximately the same value for cobalt as in the BER study, or 0.50 lb/yr. At equilibrium:

$$A_\infty = \frac{\phi\ \sigma_a\ r\,\lambda}{m \times F} \cdot \frac{1}{(\lambda + F)} \quad \text{dis/(sec)(lb } H_2O)$$

A, specific activity, disintegrations/sec lb H_2O

ϕ, thermal neutron flux	$= 2 \times 10^{13}$ neutrons/(cm^2)(sec)
σ_a, activation cross section	$= 2.1 \times 10^{-25}$ cm^2 for Al
r, Atoms of crud entering system	$= 2.72 \times 10^{18}$ atoms Al/sec
λ, decay rate	$= 5.01 \times 10^{-3}$ sec
m, weight of H_2O in system	= 4,500 lb
F, $\frac{f_1 + \alpha f_2}{m} = \frac{0.279}{4500}$	$= 6.16 \times 10^{-5}$/sec
f_1, flow of H_2O through cleanup	= 0.278 lb/sec
f_2, flow of steam out of reactor	= 1.39 lb/sec
α, entrainment factor	= 0.001

$A_\infty = 2 \times 10^{13}$ neutrons/(cm^2)(sec) x 2.1×10^{-25} cm^2 x 2.72×10^{18} atoms/sec x 5.01×10^{-3}/sec x 1/0.27 x 5.06×10^{-3}
$= 4.05 \times 10^7$ dis of Al^{28}/(sec)(lb H_2O) of 2.3-minute half-life.

$A_\infty = 7.08 \times 10^3$ dis Co^{60}/(sec)(lb H_2O) of 5.3-year half-life.

E. Fission Product Activity

The fission products generated as a result of the U^{235} fissions include roughly 30 different elements, each having several active isotopes (Table VI). The exact decay patterns are best followed on the Chart of the Nuclides, or in the Reactor Handbook.[24] The quantities that would be released as a result of a fuel plate failure would vary greatly, dependent upon both the length of irradiation time generating the products, and the length of time that the defective plate was left in the system after the defect developed. Since the probability of failure of plates increases at a rising rate with irradiation, the more safe approach to the maintenance of this system would be to assume that: (1) large quantities of fission products exist in the irradiated plates; and (2) instrumentation is available to detect, within a few minutes, any measurable amounts of fission products released to the water system by a defective plate.

Following the detection of fission products in the water system, there are two widely divergent methods of living with this defective plate. If the ion exchangers approach 100% effectiveness, and if the defective plate corrodes-erodes at a slow rate, the simplest approach would be to allow the reactor to continue to operate and to pick up the released products in the ion exchangers. When the resins become "loaded" they would be replaced.

The more conventional method, which is the practice at Hanford and Savannah River, is immediately to shut the reactor down, find the defective slug, and either remove or replace it. This method would keep the release of fission products to the system to a minimum but very likely would also result in numerous unscheduled shutdowns.

The APPR fuel plates contain very little U^{238} which means that very little plutonium will be mixed with the fission products to complicate the maintenance problem.

The gamma radiation from a fuel plate irradiated six months is estimated by a rough extrapolation from known data of a slug as follows:

Time After Shutdown	Energy, mev	Intensity at 10 ft, r/hr
4 hr	1.7	95.5
1 day	1.7	52.4
6 days	1.7	38
14 days	1.6	27.5
35 days	1.5	15.7
78 days	1.3	8.0
442 days	1.2	0.46

In the event the fission products from a defective fuel plate were allowed to completely corrode-erode away and be collected in an ion exchanger, the ion exchanger would radiate approximately the same activity indicated above except for additional self-shielding and dilution because of the larger volume.

Table VI

CORROSION-EROSION ACTIVATION IN REACTOR COOLING WATER

Element $_zN^A$	Number of Atoms/lb	Rate Corrosion-Erosion, lb/yr	Activated Isotope $_zN_i^A$	% Abund-ance	N_A Susceptible Corroded Atoms / Corroded lb	α Susceptible Corroded Atoms / Sec	λ, Sec^{-1}	γ, mev	σ_a, Barns	A_∞ Equilibrium, dis / sec lb H_2O
$_5B^{10}$	2.74×10^{25}		$_5B^{10}$	18.8					3800	Stable
$_7N^{14}$	1.96×10^{25}		$_6C^{14}$	99.6			3.73×10^{-12}	0.16β	1.74	
O^{16}	1.52×10^{25}		$_7N^{16}$	99.8			9.44×10^{-2}	6.6	38 μb	4.45×10^{7}
O^{17}			$_7N^{17}$	0.039			1.67×10^{-1}	1.neut	780 μb	4.66×10^{5}
Mg^{26}	1.05×10^{25}		$_{12}Mg^{27}$	11.3			1.2×10^{-3}	1.01	0.05	
Al^{27}	1.01×10^{25}	8.5	$_{13}Al^{28}$	100	1.01×10^{25}	2.72×10^{18}	5.01×10^{-3}	1.8	0.21	4.05×10^{7}
Cr^{50}	5.48×10^{24}		$_{24}Cr^{51}$	4.4			3.02×10^{-7}	0.32	11.0	
Mn^{55}	4.97×10^{24}		$_{25}Mn^{56}$	100			7.45×10^{-5}	2.13	13.4	
Fe^{58}	4.72×10^{24}		$_{26}Fe^{59}$	0.33			1.70×10^{-7}	1.3	0.7	
Co^{59}	4.64×10^{24}	0.50	$_{27}Co^{60}$	100	4.64×10^{24}	7.36×10^{16}	1.08×10^{-3}	1.33^a	17	8.5×10^{7}
Co^{59}							4.15×10^{-9}	1.33^a	20	7.08×10^{3}
$_{28}Ni^{64}$	4.28×10^{24}		$_{28}Ni^{65}$	1.0	4.28×10^{22}		7.39×10^{-5}	1.12	2.6	
$_{29}Cu^{63}$	4.35×10^{24}		$_{29}Cu^{64}$	69.			1.49×10^{-5}	1.34	3.9	
Cu^{65}	4.21×10^{24}		$_{29}Cu^{66}$	31.			2.68×10^{-3}	1.32	1.8	
$_{40}Zr^{94}$	2.91×10^{24}			17.4			1.23×10^{-7}	0.9	0.1	
Zr^{96}	2.85×10^{24}			2.8			1.13×10^{-5}	0.8	0.2	
$_{41}Nb^{93}$	2.94×10^{24}			100			1.75×10^{-3}	0.04	1.0	
$_{42}Mo^{98}$	2.79×10^{24}			23.9			2.97×10^{-6}	0.73	0.13	
Mo^{100}	2.74×10^{24}			9.5			7.69×10^{-4}	0.9	0.18	
$_{74}W^{180}$	1.52×10^{24}		$_{74}W^{181}$	0.14			6.67×10^{-8}	1.8	10±10	
W^{184}	1.49×10^{24}		$_{74}W^{185}$	30.6			1.04×10^{-7}	0.134	2.1	
W^{186}	1447×10^{24}		$_{74}W^{187}$	28.4			7.69×10^{-6}	0.615	40.0	

[a]per gamma for each of 2 gammas

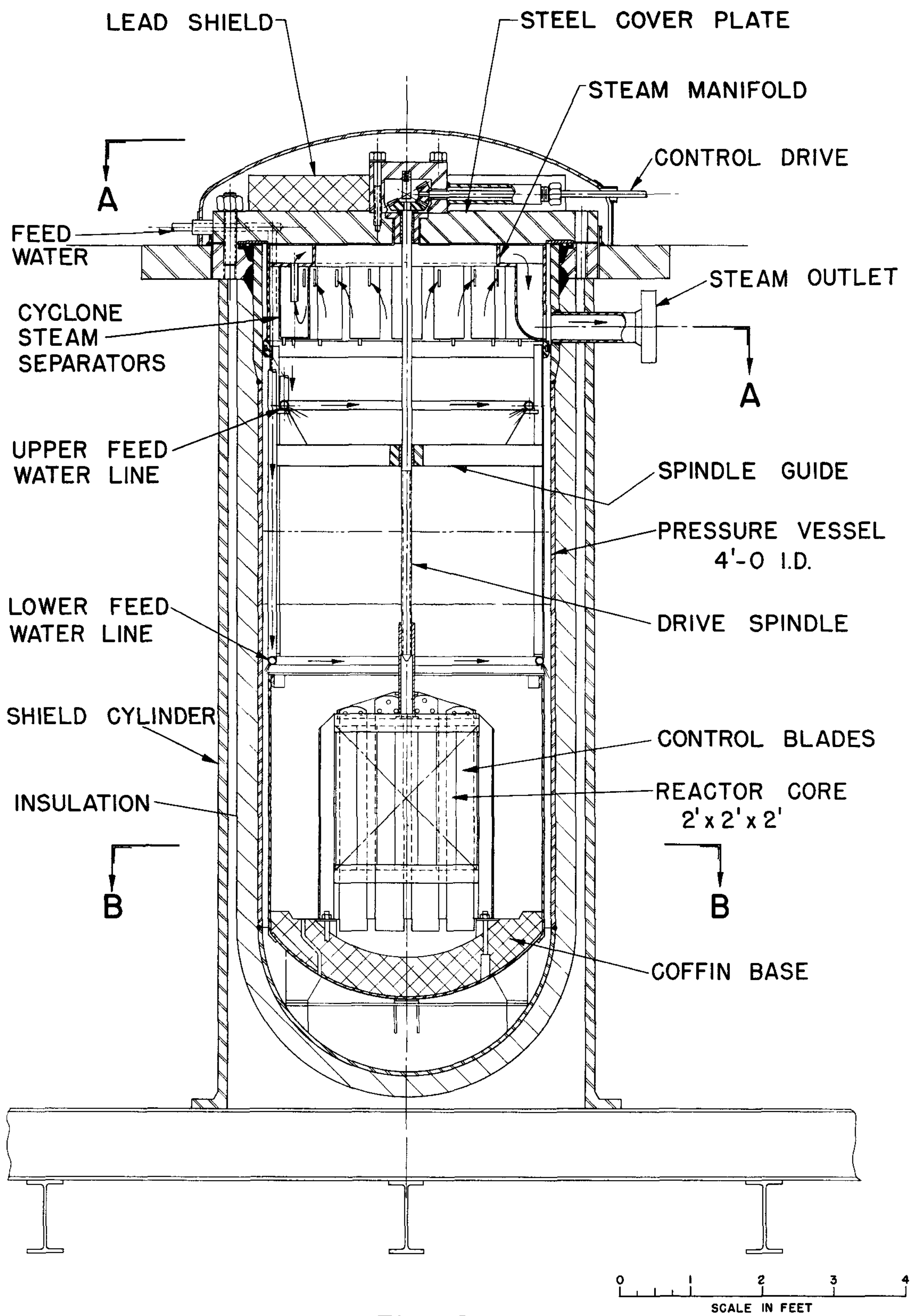


FIG. 2
1.5 MW BOILING REACTOR
ELEVATION VIEW

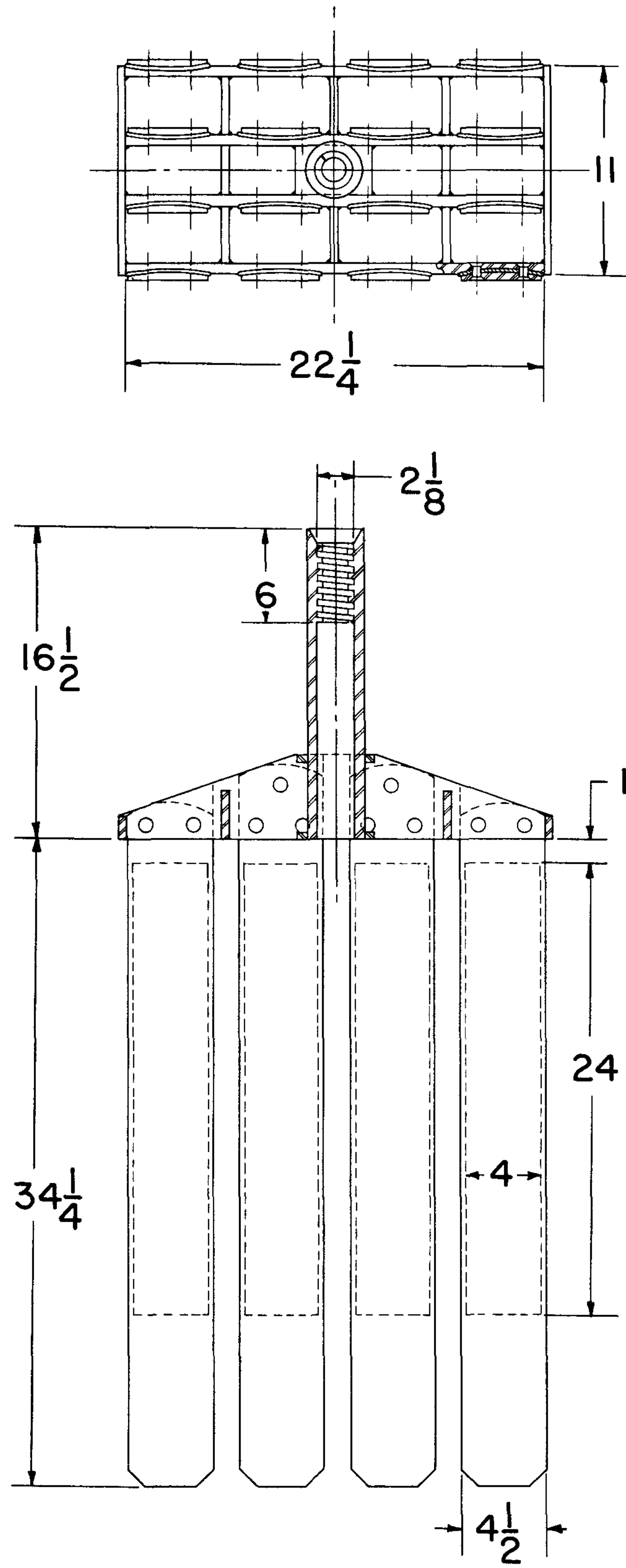


FIG. 2-A
CONTROL BLADE ASSEMBLY

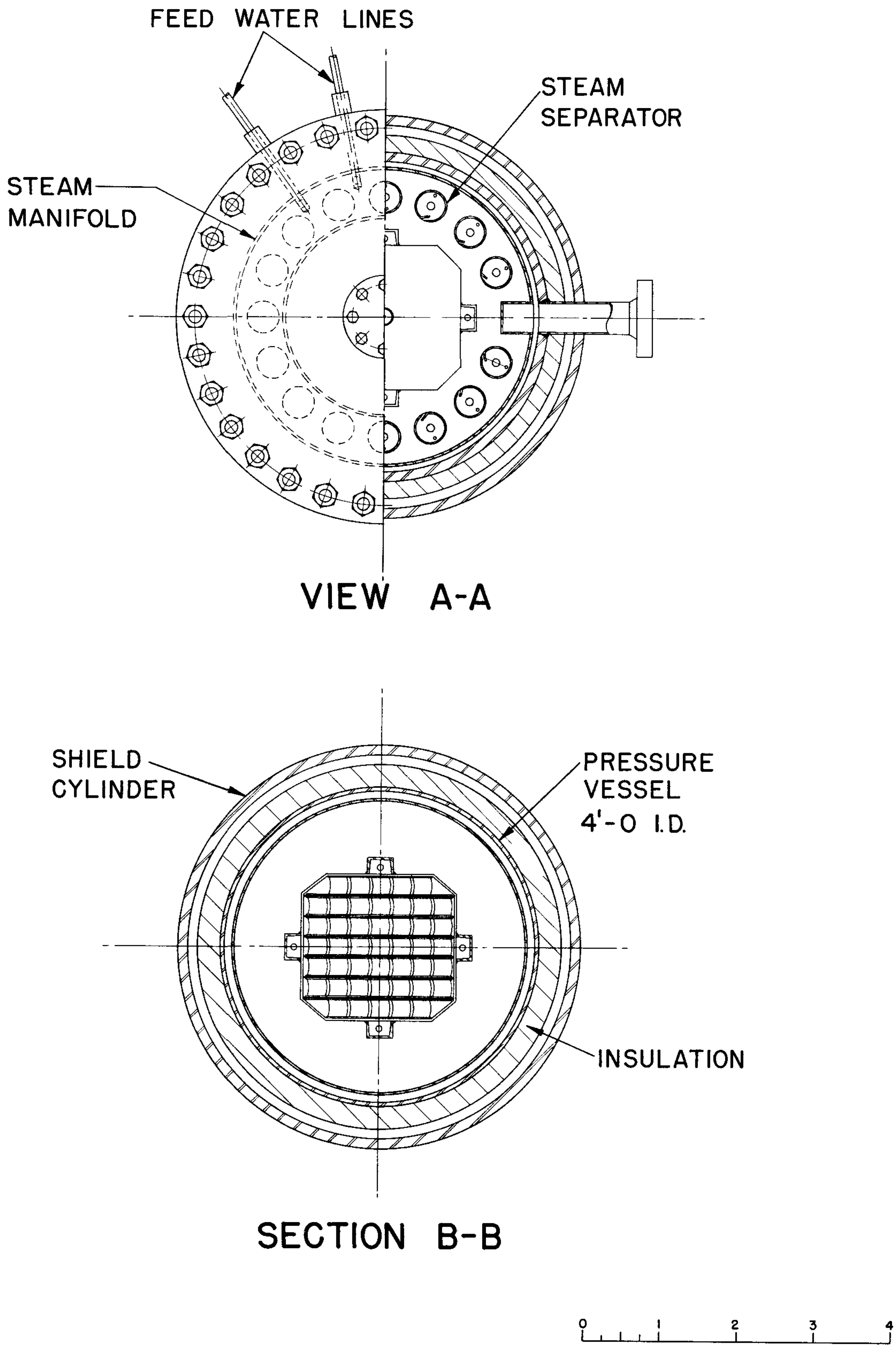


FIG. 3

1.5 MW BOILING REACTOR

737-105

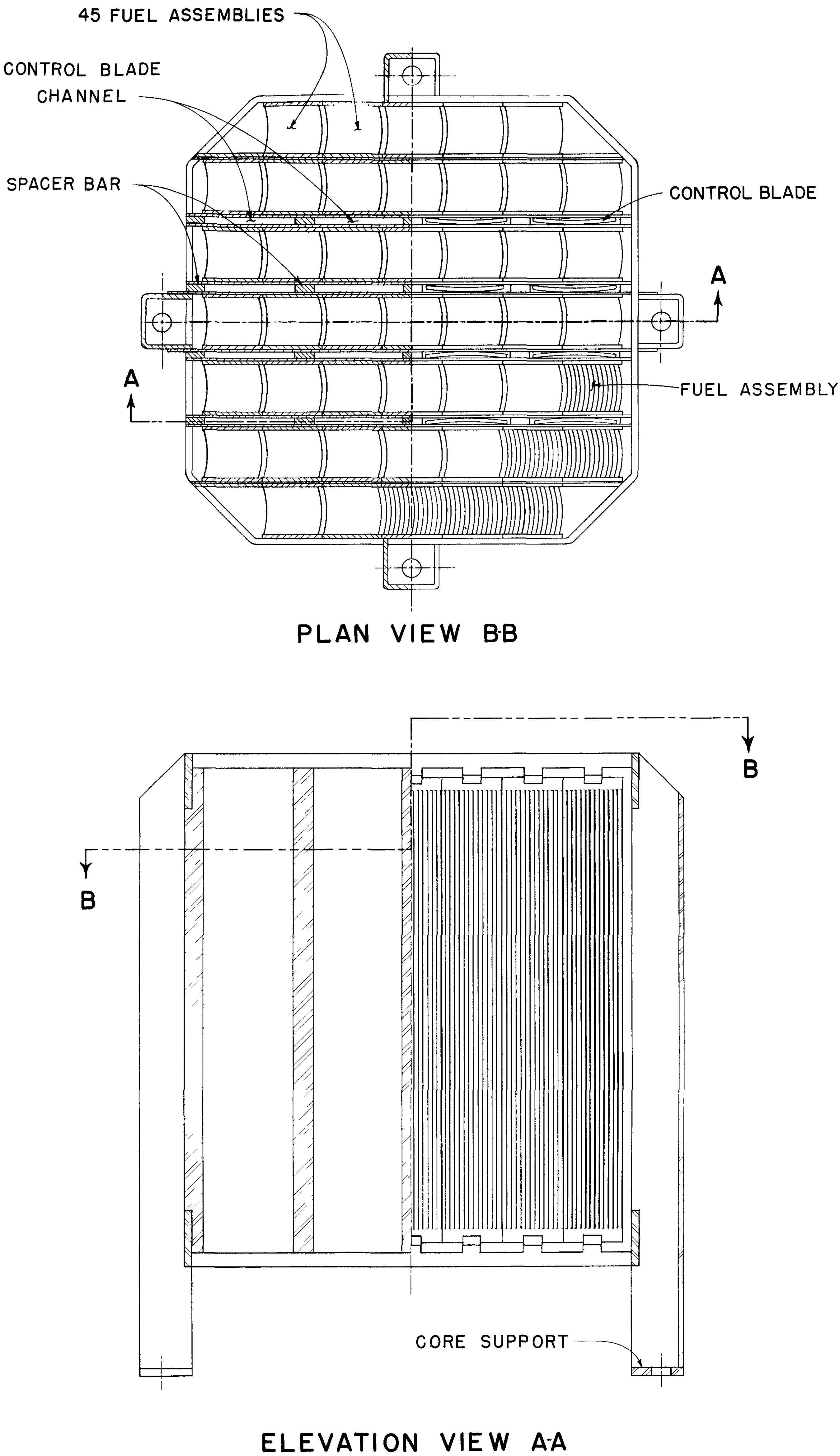


FIG. 4
1.5 MW REACTOR CORE

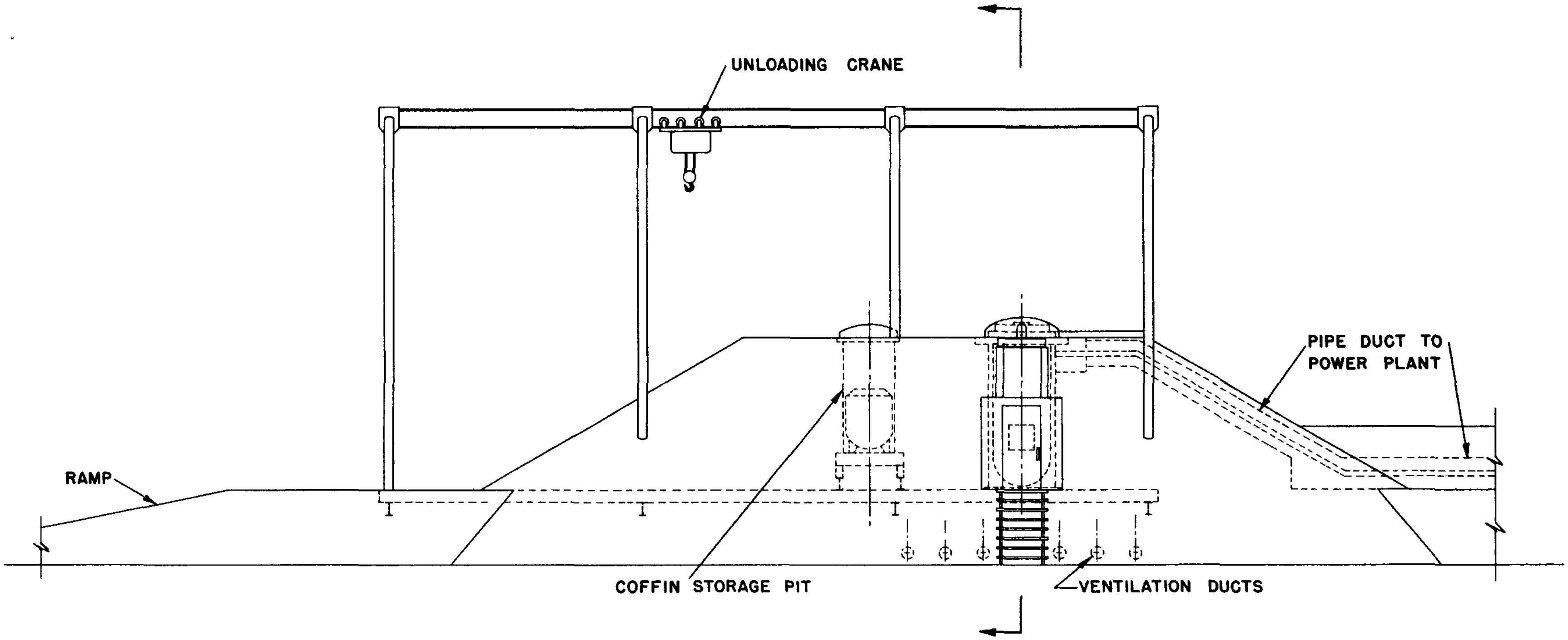


FIG. 5
REACTOR INSTALLATION
ELEVATION

107

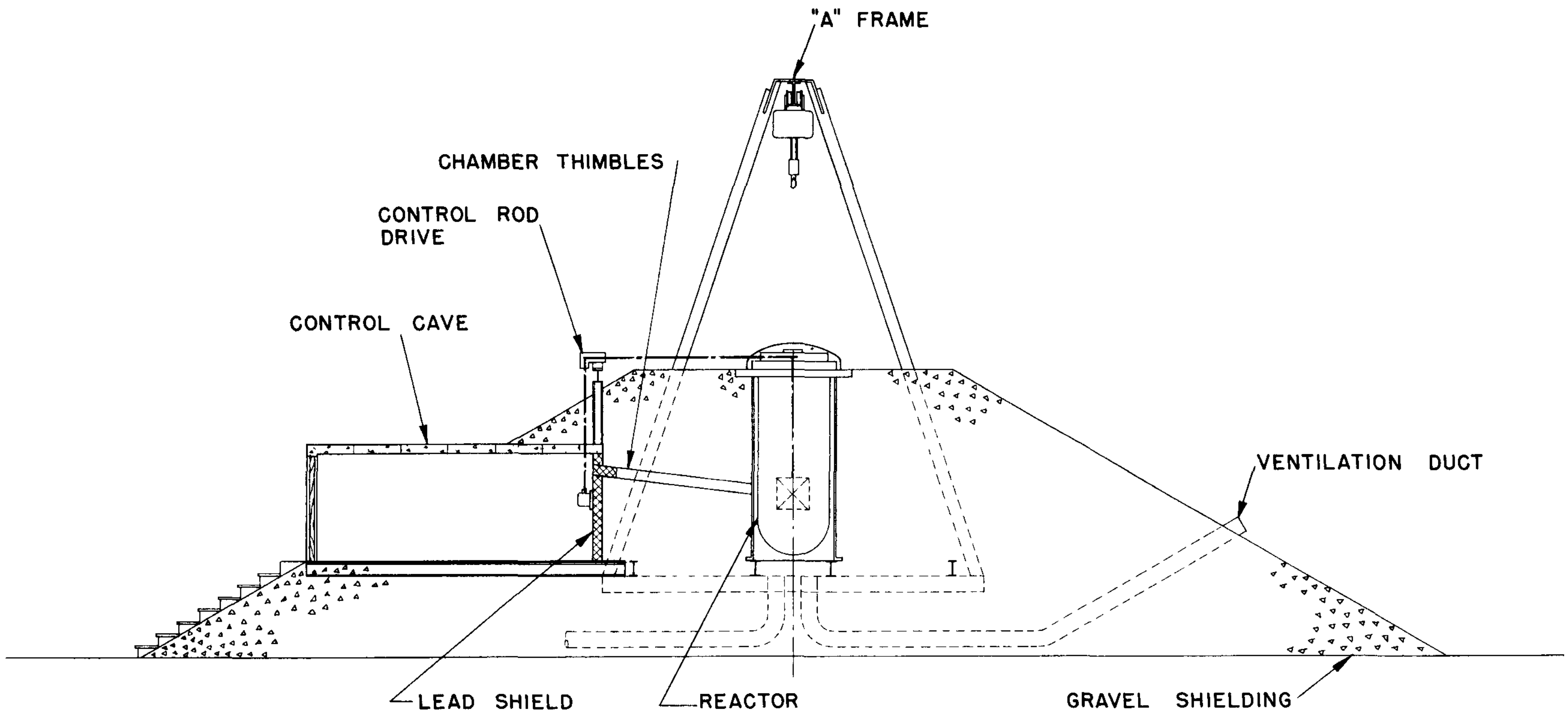


FIG. 6

REACTOR INSTALLATION

CROSS SECTION

0 2 4 6 8 10

SCALE IN FEET

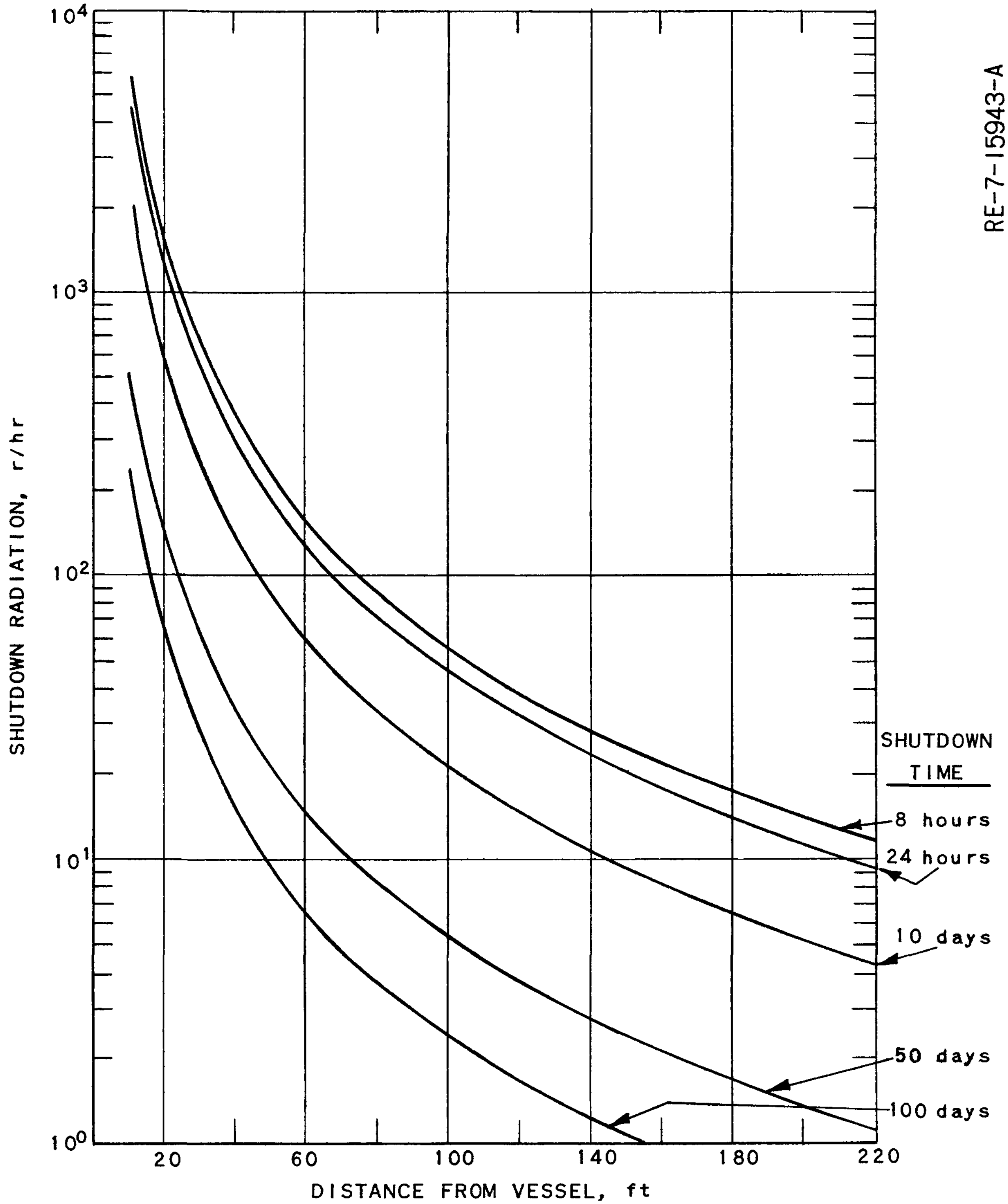


FIG. 7
1.5 mw BOILING REACTOR
ACTIVITY AFTER SHUTDOWN

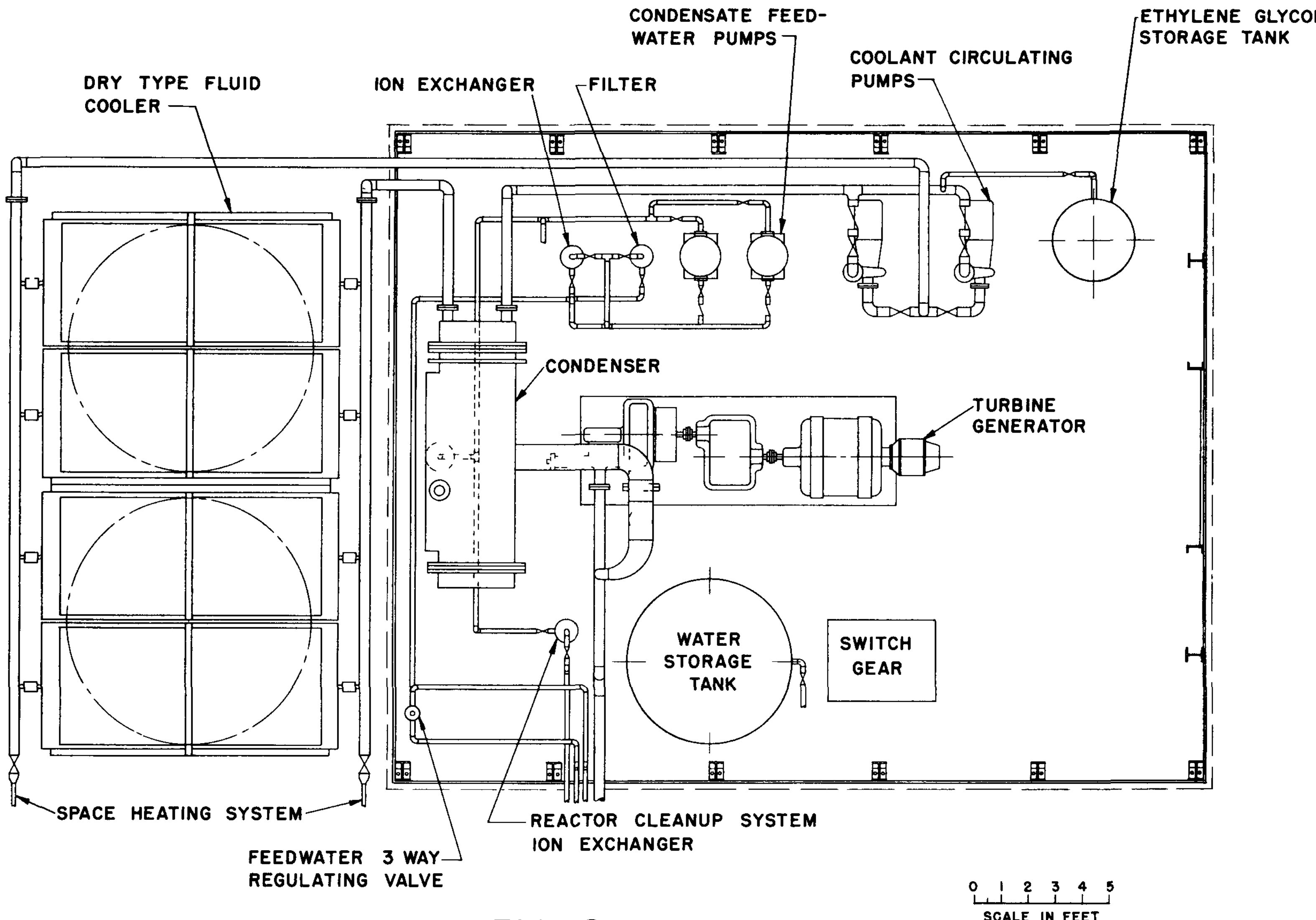


FIG. 8

POWER PLANT

FLOOR PLAN

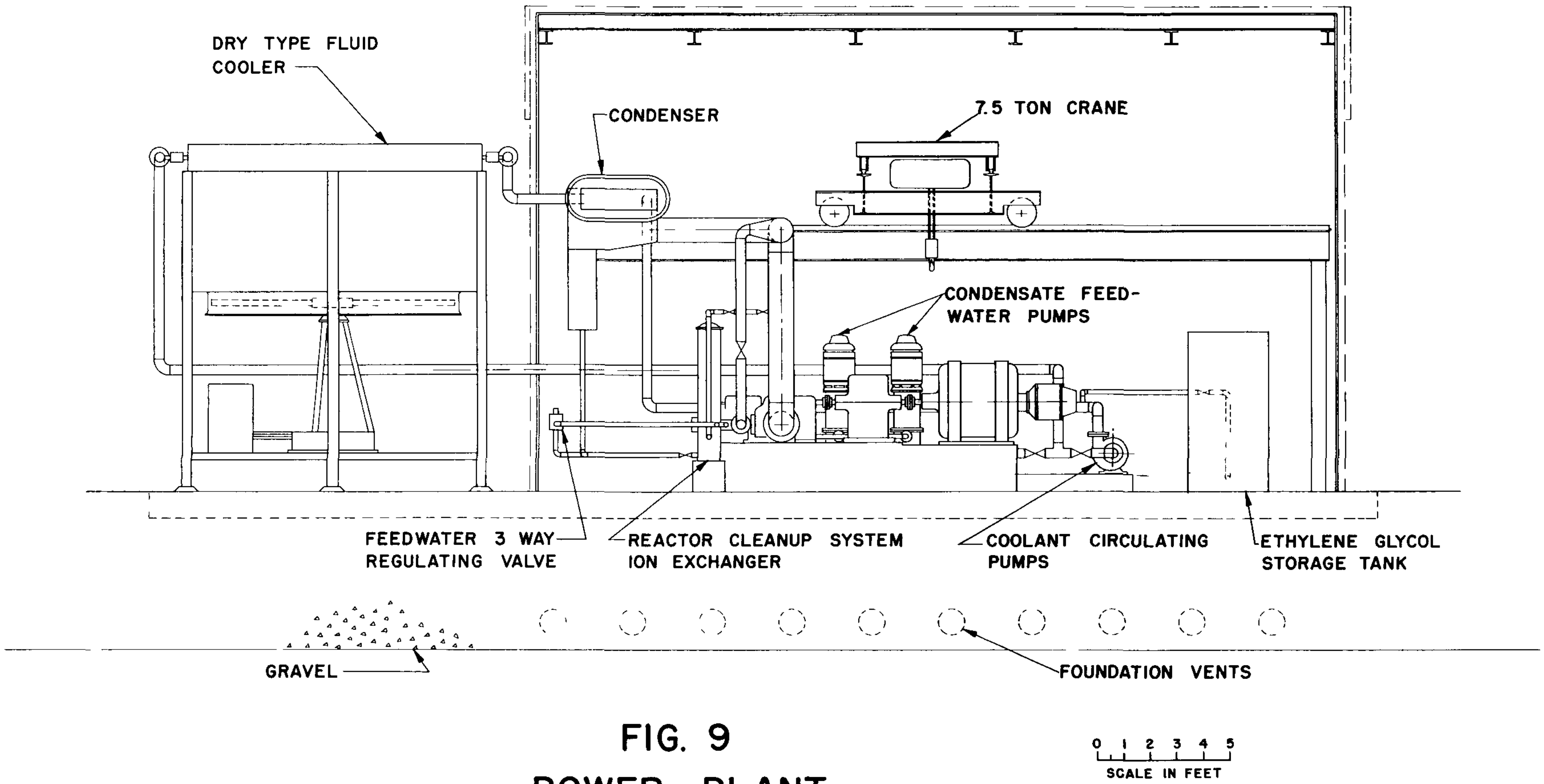


FIG. 9
POWER PLANT
ELEVATION

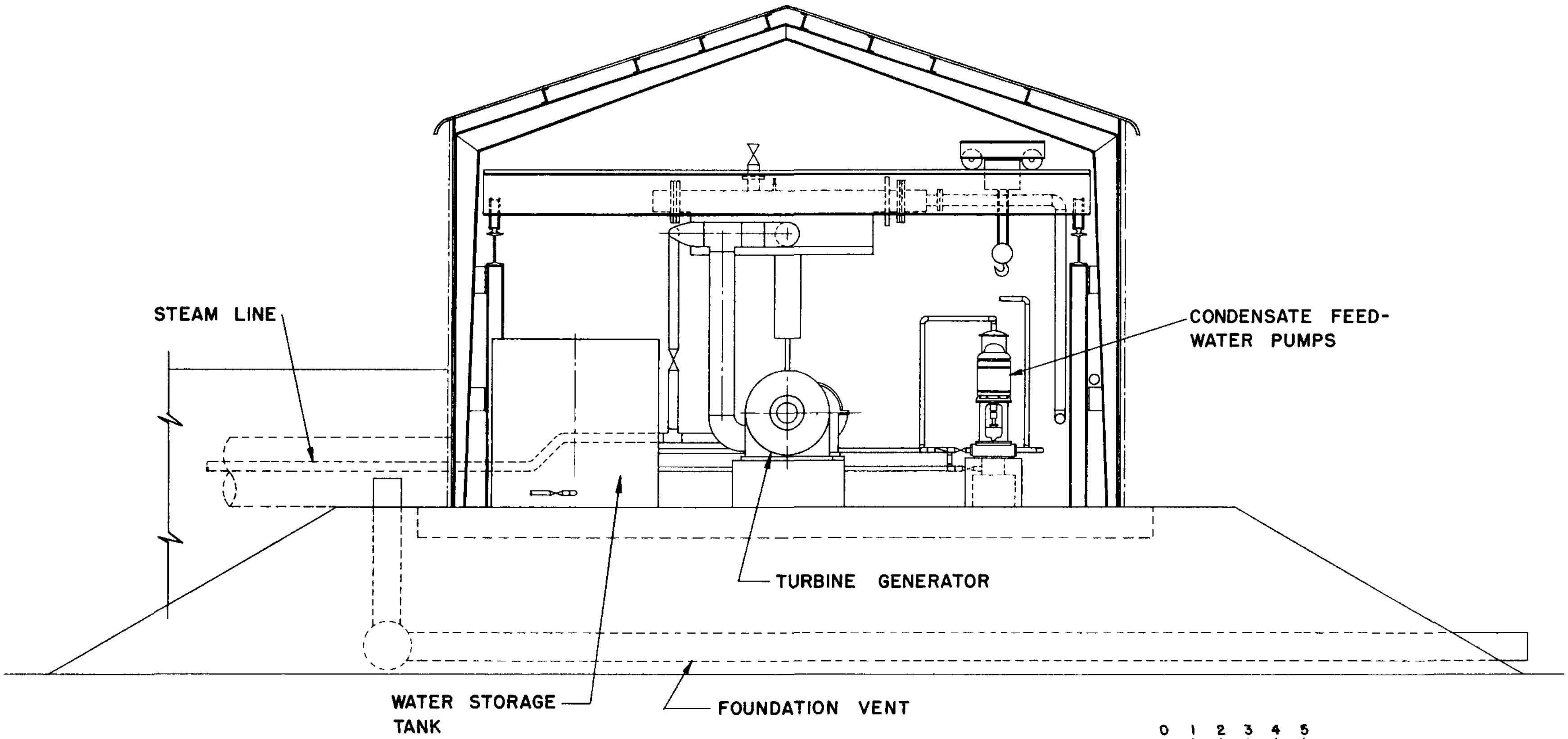


FIG. 10
POWER PLANT
CROSS SECTIONAL VIEW

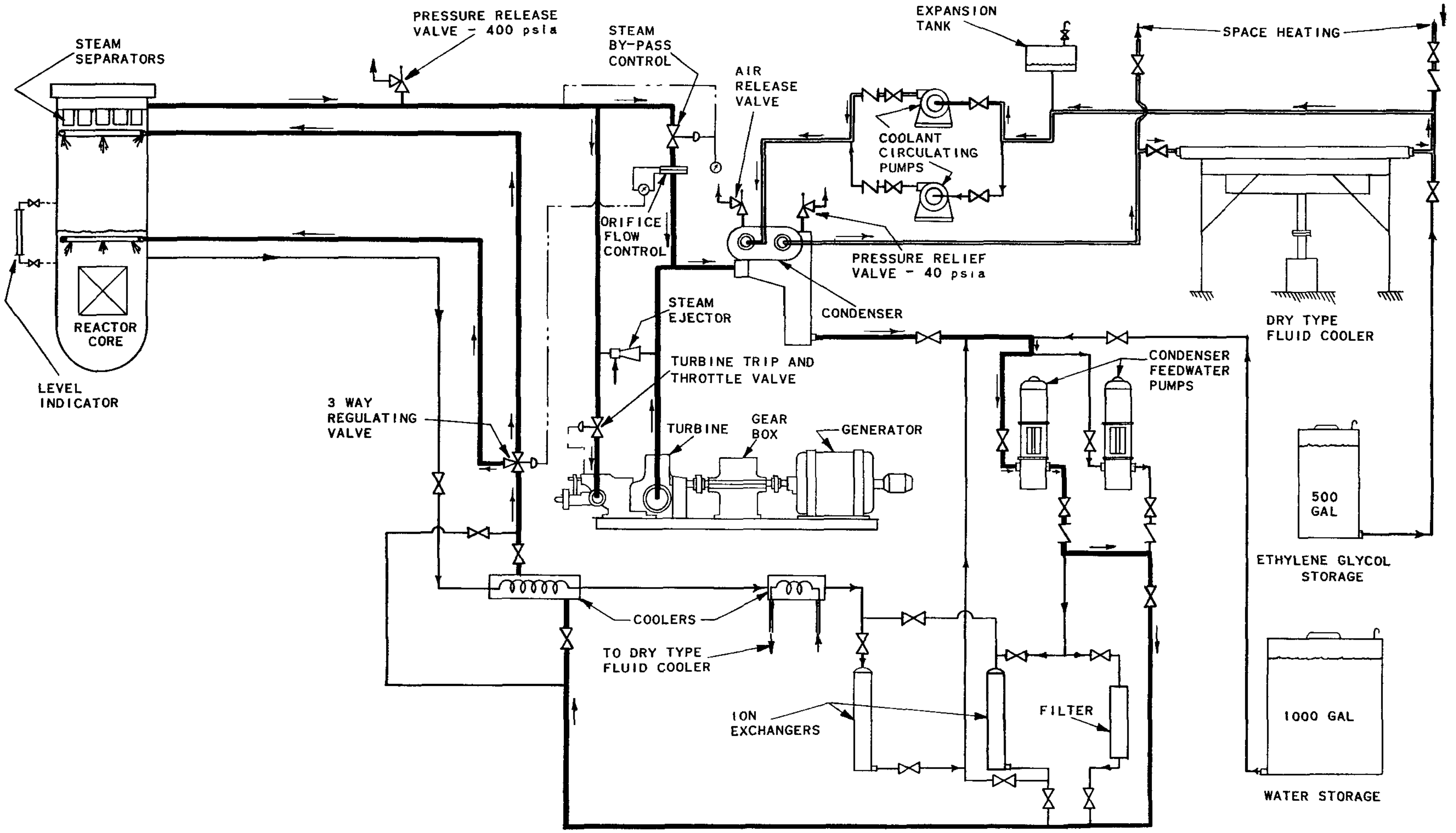


FIG. 11
FLOW DIAGRAM FOR COMBINED
POWER AND HEATING PLANT
(FEED WATER PREHEAT CONTROL SYSTEM)

RE-8-15699-B

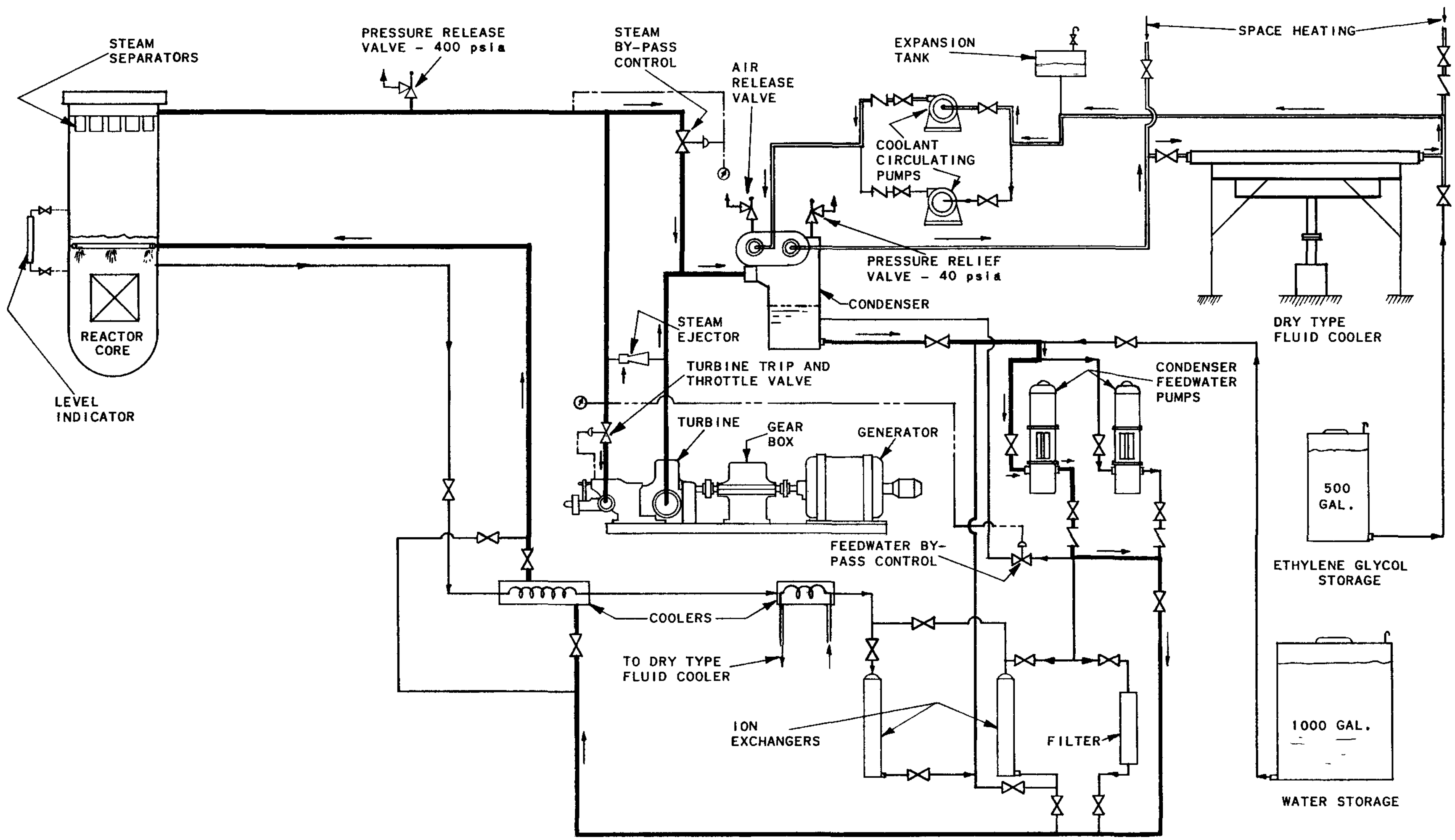


FIG. II-A
FLOW DIAGRAM FOR COMBINED POWER AND HEATING PLANT
(TOP REFLECTOR CONTROL SYSTEM)

RE-8-16067-B

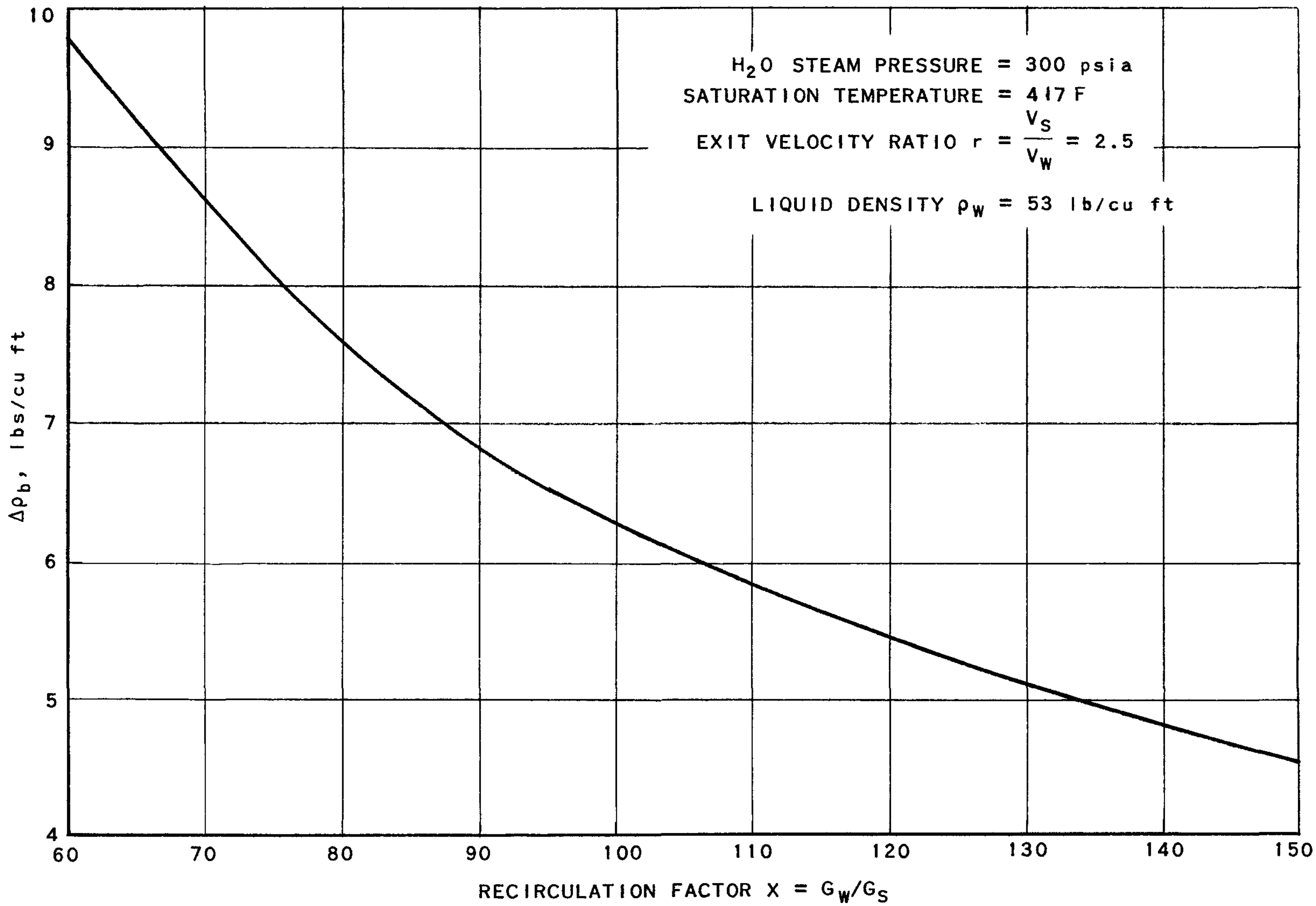


FIG. 12
AVERAGE DENSITY LOSS IN BOILING FLUID
VS RECIRCULATING FACTOR X

RE-7-15683-A

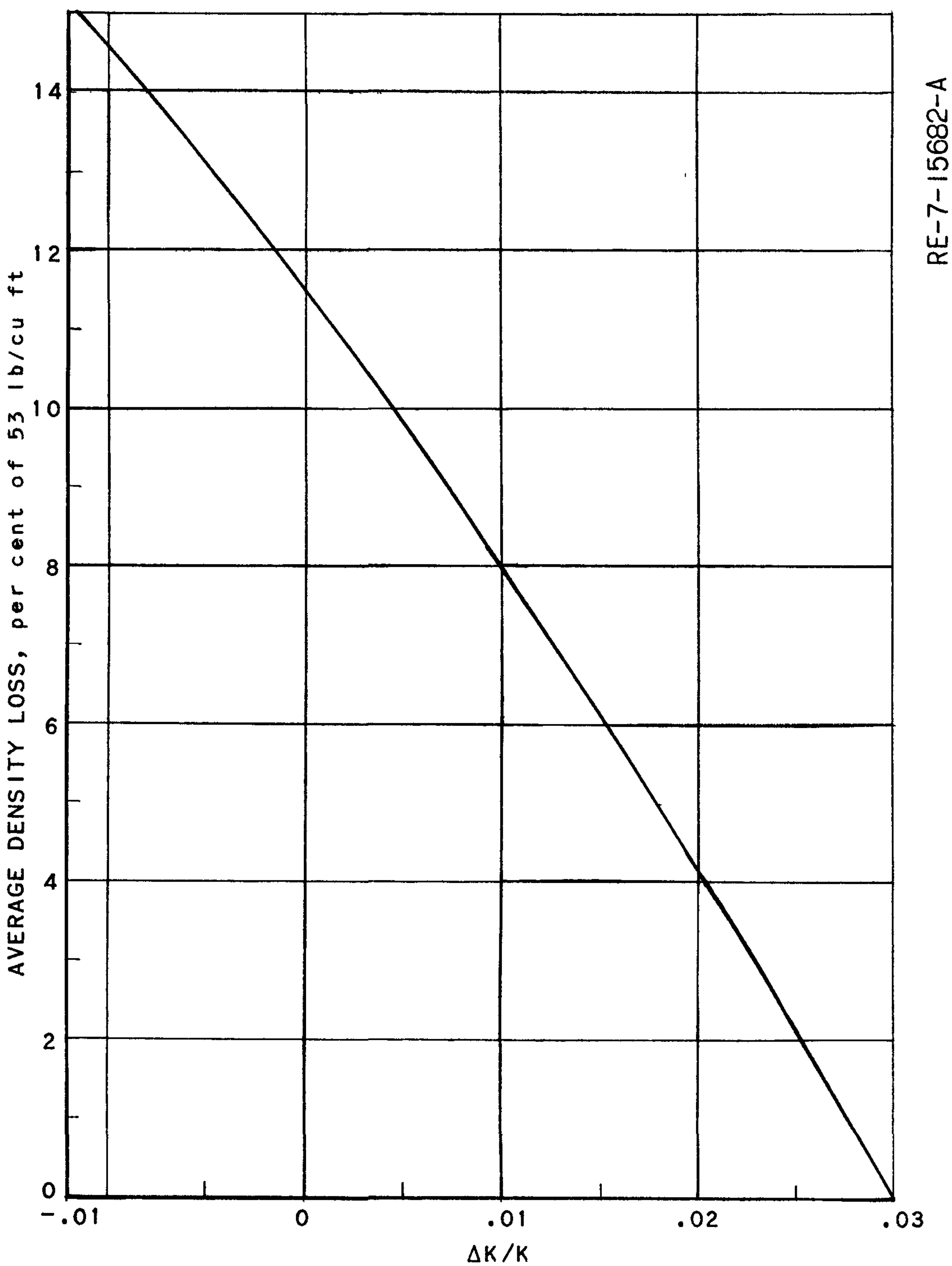


FIG. 13
REACTIVITY CHANGES VS AVERAGE
DENSITY LOSS DUE TO STEAM VOID

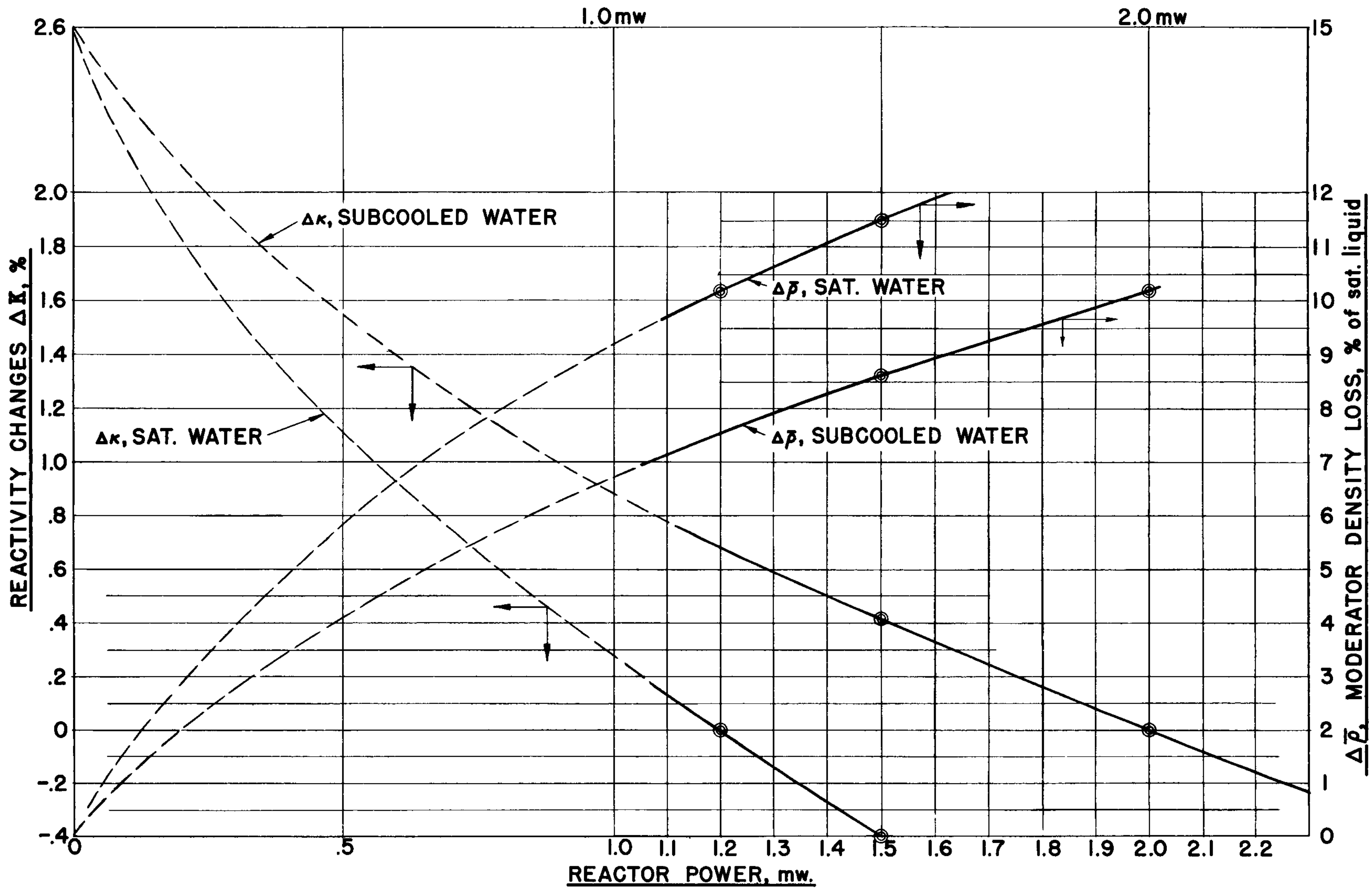


FIG. 14

REACTIVITY CHANGES EFFECTED BY FEEDWATER CONTROL SYSTEM

RE-7-15955-A

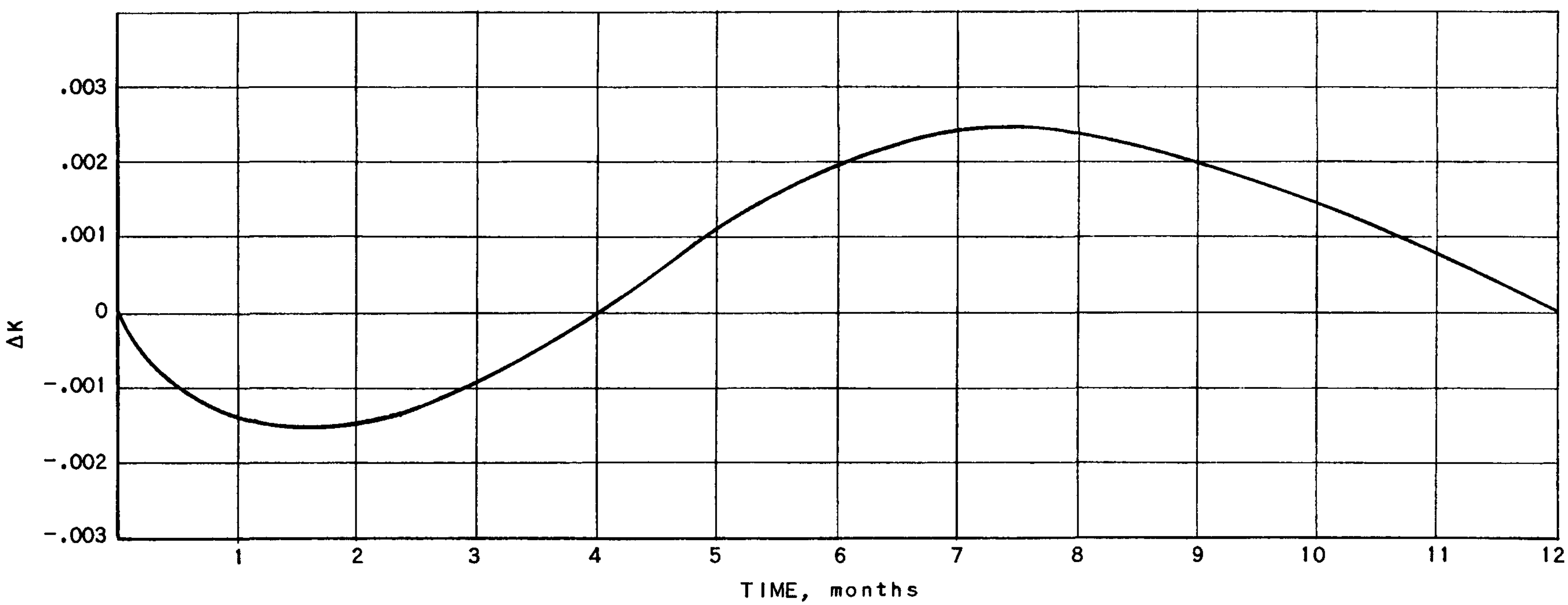


FIG. 15
INHERENT REACTIVITY CHANGES

RE-7-15913-A

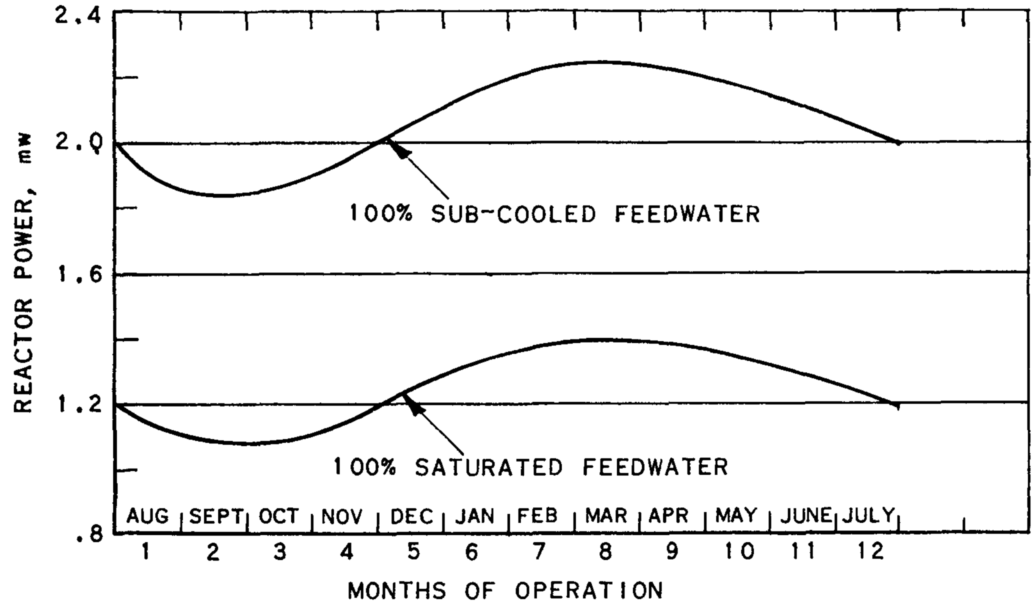


FIG. 16
REACTOR POWER VARIATION

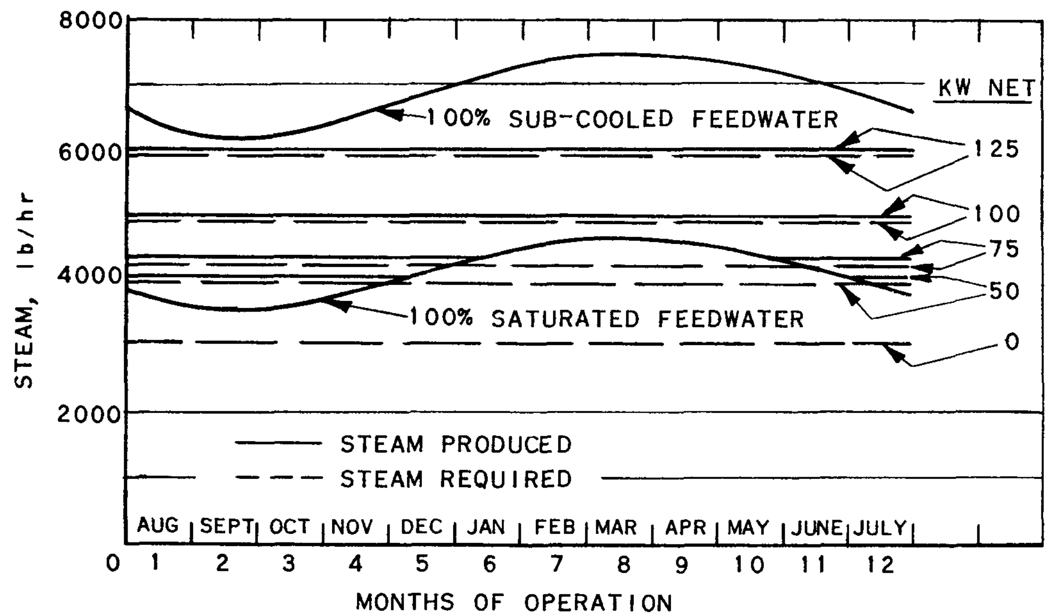


FIG. 17
REACTOR STEAM PRODUCTION

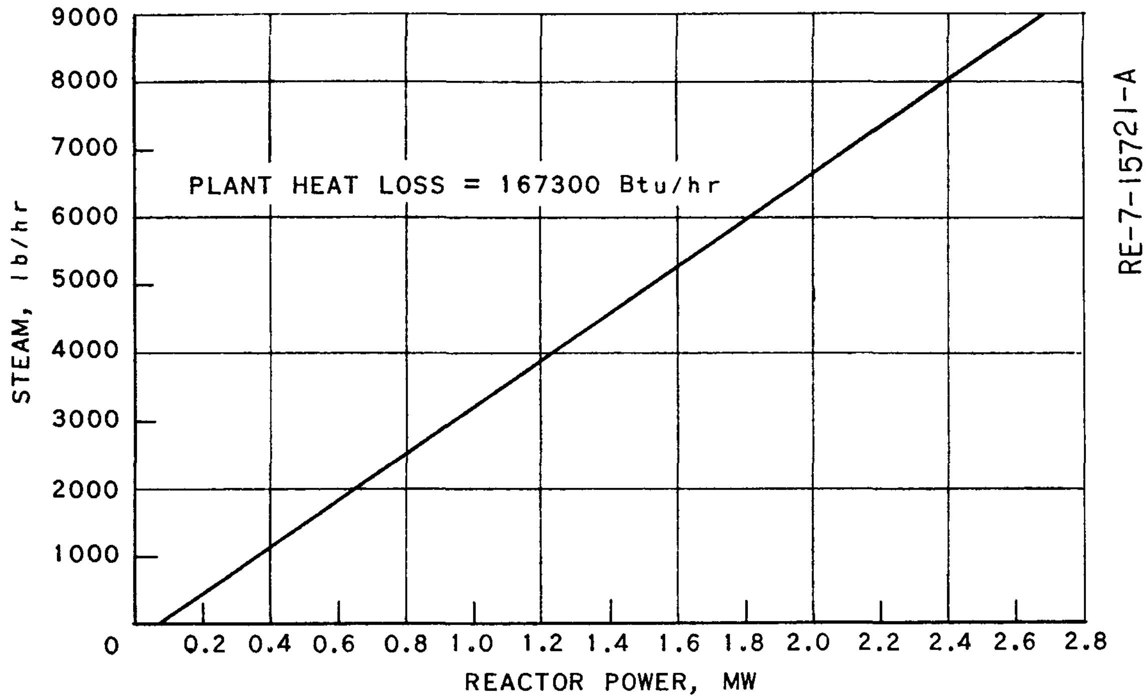


FIG. 18
REACTOR POWER LEVEL VS STEAM PRODUCTION

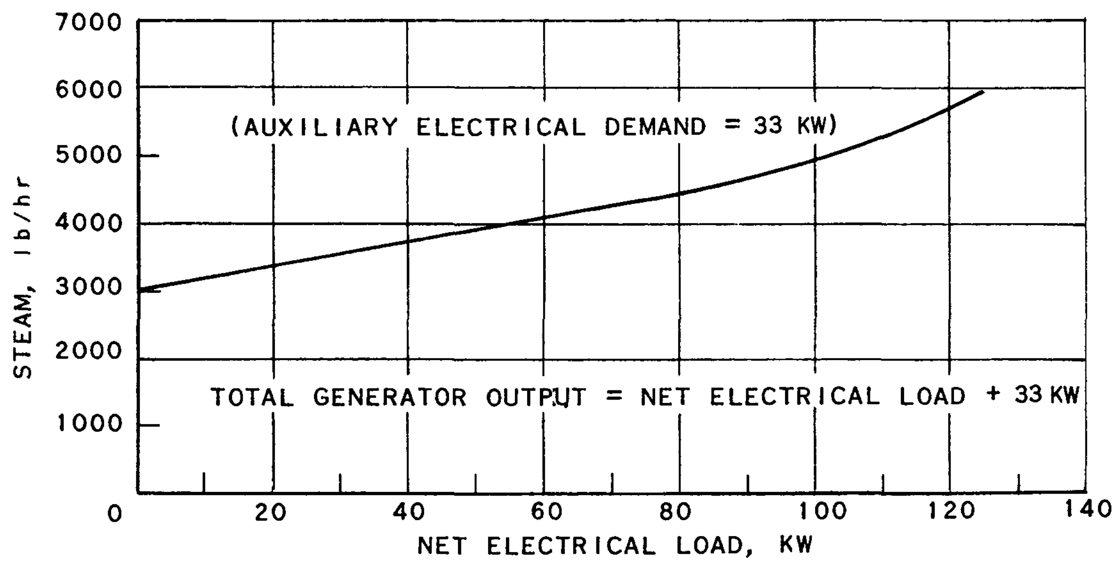
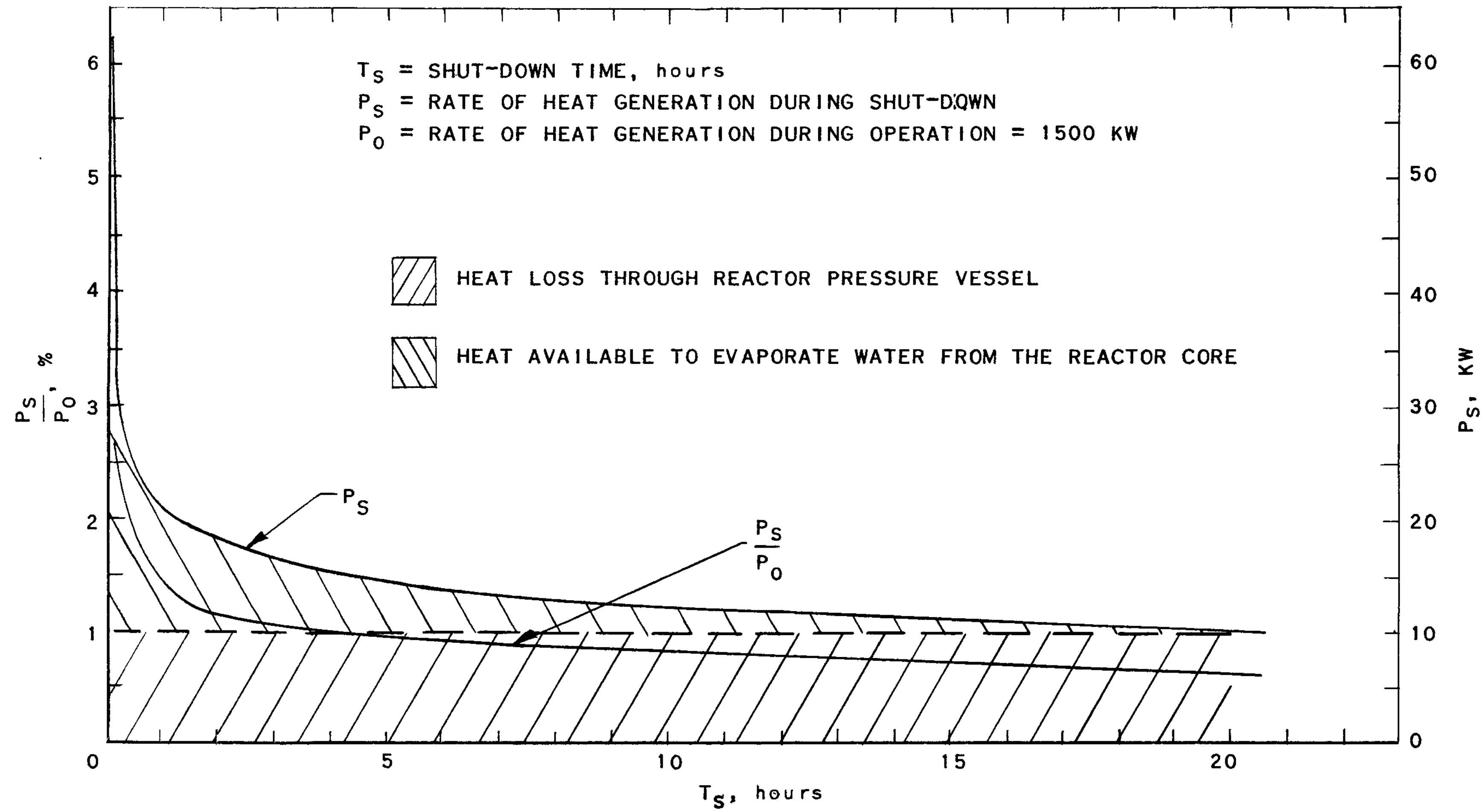


FIG. 19
TURBINE STEAM REQUIREMENTS TO PRODUCE A GENERATOR NET ELECTRICAL OUTPUT



IRRADIATION TIME = 1 YEAR

FIG. 20
SHUT-DOWN HEAT PRODUCTION IN U^{235}

RE-7-15680-A

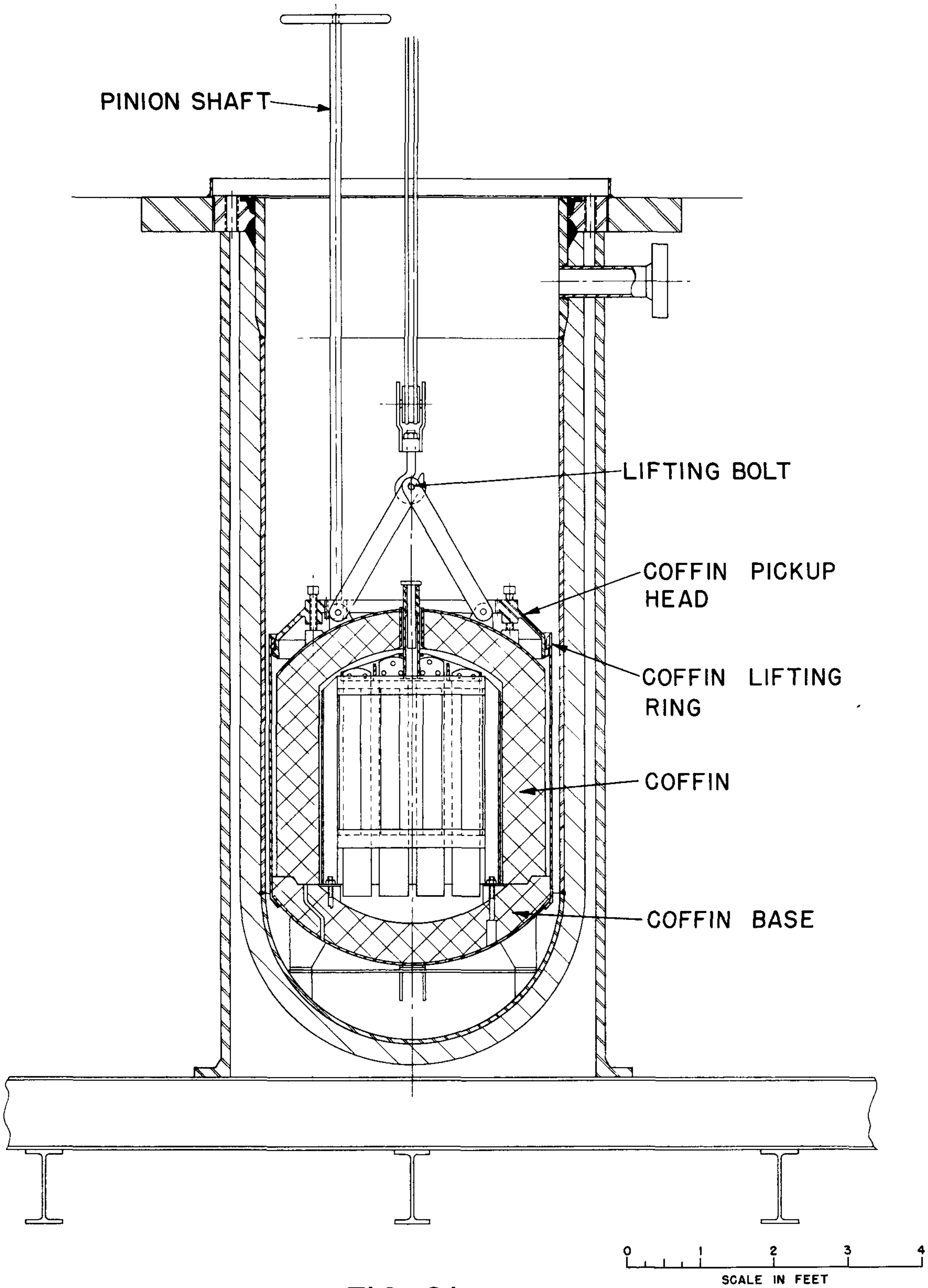


FIG. 21
1.5 MW BOILING REACTOR
EXCHANGE OF FUEL CORE

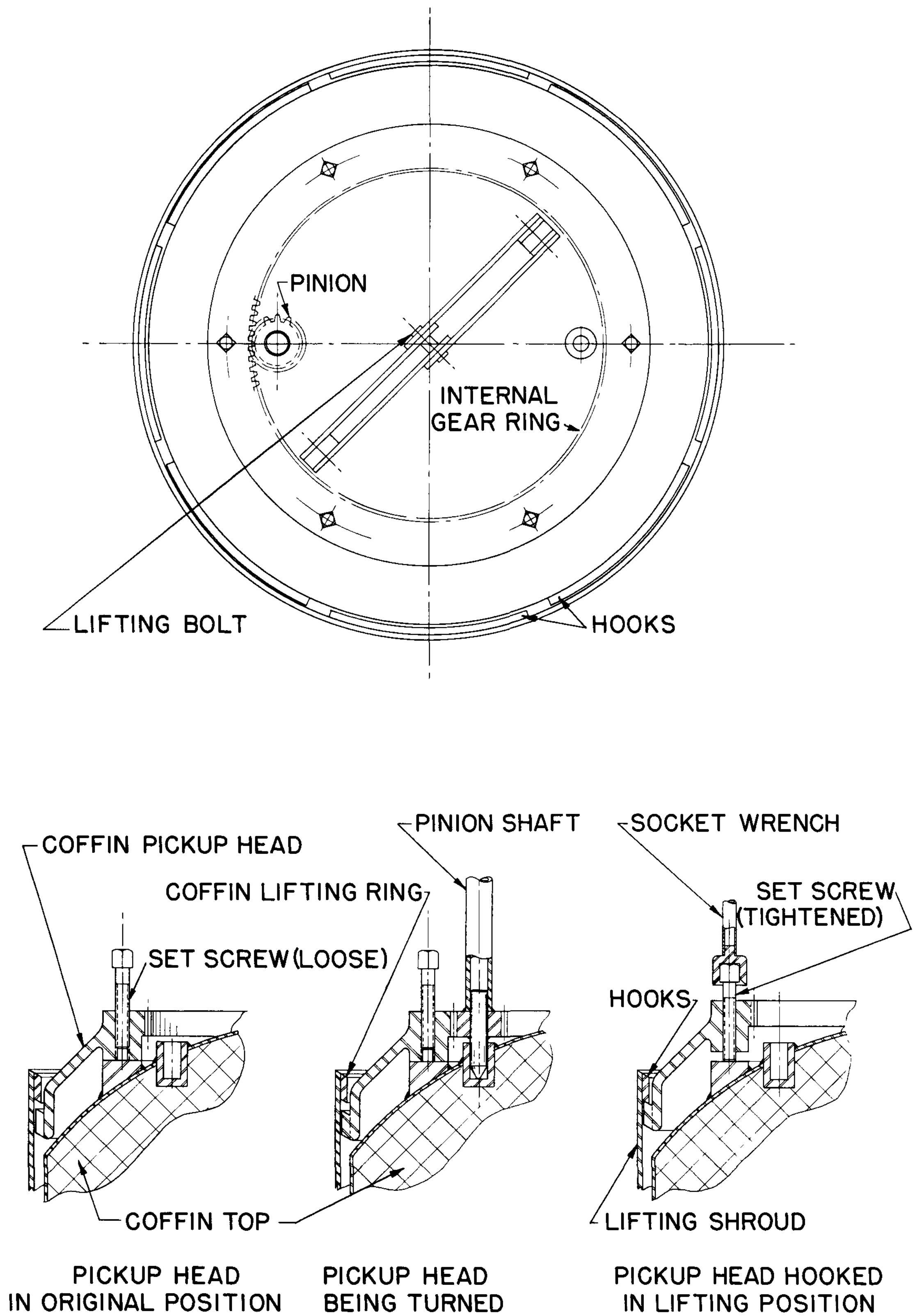


FIG.21-A
DETAIL OF COFFIN PICKUP MECHANISM

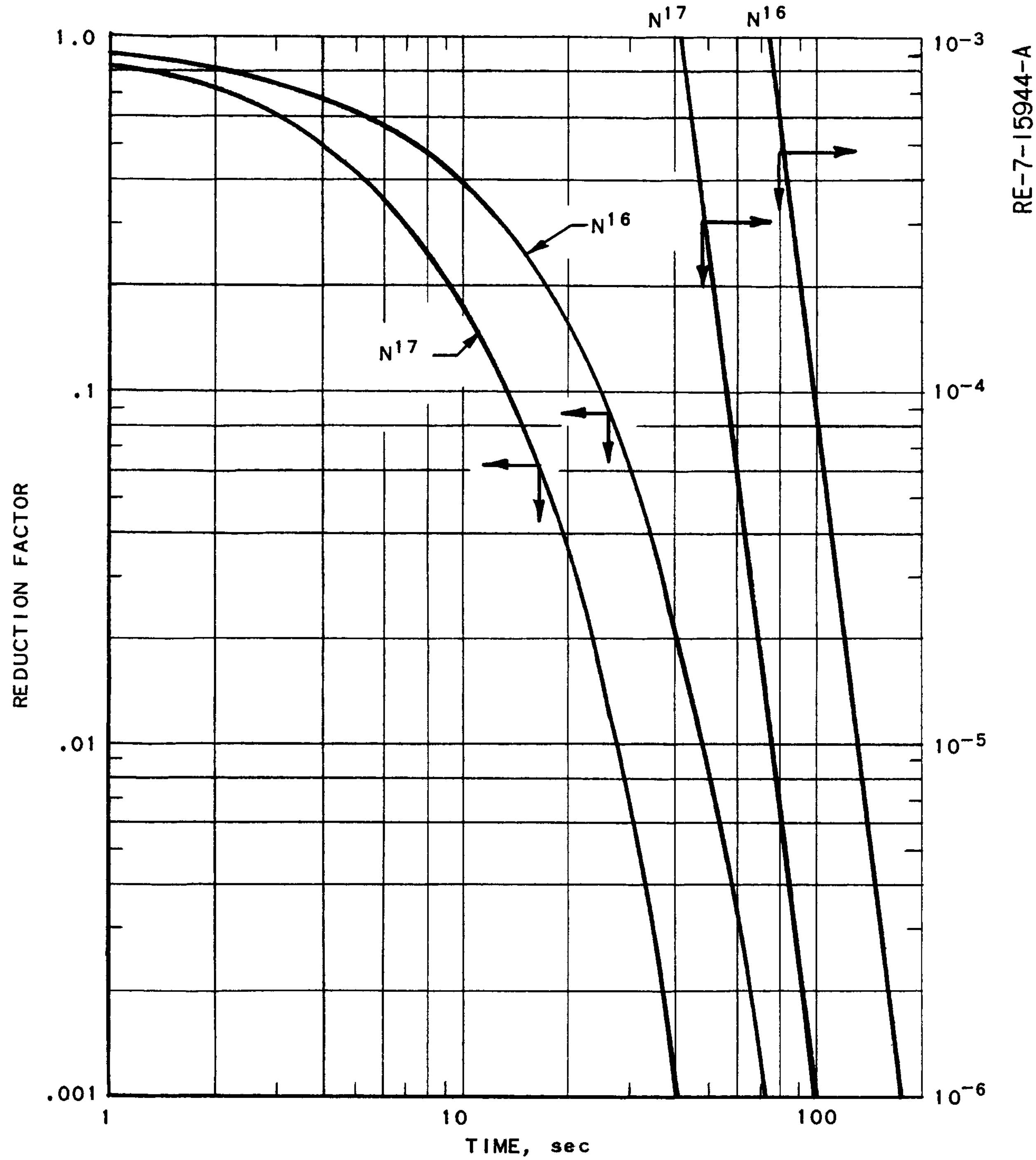


FIG. 22
N^{16} AND N^{17} DECAY INTENSITY
$I = I_0 e^{-\lambda t}$

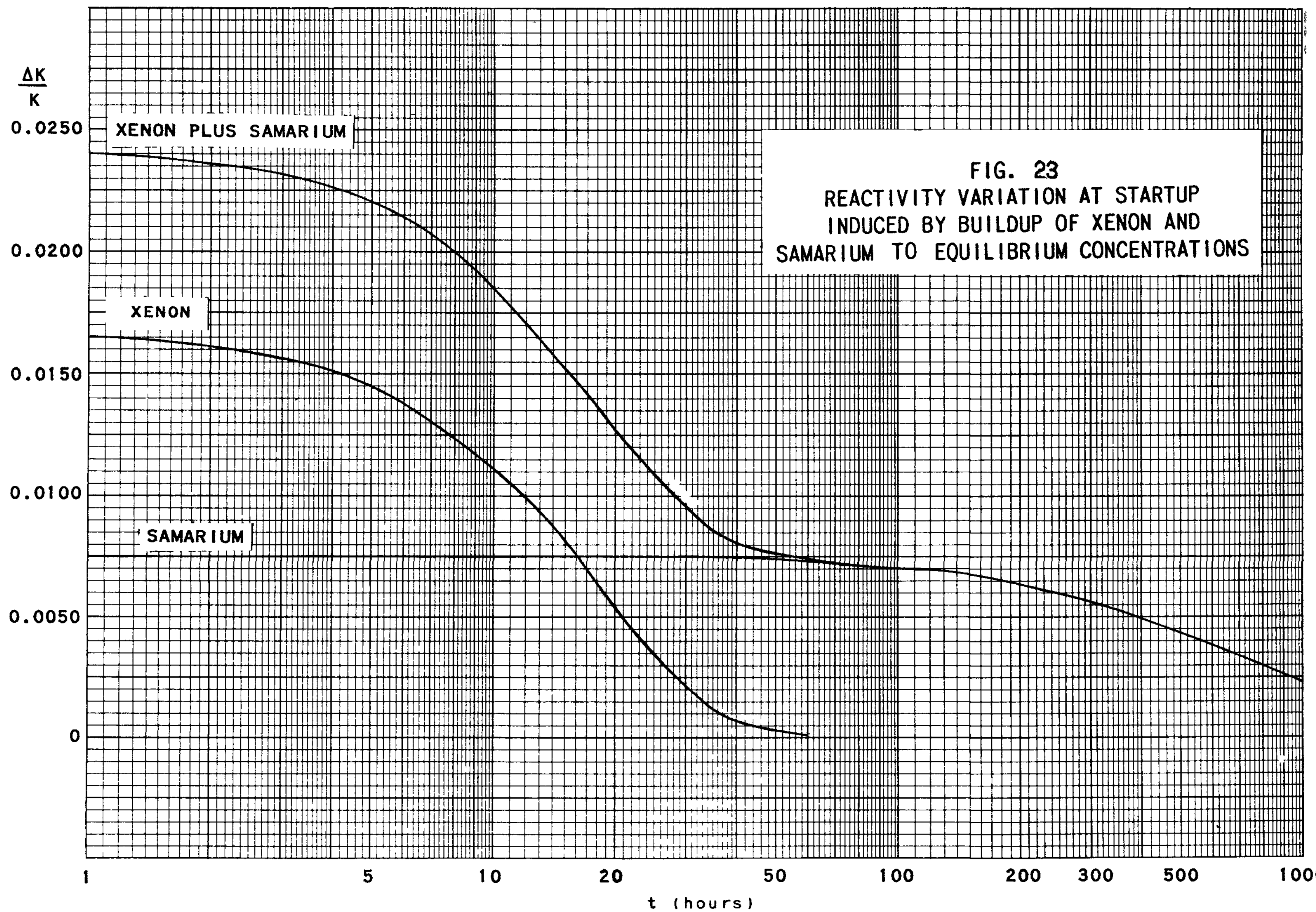


FIG. 23
REACTIVITY VARIATION AT STARTUP
INDUCED BY BUILDUP OF XENON AND
SAMARIUM TO EQUILIBRIUM CONCENTRATIONS

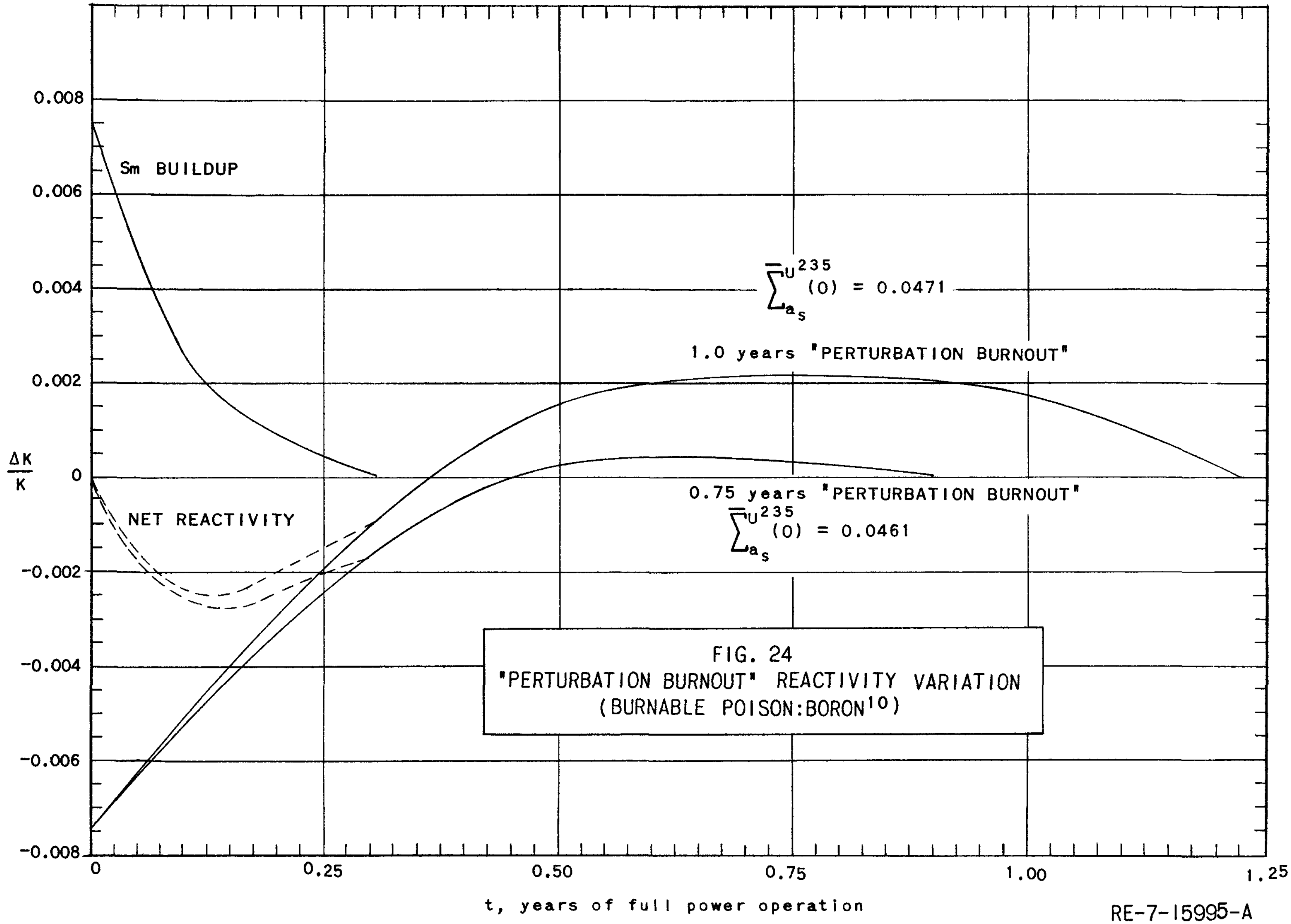


FIG. 24
"PERTURBATION BURNOUT" REACTIVITY VARIATION
(BURNABLE POISON:BORON^{10})

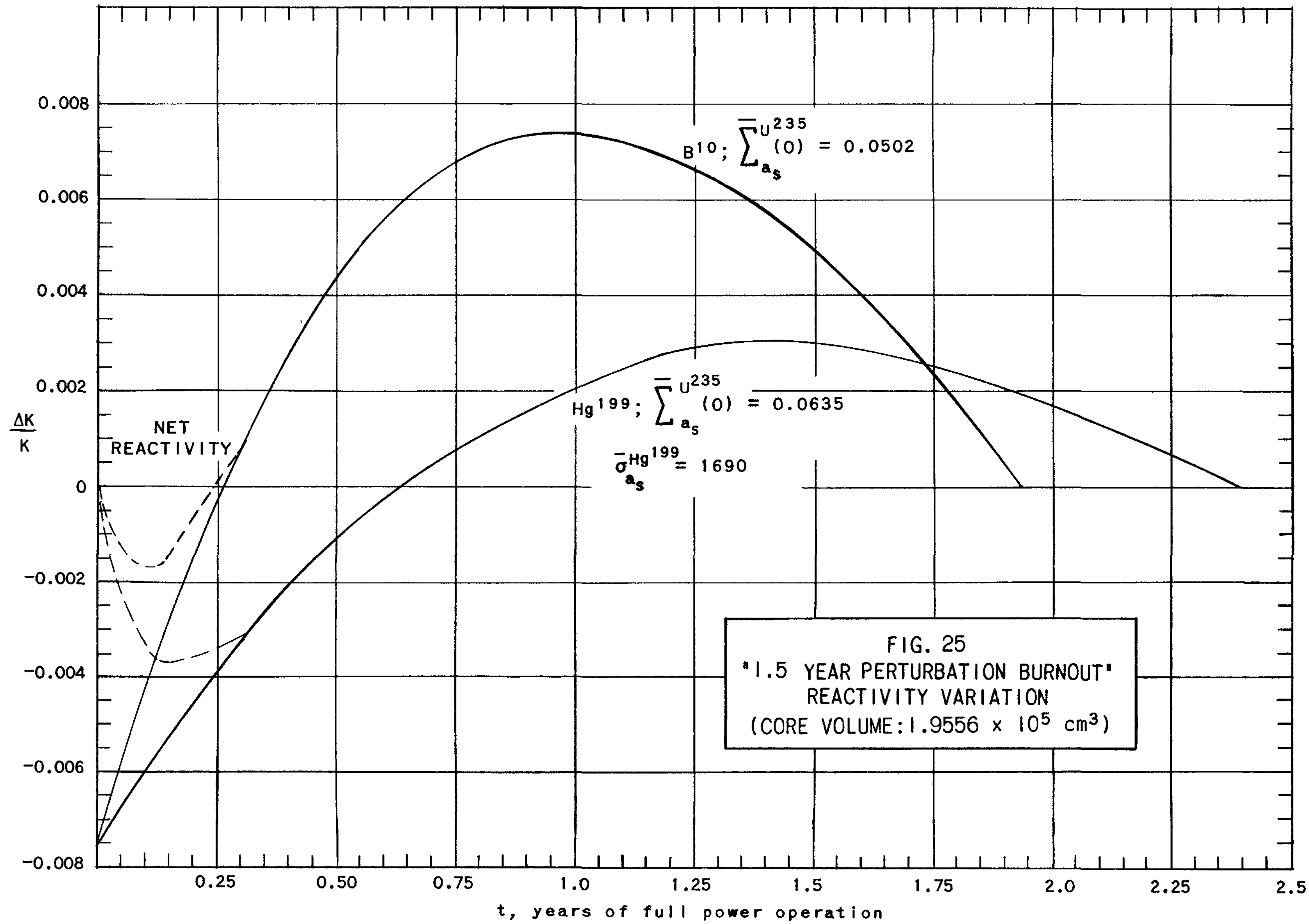


FIG. 25
"1.5 YEAR PERTURBATION BURNOUT"
REACTIVITY VARIATION
(CORE VOLUME: 1.9556×10^5 cm^3)

RE-7-15994-A

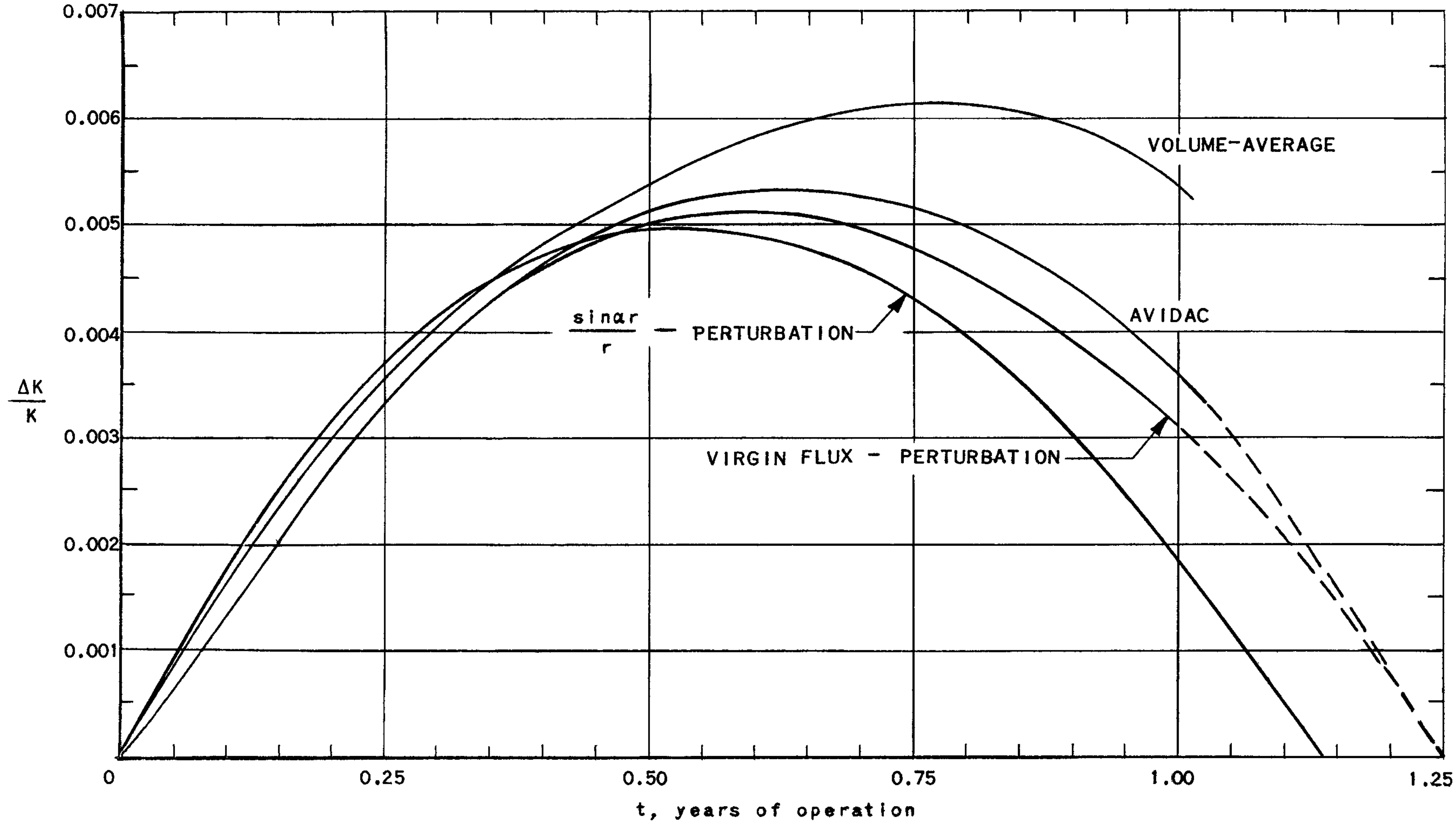


FIG. 26
REACTIVITY VS FUEL DEPLETION
FOR THE "ONE"-YEAR REACTOR

RE-7-15988-A

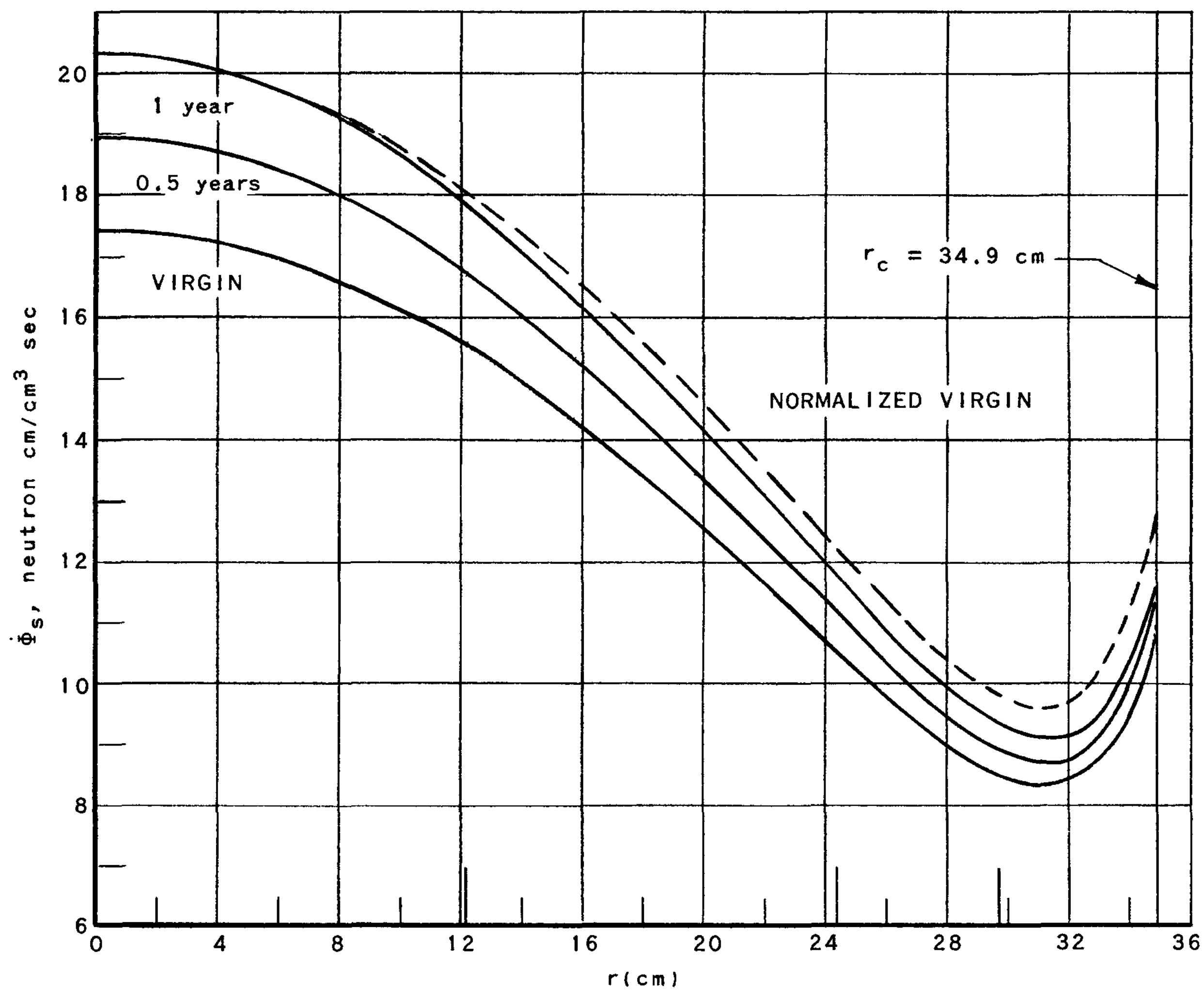


FIG. 27
SPATIAL VARIATION OF THERMAL NEUTRON FLUX - AVIDAC COMPUTATION, "ONE"-YEAR REACTOR (POWER-NORMALIZED FLUX: Φ_S (1.7357, VIRGIN) = 1.1327×10^{13})

RE-7-15993-A

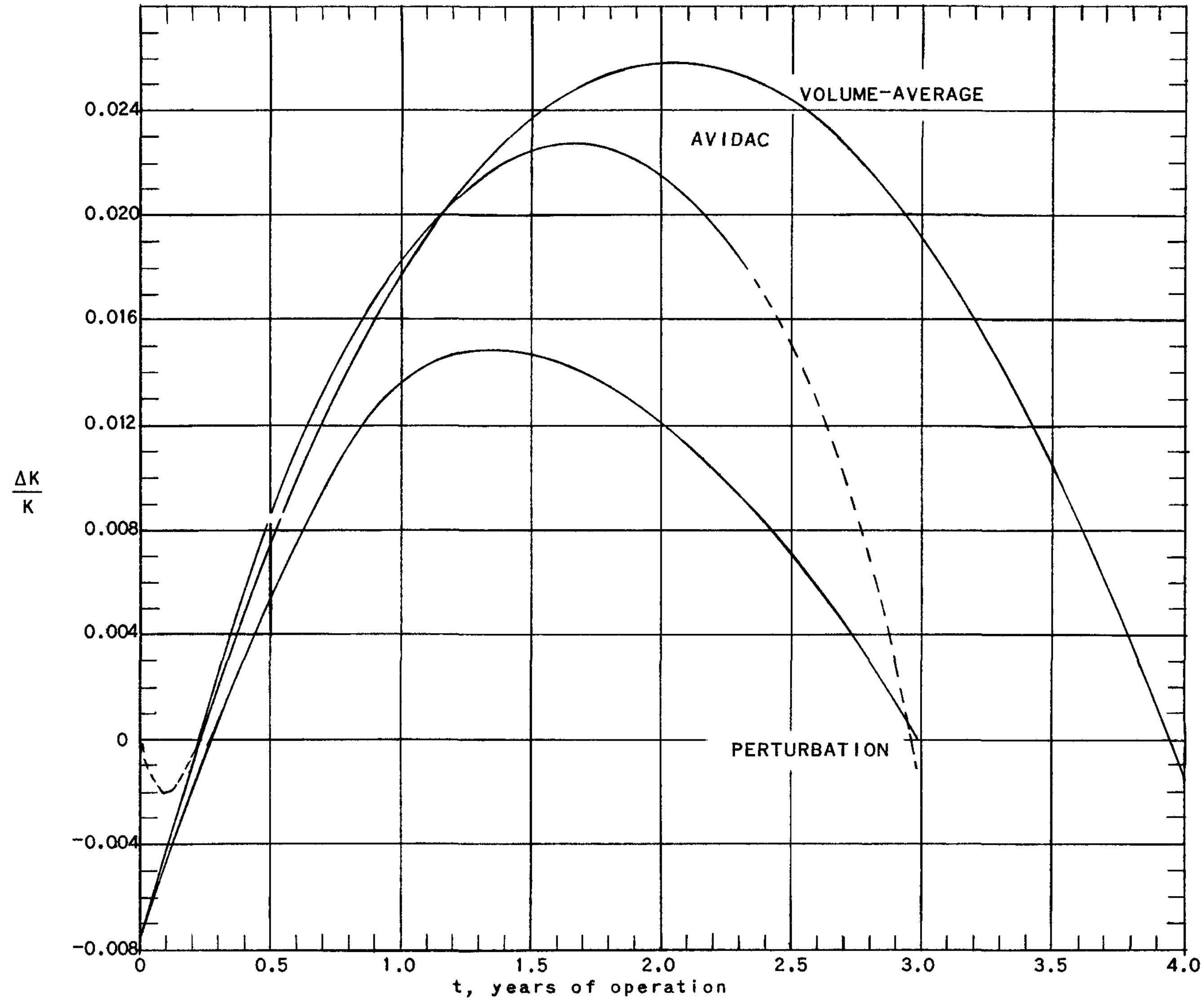


FIG. 28
AVIDAC, VOLUME-AVERAGE FLUX, "TWO-YEAR PERTURBATION-BURNOUT" REACTIVITY VARIATION (BURNABLE POISON:B^{10})

RE-7-16012-A

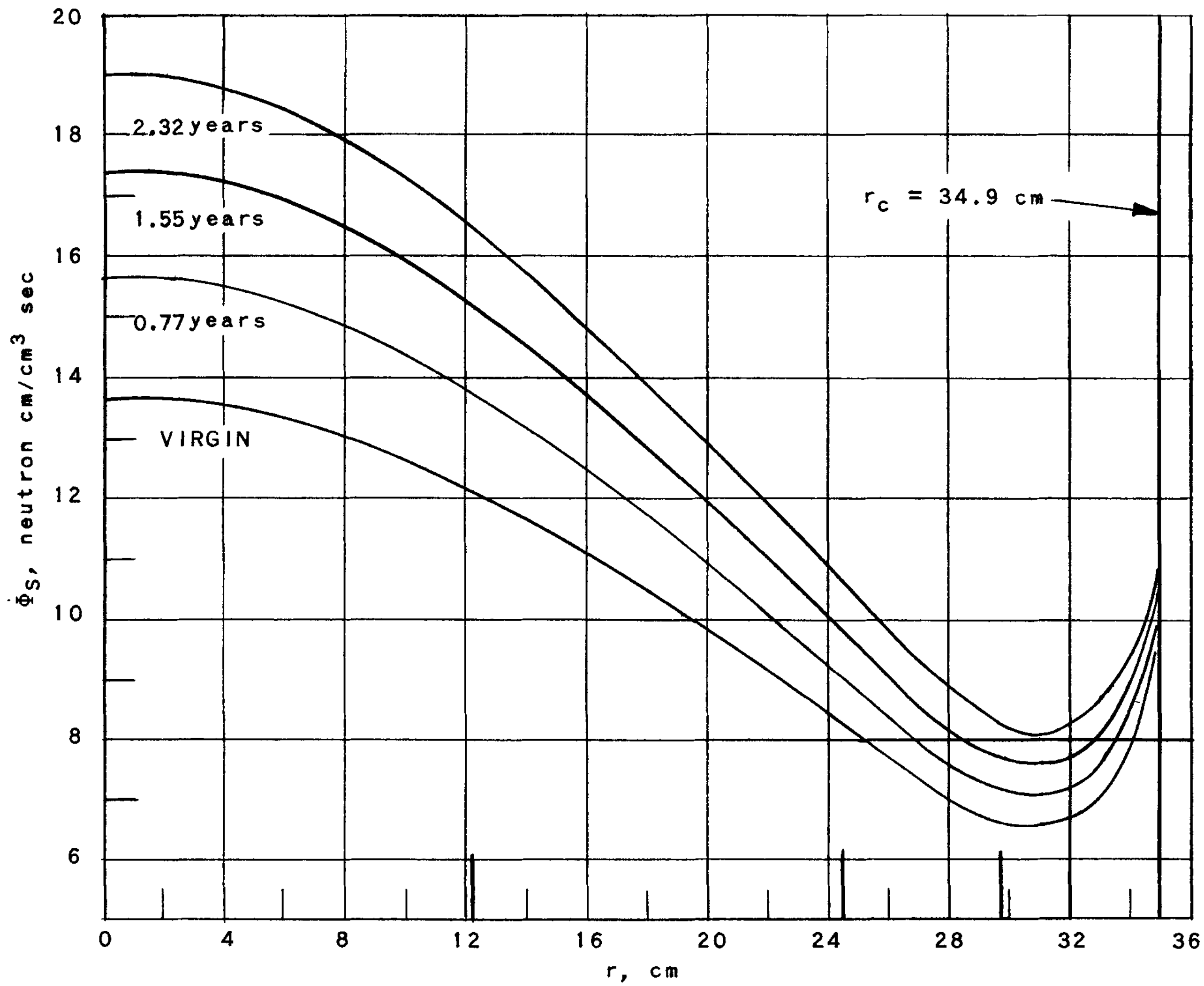


FIG. 29
SPATIAL VARIATION OF THERMAL NEUTRON FLUX - AVIDAC COMPUTATION, THREE-YEAR REACTOR (POWER-NORMALIZED FLUX: $\dot{\Phi}_S$ (1.7357, VIRGIN) = 8.8896×10^{12})

RE-7-15993-A

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ANL-5452
Reactors - Special
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No. 2 of 31 copies. Series A.

E R R A T A

for

ANL-5452

DESIGN STUDY OF A NUCLEAR POWER PLANT FOR 100-KW ELECTRIC AND 400-KW HEAT CAPACITY

by

M. Treshow, A. R. Snider, and D. H. Shaftman

Page 10:

Insert the following between the second and third paragraphs;

The control rods are essentially used only as shim rods for initial start up of the reactor.

During operation the control is accomplished by means of some form of feedwater or reflector control. Probably the simplest of these methods is the control of feedwater preheating as described in detail on pages 30 and 35. This system was only contemplated in connection with a refueling cycle of about one year. The control capacity is of the order of 0.8% $\Delta k/k$.

Page 23:

Correct third sentence in 6th paragraph to read:

"In its top position....." instead of "In its bottom position...."

Page 51:

Correct tabulation of Alternate Cost Estimate to read as follows:

Capital Charges

7% of capital investment	$ 28,256
Operating and Maintenance Cost	20,000

Fuel Charges		
Core	\$13,470	
Fuel Burnup (594 gm at \$16/gm)	9,504	
Fuel Reprocessing (8,006 gm at \$4/gm)	32,024	
Total Fuel Charges		54,998
Total Yearly Cost		\$103,254
Chargeable to Heat		
(1,750,000 kwhr at 25 mills/kwhr)	43,750	
Chargeable to Electric Power	59,504	
Cost of Electric Power (657,000 kwhr)	90.5 mills/kwhr	

Page 57, Line 7:

Change the equation to read:

$$R_V = \frac{1}{1 + \left(\frac{Xr\rho_s}{\rho_w}\right)}$$

Page 83, Equation (18):

Place a minus sign in front of the second integral within the braces.

Page 84, Line 2:

Change $\frac{\sin r}{r}$ to read:

$$\frac{\sin \alpha r}{r}$$