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PROTOTYPE BOILING WATER REACTOR

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PROTOTYPE BOILING WATER REACTOR

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SECTION 1

INTRODUCTION

The work described in this report was undertaken at the request of the Chicago Operations Office of the U. S. Atomic Energy Commission in conjunction with Sargent & Lundy. The objective was to design a nuclear power plant of 50-Mw(e) capacity which could be used to demonstrate advanced performance concepts for boiling water reactors. The time allowed for this study was about six weeks, a short time, particularly when it is realized that the designers were pressed to reach out to practical limits in performance.

Before engineering work began, some design parameters were stipulated. A full-scale plant, assumed to produce 250 Mw of electricity, was to be extrapolable from this prototype plant at $\frac{1}{5}$ scale. Where physics, heat transfer or engineering design compromises were necessary, decisions were made in favor of including that feature which would most likely prove to be optimum for the full-scale plant. In this report, the prototype at 50 Mw(e) and 165 Mw(t) is referred to as the reference design. The 250-Mw(e), 825-Mw(t) plant is referred to as the full-scale plant. Discussion of the latter is included simply to clarify the thinking as it is applied to the reference design. The full-scale plant was not worked out completely.

I. Design Stipulations

Certain quantities were fixed before design began.

1. The net electrical power output was set at 50,000 kw. A plant of this size can demonstrate a reasonable fuel cycle, and dimensional scaling of something less than a factor of two is practical extrapolation. In this size, different modes of boiling water reactor operation are possible, such as natural circulation, forced circulation and dual cycle. This decision was therefore not limiting, and further restrictions were needed to narrow down the particular design selected.

2. The steam pressure was set at 1000 psia, along with the corresponding steam saturation temperature, 544°F. This was a compromise in favor of the larger plants in reducing the pressure vessel design requirements. Higher pressures could be used for 50 Mw(e) if desired. A bulk moderated and cooled reactor was assumed, i.e., no pressure tube-type design was to be considered.

3. The feed-water return temperature was set at 350°F. This value assured a high over-all plant thermal efficiency. The effects of this decision on the physics, heat transfer, and fluid flow are discussed in later sections of this report. It was felt that fixing this quantity provided a worthwhile incentive for efficient design of a saturated steam plant.

4. A power density objective was set at 50 kw/liter of core volume. This not considered a limit for stability. In large reactors of the pressure vessel type, high power density is a secondary goal for economy, because the higher cost of larger pressure vessels is not offset by decreased fuel cycle costs. For the reference system, the high power density is accepted as a useful mockup of the full-scale system, even though its own design optimum probably is at a lower power density. Higher specific power also leads to obtaining fuel performance data at an earlier date.

5. Forced circulation plus external water-steam separation were stipulated on the basis that the full-scale, single reactor plant would require forced pumping. Separation of water from steam internal to the primary vessel was not possible without development work, especially for the larger plant. Furthermore, extrapolation of results, to make them apply to the full-scale plant, would not be possible, even if internal separation were accomplished in this reference design plant.

6. Only steam from primary H₂O coolant would be sent to the turbine. This direct cycle eliminated the use of heat exchangers in the forced pumping cycle. Improvements or economies to be obtained by the addition of primary to secondary heat exchangers were not investigated. This restriction simply makes this plant a demonstration of performance, control, and operation of the wholly direct-cycle plant.

7. The fuel would be UO₂ with Zircaloy clad. This eliminated any consideration of stainless steel or the use of unclad or coated oxide. This restriction favored good fuel economy and adequate control. In addition, fuel element fabrication development would not be required, except to achieve cost reduction.

8. Uniform enrichment in U²³⁵ and an average exposure of 15,000 Mwd/ton* were stipulated in order to establish the control and fuel cycle objectives.

*Throughout this report, the metric tonne is used.

9. The length to diameter ratio for the core was limited to between 1.1 and 1.3. This restriction favored neutron economy and eliminated the possibility that the length of the elements in this plant could be made equal to those of the large plant. Full-scale fuel assemblies and control rod demonstrations would solve fabrication problems and obtain operating experience, but some compromise in favor of good geometry for the reference design is indeed required to prevent unduly large fuel-cycle costs.

II. Design Discussion

The time limitations precluded a detailed study of comparative economic features in the fuel cycle. A beneficial long-range objective for power reactors is to get the greatest number of fissions per installed U^{235} atom before the fuel must be removed from the reactor and processed. This criterion is relatively insensitive to changes in the fuel pricing structure and, within reasonable limits, favors high conversion fuel cycles. A rough check showed that under the present pricing structure for U^{235} enrichment and for plutonium, it is not possible to make a clean-cut engineering decision based on cost comparisons alone. As discussed further in Section 5, Physics, the fuel conservation criterion leads directly to the selection of a design with closely packed fuel in the core and a minimum moderator volume. In the range of cores examined, heat transfer, fluid flow, and manufacturing problems, rather than criticality and reactivity lifetime, limited the closeness of the fuel spacing.

A triangular spacing was adopted for the fuel elements in the fuel assemblies. Triangular spacing can be used in square assemblies, but the more natural assembly shape for this cell spacing is hexagonal.

Control requirements were investigated, and it was apparent that even the most carefully designed blade system would occupy too large a fraction of reactor volume and force very high power densities in the fuel-bearing part of the core. This led to the use of control rods composed of fuel with water-filled hexagonal box-type followers of neutron-absorber material.

After these ideas were accepted, it was evident that moving the rods in from the top to increase reactivity would also prove best from the standpoint of improving the shape of the axial power distribution. This was incorporated in the design because a commercial rod drive is on hand to accomplish this motion and because design criteria should fulfill, rather than limit, core performance. The engineering features of design are discussed more fully in Sections 2 and 3 of this report.

One factor was left to be considered. If the core could be turned inside out to prolong reactivity life and provide more uniform burnup of fuel, further economies were possible, but the quantitative estimate of

this improvement did not exist. A choice had to be made between providing the inside-out fuel inversion and simply turning the element end for end. Both mechanical designs were carried out and are discussed in Section 2, Core Design. The design of the split element seems advantageous from an engineering standpoint. A smooth-sided cartridge or basket-type container to hold the two halves of the split element evolved, rather than the idea of a loading sleeve proposed for the turnover element. It is very likely that this would work better in practice because the guide for fuel insertion remains in place on the element. The turnover element could have been similarly designed.

After the split element was accepted for the reference design, calculations showed an estimated saving of 20% to 25% in fuel cycle cost.

III. Fuel Cycle

In Section 6 the cost computations for the fuel cycle in both the full-scale and the reference-size plants are discussed. As might be expected, the cost turns out to be nearly 1 mill/kw hr less for the full-scale plant than for the reference design. The total fuel cycle cost for the full-scale plant is 3.60 mills/kw hr. The reference plant should have a fuel cycle cost of about 4.54 mills/kw hr; if this value is realized, we feel that the full-scale plant will surely reach 3.60 mills/kw hr.

SECTION 2

CORE DESIGN

V. M. Kolba and C. F. Bullinger

I. Reference Core

In the reference core design the relative values with respect to volume were maintained at $\text{UO}_2 = 1$, metal (Zr-2) = 0.312, and $\text{H}_2\text{O} = 1.52$. An over-all moderator to solid material ratio was obtained at nearly 1.16. As discussed in the physics of Section 5, this ratio was as close as practical to the lowest amount of water which could be used, all engineering factors being considered.

The length-to-diameter ratio of about 1.3 to 1 was selected to favor the longer core element and tend to make extrapolation to the full-scale plant fuel length of about 9 to 10 ft more feasible.

A. Core Data

The final data covering physical dimensions and performance are given in Table 2-1.

Table 2-1

CORE DESIGN AND PERFORMANCE DATAA. Fuel Description

1. Fuel Material	UO_2
2. Pellet Diameter, in.	0.410
3. Gap Material	He (2 atmos pressure)
4. Gap Thickness, nominal (cold), in.	0.0015
5. Clad Material	Zircaloy-2
6. Clad Thickness, in.	0.025
7. Fuel Rod OD, in.	0.463
8. Fuel Section Length, in.	40.5
9. Axial Gap, in.	1.75
10. Active Zone Length, in.	81
11. Triangular Lattice Pitch, in.	0.6

Table 2-1 (Cont'd.)

B. Core Description

1. Active Zone Length, in.	82.75
2. Active Diameter, ft	5.0
3. Fuel Assembly Shape	Hexagon
4. Lattice Pitch, in.	5.125
5. Rods per Assembly	61
6. Assemblies per Core (including control rods)	121
7. Rods per Core	7381
8. Number of Control Rods	19
9. Power Density, kw/l	47

C. Core Materials

1. Weight of UO_2 , lb	29,459
2. Volume Zircaloy-2/Volume Inside Clad	0.312
3. Volume H_2O + Steam/Volume Inside Clad	1.52
4. Average Density UO_2 /Unit Volume Inside Clad, g/cc	10.0

D. Physics Data (Section 6. Symbols)

1. Fuel Enrichment (Feed), atom %	2.65
2. Fuel Enrichment (Discharge), atom %	1.60
3. Pu^{239} and Pu^{241} Content (Discharge), atom %	0.64
4. ϵ	1.05
5. P	0.71
6. τ , cm^2	90
7. B^2 , cm^{-2}	0.00104
8. k_{eff} (Initial, Hot, Voided Xe + Sm Poisoned)	1.046
9. δk (Xe, Sm), %	3.7
10. δk (Initial to Equilibrium), %	2.7
11. δk (Void), %	8.9
12. δk (Between Discharge Points), %	4.8
13. δk (Required, Hot), %	18.5
14. δk in Rods (Hot), %	38
15. Burnup at Discharge, Mwd/ton U	12,000
16. Total Maximum/Average Flux	4.90

Table 2-1 (Cont'd.)

E. Hydraulic and Thermal Data

1. Total Reactor Power (Design), Mw	165
2. Average Power/Assembly (Design), Mw	1.36
3. Total Heat Transfer Area, ft ²	6,030
4. Total Core Flow Area, ft ²	10.75
5. Average Heat Flux, Btu/(hr)(ft ²)	93,500
6. Maximum Heat Flux, Btu/(hr)(ft ²)	369,000
7. Fraction Nonboiling Length	0.25
8. Void Fraction in Boiling Zone	0.267
9. Void Fraction (Uniform Flux)*	
Total	0.20
Exit	0.445
10. Steam Exit Quality	0.0575
11. Recirculation Ratio	16.4:1
12. Slip Ratio	1.57
13. Total Flow Rate (Design), gpm	30,000
14. Inlet Subcooling, °F	9.5 (12.5 Btu/lb)
15. Steam Rate (Design), lb/hr	644,800
16. Average Core Inlet Velocity, ft/sec	6.2
17. Core Pressure Drop (Water), ft	11½
18. Average Fuel Centerline Temperature, °F	1330
19. Maximum Fuel Centerline Temperature, °F	4790

B. Core Structure

The reactor core is a 60-in. equivalent OD right cylinder, 83 in. high. The core is composed of 102 stationary hexagonal fuel assemblies in a support structure. There are 19 full-length movable fuel assemblies attached to absorber rods.

The core structure is composed of three main items:

1. Lower Support Grid

The lower core support is a 4-in. thick stainless steel plate reinforced by the welded-in control rod and fuel holder guide tubes. This plate forms a circular horizontal platform for the support and location of the fuel elements.

*See Table 4-1 for nonuniform flux values.

2. Upper Support Grid

The upper grid is made of rectangular stainless steel bars welded into the proper configuration. The control rod guide shrouds are welded to the upper side of this grid. This assembly is used both as a lateral fuel element support and as a guide structure for the fueled follower control rods. It is removable for fueling of the core. The upper grid is supported by the thermal shield, allowing for relative expansion of the core components.

3. Thermal Neutron Shield

The thermal shield inside the pressure vessel is composed of four concentric cylinders of steel and water laminations. The steel cylinder walls are, from inner to outer, 1 in., $1\frac{1}{4}$ in., $1\frac{1}{2}$ in., and $2\frac{1}{4}$ in. thick, respectively, separated by $\frac{3}{4}$ -in. water-filled gaps. Reflector space is provided by approximately 12-in. clearance between the outer row of fuel assemblies and the innermost cylinder. The cylinders are supported from the core support ring welded to the vessel wall.

C. Fuel Construction

The fuel element design for the reference reactor is shown in Fig. 2-1 and Fig. 2-2.

1. Cartridge Construction

The fuel assemblies have a hexagonal cross section and are of the cartridge type with two separate sections of active fuel in each holder. This feature is necessary for the fuel management incorporated in this design. Splitting the core at the axial center allows one to obtain the maximum reactivity gain when the fuel is moved or replaced.

The cartridge-type unit consists of a holder and two fuel element assemblies. The holder consists of a lower end fitting, angle braces, spacer bands and an upper handling and grid locating fitting. Three spacer bands are used to provide rigidity for the system. They are evenly spaced along the length of the holder. The lower end fitting is made from a 304 stainless steel casting which is machined inside for location of a removable orifice. The lower end fitting is sized for insertion into the lower support plate.

The upper end fitting has provision for lifting and positioning the fuel holder assembly. The slotted portion allows positioning of the upper spacer grid containing control rod guide shrouds. Lateral spacing of the fuel is also aided by this slotted segment and the spacer grid.

2. Fuel Assemblies

Individual fuel rods are closed at each end by a welded end plug. A gas space, $\frac{1}{2}$ in. long, was allowed at the top end of each individual fuel rod. This volume was based upon a 5% fission gas release over the lifetime of the rod. Each end plug has a shaft to be used in the positioning and assembly of the fuel rods in the fuel support and spacer grid. The fuel-support and spacer grids are machined and drilled to the proper dimensions and hole pattern. All 61 fuel rods are welded to one of the support and spacer grid assemblies; however, on the opposite end, only six of the fuel rods are welded to the support and spacer grid. This grid design offers a compromise between mechanical design and flow requirements.

3. Design Discussion

Two of the fuel assemblies are positioned in the holder prior to loading in the reactor core. The cartridge system has several features. The holder acts as a guide for fuel handling (insertion and removal of fuel from the core) and supports two fuel assemblies. The holder is a fairly stable structure and is a reusable item for insertion and removal of future fuel assemblies.

D. Control Rod Design

Nineteen neutron-absorbing control rods, which have a hexagonal configuration comparable to that of the fuel element, are used. The control rods are located on $10\frac{1}{4}$ -in. centers, as shown in Fig. 3-4. The absorber section is made up of 2% boron SS plate, $\frac{1}{4}$ in. thick. The control rod has nuclear advantages as are described in Section 5, Physics. Attached to the upper portion of the control rod is the fuel assembly shown in Fig. 2-3. This fuel assembly is comparable to the stationary assemblies already described except for the lifting and positioning end fittings, and the corner guide brackets.

E. Fuel Management

A representative fuel management system is shown in Fig. 2-4. The atypical approach will be used to start up the reactor and the typical scheme will be used during the regular equilibrium cycle fueling. The fueling plan is based on having equal areas, but the number of fuel elements is an odd value. The number of fuel elements handled per zone will vary from a minimum of 25 to a maximum of 27. The 19 fueled followers on the control rods will be handled independently from the above fuel-handling scheme. They are to be removed after burnup of the fueled portion has reached the desired peak.

With fueled follower control rods, reactivity is increased by both the removal of the neutron absorber and insertion of fuel. In the hollow absorber section, thermalization of fast neutrons that penetrate the absorber sheet takes place. Thus the effective absorber area is nearly doubled.

F. Design Method

The stipulated design parameters were combined with certain mechanical design assumptions to establish an insight into what type of problems would be involved. Ideal parameters were then relaxed to arrive at a design compromise.

Nomenclature

A = area, ft^2 or in.^2
 a = side of box, in.
 e = 2.718
 K = Thermal conductivity, $\text{Btu/hr/ft}^2/^{\circ}\text{F/ft}$
 L = Length of fuel section, ft
 M = Max./Avg. value
 n = number of rods; number of cells
 P = Pressure, psia
 Q = Heat flux per core or single rod, Btu/hr
 r = radius of inscribed circle of hexagon, in.
 t = Temperature, $^{\circ}\text{F}$
 ΔT = Temperature drop, $^{\circ}\text{F}$
 x = thickness, ft or in.

The steps followed in the first approximation were:

1. Set reactor power - 165 Mw(t)
2. Set reactor power density - 50 kw/l
3. Calculate core volume - 116.5 ft^3
4. Set water to metal ratio at unity
5. Set length to diameter ratio at 1.3:1
6. Set OD of core at 4.73 ft
7. Calculate resultant cross sectional area of core: 17.6 ft^2
8. Obtain height of core: 6.62 ft
9. Cross section of metal is 8.8 ft^2
10. Obtain max./avg. values
11. Set max. fuel centerline temp. at 4600 $^{\circ}\text{F}$
12. Determine cell pattern spacing
13. Establish number of fuel rods: $n = MQ/4\pi L\Delta T K = 6770$
14. Calculate fuel rod size: $0.187 \text{ in.}^2 = 0.488 \text{ in. OD}$
15. Establish number of cells/core

16. Calculate cell size:

Square Cell

$$A = a^2$$

$$nA = 17.6$$

Hexagonal Cell

$$A = 6 r^2 \tan 30^\circ = 3.461 r^2$$

$$nA = 17.6$$

17. Having determined cell size, re-apportion the handling clearances and set fuel rod spacing and pitch.
18. Calculation of fuel centerline temperature:

- a. Ambient H₂O temperature is given at 544°F.
- b. Calculate film temperature drop.
- c. Calculate scale temperature drop.
- d. Calculate clad temperature drop.
- e. Calculate bond temperature drop.
- f. Calculate fuel pellet temperature drop.
- g. Calculate over-all temperature drop and obtain centerline temperature using the following relations:

$$T_{\text{centerline}} = T_{\text{sat}} + \Delta T_{\text{film}} + \Delta T_{\text{scale}} + \Delta T_{\text{clad}} + \Delta T_{\text{bond}} + \Delta T_{\text{pellet}}$$

$$= T_{\text{sat}} + \frac{1.9 (Q/A)^{1/4}}{e P/900} + \frac{Qx}{K_s A} + \frac{Qx}{K_c A} + \frac{Qx}{K_b A} + \frac{Q}{4\pi K_p L}$$

The fuel centerline temperature can be calculated for the average as well as that at the local hot spot area if the appropriate maximum/average value is used as a factor for Q values.

G. Fuel-handling Method

Fuel-handling details have not been completely resolved. A submerged in-water handling technique is probably most convenient for the length assemblies employed in this prototype. The concept incorporates a portable open side tank which can be bolted to the top flange of the pressure vessel and to the side of the fuel storage pit. Fuel handling takes place with the vessel head and upper grid support removed and the handling canal filled with water to permit transfer of spent fuel to the storage pit. The fuel will be removed with a semirigid fuel-handling tool attached to the overhead crane. The fuel-handling tool will be positioned within the end fitting of the fuel element cartridge and locked in place by two pins extended from the tool through the holes in the fuel element end fittings. Removal of the fuel assemblies from the holder and relocation of the fuel within the holder will be done in the fuel storage pit. After rearrangement of the fuel assemblies in the holder the unit is reloaded into the reactor core.

II. Alternate Designs

A. General

The design of the reference core evolved after due consideration was given to several possibilities. Presentation of some alternate designs is included to preserve the ideas for use in other design proposals or for offering possible alternates to the reference design details.

B. Square Lattice Concept

1. Element Description

The first arrangement considered was a conventional square fuel assembly with cruciform fueled follower control rods located on 12.75-in. centers. The rods were to be driven up out of the core during operation and allowed to drop back in place by gravity for shutdown.

The fuel assembly is shown in Fig. 2-5. This fuel assembly was composed of two fuel subassemblies which were held together by removable straps between the lifting and locating end fittings. Each of the subassemblies contained 100 fuel rods, 0.500 in. OD x 42 in. long, spaced and fastened in end support grids. Location of the rod in the support and spacer grid may be accomplished by several methods. An initial concept was to thread the end piece of the fuel element into the lower support grid. The top end units would be secured by nuts with the threaded portion of the shaft extending beyond the nuts and being flared out to secure the nuts. This design is conventional and requires no research and development. Two of the subassemblies together with the handling and locating end fittings form a fuel element $5\frac{3}{4}$ in. x $5\frac{3}{4}$ in. square by 105 in. long.

The element subassemblies have identical end fittings, which permit disassembly in the fuel storage pit and subsequent interchange or axial position changing in the core. This procedure allows maximum burnup of fuel. This concept was discarded for the following reasons:

- a. Low moderator to material ratio dictates closer fuel rod spacing than desirable for mechanical reasons.
- b. Wide gap control rod channels defeat the primary purpose of securing low moderator to material ratio.

2. Control Rod Description

A cruciform shape, 2% boron stainless steel, control rod was selected for the square lattice design. Fuel was placed in the follower section of the lower portion of each blade, as shown in Fig. 2-6. This type of control rod requires the use of a large water gap between fuel assemblies and addition of structural material in the core. It is expensive to fabricate. These disadvantages contributed to the decision to abandon the square lattice core concept.

C. Hexagonal Lattice Concept

1. General

The hexagonal cross section of the fuel assembly was chosen because of the compact spacing feature of the triangular pitch arrangement. Approximately 5% more rods may be installed in a given area, compared to equivalent center-to-center distance spacing of a square pattern.

The cell size is the same for all the hexagonal designs discussed here.

2. One-piece Invertible Assembly

The fuel assembly is shown in Fig. 2-7. The unit has identical end fittings, permitting inversion with approximately a symmetrical system. In this unit, the fuel rods are spaced at intervals along the length of the fuel assembly. The fuel rods are threaded into adjoining rods. On the end grids the threaded shaft extends through the grid. A nut was used to tighten the rod against the grid. To hold the nut in place it could be pinned, or the rod could be flared.

A disadvantage of the long element is the water gap associated with the intermediate spacers. In the case of oxide fuels, a void area at the end of the rods must be included to accommodate fuel expansion and fission gas release. It appears difficult to reduce each gap to less than $1\frac{3}{8}$ in. with the use of threaded connections. A sleeve loading method, shown in Fig. 2-8, is required to permit the handling of the open-sided fuel elements. This concept was discarded because end for end rotation of the fuel did not provide as much fuel cycle flexibility as the interchangeable split element design.

3. Divided Fuel - Banded Element

Several methods of accomplishing the divided fuel were proposed. The first attempt utilized a strap system similar to that proposed for the rectangular fuel element previously described (see Fig. 2-9). The concept presents the problems of remote fastener and strap removal after irradiation and of leaving exposed edges at the spacer grids, thereby permitting possible hang-up during core fueling operations. The latter objection could be alleviated by placing the straps flush at the corners of the hex and thereby providing guides for the fuel-handling operation.

4. Divided Fuel - Bolted Assembly

In this design (Fig. 2-10) the fuel subassemblies have been modified by removing three fuel rods and replacing them with Zircaloy tubes to permit stringing the subassembly on the Zr-2 rods. The design

incorporates a $\frac{1}{4}$ -in.-thick end grid plate to reduce distance between core halves and fuel rod end fittings which have stepped shafts fitting into the grid plate. All rods are welded to the bottom support and spacer grid. Six corner rods are welded to the top grid plate. The remaining 55 rods are free to slide in holes to accommodate any differential expansion. Two fuel assemblies are fastened together with Zircaloy tension rods which extend through the spacer tubes which are attached to a lower locating fitting and are bolted to the top handling fitting.

Disadvantages of this design are:

1. Difficulty is encountered in remote disassembly and reassembly. Thread damage is possible and alignment when inserting rods through the tubes may be difficult.
2. 5% of fuel space in the core is occupied by the tubes and rods.
3. Additional neutron-absorbing material is introduced, necessitating further reduction of moderator volume to maintain water/fuel ratio.
4. A loading sleeve is needed for insertion and removal of fuel from core.

5. Divided Fuel - Half Basket Assembly

Before selection of the reference design, a "half basket" concept, shown in Fig. 2-11, was studied. The minimum thickness end plate-welded rod design was incorporated just as was used in the bolted assembly. However, the spacer tubes were replaced with fuel rods to make a 61-rod element. The design seemed complicated. The bottom assembly must be attached to a bottom end fitting. This concept was abandoned because of handling and locating difficulties.

FIG. 2-2
REFERENCE FUEL HOLDER

FIG. 2-3
REFERENCE FUELED FOLLOWER CONTROL ROD

A-H DENOTE FUEL LOADING
 T DENOTES TOP FUEL ASSEMBLY
 B DENOTES BOTTOM FUEL ASSEMBLY

FIG. 2-4
FUEL MANAGEMENT CYCLE

FIG. 2-5
SQUARE LATTICE DIVIDED FUEL ELEMENT

FIG. 2-6
 CRUCIFORM FUELED FOLLOWER CONTROL ROD

FIG. 2-7

ONE-PIECE REVERSIBLE FUEL ELEMENT

FIG. 2-8
SLEEVE LOADING METHOD

FIG. 2-9
DIVIDED FUEL - BANDED ASSEMBLY

FIG. 2-10
DIVIDED FUEL - BOLTED ASSEMBLY

FIG. 2-11
DIVIDED FUEL-HALF BASKET ASSEMBLY

SECTION 3

MECHANICAL DESIGN

C. F. Bullinger, N. Balai, L. E. Genens

I. Reactor VesselA. Reference Vessel Data

The reference design reactor vessel data compiled from the study are shown in Table 3-1.

Table 3-1

PRESSURE VESSEL DATA

Inside Diameter, ft.	8 $\frac{1}{2}$
Inside Length, ft	31 $\frac{1}{4}$
Main Side-wall Thickness, in.	3.25
Material	
a. Shell Plate	SA-212-B
b. Flanges	SA-105-II
c. Nozzles	SA-106-B
Shipping Weight, lb	210,000
Operating Pressure, psia	1000
Design Pressure, psia	1100
Operating Temperature, °F	545
Design Temperature, °F	650
Vessel Liner	
a. Thickness, in.	0.187
b. Material	Type 304 Stainless Steel

The general features of the reference reactor vessel are shown in Fig. 3-1. SA-212-B carbon steel was used for the vessel wall because of the known reliability of the grade of steel. Flange forgings are made from SA-105 Grade II carbon steel. The flange detail dimensions are shown in Fig. 3-2.

B. Nozzles

The nozzles required for coolant flow have been shown. Any additional nozzles required can be specified as detailed design progresses. The detailed nozzle dimensions are shown in Fig. 3-3. All internal surfaces are lined with 0.187-in. thick Type 304 stainless steel. The flow nozzle data are given in Table 3-2.

Table 3-2

FLOW NOZZLES

A. Coolant Inlet		
1.	Inside Diameter, In.	13.938
2.	Number Required	4
3.	Flow Velocity, FPS at 30,000 gpm	16
B. Coolant Outlet		
1.	Inside Diameter, In.	20.938
2.	Number Required	4
3.	Flow Velocity, FPS	16

The control rod guide nozzle and lower head penetration details are shown in Fig. 3-4. As shown, the nozzles are located on a minimum center distance triangular pitch pattern of 10.25 in. A maximum bore of 4.5 in. has been shown to provide sufficient control rod drive hydraulic actuator piston area. Final requirements must be determined during detailed design work.

Six instrumentation and three access connections are provided in the top area of the pressure vessel.

C. Vessel Support

The pressure vessel is supported by brackets attached to the shell on the coolant outlet centerline. These brackets rest upon the top plate of the external thermal shield. Bottom support using a skirt or legs was considered. Excessively long members were required to reduce the thermal expansion stresses in the vessel wall and support. For this reason, the bottom support method was rejected.

D. Discussion of Design

An internal thermal shielding consisting of four concentric stainless steel cylinders is shown in Fig. 3-1. The adequacy of the design, choice of materials and means for cooling the thermal shield are subject to further consideration resulting from core design. Thermal neutron absorbers in the form of homogeneous or clad plate probably could be incorporated into the thermal shield design to limit nuclear heat generation in the walls of the pressure vessel. A thermal shield for the protection of the "conventional" vessel steels is advisable because material damage has been reported at low dosages. Published data obtained from irradiated test samples show the following effects in the various steels:

1. The yield strength is preferentially altered. The elastic ratio is increased from approximately 0.5 to 0.09 or higher.⁽¹⁾
2. A marked increase in hardness occurs.⁽²⁾
3. The residual impact strength is about two-thirds of the unirradiated strength.⁽³⁾
4. An increase of 100 to 200°F in both the nil-ductility and fracture ductility transition temperatures has been reported.⁽⁴⁾

A threshold damage level for the ferritic steels is not well known, because in all of the data on irradiation effects available, the neutron spectra are reported in either the thermal neutron flux, or the fast flux range, but complete spectra are not given. Chemical and metallurgical histories are either poorly or completely undefined.

The data of Churchman, Hull and Mogford⁽¹⁾ bracket the damage level between 1×10^{18} and 1.5×10^{19} nvt. The data of Baldwin⁽⁵⁾ shows a substantial change at 4×10^{18} nvt exposure in SA302B material. The data of Berggren and Wilson⁽⁴⁾ show minor simultaneous effects on strength levels, losses of impact strength, and upward shifts of the transition temperatures at a 10^{19} -nvt level. The data of Trudeau⁽⁶⁾ confirms the changes in mechanical properties in a special neutron spectrum at the higher exposure level of 5×10^{19} nvt.

Postirradiation data derived from operational equipment is almost nonexistent. Bartz⁽⁷⁾ reports that A201-B material examined after receiving 4×10^{19} nvt (>1 Mev) received only minor damage; the elongation was reduced from 41% to 29%; the hardness increased from 75R_p to 92R_p, and small increases ($<10\%$) of both yield and tensile strengths occurred. At the irradiation level of 3×10^{20} nvt (>1 Mev), the SA201B material was severely hardened and lost virtually all of its ductility.

Preliminary results from irradiation of fine-grained SA212B steel (part of the plate used for the fabrication of the lower EBWR head) irradiated to levels ranging from 10×10^{19} to 7×10^{20} nvt (>1 Mev) show severe embrittlement. Subsize impact specimens in base metal (longitudinal and transverse directions), the heat-affected zone, and weld metal show the following effects:

- (1) For temperatures up to at least 250°F, impact resistance falls from 6 to 7 ft lbs to 0.1 ft lb (in subsize specimens).
- (2) The nil ductility temperature is elevated from 0 to 300°F. The (upper) fracture transition temperature is above 350°F.

An exploratory heat treatment was carried out using a few irradiated impact bars for a time of one hour at 1125°F in an argon furnace atmosphere with the following results:

- (1) The impact properties were restored to at least the levels existing prior to irradiation. Half of the heat-treated irradiated bars displayed enhanced impact resistance properties.
- (2) A marked softening of the material took place, reaching levels below that for unirradiated material. The same heat treatment for unirradiated bars showed no additional softening.

A further manifestation of irradiation damage was the inability to distinguish between the base metal, the heat-affected zone, and the weld metal by macro etching of irradiated bars. The marked softening (below that of unirradiated metal) coupled with homogenization of the wrought and cast (weld) structure is interpreted as confirming the data of Churchman, Hull and Mogford⁽¹⁾ and Bartz⁽⁷⁾ which bracket the damage threshold.

The scarcity of available data, accordingly, does not admit a total irradiation dosage exceeding 10^{19} nvt (neutron energies exceeding 1 Mev) as a conservative best estimate. The applicability of the estimate is further conditioned to thermal reactor types whose neutron spectra are predominantly thermal, as the irradiation effects data obtained to date cannot be correlated for irradiation damage effects in the epithermal neutron energy range above 200 ev.⁽⁸⁾

The thermal shield also acts as an upward flow director since it surrounds the core above the lower grid support structure. All of these components can be prefabricated and installed after placement of the vessel.

To prevent excessive flow bypassing, the clearance between the edge of fuel assemblies and inside of the core thermal shield cylinder must have additional study. Sufficient flow must be maintained between the thermal shield cylinders and inside of vessel to cool all surfaces.

The thermal shield and core support grid assembly details have not been completely defined. The structural requirements and fabrication methods must be determined in conjunction with fuel element holder nose piece design. The nozzle type support tubes inserted in the plate provide some reinforcement.

The flow inlet baffle arrangement in the lower portion of the vessel must be studied in detail. For uniform distribution of the maximum circulation rate of 50,000 gpm, perforation of the cylinder may be desirable.

E. Full-scale Plant Pressure Vessel

Figure 3-5 shows a concept of a reactor vessel for the full-scale plant. The physical problems encountered in providing sufficient coolant outlet nozzles to transport the steam-water mixture to the separating drum are evident. As shown, nozzles, 20 to 21 in. ID, are required on the 19-ft-diameter band near the top of the vessel. These nozzles are located on one horizontal centerline to help minimize flow distribution problems in the vertical risers to the steam drums. Flow velocities have been limited to 16 fps because adverse effects are possible due to high riser velocity and resultant resonance instability.

Steam-water separation requirements for the full-scale plant may dictate the use of an 8-ft-diameter drum with a total effective length of up to 170 ft, probably in two separate units. As in the case of the reference design, irradiation effects on the pressure vessel wall may require additional diameter to provide moderator and shield space.

II. Control Rod Drives

A. Design Data

For this study an original design of a control rod drive mechanism was not made. There is at least one commercial design available which is capable of meeting the operating requirements given in Table 3-3.

Table 3-3

CONTROL ROD DRIVE DATA

1. Number Required	19
2. Type of Operation	Gang-Hydraulic-Mechanical
3. Number of Gangs	6
4. Number of Drives per Gang	3
5. Center Rod Drive	1
6. Normal Driven Speed, in./min.	12
7. Fast Shutdown Time, sec	3
8. Control Rod Weight (including fuel), lb	375

III. Pumps

The rated coolant flow rate at full power in the reference reactor is 30,000 gpm. This flow rate is based upon an average steam void condition of 20% in the core. Two pumps, each of 25,000-gpm capacity, are specified for the installation. This pump size was selected to secure

operating experience on units of a size which would be required for the full-scale system. In that case six 25,000-gpm pumps would be required. The total capacity of 50,000 gpm for the reference reactor will provide an ample reserve for variable flow tests. Magnetic couplings have been provided between the drive motor and pump to give control of a wide range of speed for test purposes.

IV. Biological Shielding

A steel and water laminated shield, shown in Fig. 3-6, was designed. Calculations of neutron flux in the reactor shield were based on a one-group diffusion theory approach using a fast flux source term obtained from removal theory.⁽⁹⁾ The RE-34 program on the IBM-704 was used to solve the diffusion equation⁽¹⁰⁾ and the RE-157 program to calculate capture gamma-ray attenuation. The radiation doses at the outside of the biological shield are noted in Fig. 3-6 as a function of concrete thickness. The minor advantage of borating the water in the shield tank is indicated, roughly by the proportional decrease in dose, as shown below:

<u>% by Weight</u>	<u>Dose at Outside of Shield (7 ft Concrete), mr/hr</u>
0	20.2
1	17.8
2	16.8
4	15.9
8	15.0

Capture gamma rays leaving the pressure vessel wall are the predominant contributors to this dose. They over-ride the dose from the shield tank region and make the addition of boron to the shield tank water of negligible value.

As in the case of the internal thermal shield, this design must be recalculated after the core and pressure vessel shields are fixed.

FIG 3-1
REFERENCE REACTOR VESSEL

FIG 3-2
FLANGE DETAIL

COOLANT OUTLET NOZZLE (4)

COOLANT INLET NOZZLE (4)

FIG 3-3
NOZZLE DETAILS

FIG. 3-4
CONTROL ROD GUIDE NOZZLES

FIG. 3-5
FULL-SCALE REACTOR VESSEL

DOSE RATES IN mr./hr.
HEATING AT CONCRETE INNERFACE = 4.2 btu/hr.-ft³

FIG 3-6
SHIELDING CROSS SECTION

SECTION 4

HEAT TRANSFER AND FLUID FLOW

P. A. Lottes and M. Petrick

I. General

Heat transfer and fluid flow calculations are linked closely to physics calculations. The approach used here was to assume a neutron flux or power distribution and obtain void fraction and steam quality values. As shown in Section 5, Physics, the results of these calculations were used to obtain more accurate flux or power distributions. The physics results could have been used again to recalculate the heat and fluid flow values, but lack of time prevented a second set of calculations.

The peak to average values given in the Physics section were, therefore, taken as final, and the core parameters depending upon this value were adjusted accordingly.

The reference design was limited by a maximum central oxide fuel temperature set at 4600°F. The core configuration was designed to have a 1 to 1 total metal to water ratio. The heat transfer and fluid flow characteristics of the core are given in Tables 4-1 and 4-2.

II. List of Symbols

A	Area
a	Coefficient in Eq. 4.10
D_e	Equivalent diameter
H	Active core height
h	Enthalpy
k	Thermal conductivity
L	Length
N	Number of fuel rods
P	Power
Q	Thermal Power
q	Volumetric heat generation rate
q''	Heat flux
R	Ratio of maximum to average flux in the reactor
r	Radius
T	Temperature
V	Velocity
W	Weight
x	Steam quality (weight flow fraction)
ρ	Density
α	Steam volume fraction

Subscripts

b	Boiling zone
c	Core
ex	Exit
in	Inlet
o	Feedwater makeup
f	Saturated liquid
g	Saturated vapor
T	Total
sub	subcooling

III. Calculation Methods Used in Core Design

A. Assumed Power Distributions

1. Axial Power Distribution (First Approximation)

A cosine power distribution was chosen for the first half of the core, and a linear decreasing power distribution for the second half of the core with the flux decreasing to zero at 8 cm beyond each end of the core. Figure 4-1 shows the axial distribution. The maximum to average ratio is 1.64.

2. Radial Power Distribution (First Approximation)

The radial power distribution was assumed to be flat for the middle third of the core and to be a decreasing function of the radius, dropping to zero at a distance 8 cm (3.2 in.) from the outer edge of the core. The distribution is described by the equations

$$q/q_{\max} = 1.0 \quad \text{for} \quad 0 \leq r \leq 10 \quad (4.1)$$

$$q/q_{\max} = 1.1 - 0.001 r^2 \quad \text{for} \quad 10 \leq r \leq 30 \quad , \quad (4.2)$$

where r is given in inches.

Then

$$\bar{q}/q_{\max} = 0.645$$

or in the more common form

$$(q_{\max}/\bar{q})_{\text{radial}} = 1.55$$

Figure 4.2 shows the assumed radial power distribution. A cosine radial power distribution is also shown in Fig. 4.2.

B. Local Fluid Properties

1. Heat Balance

A measure of the heat input up to any point along the reactor core height may be represented by the area under the curve of Fig. 4-1.

A heat balance therefore may be written as, with D_e being the equivalent diameter,

$$\int_0^L q'' dL = (\rho_f V_{in} D_e/4)[h - h_{in}] \quad (4.3)$$

The local fluid stream enthalpy is

$$h = h_{in} + (4 q''_{max}/D_e \rho_f V) \int_0^L f(L) dL \quad , \quad (4.4)$$

where

$$f(L) = q''/q''_{max} = q/q_{max} \quad (4.5)$$

From Fig. 4-1, an approximation may be introduced such that

$$\left. \begin{aligned} f(L) &= 0.2 + 0.025 L & \text{for } 0 \leq L \leq 32 \\ f(L) &= 1 & \text{for } 32 \leq L \leq 40.5 \\ f(L) &= 1.92 - 0.0227 L & \text{for } 40.5 \leq L \leq 81 \end{aligned} \right\} \quad (4.6)$$

The integrated values may be shown to be equal to

$$\left. \begin{aligned} \int_0^L f(L) dL &= 0.2L + 0.0125L^2 & 0 \leq L \leq 32 \\ &= L - 12.8 & 32 \leq L \leq 40.5 \\ &= 1.92 L - 0.0113 L^2 - 31.5 & 40.5 \leq L \leq 81 \end{aligned} \right\} \quad (4.7)$$

2. Steam Quality

Equation 4.7 is plotted in Fig. 4-3. The local steam quality is defined as

$$x = (h - h_f)/h_{fg} \quad (4.8)$$

It should be noted that the equivalent diameter in equation 4.3 is based upon the heated perimeter and differs from the equivalent diameter used for pressure drop calculations.

3. Steam Volume Fraction

The local steam volume fraction is defined as

$$\alpha = \frac{1}{1 + \frac{\rho_g V_g}{\rho_f V_f} \left(\frac{1}{x} - 1 \right)} \quad (4.9)$$

For these calculations the velocity or slip ratio was assumed to be

$$V_g/V_f = 1.57$$

The remaining properties necessary for calculation of steam volume fraction are

$$\rho_f = 46.2 \text{ lb/ft}^3$$

$$\rho_g = 2.24 \text{ lb/ft}^3$$

$$h_{fg} = 649.4 \text{ Btu/lb}$$

$$h_f = 542.4 \text{ Btu/lb}$$

$$h_g = 1191.8 \text{ Btu/lb}$$

$$h_{350^\circ\text{F}} = 321.6 \text{ Btu/lb}$$

The variations of α with x are shown in Fig. 4-4.

IV. Steam Volume Fraction Distribution

A. Discussion of Methods

In any boiling reactor core, the power distribution and steam volume distribution are governed by the natural laws of physics and hydrodynamics. Approximations of these laws are usually made, and the question to be answered then becomes: "What are the errors due to making the simplifying assumptions?" This question has been partially answered by calculating the average reactor steam voids in three different ways:

- (1) uniform heat flux, constant inlet velocity;
- (2) nonuniform heat flux, constant boiling length; and
- (3) nonuniform heat flux, constant inlet velocity.

B. Core Parameters

All calculations were based on the specifications for the core given in Table 2-1.

The average exit quality and inlet subcooling are the same for all three cases:

$$x = W_g/W_T = 644,800/11,200,000 = 0.0575 \quad .$$

The ratio of steam heat input to total heat input is

$$Q_g/Q_T = h_{fg}/(h_g - h_0) = 649.4/(1191.8 - 321.6) = 0.745 \quad .$$

Also

$$h_f - h_{in}/(h_f - h_{in} + xh_{fg}) = 1 - (Q_g/Q_T) = 0.255$$

$$h_f - h_{in} = (0.25/0.75)xh_{fg} = (0.0575)(649.4)/3 \quad .$$

$$h_f - h_{in} = 12.4 \text{ Btu/lb} \quad .$$

The corresponding subcooling is found by dividing the subcooling change by the average specific heat:

$$\Delta t_{\text{sub}} = (h_f - h_{in})/C_p = 12.4/1.3 = 9.5^\circ\text{F} \quad .$$

C. Calculation of Fluid Quantities

1. Case 1. Uniform heat flux, constant inlet velocity

This case is the simplest to evaluate. The exit voids are

$$\alpha_{\text{ex}} = 0.445 \text{ (from Fig. 4-4 for } x = 0.0575) \quad .$$

The mean voids in the boiling zone are equal to

$$\bar{\alpha}_b = 0.265 \text{ (from Fig. 4-5)} \quad ,$$

and the average core voids are

$$\bar{\alpha}_c = (\bar{\alpha}_b) L_b/L_T = 0.265 (0.75) = 0.199 \quad .$$

Figure 4-5 shows the quality and void distributions.

2. Case 2. Nonuniform heat flux, constant boiling length

This case has the power distribution given in Figs. 4-5 and 4-1 with the further provision that the inlet velocity is varied

radially to maintain a constant boiling length. Figure 4-6 shows the results of these calculations. In this case every channel has the same axial void distribution.

3. Case 3. Nonuniform heat flux, constant velocity

From a design viewpoint, it is more practical to plan a constant-velocity core. Cross-flow considerations as discussed earlier showed that orificing an open rod-type core is difficult and may not be practical.

With a constant core inlet velocity, each radial position will exhibit different characteristics. Steam volume fractions are now a function of length and radius. Figure 4-7 summarizes the axial steam void distributions and Fig. 4-8 shows the average of the axial distributions as a function of radius. Again, it may be noted that the square of the radius is shown rather than the first power of the radius.

D. Summary of Results

The results of these cases may be summarized as follows in Table 4-1.

Table 4-1

REACTOR VOIDS - FIRST APPROXIMATION

Average Exit Quality	0.0575		
Inlet Subcooling (at 30,000 gpm)	12.5 Btu/lb or 9.5°F		
Case	$\bar{\alpha}_{ex}$	$\bar{\alpha}_b$	$\bar{\alpha}_c$
(1) Uniform flux, constant inlet velocity	0.445	0.265	0.199
(2) Nonuniform flux, constant boiling length	0.445	0.311	0.215
(3) Nonuniform flux, constant inlet velocity	-*	-*	0.209
Ratio of steam power to total power	0.75		

*The values are a function of core radius

E. Pressure Drop through Core

The results of pressure-drop calculations are given in Fig. 4-9. The ordinate is the sum of the friction, momentum, and hydrostatic head components, and the abscissa is the square of the core radius. Pressure drop was calculated in accordance with the latest techniques.⁽¹¹⁾ Some orificing is desirable in the outer elements to prevent excessive bypassing of water from the central elements. The method or scheme to be used needs study and evaluation. It is apparent, however, that the case of constant inlet velocity is easier to build than the case of constant boiling length.

F. Rod Centerline Temperature

The centerline temperature of the fuel element was obtained in the following manner: Starting with a base temperature of 553°F for the saturated water at 1000 psi, Table 4-2 lists the temperature drops through the various barriers between the coolant at the outside of the oxide pellet. The temperature drop between the pellet surface and the clad was based on experimental data on the contact resistance to heat flow.⁽¹²⁾

Table 4-2

TEMPERATURE DROPS

	<u>° F</u>
H ₂ O Temperature	553
Film Drop	14
Clad (at maximum heat flux)	104
Contact, ΔT, Pellet to Clad	226
Outside Temperature of Pellet	897

For the thermal conductivity of UO₂ data of Kingery⁽¹³⁾ and from Armour⁽¹⁴⁾ were compared. The Kingery data are probably more precise, but the Armour are more conservative and were used for that reason. At a density of 10.0 gm/cc, the Kingery data yield the equation

$$k = 3580/T_R \quad (4.10)$$

for conductivity, where k is in units of Btu/(hr)(ft)(°F) and T_R is Rankine temperature.

The Armour data yield the coefficient of 3200 in Equation 4.10 in place of 3580. The maximum heat source strength is computed from the equation:

$$Q_{\max} r^2/4 = (0.853 \times 10^6) PR/\pi NH, \quad (4.11)$$

where the left-hand side is expressed in units of Btu/(hr)(ft) and P is in megawatts. Three values of R were used in the calculation:

- a. $R = 1$ to give the average centerline temperature throughout the reactor;
- b. $R = 3.92$ to give the maximum flux except for the water gap peaking (discussed in the Section 5, Physics).

c. $R = (3.92)(1.25) = 4.90$ to give the maximum heat flux if the peaking introduced by the water gap between fuel elements is not overcome.

The integration of the equation of thermal conductivity between the centerline of the cylindrical pellet and the outside yielded the following equation:

$$\ln T_R (CL)/T_R (S) = Q_{\max} r^2/4a \quad , \quad (4.12)$$

where CL refers to centerline, S refers to surface, and a is the coefficient in equation 4.10. The results of these calculations for the centerline temperature are listed in Table 4-3, based on a surface temperature of 900°F (1360°R).

Table 4-3

FUEL PELLETT CENTERLINE TEMPERATURE

<u>R (Maximum-to-Average)</u>	<u>T (CL) ($^\circ\text{F}$)</u>
1.0(Ave)	1330
3.92	3540
4.90	4790

G. Fuel Rod Heat Transfer

The values of Table 4-4 summarize the results of calculations covering the fuel rod heat transfer.

Table 4-4

FUEL ROD HEAT TRANSFER - SECOND APPROXIMATION

Pellet Diameter, in.	0.417
Pellet Clearance, in.	0.0015
Clad Thickness (Zircaloy-2), in.	0.025
Fuel Rod Diameter, in.	0.470
Thermal Conductivity of Pellet, $(15) \text{Btu}/(\text{hr})(\text{ft})(^\circ\text{F})$	1.0
Thermal Conductivity of Zircaloy-2, $(16) \text{Btu}/(\text{hr})(\text{ft})(^\circ\text{F})$	8.0
Maximum Heat Flux, $\text{Btu}/(\text{hr})(\text{ft}^2)$	403,000
Maximum Fuel Centerline Temperature, $^\circ\text{F}$	4,617
Temperature Drop in Pellet (Maximum), $^\circ\text{F}$	3,940
Temperature Drop in Clad (Maximum), $^\circ\text{F}$	105
Temperature Drop in Boiling Film (Maximum), $^\circ\text{F}$	14
Saturation Temperature, $^\circ\text{F}$	553
Average Heat Flux, $\text{Btu}/(\text{hr})(\text{ft}^2)$	92,000
Temperature Drop in Pellet (Average), $^\circ\text{F}$	896
Temperature Drop in Clad (Average), $^\circ\text{F}$	24
Temperature Drop in Boiling Film (Average), $^\circ\text{F}$	9
Saturation Temperature, $^\circ\text{F}$	553
Average Fuel Centerline Temperature, $^\circ\text{F}$	1,482

FIG. 4-1
AXIAL POWER DISTRIBUTION (FIRST APPROXIMATION)

FIG. 4-2
RADIAL POWER DISTRIBUTION (FIRST APPROXIMATION)

FIG. 4-3
INTEGRAL OF POWER VS CORE HEIGHT

FIG. 4-4
STEAM VOLUME FRACTION VS QUALITY

FIG. 4-5
STEAM VOLUME FRACTION VS. CORE HEIGHT FOR CONSTANT FLUX, CONSTANT VELOCITY

FIG. 4-6
STEAM VOLUME FRACTION VS. CORE HEIGHT FOR CONSTANT BOILING LENGTH

FIG. 4-7
STEAM VOLUME FRACTION VS. CORE HEIGHT FOR CONSTANT INLET VELOCITY

FIG. 4-8
AVERAGE AXIAL STEAM VOLUME FRACTION
VS. CORE RADIUS FOR CONSTANT INLET VELOCITY

FIG. 4-9
RADIAL VARIATION OF CORE PRESSURE DROP

SECTION 5

PHYSICS

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I. General

The physics of the reference reactor was started by selecting a reasonable range of core configurations. Moderator to fuel ratios and enrichments were calculated for both the reference reactor and the full-scale plant. The values finally selected were based on those most likely to give information pertaining to the full-scale core performance.

In this study, as in most design work, the complete calculation was an iterative process. The first iteration involved making judicious choices of certain features, numerical values, etc., that enter into the calculation in a way not grossly affecting the final answer. A second iteration then used the first answer to re-evaluate the input information. This discussion represents largely the results of the first iteration, especially in regard to the space-energy-time distribution function of neutron flux. This limitation is due to design time restrictions. For the same reason, the simplest methods of calculation were sought. Corrections were applied wherever possible for the inadequacy of these calculations. For these reasons, further iterating and refining may be necessary if more precise results are desired.

The following symbols in the order of their appearance are used in this section:

B^2	buckling
K	Boltzmann constant
T_n	neutron temperature
E	energy
r	Westcott's spectral parameter
σ_i	microscopic cross section of type i
b	barns
p	resonance escape probability
a_i	capture to fission ratio of material i
ν_i	neutrons per fission of type i
Σ_i	macroscopic cross section of type i

F	fast to thermal fission ratio
ϵ	fast effect factor
τ	age
t	time
ϕ	flux
N	number of atoms
$\int t$	time integral of flux
η_i	neutron per i^{th} fissile materials absorption
L_f	fast nonleakage probability
λ	decay constant
δK	reactivity
L	thermal diffusion length

II. Physical Details of the Lattice Studied

The engineering details of the core lattice have been given in Section 2 and Section 3. Properties which entered into the physics calculations are listed or repeated here.

1. Lattice shape: Hexagonal, cylindrical rods
2. Fuel rods: UO_2 , 0.420 in. OD, density 9.2 g/cc. The density is thus reduced from the microscopic density of the UO_2 by the gap fraction within the cladding inner wall; the gap was not considered as a separate region.
3. The volume ratio $\text{H}_2\text{O}:\text{UO}_2:\text{Zr-2}$: taken as 1.4:1:0.4. The UO_2 volume was taken as total volume within cladding walls. The Zr-2 volume includes miscellaneous core structure not associated with a given fuel rod, and the H_2O includes water between fuel elements. Therefore, although the lattice is in reality triangular, 0.6 in. center to center, the circular cell used for computation had an outer diameter of 0.703 in. In the design presented in Section 2 the mass ratio of H_2O to UO_2 has been preserved, although the system density is slightly higher than assumed here. The physics remains essentially unchanged.
4. Void distribution: for the calculation of reactivity and burnup it was assumed that there is a uniform distribution of voids to the extent of 20% of the water volume (see Fig. 5-1 for actual distribution).

5. Buckling: the geometric buckling included the effects of 22-cm vertical reflector savings and 8-cm radial savings. Two sets of cores were studied: the reference reactor with a buckling $B^2 = 0.00101 \text{ cm}^{-2}$, and a large core (full-scale reactor) with a buckling $B^2 = 0.00038 \text{ cm}^{-2}$.

III. Computational Methods

A. Cross Sections

Absolute reactivities were found by two and one-half group theory. To determine the reactivity as a function of burnup one-group cross sections, which gave the correct rate of absorption in each element, were employed. Since Pu^{239} plays a large role in the burnup problem, the spectrum was assumed to be a Maxwellian spectrum with an average neutron energy $kT_n = 0.05 \text{ ev}$ plus a $1/E$ -tail adjoined at $E = 0.1 \text{ ev}$. The blackness⁽¹⁷⁾ (Westcott's parameter, r) was increased by 10% over its value at zero burnup to allow for the increase in absorption cross section during the fuel cycle. The cross sections used, as a function of enrichment are given in Table 5-1. These cross sections are normalized to

$$\sigma_f(\text{U}^{235}) = 545 \text{ b.}$$

Table 5-1

<u>THERMAL CROSS SECTIONS</u>				
[Cross sections in kilobarns (10^{-21} cm^2) or cm^{-1}]				
Enrichment	2%	2.4%	3%	Notes
<u>Isotope</u>				
U^{235}	0.675, $\alpha = .238$	0.680, $\alpha = .252$	0.686, $\alpha = .263$	1; $\nu = 2.45$
U^{236}	0.023	0.023	0.023	2
U^{238}	0.00275	0.00275	0.00275	(excluding resonance capture)
Pu^{239}	1.89, $\alpha = .575$	1.96, $\alpha = .587$	2.05, $\alpha = .601$	$\nu = 2.90$
Pu^{240}	0.96	1.13	1.38	3
Pu^{241}	1.85, $\alpha = .4$	1.866, $\alpha = .4$	1.89, $\alpha = .4$	$\nu = 2.91$
Pu^{242}	0.360	0.43	0.53	
Fission Products	0.066	0.066	0.066	
$\Sigma_A(\text{Zr}) + \Sigma_A(\text{H}_2\text{O})$	0.01160 cm^{-1}	0.01170 cm^{-1}	0.01219 cm^{-1}	(20% average void)
$\Sigma_A(\text{Xe,Sm})$	0.00614 cm^{-1}	0.00614 cm^{-1}	0.00614 cm^{-1}	4

Notes:

- 1 All values normalized to $\sigma_f = 0.545$.
- 2 Pile average (private communication from Paul Fields).
- 3 Based on extrapolation of NRX data.
- 4 Based on operation at constant power. this number is independent of enrichment.

Critical calculations were made after applying the usual temperature correction to the 2200-meter/sec flux and separating out the "thermal" cross section through

$$\sigma = \hat{\sigma}_A \sqrt{(\pi/4)(T_O/T_N)} - r(R.I.) \quad ,$$

where R.I. is the resonance integral of the isotope in question.

B. Computation of the Resonance Integral

The resonance integral of U^{238} was found by using Hellstrand's formula⁽¹⁹⁾ and applying a Dancoff-Ginsburg⁽¹⁹⁾ or equivalent Bell⁽²⁰⁾ correction for shadowing. To account for Doppler broadening, 1 b(10^{-24} cm²) was added to the resonance integral. The resonance escape probability at 20% average void was found to be

$$p = 0.710$$

for the reference lattice.

C. Fast Effect

Cross sections used in calculating the fast effect are given in Table 5-2.

Table 5-2

FAST EFFECT CROSS SECTIONS (In Barns, 10^{-24} cm²)

Element	σ_{Capture}	σ_{Fission}	$\sigma_{\text{Inelastic}}$	
U^{235}	0.10	1.3	1.4	$\nu_{ff} = 2.65$
U^{238}	0.04	0.53	2.1	$\nu_{ff} = 2.65$
H	0	0	1.32	
O	0.024	0	0.17	
Zr	0	0	0.8	

(These are averaged over the fission spectrum above U^{238} fission threshold. The fraction of neutrons born in fission above the threshold is $x = 0.575$.)

The following formulae were used in calculating the fast effect:⁽²¹⁾

$$F_1 = \nu_{ff} x \sigma_f / (\sigma_c + \sigma_f + \sigma_{lM});$$

$$F = F_1 / [1 - (\nu_{ff} / \nu_{th}) F_1];$$

$$\epsilon = 1 + 0.485 F (1 - 3.726 F) / (1 - 2.431 F); \quad (5.2)$$

F.C.R. = fast conversion ratio

$$= 0.268 F (1_i + 2.445 F) / 0.69885$$

At 20% voids, $\epsilon = 1.046$ and F.C.R. = 0.0619.

D. Neutron Age

The age was calculated by reference to Zr-H₂O experiment⁽²²⁾ and equivalence factors.⁽²³⁾ At 20% voids the average age found by these methods was $\tau = 90 \text{ cm}^2$.

E. Initial Conversion Ratio

The ratio of U²³⁸ atoms transmuted to Pu²³⁹ per U²³⁵ atom destroyed by fission or radiative capture was I.C.R. = 0.73 (hot operating critical core).

F. Long-term Reactivity Effects

Production of isotopes was studied by solution of the following burnup equations on the LGP-30, a digital computer at ANL:

$$(i) \quad \tau = \int_0^t \phi dt \quad (5.3)$$

$$(ii) \quad d(N\sigma)^{25}/d\tau = N\sigma^{25} \quad (5.4)$$

$$(iii) \quad d(N\sigma)^{26}/d\tau = \sigma^{26} (N\sigma)^{25} \alpha_{25} / (1 + \alpha_{25}) \quad (5.5)$$

$$(iv) \quad d(N\sigma)^{49}/d\tau = \sigma^{49} [(N\sigma)^{28} + \sum_i (N\sigma)^i \eta^i L_f - (N\sigma)^{49}]; i = 25, 41, 49 \quad (5.6)$$

$$(v) \quad d(N\sigma)^{40}/d\tau = \sigma^{40} [(N\sigma)^{49} (\alpha_{49}/1) + \alpha_{49} - (N\sigma)^{40}] \quad (5.7)$$

$$(vi) \quad d(N\sigma)^{41}/d\tau = \sigma^{41} \left[(N\sigma)^{40} - (N\sigma)^{41} \left[\frac{\sigma^{41} + (\lambda^{41}/\phi)}{\sigma^{41}} \right] \right] \quad (5.8)$$

$$(vii) \quad d(N\sigma)^{42}/d\tau = \sigma^{42} (N\sigma)^{41} \alpha_{41} / (1 + \alpha_{41}) \quad (5.9)$$

$$(viii) \quad d(N\sigma)^{FP}/d\tau = \sigma^{FP} [(N\sigma)^{25} / (1 + \alpha_{25}) + (N\sigma)^{49} / (1 + \alpha_{49}) + (N\sigma)^{41} / (1 + \alpha_{41})] [1 + (F.F./T.F.)] \quad (5.10)$$

In equations, 5.3 to 5.10, these definitions hold: the index 25 indicates U^{235} , 26 corresponds to U^{236} , 28 to U^{238} , 49 to Pu^{239} , 40 to Pu^{240} , 41 to Pu^{241} , 42 to Pu^{242} , FP to fission products (nonsaturating); $(N\sigma)^i$ is the cross-section density of isotope i , σ^i the microscopic cross section of isotope i ; $1 + \alpha_i$ is the ratio of total capture to fission ratio of isotope i ; L_f is proportional (by the factor η) to the nonthermal conversion ratio (this is assumed constant over the cycle); $(N\sigma^{28})$ is the average cross-section density of U^{238} and is inserted as an initial datum; $(\lambda/\phi)^{41}$ takes into account the radioactive decay of Pu^{241} . F.F./T.F. is just the ratio (assumed constant over the cycle) of fast fissions to thermal and epithermal fissions. If the σ^i are in kilobarns ($1 \text{ kb} = 10^{-21} \text{ cm}^2$) and τ is measured in neutrons/kb, convenient numerical quantities occur.

IV. Fuel Cycle

The reference reactor will be used to give both the characteristics of this reactor type with a fresh core and the changes of characteristics after long burnups. During design, it was necessary to compute the characteristics of the initial core and the equilibrium core. By an equilibrium core is meant one which is removing the same amount of fuel with the same burnup at the end of each cycle in a constant reloading pattern.

Figure 2-4 shows that the proposed fuel loading consists of adding fresh fuel in the center and removing spent fuel from the edge. To start the iterative process, assume that the four equal volume zones have average fluxes in the approximate ratios of 7:5:3:1 and one-group perturbation theory importance functions in the ratios of about 37:19:7:1. These numbers correspond to regional averages over four regions of equal volume in a cylinder whose radial flux goes quadratically to zero at the outer boundary. The corresponding Max/Ave radial flux is 2.0. Changes in the flux shape as a function of burnup will be observed upon further iteration.

The six diagrams in Fig. 2-4 show the fuel arrangement at the beginning of each of the first six cycles. To begin the second cycle, the fuel in zones 1 and 2 is first reversed (i.e., the partially burned centermost fuel will now be at the top and bottom of the reversed element), and then the interchanges, 1 and 4, as well as 2 and 3, are made. To begin the third cycle, the fuel in zones 1 and 2 is first reversed, and then the fuel in zone i moves to zone $i + 1$. Fresh fuel enters the center as the first quarter core, having about 77% of the reference burnup, is removed from zone 4. Reactivity is, therefore, conserved best by allowing fresh fuel to reside in regions of high importance.

At the beginning of the third and each succeeding cycle the reloading pattern is the same. The fuel in zone 1 is first reversed and then the fuel in zone i moves to zone $i + 1$. Fresh fuel always enters the center. The fuel removed from zone 4 at the end of the second, third, fourth, and succeeding cycles has respectively 85%, 96%, 93%, and 100% of the reference burnup.

Both the prototype and the full-scale reactor are assumed to have this same fuel cycle. The one difference, however, is that the reference and the full-scale reactors have burnups of 10,700 and 16,100 MWD/ton of uranium metal contained, respectively. The corresponding cycle times are eight months and one year, at 80% load factor.

V. Results of Reactivity Calculations

The reactivity at discharge was computed for the reference core for the fuel cycle discussed under fuel cycle IV (above). The computed reactivity was divided by 1.02 to yield an allowance for error. The results are given in Table 5-3.

Similar calculations were done for the full-scale core and for cores with a higher ratio of moderator to fuel. The results are also shown graphically in Fig. 5-2 and 5-3. The curve of enrichment vs MWD/ton shows that the recommended enrichment of 2.65% will yield a burnup of 12,000 MWD/ton.

Table 5-3

<u>k_{eff} AT DISCHARGE (REFERENCE CORE)</u>					
Enrichment	Burnup: MWD/Metric Ton of U^{238}				
	0	6000	12000	15000	18000
2%	0.97 ₈	0.95 ₇	0.92 ₆	0.91 ₈	0.90
2.4%	1.02 ₇	1.00 ₃	0.97 ₆	0.96 ₁	0.94 ₅
3.0%	1.08 ₂	1.05 ₃	1.02 ₅	1.01 ₁	0.99 ₈

The discharged fuel will contain approximately 1.60 atom % U^{235} , and 0.64 atom % Pu^{239} and Pu^{241} , at the recommended discharge point.

Calculations of long-term reactivities in the full-scale core show that the reactivity as given in Table 5-3 and Fig. 5-3 can be multiplied by 1.059 to yield the corresponding number for the larger core. Of this, the value 1.056 stems from the reduction in leakage, the remainder being attributed to increased conversion.

Calculations of long-term reactivities in cores of higher moderator to fuel ratio yield the results in Table 5-4.

Table 5-4

<u>FISSIONS PER U^{235} ATOM BURNED AT 12000-MWD/T DISCHARGE</u>	
Moderator to fuel ratio	Fissions per atom of U^{235} burned
1.4	1.6
1.54	1.47
1.65	1.33

VI. Control

A. Requirements

Calculations of approximate reactivity effects yielded the results listed in Table 5-5. For void and temperature effects, the only significant factors are changes in water density and elimination of Doppler effect in going from hot to cold. The estimates were made by one-group calculation, considering the thermal and density changes in M^2 and in the various components of the four-factor formula.

Table 5-5

APPROXIMATE REACTIVITY EFFECTS (% δk) FOR THE REFERENCE REACTOR

	<u>Initial Core</u>	<u>Equilibrium Core</u>
Xe + Sm	3.7	3.7
Voids	8.7	8.9
Burnups	7.7	4.8
Total Hot	21.6	18.4
Temperature Effect	8	8
Total Cold	31.0	28.0

Note: the effect of a number of factors is found by

$$\delta k = \prod_i (1 + \delta k_i) - 1$$

For burnup, the results of the previous section were applied directly. The initial core k_{eff} gives directly the burnup requirement. In the equilibrium case, reactivity averages of the importance-weighted k_{eff} of each radial region were compared at the beginning and end of a typical quarter-cycle.

For Sm, a fission yield of 1.5% was assumed, and for Xe, 6.7%.

B. Control Rod Worth

The particular array studied was that of nineteen control rods distributed uniformly over the lattice with one in the center. Each rod was assumed to be a boron-steel box that replaced a fuel box and absorbed only thermal neutrons. Water at 20% mean voids filled the inside of the boxes. The reactivity difference between this array and one with no control rods was estimated by performing a two-dimensional calculation using the PDQ Code⁽²⁴⁾ with square rods of approximately the same perimeter per unit height and distribution. Extrapolation to the cold condition was done by using the Hurwitz-Roe method,⁽²⁵⁾ which states that, among other things, the reactivity controlled by the rods is proportional to L , the diffusion length. In the cold core the 19 control rods are worth 19%; in the hot core 38%.

C. Fluid Shutdown Control

The mechanical control system is completely adequate for all conditions encountered with the reactor hot. However, there is a deficiency in control of the cold reactor, mainly due to the decreased control rod worth in the cold condition. This deficiency, which is a shutdown phenomenon, can be overcome by the addition of H_3BO_3 . Approximately 0.0013 mol of H_3BO_3 per mol of H_2O will be required for the initial core; at equilibrium approximately 0.001 mol H_3BO_3 per mol of H_2O will be required. These concentrations are about one-tenth of the solubility limit of H_3BO_3 at the temperatures considered.

VII. Flux Pattern

A. Over-all Reactor

Our calculations of any specific lattice depend on the approximation that reactivity is about the same for the actual void concentration as for uniform distribution of voids. To check this, an assumed power density was introduced into the heat and flow equations. An estimate of the void distribution based on past experience and heat transfer analysis was made (see Fig. 5-1), and a two-dimensional calculation of the power was made using the IBM-704 Code, PDQ.⁽²⁴⁾ The resulting power distribution is shown in Figs. 5-4 and 5-5. This power distribution is reasonably consistent with the void distribution shown in Fig. 5-1, so no further iteration is necessary.

The power distributions shown in Figs. 5-4 and 5-5 yield a maximum-to-average flux of 4.38. The situation is actually somewhat pessimistic because no reflector was included in the calculation. The figures are intended purely for illustrative purposes. A recalculation taking the actual reflector into consideration gave a peak-to-average of 3.92.

The configuration with all control rods out of the core is the most pessimistic during cycle lifetime. Although the maximum amount of fuel is in the core at this point, the axial flux shape is by far worse than at any other time during core history and this effect overbalances the diminished source volume effect.

It might be mentioned at this point that the approximation of taking the mean statistical weight of voids as being the mean core statistical weight is a reasonable one. To some extent this is true because the radial and axial statistical weight effects typically lead to corrections of opposite sign on the mean core statistical weight approximation.

B. Hot Channel

In addition to the over-all reactor flux peaking, an investigation was carried out to determine the seriousness of the problem posed by having

clearance between adjacent fuel elements corresponding to an effective water gap of about 0.4 in. An S-4⁽²⁶⁾ transport theory calculation of this effect indicated that this channel was responsible for a peak-to-average flux ratio of 1.25 to be superimposed upon the other hot channel factor. A diffusion theory calculation yielded a figure of 1.08. Although diffusion theory is often resorted to for this calculation, it should be pointed out that the transport calculation is certainly more correct.

FIG. 5-1
ESTIMATED VOID DISTRIBUTION

FIG. 5-2
ENRICHMENT VS. MW days/tonne U²³⁸

FIG. 5-3
 K_{eff} OF SMALL CORE VS BURNUP AND ENRICHMENT
 (K_{eff} OF BIG CORE IS FOUND BY MULTIPLYING ORDINATE BY 1.059)

FIG. 5-4
 RADIAL POWER DISTRIBUTIONS AT VARIOUS DISTANCES
 BELOW THE TOP OF THE FUEL FOR ALL CONTROL RODS OUT

FIG. 5-5
AXIAL POWER DISTRIBUTIONS AT VARIOUS RADII
FOR ALL CONTROL RODS OUT

SECTION 6

FUEL CYCLE EVALUATION

V. M. Kolba and B.I. Spinrad

I. Discussion

An evaluation of the invertible fuel management system versus the split or half-element fuel management system is in order. For a true cost comparison, actual fuel performance data should be at hand for the non-invertible, non-split fuel element as a base line for cost comparison. The basis used is the result of calculations of the additional enrichment requirement for each of the various schemes which are: radial shuffling only, invertible fuel and split fuel with radial and axial shuffling. The enrichment for the latter two cores are, respectively, 2.8% and 2.5%. If the item involved is the enrichment and its economic effect on the fuel cycle, the choice for a long-range advantage must remain with the lower enrichment value for equivalent fuel burnup and reactor power. There are certain items, included in fabrication cost, which do not stand out properly if one uses the "good" round number of \$140/kg U as the fabrication cost base.

The time allotted for fuel changes and its effect on operating economy is not completely reflected in the study. If a fuel management program were set up to obtain 100% burnup at the expense of shutting down daily for fuel movement, the economic disadvantage factor is quite apparent. In assessing the value of the invertible vs the split fuel element, the time factor is slightly more uncertain. The inversion and radial shuffling of two-piece elements may require a minor increase in down time compared to radial only re-arrangement. Another slight disadvantage of the split system is the introduction of more inert neutron-absorbing material into the core. However, this system offers the possibility that flux flattening could be achieved eventually, by revising the fuel management scheme.

II. Cost Data

The cost comparison of the inverted and split fuel element cores for the reference reactor and the full scale reactor are shown in Tables 6-1 and 6-2. Table 6-2 is a summary of Table 6-1 data for easier comparison. Pertinent data for enrichments, etc., are based on the numbers from physics calculations discussed in Section 5.

Table 6-1

FUEL CYCLE COST DETAILS

Item	825 Mw(t)		165 Mw(t)	
	Divided	Invert	Divided	Invert
Burnup, MWD/T	18,000	18,000	12,000	12,000
Initial enrichment, %	2.5	2.8	2.65	3.12
Final enrichment, %	0.96	0.85	1.60	1.4
Final atom % Pu	0.82	0.82	0.64	0.64
Initial loading UO ₂ , kg	67,050	67,050	13,410	13,410
Initial loading, kg U metal	58,500	58,500	11,700	11,700
Fuel cost, \$/kg U metal ^a	297	350	320	394
Initial fuel cost, \$, including 1% loss	17,375,000	20,475,000	3,749,000	4,612,000
Conversion of UF ₆ to UO ₂ (\$20/kg U)	1,170,000	1,170,000	234,000	234,000
Spent fuel, kg U	57,600	57,340	11,575	11,500
Spent fuel value, \$/kg U ^a	70	56	157	131
Credit for spent fuel, \$	4,061,000	3,225,000	1,822,000	1,509,000
Amount Pu, kg	471	469	74.1	73.6
Credit for Pu, \$10.50/g ^b	4,945,000	4,925,000	778,000	773,000
AEC use charge, 4% ^c	3,475,000	4,095,000	550,000	676,000
	5 yr	5 yr	3- $\frac{2}{3}$ yr	3- $\frac{2}{3}$ yr
Fabrication cost at \$140/kg U (UO ₂ to final assembly) ^d	8,272,000	8,272,000	1,655,000	1,655,000
Interest on capital charges for fabrication at 6% ^e	2,482,000	2,482,000	331,000	331,000
	5 yr	5 yr	3- $\frac{2}{3}$ yr	3- $\frac{2}{3}$ yr
Reprocessing cost to UNH, \$15,300/1000 kg ^f	895,000	895,000	179,000	179,000
Conversion of UNH to UF ₆ , \$5.60/kg U	323,000	321,000	65,000	65,000
Transportation to and from site, \$5/kg UO ₂	335,000	335,000	67,000	67,000

Notes: ^a AEC Semi-Annual Report, July-December, 1956, p. 343.

^b \$12/g less \$1.50/g for processing.

^c 4% per year x [1 + residence time (yr)].

^d AEC Semi-Annual Report, January-June, 1958, p. 405
(Euratom Guarantee) \$140/kg contained uranium.

^e 6% per year x [1 + residence time (yr)].

^f \$25/kg of UO₂; \$25/kg of Zr.

Table 6-2

FUEL CYCLE COST SUMMARY

Item	825 MW(t)		165 MW(t)	
	Divided	Invert	Divided	Invert
Fuel preparation	1,170,000	1,170,000	234,000	234,000
Fabrication and assembly	8,272,000	8,272,000	1,655,000	1,655,000
Interest on capital investment at 6%	2,482,000	2,482,000	331,000	331,000
Core life in years	5	5	$3\frac{2}{3}$	$3\frac{2}{3}$
Fuel burnup				
Value of fuel	17,375,000	20,475,000	3,749,000	4,612,000
AEC use charge	3,475,000	4,095,000	550,000	676,000
Value of spent fuel	4,061,000	3,225,000	1,822,000	1,509,000
Burnup cost	16,789,000	21,345,000	2,477,000	3,779,000
Pu credit	4,945,000	4,925,000	778,000	773,000
Net burnup cost	11,844,000	16,420,000	1,699,000	3,006,000
Transportation to and from site	335,000	335,000	67,000	67,000
Reprocessing spent fuel to UNH	895,000	895,000	179,000	179,000
Conversion of UNH to to UF ₆	323,000	321,000	65,000	65,000
Total costs	25,321,000	29,895,000	4,230,000	5,537,000
Total Mw(e)	7.02×10^6		9.32×10^5	
Cost mills/kw(e)	3.60	4.26	4.54	5.95

SECTION 7

DISCUSSION AND RECOMMENDATIONS

I. General

In the steam plant, all the equipment is commercially available except the recirculation pumps in the size specified. This is a manufacturing problem and should not prove to be a costly design development.

The plant should prove to be a real advancement in the art and provide a great deal of operating and design information, if some of the key ideas incorporated in this proposal are not abandoned. While it is possible that the system specified might not be optimal, even within the boiling water reactor area, most of the nuclear features can be translated into specifications for natural circulation or dual-cycle plants, to name two of the competitive variants.

The objective was to determine the lattice structure which, within the limitations imposed by engineering, heat transfer, and material property requirements, would be capable of a fuel cycle that would yield the greatest number of fissions per U^{235} atom originally loaded into the reactor without reprocessing fuel. To this end, a variety of lattices with different moderator-to-fuel ratios were studied and preliminary estimates of their fuel cycle were made. These studies indicated that the lower the moderator-to-fuel ratio, the better was the fuel cycle. It is clear that this tendency cannot go on indefinitely, but we are not yet past the optimum. On this basis, the lattice selected for further study was the one indicated in the previous paragraphs. Within the limitations imposed by engineering requirements, this is the tightest practicable lattice.

As demonstrated in Section 4, Heat Transfer and Fluid Flow, a statement of maximum power density per unit volume of core and of thermal conductivity and limiting central temperature of fuel immediately defines the (not heat transfer limited) number of rods times reactor height. In this study, therefore, a statement of max/ave power density is the sole physics parameter affecting the fuel cell size. In consequence, the conditions of parametric study of lattice effects involves simple variation of fuel rod radius in a fuel cell of fixed dimensions and in a reactor of fixed buckling.

A definite conclusion of this study is that under these conditions the reactor of highest conversion gives the most favorable fuel cycle. This results from an observation made in the course of determining enrichment to give high burnup: that the initial enrichment to give burnup of the order of 10,000 to 20,000 MWD/t is almost independent of the critical enrichment

of the fresh lattice over a wide range of moderator/fuel ratios. Although moderator to fuel volume ratios in the range of 2 to 3 yield the lowest critical enrichments, the higher conversion ratio characteristic of a tighter lattice just about cancels the reactivity loss in tightening up the lattices, when the consequences of burnup are included.

The merits of the tight, high-conversion lattice relative to a looser, low-conversion one are:

1. Since more fuel is in residence in the core, a given fuel burnup limit involves a larger total reactor output between fuel changes, or permits a larger number of fuel shifts per fuel cycle.

2. For a given energy output of the reactor, a larger fraction of the fissions is due to Pu burned in place; for a given fuel burnup, the value of the spent fuel is higher.

3. Essentially identical fabrication charges are applied to a larger total quantity of fuel.

The demerits are:

1. It makes fuel assembly more difficult.

2. Due to larger in-pile inventory, fuel rental charges are high.

In terms of fuel cycle costs, advantage (2) probably outweighs disadvantage (2), and advantage (3) and disadvantage (1) probably cancel.

It also appears that, for a given reactor volume, a point will ultimately be reached where the above argument fails. This point occurs when under-moderation is so severe that it increases neutron age (and therefore leakage) markedly, and it induces an undue fraction of epithermal fissions. For reactors in this study, mechanical design limitations are reached before this effect occurs.

II. Power Density

The power density requirement leads to a core of small size and to a resultant 9.1% reactivity and production loss in the reference reactor due to leakage of neutrons. An increase in power and size by a factor of five reduces this loss to 3.5%, with a marked decrease in enrichment level and increase in production. Considering only factors related to the fuel cycle, significant improvement in the performance of the reference reactor core is possible by decreasing power density and increasing core size. The savings are almost certain to be enough to amortize the required increase in core vessel size.

For the full-scale reactor, this factor is less certain. A leakage of 3.5% cannot be reduced as drastically, and the amortization of successively larger core vessels may not be possible.

We conclude that the power density demanded is almost certainly too high to be optimal for the reference reactor. It may be close to optimal for the full-scale reactor, although this point ought certainly to be re-examined in further studies on that system. The reference reactor is not recommended, except as a scaled demonstration of engineering feasibility of the larger system.

III. Power Distribution

The source of the poor power distribution lies in the high density of voids at the top of the central radial zone. One method of correcting the distribution is to provide optimum orificing. In this way, one can make flow match power as nearly as possible. Another method is to increase the pumping power. In this case, adverse factors will accrue which are the loss in steam quality and net plant power output. A third method is to diminish the degree of feed-water heating in order to get greater inlet sub-cooling. This decreases plant efficiency, but may simplify layout and recirculating pump design.

For the full-scale reactor core, a substantial improvement in power pattern is expected, the larger size being appreciably more flattened in its radial flux shape. Hence, these steps may not be necessary; or, if they are taken, the lattice can be improved.

The radial power distribution compared closely with that assumed for reactivity effects in fuel management. Since the effect of flux shape is small compared with the quantum effect, the basis of the fuel management calculations is substantially verified. The fuel management for the reference core must, by design stipulation, start from a uniform fuel enrichment. By use of a fuel feed of two different enrichments, the management of fuel could be adjusted to obtain flux flattening.

A disturbing feature of the flux distribution is the high peak-to-average ratio generated by the presence of a water gap between adjacent fuel assemblies. Two immediate possibilities for adjusting this factor are:

1. The design of the fuel elements is such that zirconium spacer plates or wyes could be inserted between elements after they are loaded and removed before they are unloaded. The displacement of 60% of the water in the gap by zirconium would reduce the factor of 1.25 to approximately 1.10. Such spacer plates could also be used as a vehicle for incorporating burnable poison.

2. The outer row of fuel rods in the assembly could be reduced in enrichment by a factor of about 15% with corresponding increased enrichment in the remainder of the rods. With this arrangement there will be a tendency for fissionable material to grow back into the outer rods so that the plutonium content of the spent fuel should be reasonably uniform. In addition, this method would allow the reduction of hot channel factor to apply as well to the fueled portion of the control rods.

IV. Fluid Flow

The core should operate with a mean steam volume fraction of 0.20. Conservative values of slip ratio were used, indicating that the value of 0.20 is probably an upper limit at the design power (165 Mw) and the design flow (30,000 gpm).

If attempts are made to vary the radial flow distribution to get uniform steam voids radially, there will be rather large radial pressure gradients in the core tending to cause cross flow (see Fig. 4-9).

The resistance to cross flow is about seven times as great as the resistance to normal flow. It is impossible to guarantee flow distributions by orificing, since the cross flow cannot be calculated. The close packing of the core will help to channel the flow and any orificing attempts will not be altogether useless.

Except for the effect on the physics of the core, the mean reactor voids for the two cases considered, variable flow and constant flow, are almost identical, 0.209 compared to 0.215. It would, therefore, be more practical from a design viewpoint to try to obtain constant flow rate without orificing. If orifices are used, the mean voids will lie somewhere between the two cases calculated and the pressure gradients will also lie somewhere between the two calculated values.

V. Fuel Cycle and Management

Estimates of fuel cycle costs for the reference reactor indicate that further increases in enrichment to give higher fuel burnups continue to give cost savings up to the level of about 20,000 Mwd/t burnups (a 3.5% enrichment). The fuel cycle recommended is based on a desire to acquire reasonable burnup experience and rapid experience in fuel management. For the full-scale reactor plant, higher burnup level is predicated on this prior experience.

The technique of fuel management is preferable to straight full-core burnup, primarily because it allows the balancing of low-reactivity spent fuel against high-reactivity fresh fuel. Ideally, continuous reloading is preferred because it would eliminate shimming requirements between

fuel shifts. The approximation of quantizing fuel shifts requires reactivity allowances for shimming, which are reduced from the single burnout allowance by a factor approximately equal to $1/n$. Actually, this reduction is somewhat less, because greatest burnout occurs in regions of highest importance. For the four-stage scheme adopted here, the shimming requirement is 0.31 times that needed for complete burnup, rather than 0.25. The ratio of these numbers, 1.24, is approximately preserved as the number of steps is increased.

One problem which was crudely evaluated in this study concerned the difference in reactivity loss between fuel which is reversed (i.e., turned upside down) and inverted (i.e., reversed and then turned inside out from the center). This estimate is quite sensitive to the axial power distribution, which will be discussed subsequently. On the basis of a power distribution that was taken to be a cosine on the bottom half and a straight line on the top half, it was estimated that reversal would require about 30% more reactivity margin than would uniform burnup, and inversion would require about 10%. The 20% difference corresponds to a difference in required enrichment level which would appear to justify the development of invertible fuel. (For the power distributions of Fig. 5-4 and 5-5, the difference is smaller; but for other reasons, this distribution is not desirable.)

Based on enrichment only, the choice of the split-fuel approach should be the unanimous one. The differential in both cases is noticeable, being from 0.7 mill/kw-hr on the full-scale plant core to 1.4 mill/kw-hr on the prototype core. To all intents, the end for end inversion of the fuel element alone accomplishes little, if any, reactivity increase over that which prevails for the pre-inverted case. This is absolutely true if the reactor has an ideal flux distribution. However, in the practical case, an axial skewed flux will no doubt exist and the straightforward comparison may not be accurate. In any event, the high utilization of fuel factor seems to provide a worthwhile advantage even in the present day pricing structure.

VI. Fuel Assembly Design

In a design of this type, compromise is often necessary. It is not possible to argue that all the design ideas presented here for the core construction are absolutely desirable under all conditions. The fuel assembly design is definitely in this category, since reduction to inexpensive and high quality manufacture is required in order to realize its potential economy. It is however, so promising that a strong effort to carry the design into practice is justified.

To check out the idea of the assemblies, a series of simple tests using full-scale dummy assemblies should be carried out by experienced reactor engineers to establish the method by which the "inside-out" core management scheme can be put into practice. The fuel assembly itself is not very long, even in the full-scale plant, and the non-rigid construction should be advantageous.

VII. Control Rods and Drives

The proposed hollow control rod with fuel follower was first used in MTR. The operating feature of drawing the absorber section of the control rod out of the bottom of the core is being used in commercial designs and it seems likely that this requirement for boiling reactors will continue. This type of control rod drive mechanism may be more difficult to design than units which operate with gravity. The advantages, on the other hand, are worth the effort.

While no development work is required for the reference control rod drive, it may be necessary to develop a unit capable of moving a 560-lb load through a 10-ft stroke length for the full-scale plant. The bottom-mounted drive has been chosen to facilitate fuel handling and installation of in-core measurements. Location of the drives at the bottom of the prototype does not mean that top or side-mounted drives would not be desirable for the full-scale plant.

VIII. Shielding

More detailed calculations are necessary to optimize the thermal shield and reflector regions. An increase in thickness of either or both of these regions will decrease the radiation levels shown in Fig. 3-6. An increase in thermal shield or reflector thickness will reduce the capture gamma rays passing from the vessel into the concrete shield to a point where boration of the shield tank will provide an appreciable reduction of the dose rate outside the shield.

SECTION 8

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