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EVALUATION OF ZIRCONIUM HYDRIDE AS
MODERATOR IN INTEGRAL,
BOILING WATER-SUPERHEAT REACTORS

AEC Research and Development Report

ATOMICS INTERNATIONAL

A DIVISION OF NORTH AMERICAN AVIATION, INC.

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EVALUATION OF ZIRCONIUM HYDRIDE AS
MODERATOR IN INTEGRAL,
BOILING WATER-SUPERHEAT REACTORS

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I. INTRODUCTION

This report presents the results of a study conducted by Atomics International for the U.S. Atomic Energy Commission as part of the Nuclear Superheat Program under Contract AT(11-1)-GEN-8, Project 4528. The purpose of the study was to evaluate the merits of using zirconium hydride as a solid moderator in an integral boiling water-nuclear superheat reactor of the pressure vessel type. Several experimental nuclear power plants of this general type are currently being developed, including BORAX-V, BONUS, and PATHFINDER. In all of these, the steam is produced and superheated in separate regions of the same core. The principal advantage expected for this type of plant is a decrease in power costs due to the increased efficiency obtainable with a superheated steam cycle.

A combination of several factors prompted this study, including the fact that the technology of zirconium hydride has recently been accelerated through development efforts in the SNAP program. Methods have been developed whereby fueled zirconium hydride moderator elements having a high hydrogen content have been successfully produced and operated in an experimental, sodium-cooled reactor at temperatures exceeding 1200°F.

Considering its unique properties, it appeared that another promising application for this material would be as a high-temperature, compact, solid moderator in the steam-cooled region of a superheat core. When the study was initiated, most of the concepts being developed in the Superheat Program used light water (at or near saturation temperature) as moderator in the superheater. Thermal insulation was usually required, therefore, to avoid excessive moderator boiling or heat losses from the high temperature steam coolant to the relatively low temperature water. This insulation characteristically took the form of a double-walled, stainless steel process tube, with a stagnant steam gap between the two tubes providing the necessary thermal resistance. Since zirconium hydride can be used at temperatures greater than that of the outlet steam, thermal insulation would not be required. Substitution of zirconium hydride for water was thus expected to decrease the parasitic absorption of neutrons due to a reduction in stainless steel, and to eliminate the heat loss from the sorely won, high-temperature steam. Both of these factors were expected to result in reduced fuel cycle costs.

Another potential advantage foreseen in substituting zirconium hydride for water was the possibility of increasing the power density in the superheat region of the core. The hydrogen density of the metallic hydride in the delta phase, for example, is about 33% higher than that of saturated water at 1500 psi. Thus, for the same degree of moderation (H/U ratio), the moderator volume could be decreased proportionately. In a high-pressure reactor, any reduction in core size should result in a smaller, less expensive pressure vessel.

The chief disadvantages of using zirconium hydride which were recognized at the outset were the relatively high cost of the material, compared to water, and the slightly higher parasitic absorption of the zirconium in ZrH_x compared to the oxygen in H_2O .

In order to assess the relative merits of the two moderator materials on a quantitative basis, conceptual designs were developed for both a water-moderated and a zirconium hydride-moderated reactor. The same assumptions, analytical methods, etc, were used for each design, wherever applicable, to insure consistency; and the cost and performance figures for the two concepts were then compared on a differential basis. This approach is thought to be logical for an evaluation study of this nature since only those portions of the reactor plants which are different due to inherent differences in the two moderators need be investigated in detail. For example, in this study it was found possible to design both reactors to produce the same amount of steam at the same pressure and temperature and with the same overall efficiency. Consequently, all of the major plant items external to the reactor pressure vessels would be essentially identical in design and cost; and for this reason the evaluation was generally limited to those components internal to and including the reactor vessels.

An alternate approach would have been to develop the design and cost data for a complete power plant complex based on an integral, boiling-superheat reactor using zirconium hydride in the superheater and then compare the final costs and performance with similar water-moderated designs being developed elsewhere on an absolute basis. To do this, however, would have entailed a much larger scope of work; and any differences in total power costs due to using zirconium hydride in place of water could easily have been obscured by any one of a multitude of design and cost variables.

As agreed to in the original scope of work, the main effort of the comparative design study was to be placed on reactor plants with a net electrical power

output of 300 Mwe. If an economic incentive for the zirconium hydride moderator concept was found at this power level, a 50-Mwe design was to be evolved to illustrate the characteristics of a possible prototype plant. In addition, the following ground rules were established in order to expedite the study and to limit the design variables and possibilities to those which were felt to be most germane to the evaluation of zirconium hydride for this application:

- a) The general core configuration was limited to that of a central superheat region surrounded by a peripheral boiling region;
- b) The solid moderator was to be used only in the steam-cooled, superheat region of the core;
- c) The design of the boiling region of the core was to be based on existing technology or a reasonable extrapolation thereof; and
- d) The design of the superheat fuel elements was to be based on the use of stainless steel cladding and uranium dioxide (UO_2) fuel.

The results and conclusions based on this study are summarized in the following section of this report.

II. SUMMARY

Conceptual designs were developed for several integral boiling water superheat reactors of the pressure vessel type, in order to obtain a direct economic and technical comparison of zirconium hydride and water as moderators in the superheat core region. The evaluation study was based on plants with a net electrical output of 300 Mwe, but a limited effort was also directed toward the design of a 50-Mwe plant.

Prior to firming up the reference designs, nuclear and thermal parameter studies and material investigations were performed to insure that each material was used to its best advantage. The nuclear characteristics were analyzed using standard methods for the water-moderated regions, but a special code had to be developed to determine the thermal neutron spectrum in the zirconium hydride-moderated core region due to binding energy effects in the hydride lattice. The important findings of these preliminary studies follow.

- a) The hydrogen density in the zirconium hydride should be as high as possible to obtain the maximum benefits from the solid moderator. Metallic $\text{ZrH}_{1.7}$, with an $N_{\text{H}} - 6.0 \times 10^{22}$ atoms/cm³ at design temperatures, was selected, therefore, as the reference material.
- b) The zirconium hydride must be clad to limit hydrogen leakage and for corrosion protection in the high-temperature-steam environment. Stainless steel was the logical (though not ideal) choice for a cladding material; but the austenitic types (300 series) could not be used due to differential expansion between the clad and the zirconium hydride.
- c) A two-pass steam coolant flow arrangement was required in the superheat core to obtain an outlet temperature of 1000°F and a maximum fuel-clad surface temperature of about 1250°F.
- d) The thermal neutron spectrum in a zirconium-hydride-moderated lattice is substantially harder than that obtained in water, with the average thermal neutron energies differing by about a factor of 2.
- e) For UO_2 fuel, the optimum moderator-to-fuel ratio (with respect to reactivity) was found to be 1.5 with $\text{ZrH}_{1.7}$ and approximately 2.5 with H_2O . At these ratios, the maximum k-infinity achievable with

$\text{ZrH}_{1.7}$ is about 7-1/2% lower than that obtained with H_2O for the same enrichment and steel content; but the initial conversion ratio is about 11-1/2% higher.

The major design and performance characteristics for the four 300-Mwe concepts developed are summarized in Table I. The main features distinguishing these designs are as follows.

1. Zirconium-Hydride-Moderated, Moderator Log Concept (ZBSR-ML)

In this concept, the zirconium hydride is installed as a semi-permanent part of the superheat core structure in the form of moderator log assemblies. The superheat fuel elements consist of 7-rod clusters of thin-walled stainless steel tubes loaded with sintered UO_2 powder. The fuel elements are placed in circular flow channels within the moderator log assemblies.

2. Zirconium-Hydride-Moderated, Integral Fuel-Moderator Concept (ZBSR-IFM)

This design represents an attempt to use the zirconium hydride both as a moderator and as a thick-walled, noncollapsing fuel-element cladding material. The moderator is thus an integral part of the superheat fuel element and must be discarded when the spent fuel is reprocessed.

3. Water-Moderated with Stainless Steel Process Tubes (WBSR-STPT)

This design is very similar to the ZBSR-ML concept except that water is used in place of zirconium hydride in the superheat region. The superheat fuel elements are identical to those used in the ZBSR-ML except that the 7-rod clusters are placed in stainless-steel, double-walled, insulated process tubes. The process tubes are an integral part of the superheat fuel element assemblies.

4. Water Moderated with Zircaloy Process Tubes (WBSR-ZrPT)

This concept is identical to "C" except that the outer pressure tube of the process tube assembly is composed of Zircaloy-2 instead of stainless steel.

All of the designs use a core configuration consisting of a central superheat region surrounded by a peripheral boiling region. The boiling regions are cooled by forced circulation and the steam is separated from the water inside the reactor pressure vessel with the aid of mechanical steam separators.

The average specific power in the boiling and superheat regions of each core studied was made equal in an attempt to achieve a uniform burnup and thus avoid

gross power shifts between the two regions. Thus, the thermal performance is essentially the same for all of the reference designs except for slight differences in maximum fuel temperatures. The average power densities in the superheat region of the zirconium-hydride-moderated designs are substantially higher than that obtained in the water-moderated superheater, but the resulting decrease in pressure vessel diameter is small.

The initial enrichments quoted for each case are those required to obtain a k-effective of 1.15 in the hot, boiling core and the proper initial power split between the two core regions. The fuel cycle costs for the reference designs were calculated on the basis of a batch refueling cycle and an average irradiation exposure of 10,000 Mwd/MTU in each core region. The results, rounded off to three figures, are summarized below:

<u>Reactor Concept</u>	<u>Fuel Cycle Costs (mills/kwhr)</u>
Zr-Hydride Moderated, Moderator Log	3.27
Zr-Hydride Moderated, Integral Fuel-Moderator	3.65
Water-moderated with Stainless Steel Process Tubes	3.43
Water-moderated with Zircaloy Process Tubes	3.39

It will be noted that the spread in fuel cycle costs is small but the zirconium-hydride-moderated, moderator log concept enjoys 0.12-mills/kwhr advantage over the best water-moderated design. This difference, however, is due entirely to the higher fabrication cost of the water-moderated, superheat fuel elements which include the process tubes. The difference between concepts "A" and "C" is 0.16 mills/kwhr, of which approximately 0.1 mills/kwhr is due to higher fabrication costs for the water-moderated design. The neutron economy of the integral fuel-moderator concept is better than that of the other three designs; but its fuel cycle costs are higher, due to the extremely high fabrication costs estimated for this type of element ($\sim \$250/\text{kgU}$).

A comparison of the capital costs for the zirconium-hydride-moderated, moderator log concept and the water-moderated designs results in a direct-cost

penalty of 0.145 mills/kwhr for the ZBSR, assuming a capital charge of 14% per year and an 80% plant factor. This cost penalty is due primarily to the high cost of the canned zirconium hydride moderator log assemblies. If the indirect costs are added as a fixed percentage of the direct costs, the capital-cost penalty increases to about 0.22 mills/kwhr. Thus, a net cost disadvantage of approximately 0.1 mills/kwhr is estimated for the "best" zirconium-hydride-moderated design compared to the "best" water-moderated concept.

Although design modifications have been thought of which could improve the performance of both concepts, these generally favor the water-moderated design. It is concluded, therefore, that an economic incentive for substituting zirconium hydride for water as moderator in this type of reactor does not exist. The technical problems encountered with the zirconium hydride are also considered to be more severe than those in the water-moderated design, although the questionable integrity of the thin-clad, superheat fuel elements is a major problem shared by both concepts.

TABLE I
SUMMARY OF DESIGN AND PERFORMANCE DATA FOR REFERENCE DESIGNS

Reactor Concept	A	B	C	D
	Zr-Hydride-Moderated		Water-Moderated	
	Moderator Log	Integral Fuel Moderator	St. Steel Process Tubes	Zircaloy Process Tubes
<u>Plant Data</u>				
Net Electric Power (Mwe)	300	300	300	300
Gross Thermal Power (Mwt)	778	778	778	778
Net Efficiency (%)	38.5	38.5	38.5	38.5
Throttle Steam Pressure (psig)	1450	1450	1450	1450
Throttle Steam Temperature (°F)	1000	1000	1000	1000
Steam-flow Rate (10^6 lb/hr)	2.45	2.45	2.45	2.45
Water-flow Rate (10^6 lb/hr)	32.4	32.4	34.73	34.73
<u>Boiling Region</u>				
Equivalent Diameter (ft)	9.04	9.06	9.56	9.56
Active Height (ft)	11.25	11.0	11.0	11.0
Thermal Power (Mw)	540	540	525	525
Average Power Density (kw/l)	40	40	40	40
Structural Material	Zr-2	Zr-2	Zr-2	Zr-2
Fuel Material	UO ₂	UO ₂	UO ₂	UO ₂
Moderator-to-fuel Volume Ratio	2.5	2.5	2.5	2.5
Fuel Loading (kg U)	28,400	28,400	27,600	27,600
Average Specific Power (kw/kgU)	19.0	19.0	19.0	19.0
Initial Enrichment (at. %)	2.53	2.47	2.64	2.59
Initial Conversion Ratio	0.575	0.580	0.562	0.564
<u>Superheat Region</u>				
Equivalent Diameter (ft)	5.25	5.20	6.08	6.08
Active Height (ft)	11.25	11.0	11.0	11.0
Thermal Power (Mw)	238	238	253	253
Average Power Density (kw/l)	35	39	28.0	28.0
Structural Material	St. Steel	St. Steel	St. Steel	St. Steel + Zr-2
Moderator	ZrH _{1.7}	ZrH _{1.7}	H ₂ O	H ₂ O
Fuel Material	UO ₂	UO ₂	UO ₂	UO ₂
Moderator-to-fuel Volume Ratio	1.5	1.5	2.35	2.35
Fuel Loading (kg U)	12,500	12,500	13,300	13,300
Average Specific Power (kw/kgU)	19.0	19.0	19.0	19.0
Initial Enrichment (at. %)	3.39	2.79	3.50	2.62
Initial Conversion Ratio	0.548	0.639	0.475	0.530

III. DESIGN DISCUSSION

A. PROPERTIES AND CHARACTERISTICS OF ZIRCONIUM HYDRIDE

1. The Hydrogen-Zirconium System

Hydrogen and zirconium form interstitial alloys¹ as shown in the phase diagram, Figure 1. The alloy composition, as in any alloy system, is temperature- and pressure-dependent. In this case, since one component is a gas, the pressure dependency is particularly accentuated. Although the hydride is stable at moderate temperature, it dissociates to hydrogen and zirconium at higher temperatures. The relationship between dissociation pressure, temperature, and hydrogen/zirconium ratio² is given in Figure 2.

Figure 1. Zirconium-Hydrogen Phase Diagram

Figure 2. Dissociation Pressure Isochores of Zirconium Hydride

The hydriding of zirconium is not basically a difficult operation. It requires only that the clean metal be heated in an atmosphere of pure hydrogen, the alloying being a simple diffusional operation. Temperature and pressure must be controlled to obtain the desired final composition. Since hydriding is a diffusion process, the thickness of the zirconium is one hydriding rate determinant. It has been found that one dimension of the piece to be hydrided should not exceed approximately 2 in.

2. Density

The addition of hydrogen to zirconium results in a decrease in the room temperature density. Table II gives densities calculated from x-ray data; bulk density of massively hydrided zirconium has been found to be generally 2% lower than the x-ray density.³

TABLE II
ROOM TEMPERATURE X-RAY DENSITY OF ZIRCONIUM HYDRIDE

H/Zr	0	1.54	1.81	1.87	1.90	1.94
Density, gm/cm ³	6.49	5.66	5.62	5.61	5.59	5.46

The hydrogen density (N_H) of the metal hydride in the delta phase increases with the H/Zr ratio as shown in Figure 3. It will be noted that the N_H of pure ZrH_2 is about 7.0×10^{22} atoms/cm³; and the H/Zr ratio of the hydride with an N_H of 6.0×10^{22} atoms/cm³ (at operating temperatures) is about 1.70.

3. Thermal Conductivity

Standard techniques for measuring thermal conductivity of a metal, and its temperature dependence, are not wholly accurate for zirconium hydride because of its tendency to lose hydrogen at elevated temperatures during the test; and because of the hydrogen redistribution within the sample caused by the temperature gradient required by the test. Therefore, no attempt has been made to show variation of thermal conductivity with hydrogen content or temperature. An arithmetical average value for the range 200°F to 1200°F for hydride with an N_H of 6 is given² as 13 ± 1 Btu/hr-ft-°F, where zirconium tested similarly gave values of 10-12 Btu/hr-ft-°F from 200°F to 900°F.³

Figure 3. Hydrogen Density (N_H) vs H/Zr Ratio for Zirconium Hydride at Room Temperature

4. Thermal Expansion

Data for crystal-bar zirconium and the delta-phase hydride are given in Table III.⁴ It is evident that the addition of hydrogen increases the thermal expansion significantly.

5. Elastic Modulus

The dynamic elastic modulus of zirconium hydride with an H/Zr ratio of 1.6 was measured at room temperature as 9.1×10^6 psi.⁵ This value is expected to decrease with increasing temperature to perhaps two-thirds its room temperature value at 1200°F.

6. Ultimate Tensile Strength

The ultimate tensile strength of zirconium hydrides with H/Zr ratios in the range of 1.7 to 1.8 was found to be 8000 to 10,000 psi both at room temperature and at 1100°F; and both by standard tensile tests and transverse rupture tests.³ Later experiments with hydrided zirconium alloys containing small

TABLE III
THERMAL EXPANSION

Material	Total Expansion from Room Temperature (mils/in.)					
	200°F	400°F	600°F	800°F	1000°F	1200°F
Crystal-Bar Zirconium	0.12	0.85	1.70	2.52	3.40	4.45
Zirconium Hydride, H/Zr = 1.70	0.33	1.17	2.30	3.59	5.11	6.88

amounts of grain-refining elements show transverse rupture strengths twice as high; results on samples in the H/Zr range of 1.8 to 1.9 are 20,000 psi at 800°F.⁶ Work on these alloys is continuing.

7. Compressive Strength

For zirconium hydride with H/Zr ratio of 1.75, a compressive strength of 19,000 psi was measured at room temperature, with the compressive yield strength being 13,000.⁶ Again, these strengths are expected to increase by a factor of two or three as grain-refining alloy additions are made.

8. Hydrogen Retention

As noted previously in Figure 3, the hydrogen pressure in equilibrium with a particular H/Zr ratio varies with the temperature in an Arrhenius relationship. At the operating temperatures expected for the present application, the dissociation pressure is such that the moderator would soon lose its hydrogen and its effectiveness without cladding to contain the hydrogen evolved and stop the dissociation. This problem is discussed in more detail in Section III, B.2 of this report.

B. SPECIAL CONSIDERATIONS IN USING ZIRCONIUM HYDRIDE IN AN INTEGRAL BOILING-SUPERHEAT REACTOR

1. Configuration

In the course of developing a reference design of a zirconium-hydride-moderated, integral boiling superheat reactor, several different configurations were explored in an effort to obtain the best overall design. Early in the study, however, the decision was made to limit the use of zirconium hydride to the superheat region of the core. The reason for this is that there is no apparent advantage in using the solid moderator in the boiling region where water serves the dual role of coolant and moderator. To displace part of the coolant water would, among other things, require closer fuel-rod spacing and increase the coolant pressure drop. An exception to this argument might be the case for a small, special purpose reactor wherein zirconium hydride may be used as a diluent for enriched fuel. A large increase in power density may thus be achieved which could overshadow the disadvantages mentioned. In the present case, however, where a low fuel enrichment is required to obtain low fuel cycle costs, the only logical application for the solid moderator was felt to be as a substitute for water in the steam-cooled, superheat region of the core.

Another item which may at first appear to be arbitrary is the choice of a centrally located superheater surrounded by a peripheral boiling region (as opposed to a peripheral superheater with a central boiler or perhaps a mixed lattice). Recent studies have shown that, for a large water-moderated reactor, power costs are essentially insensitive to the relative locations of the two core regions as long as the design is optimized in each case.⁷ There is reason to believe, however, that the configuration chosen is best for the zirconium-hydride-moderated design. If the relative location of the boiler and superheater regions were reversed, the radial peak-to-average power density ratio in the superheater would increase due to the rapid fall-off in flux that occurs at the edge of a core when the peripheral region has a higher absorption cross-section than the central region. This would increase the size of the superheat region; and since zirconium hydride is fairly expensive, any increase in moderator inventory would result in a capital cost penalty. Also, the configuration chosen results in a more logical mechanical arrangement and flow pattern for the cooling water which is recirculated through the boiling region.

Having limited the investigation to the superheat region and selected the relative location of this region in the core, there are still several plausible alternatives for the configuration of the solid moderator. The zirconium hydride may either be incorporated as an integral part of the fuel element assembly or installed as a "permanent" part of the reactor. The first approach is particularly interesting if the hydride can also be used as the fuel-cladding material, since a reduction in steel content may thus be achieved. Any improvement in neutron economy (and thus fuel-cycle costs) so obtained, however, must be sufficient to overcome the increased costs which result from disposal of the moderator along with the spent fuel. Since the answer to this question depends on a careful evaluation of the fuel cycle economics, a conceptual design of a core using an integral fuel-moderator element was developed to the point where sufficient cost data could be obtained. This concept is described in Section IV.C of this report. The second approach, wherein the zirconium hydride is installed as a permanent or semipermanent part of the core structure, has the advantage that the cost of the moderator may be amortized over a much greater period of time. The penalty due to the higher cost of zirconium hydride relative to water may thus be minimized. This approach resulted in the "moderator log" concept which is described in Section IV.A.

An alternate configuration employing a calandria was also considered but discarded for the following reasons:

- a) If the moderator assembly were damaged in any way after sustained operation, removal of the calandria for repair or replacement would be extremely difficult and costly.
- b) Both axial and radial migration of the hydrogen in the moderator from hot to cold regions would require subdivision of the calandria to the point where it would resemble an assembly of moderator logs.

Although there is still a large number of variations possible in the design of the moderator logs, the configuration developed for the reference design represents an attempt to achieve the minimum zirconium hydride inventory and steel volume fractions consistent with other design requirements in the superheat core.

2. Moderator Cladding

One of the major factors affecting the design and feasibility of using zirconium hydride as moderator in the steam-cooled region of this type of reactor is the need for cladding the zirconium hydride. This is required both to protect the moderator material from corrosion in the high temperature steam environment and to avoid or limit the leakage of hydrogen. Both effects are temperature-dependent, but in the temperature range of interest ($\sim 1000^\circ\text{F}$) the need for cladding is clear. Although no specific experimental data on the corrosion of zirconium hydride in steam have been found, there is no reason to believe that the oxidation rate would be any lower than that of pure zirconium, which is prohibitive.

Stainless steel is a logical, though not ideal, choice for corrosion protection, but steel alone is not sufficient to eliminate hydrogen leakage. This fact is demonstrated in Figure 4, which shows the initial hydrogen loss rate expected due to diffusion through a 10-mil-thick stainless steel cladding as a function of the temperature at the clad-hydride interface. The relationship shown is for zirconium metal hydride with an initial hydrogen density of 6×10^{22} atom/cm³ and a surface-to-volume ratio of 1 cm²/cm³, which is close to that used in the reference designs. If the moderator is to be used (and amortized) for any reasonable period of time, it becomes obvious that the thin steel cladding alone is inadequate. Fortunately, enamel-like coatings have been developed

Figure 4. Hydrogen Loss Rate Through 10-Mil-Thick Stainless Steel Moderator Can

which provide a satisfactory solution to this problem. The coating is applied directly to the inner surface of the cladding, since the curing temperature is too high to permit direct application on the metal hydride. Although this procedure complicates the manufacturing process somewhat, the hydrogen leakage is reduced by several orders of magnitude.

Thus, the metal cladding is required as a support surface for the diffusion barrier, and the choice of clad material and thickness are dictated primarily by corrosion considerations. In this respect, it is important to note that the thickness must be such that protection is assured for a period of 20 or 30 years if the moderator is to be a semi-permanent part of the reactor.

Another item which must be considered for this application is the fact that the high system pressure and temperature will cause the thin steel cladding to collapse onto the moderator material. If this were to be avoided, the moderator enclosure would have to be designed as a high-temperature pressure vessel and the resultant steel volume fractions would be positively prohibitive, from a nuclear standpoint.

Although the collapsing process does have one virtue in that adequate moderator cooling is assured due to the good thermal bond that results between the hydride and the clad, the mechanical problems which arise are fairly severe. Close dimensional tolerances are required to limit the total strain on the clad, particularly at joints. One solution might be to preheat each assembly in an

autoclave, slowly apply pressure, and depend on high temperature creep to provide uniform collapse. Assuming that this could be done successfully, a problem would still exist during operation due to differential expansion under conditions of thermal cycling. The frictional forces between the clad and the moderator require that the cladding material have about the same coefficient of expansion as the zirconium hydride if excessive stretching or wrinkling of the clad is to be avoided. This eliminates the choice of austenitic stainless steels and narrows down the selection to one of the ferritic or martensitic (400 series) stainless steels. The integrity of the ceramic diffusion barrier under these conditions may also be questioned, but the coefficient of expansion of this material can be adjusted to match that of the clad; and at operating temperatures it is reported to be fairly plastic.

From this discussion, it may be seen that the purely mechanical problems encountered in cladding zirconium hydride for use in a high temperature, steam-cooled core are certainly more severe than would be encountered in a low pressure or a low temperature environment.

3. Metal vs Powder

Another possible choice open to the designer when considering zirconium hydride is that of using the material either in powdered or metallic form. For the present application, thermal and structural requirements dictated the selection of the metallic hydride. The moderator material must be relied upon to support the loads caused by system and differential pressures that exist in the core, otherwise the steel cladding thickness would have to be increased considerably. Although the properties of the powder are not well known, its thermal conductivity and strength are reported to be considerably less than that of the metal. Use of powder would thus aggravate what is already a fairly difficult thermal-stress and hydrogen-migration problem in the metallic hydride moderator assemblies.

4. Hydrogen Density

Unlike most moderator materials, zirconium hydride offers the possibility of selecting from a wide range of moderator atom densities. In order to realize the potential advantage of high power density, it was felt that the hydrogen density (N_H) for this application should be as high as practical considering such factors as producibility, lattice spacing, and physical properties. Another

factor favoring the use of a high N_H material is the nuclear advantage that comes with decreasing the number of zirconium atoms per hydrogen atom. The total parasitic absorption cross-section per hydrogen atom of $ZrH_{1.7}$, for example, is about 30% higher than that of H_2O ; and at lower H/Zr ratios, the relative absorption increases. Also, inspection of the phase diagram for ZrH_x as shown in Figure 1 reveals a phase change at $\sim 1020^\circ F$ for all but the low N_H alpha region and the hydrogen-rich delta region. In a high temperature, superheat core, cycling through this boundary could lead to serious design problems, since the phase change is accompanied by a volume change of about 1/2%. These considerations limit the selection to a zirconium hydride which is safely into the delta phase (i.e., with a H/Zr ratio greater than ~ 1.5).

The upper limit on hydrogen density was set by the fact that satisfactory production runs (without prohibitive scrap rates) have been accomplished for material with an N_H up to about 6×10^{22} atom/cm³ ($ZrH_{1.7}$); but extrapolation beyond this point cannot be made with confidence, based on current technology. Thus, the choice was narrowed down to a material with a H/Zr ratio lying between 1.5 and 1.7. Over this range, there is little to choose from as far as mechanical properties are concerned. The material is brittle throughout the design temperature range, with the ultimate and yield strength being identical and equal to about 10,000 psi. The thermal conductivity is believed to improve with increasing hydrogen content, however; and for this reason, in addition to the other advantages cited previously, zirconium hydride $ZrH_{1.7}$ with an $N_H = 6 \times 10^{22}$ was selected as the reference material for this study.

5. Thermal and Migration Stresses

A design problem common to most solid moderators which is not encountered with liquids is that of thermal stress within the moderating media. A complete analysis of this problem requires an accurate prediction of the volumetric heat generation rates, the resultant temperature distributions, and finally, the stresses which result from the differential expansion due to the temperature gradients. The problem is aggravated by the relatively large thermal path lengths and the irregular geometries which are usually encountered in a typical moderator assembly. The brittle nature of zirconium hydride makes it particularly susceptible to thermal stress, since the material cannot yield to relieve the stress as would be the case for a more ductile material. Again, if the moderator is to be depended upon to support at least part of the loads encountered in the core, this fact must be considered in the design.

In the case of zirconium hydride, another phenomenon occurs which is similar to that discussed above but caused by the migration of hydrogen. When subjected to a thermal gradient, the hydrogen migrates from the hot to the cold region, causing the cold region to expand and the hot region to contract. This results in a "migration stress" which is in the opposite sign (or direction) of the thermal stress. Since this process is time-dependent, a temperature gradient in zirconium hydride will first cause a thermal stress which is slowly relieved by hydrogen migration. The final stress on the material will be that due to the superposition of the two phenomena, with the equilibrium migration stress believed to be somewhat larger in absolute magnitude. Consideration of these two inter-related problems, which are discussed in more detail in Section V.C, imposed the following limitations on the reference design of the zirconium hydride moderator log concept.

- a) The design was limited to one pass of the steam coolant per log to avoid the large thermal gradients which would otherwise result between adjacent flow channels at the inlet-outlet end if two passes were used within individual logs.
- b) Internal diffusion barriers were required in each log to avoid gross migration in the axial direction.
- c) The moderator logs could not be immersed in the relatively cold water, and the outer cans on the logs had to provide some degree of thermal insulation from the control rod coolant.

The last item requires that the superheat control rods be steam-cooled, and that the superheat region be separated from the boiling region by a suitable flow baffle.

6. Nuclear Characteristics

Although zirconium hydride is similar to water in that both are hydrogenous moderating materials, the former has several unique nuclear characteristics which affect the design and analysis of a zirconium hydride moderated core. These will be discussed in greater detail in Section V.B, but some of the major factors affecting the design will be mentioned briefly here.

The first item to be considered is the fact that the thermal neutron spectrum in zirconium hydride is "harder" than that obtained in water due to

binding-energy effects. This results in the need for a higher fuel enrichment for the same reactivity, primarily because of increased capture in the fast and resonance energy range. The increased enrichment is compensated by an increase in conversion ratio, however, and with current fuel prices and practices, the net effect on fuel cycle burnup costs is small.

A more significant factor, perhaps, is the fact that the optimum moderator-to-fuel volume ratio (with respect to reactivity) for zirconium hydride is substantially lower than that for water, due partly to the higher absorption cross-section per hydrogen atom and primarily to the higher hydrogen density of the metal hydride. For example, the optimum moderator-to-fuel volume ratio for a small diameter UO_2 fuel rod is about 1.5 for $\text{ZrH}_{1.7}$ as compared to 2.5 for H_2O at 600°F. Although it is known that the fuel cycle costs in a water-moderated, superheat core are relatively insensitive to this parameter (due to several compensating factors), the economic optimum for zirconium hydride would undoubtedly be obtained with an undermoderated lattice because of the relatively high cost of the material. However, in the case of the moderator log design, where the fuel is set in separate flow channels within the log, it becomes extremely difficult from a mechanical standpoint to decrease the lattice spacing below the nuclear optimum unless a large number of fuel rods are clustered within each channel. This alternative leads to flux depressions within the fuel bundles and flux peaking in the moderator, which is particularly undesirable since the moderator has a fairly high absorption cross-section.

7. Cost

Certainly one of the most important factors which must be considered in designing a zirconium hydride moderated superheat core is the cost of the material. As is the case with most manufactured products, however, it is not possible to assign a fixed value to zirconium hydride on a dollar per pound basis without considering the specific configuration in question. The cost of nuclear grade zirconium metal before hydriding, canning, etc, can range from about \$10/lb in bulk form up to approximately 50 to 60 \$/lb as extruded, thin-wall tubing. The final machining process for hydrided zirconium requires grinding, due to the hard, brittle nature of the material; and the zirconium metal must therefore be approximately the same size and shape before hydriding as the desired finished product. Thus, the cost of the base material depends directly on the design configuration; and for this reason, several alternate geometries

were investigated for both the moderator logs and the integral fuel moderator elements before selecting those used in the reference designs. The final cost figures for these configurations are discussed in Section VI.

Another cost factor, over which the designer has some degree of control (besides geometry) is the actual moderator inventory. Since at least a part of the final cost is proportional to the total volume of the material, there is obviously an economic incentive for minimizing the amount of zirconium hydride used. For a given moderator-to-fuel ratio, the moderator inventory depends directly on the fuel inventory, and this puts greater emphasis on high specific power as a design goal. The specific power can usually be increased by decreasing the fuel rod diameter, but in the present application this approach is limited by heat transfer and mechanical design considerations. Here again, the reference designs represent a compromise, although in general a fairly high specific power was achieved.

C. OTHER MAJOR DESIGN CRITERIA, ASSUMPTIONS, AND PHILOSOPHY

1. Choice of Steam Cycle and Conditions

The primary justification for adding nuclear superheat to a boiling water reactor is to increase the thermal efficiency in an attempt to reduce nuclear power costs. For this study, the choice of a direct steam cycle with superheat is felt to be consistent with this goal as well as being a practical necessity for an integral, boiling-superheat reactor of the pressure vessel type. Use of an indirect cycle would require additional heat-transfer equipment, and would result in degraded steam conditions at the turbine throttle, thus lowering the efficiency. Direct reheat of intermediate pressure steam cannot be obtained with this type of reactor without introducing relatively thick pressure tubes within the pressure vessel. Other variations in the thermodynamic cycle might be conceived which could possibly improve the total plant performance somewhat, but these would not be expected to affect the comparison of moderator materials for the superheat core.

Similarly, it was felt that the selection of steam temperatures and pressures should be representative of plausible design goals for this type of reactor, if not necessarily optimum. Although there is currently some controversy over what may be the best steam conditions for an integral boiling-superheat reactor, the trends in the evolution of modern conventional power plants would suggest

that the steam temperature be as high as modern turbine technology permits ($\sim 1100^{\circ}\text{F}$). This was reduced to 1000°F , however, in consideration of the current status of superheat fuel element technology.

The comparison of zirconium hydride with water is a little more sensitive to the selection of system operating pressure, since the hydrogen density of saturated water varies with pressure. Recent studies have shown that the optimum pressure for a current status, forced circulation, boiling water reactor using external steam separation is about 1000 psi.⁸ Although the cycle efficiency increases with turbine throttle pressure, this gain is compensated by increased capital costs. When this type of reactor was combined with a separate superheat reactor, the same conclusion was reached even though the optimum pressure for the superheat reactor was higher because most of the thermal power comes from the boiling reactor.⁷ In the present case, where boiling, superheating, and steam separation all occur within the same vessel, it is believed that the optimum steam pressure would shift upward, since some of the large, high pressure, capital cost items such as the steam drum, risers, and associated shielding have been eliminated. For this reason, a turbine throttle pressure of 1450 psig was selected for this study.

2. Reactor and Pressure Vessel Size Limit

The maximum electrical capacity of the reactors considered in this study was set at 300 Mwe in order to provide a basis for comparison with other designs developed under the AEC power reactor program. For the 300-Mwe plants studied, the size of the reactor pressure vessel is very close to the "limits" of current technology.⁸ These limits, which are set by a requirement for competitive shop fabrication and stress annealing by at least three possible vendors, result in a maximum OD of approximately 16-1/2 ft including nozzles; and/or a maximum weight of approximately 300 tons, not including heads. Both of these requirements are satisfied in the reference designs, if the ID of the vessel is held to approximately 11-1/2 ft. It is assumed that the vessel would be barged to the plant site.

3. Steam-Water Separation and Seals

An economic incentive exists for separating the steam which is generated in the boiling region of the core from the water inside the reactor pressure vessel, since the cost of a steam drum, interconnecting piping, and associated

shielding may thereby be eliminated. It became apparent early in the study of the 300-Mwe designs, however, that the area available for gravity separation at the surface of the water would be inadequate unless the pressure vessel diameter and weight were increased beyond the limits mentioned above. The steam would carry over-excessive moisture, and bubble entrainment ("carry under") in the recirculated coolant water would undoubtedly be a problem.

Dry steam is required at the inlet to the superheater to protect the superheat fuel elements from possible erosion and crud buildup. If, for example, as much as 1% moisture were carried over with the steam and if the water purity was such that it contained 1 ppm of dissolved solids, then approximately 300 lb of crud would be deposited on the fuel elements during their lifetime in the core. Obviously, the water purity and the moisture content must be much better than this if serious fouling is to be avoided.

These problems are well understood and appreciated by others working in the Nuclear Superheat program, and development efforts are currently proceeding on several types of mechanical steam separators. For the purpose of this study, therefore, it was assumed that this development work will be successful; and mechanical separators were incorporated in the upper extension of the boiler fuel element assemblies as a hydrodynamic "black box." Steam dryers were also used to reduce the moisture content to tolerable limits, and underwater mechanical joints or seals were avoided in all of the reference designs.

4. Radiation Damage to Pressure Vessel

Another assumption that had to be made, if a 300-Mwe design was to be developed within the framework of current pressure vessel technology, concerns the problem of radiation damage to the vessel. Harrer's discussion of this problem⁹ suggests that the total exposure of carbon steel to fast neutrons with energies greater than 1 Mev be limited to $\sim 10^{19}$ nvt. Although this level of exposure is admittedly conservative, due to both the lack of sufficient experimental data and the proper translation of radiation effects into design practices, it has recently been applied as a ground rule for current AEC reactor evaluation studies.

Preliminary calculations, however, indicate that approximately 2 ft of water would be required between the edge of the core and the vessel wall in order to reduce the fast neutron dose to 10^{19} nvt for the reference designs studied.

Even with an average power density of 35 to 40 kw/l, a net thermal efficiency of 38%, a core length-to-diameter ratio of 1.25, and a vessel ID of 12 ft, the maximum power output possible from a single, integral superheat reactor of this type would be approximately 200 Mwe, if this limit on radiation exposure is observed. It becomes obvious, therefore, that the assumptions of a single 300-Mwe reactor, a pressure-vessel size based on current technology, and a maximum integrated neutron dose of 10^{19} nvt are mutually incompatible. For the present study, it was felt that the last restraint was the least important to the comparison of superheat moderators and also the one most subject to being changed in the near future. Since it is noted that several large reactors currently in operation do not feature more than about a foot of water between the core and the vessel, it may be surmised that additional information on this problem should be available soon.

5. Flooding

One of the new operational problems expected for an integral boiling-superheat reactor concerns flooding of the steam passages in the superheat core region with water. This would be done as a normal procedure in refueling and might occur by accident during operation. In theory, at least, the reactivity change caused by this perturbation can be minimized by proper design of the superheat lattice. If the moderator-to-fuel ratio is chosen so that it is nearly optimum with respect to reactivity, the flooding coefficient would generally be expected to be negative. Where the superheat fuel element consists of a cluster of rods, the size of the cluster may also be chosen so that the increase in resonance capture upon flooding is greater than the increase in thermal utilization. These two approaches were used in developing the reference designs to minimize the magnitude of the reactivity swing due to accidental flooding, and to assure that the coefficient would be negative in sign. Other than this, however, flooding was not considered to be a primary design parameter. That is, no attempt was made to insure that the reactivity change would be less than a dollar, or any other specific number. Similarly, the reactivity change due to draining the superheater at startup was not felt to be of fundamental importance, since this can probably be done slowly enough to be compensated by moving the control rods.

It is believed that the design approach or philosophy regarding the flooding problem will probably follow the pattern that has been observed with boiling water reactors, where initial concern over investing several percent reactivity

in steam bubbles has given way to near-complacency as satisfactory operating experience has been accumulated. The real criterion would appear to be the total response of the entire core to a flooding accident, which involves complex interactions between many hydraulic, thermal, and nuclear parameters. Needless to say, the scope of such an analysis was felt to be beyond that required for this study.

6. Decay Heat Removal

Another operational problem which may affect the design of an integral boiling-superheat core is that of removing decay heat from the steam-cooled core after shutdown. Several design groups feel that the superheat fuel elements should be designed so that the decay heat may be transferred to the moderator by radiation and conduction through the insulated process tube to the water moderator. While this degree of conservatism may be justified for first generation experimental reactors, it also eliminates many fuel-element geometries that may otherwise be of interest, such as multiple concentric cylinders or clustered rods. In the present study, therefore, it was assumed that post-shutdown cooling would be accomplished by maintaining adequate steam flow through the superheat core. In principal, this should be possible by by-passing steam from the main steam line to a suitable condenser, and maintaining the water level in the reactor with the system feed pumps. As the reactor water temperature decreases, the system pressure would slowly drop; but by the time it reaches atmospheric pressure, it should be possible to either raise the reactor water level and flood the superheat core, introduce an emergency steam supply, or inject water spray cooling. It should be mentioned that, while this general method of cooling is believed to be feasible, it is also a necessity for the zirconium hydride moderated design; whereas, the water-moderated core can be designed so that radiation cooling would provide an additional margin of safety.

7. Power Split and Fuel Management

One of the most severe operating limitations of the integral boiling-superheat reactor is thought to be the problem of matching the burnup characteristics of the two core regions. This problem differs from that of most two-region cores in that the power split between the boiling and the superheat regions is dictated by the thermodynamic cycle and must remain constant throughout core life.

During the early part of the fuel cycle, the correct power split can be maintained by manipulating the shim rods which would normally be inserted in the core to hold down the reactivity required for burnup. If one region loses reactivity faster than the other, however, an increasing fraction of the shim control must be shifted to the more reactive zone until eventually it would contain all of the remaining shim control poison. At this point, the reactor would have to be shut down and refueled, since further withdrawal of the shim rods would unbalance the power split. The excess reactivity held down by this poison would normally be available for additional burnup in a one-region core, and thus this mode of operation would increase the fuel cycle costs of the integral boiling superheat reactor.

The problem, then, is to try to match the nuclear characteristics of the two core regions so that the proper power split is obtained at the end of the fuel cycle period. From a design standpoint, this is easier said than done, however, since present analytical techniques for predicting burnup phenomenon are marginal at best. In this case, the problem is complicated by such factors as the nuclear coupling between the two core regions, the effects of the poison distribution on conversion ratios, dissimilar compositions, enrichments, conversion ratios, spectra and flux levels in the two regions, axial variations in power and void distribution, etc. To predict accurately the initial enrichments in each region necessary to obtain the proper final power split would thus require an iterative analysis using a three-dimensional burnout code with a poison search routine to simulate the proper control rod distribution throughout life (and maybe a side routine to calculate void distributions as well). Although computer codes approaching this degree of sophistication are being developed, the scope of such an analysis could easily exceed that of this entire study.

The approximate methods used for this study, therefore, were to adjust the initial enrichment in each region to obtain the proper power split at the beginning of core life, and to match the specific power in each region (thermal power per kilogram of uranium) in an attempt to achieve similar burnup characteristics. This approach was recommended on the basis of burnup studies conducted for similar cores.¹⁰ The initial k_{eff} was set at 1.15 (hot, boiling) for all designs studied, which is approximately the value that would be required to carry a similar one-region core to the average exposure selected - 10,000 Mwd/MTU. Batch refueling of both regions at the same shutdown period was assumed, since

it is not at all obvious that other methods of fuel management could be employed with this type of reactor. The possibilities for radial reshuffling are obviously limited, since the boiling and superheat elements are not interchangeable and the radial depth of each region is smaller than would be the case with a one-region core. Axial reshuffling of the boiler elements might be feasible, but again, this would be limited by the power split requirements. A more detailed discussion of the methods used to analyze the burnout characteristics is included in Section V.B.

8. Superheat Fuel Elements

A major uncertainty currently exists regarding the feasibility and integrity of steam-cooled, superheat fuel elements. In order to superheat steam in a reactor core to temperatures of 900 to 1000°F, the maximum fuel cladding temperature is typically in the range of 1200 to 1300°F. At these temperatures, a thin cylindrical, stainless steel cladding subject to the reactor system pressure will collapse onto the fuel, primarily due to creep. If the cladding is made thick enough to be free-standing, the additional parasitic absorption in the superheat region will increase the fuel cycle costs to the point where the economic incentive for nuclear superheat may be lost. Consequently, all of the reactor projects currently being developed under the AEC Nuclear Superheat Program (which use low enrichment fuel in a pressure vessel type reactor) are based upon the "thin clad" concept, wherein the cladding is allowed to collapse onto the fuel for support. The problems encountered with such a design are discussed in more detail in Section V.C; but suffice it to state that, despite the uncertainties involved, the "thin clad" concept was accepted for this study since the main purpose here was to evaluate the moderator materials and not the fuel elements, per se. One exception to this approach was explored with the "integral fuel-moderator element" concept, which is described in Section IV.C.

Rod-type, UO_2 fuel elements were selected for this study, on the basis that: (1) a cylindrical geometry is required for pressure containment; and (2) the fabricating costs may be estimated with greater confidence than for other possible configurations. The cladding thickness for the reference designs was based primarily on the requirement that it contain fission gas pressure when the reactor is depressurized during a normal refueling period. Type 304 stainless steel was chosen for a cladding material; and the maximum fuel-element surface temperature during normal operation was limited to the range of 1250 to 1350°F,

although this limit is considered to be more intuitive than factual at the present state of the art. Once the cladding has collapsed, its strength requirements are determined mainly by thermal distortion considerations, which depend strongly on fuel geometry and spacer design.

IV. DESCRIPTION OF REFERENCE DESIGNS

A. ZIRCONIUM HYDRIDE MODERATED, BOILING-SUPERHEAT REACTOR - MODERATOR LOG CONCEPT (ZBSR-ML)

1. General

The reactor concept described here is a direct-cycle, integral boiling water-steam superheat reactor of the pressure vessel type. The term integral denotes the fact that the steam is produced and superheated in the same core. A summary of the reactor performance and design characteristics is presented in Table IV and drawings of the reactor and its major components are shown in Figures 5, 6, and 7.

The reactor core is roughly cylindrical in shape, with an active height of 11 ft 3 in. and an equivalent diameter of 9 ft 1/2 in. It is divided radially into two regions; a central superheat region surrounded by a peripheral boiling region. The steam is produced in the boiling region. The steam is produced in the boiling region, which is cooled and moderated with light water, and separated from the water within the reactor pressure vessel with the aid of mechanical steam separators. After passing through the dryers located near the top of the vessel, the steam is ducted through the central region of the core in a two-pass flow arrangement. The superheated steam leaves the reactor through the nozzle located in the top head and is then piped directly to the turbine.

The reactor is designed to produce a net electrical output of 300 Mwe with steam conditions of 1450 psig and 1000°F at the turbine throttle. The reactor heat balance and schematic flow diagram for these conditions are shown in Figure 8. A net cycle efficiency of 38.5% is obtained, which results in a gross thermal power requirement of 778 Mwt, including a 1% heat loss. The boiling region of the core produces 540 Mwt and the remaining 238 Mwt (30.6%) comes from the superheater. The nominal operating pressure in the boiler is approximately 1550 psig, for which the saturation temperature is 600°F.

The superheater is fueled with 12,500 kg of uranium enriched to 3.39% U^{235} , and the boiler contains 28,400 kg enriched to 2.53%. The specific power in each region is thus approximately 19 kw/kg U. The two regions are coupled from a nuclear standpoint; and the enrichments quoted are those required to obtain the proper power split in the hot, clean, boiling core and a reactivity

lifetime of 10,000 Mwd/MTU. The average energy of the neutrons causing fission is predominantly thermal in both regions.

The boiling region of the core contains 84 fuel assemblies and 68 control rods, arranged on a 8-1/2 in. square pitch as shown in Figure 5. The boiler fuel elements consist of UO_2 pellets loaded into Zircaloy-2 tubes which are assembled into square bundles. Zirconium hydride is used as moderator only in the superheater, and the design of the boiler is thus fairly conventional.

The superheat region consists of 45 zirconium hydride moderator log assemblies, 720 fuel assemblies, and 36 control rods arranged on an 8-1/4 in. square pitch. The superheat fuel elements also contain UO_2 , but are clad with stainless steel tubing and assembled into 7-rod clusters. These are placed in circular flow channels within the moderator logs, and the cruciform-shaped control rods are located in the space provided between adjacent logs. The entire superheat region is in a steam environment during operation, and is separated from the boiling region by a flow baffle which runs from the core support plate up to the steam dryer structure. The moderator logs can be removed for refueling, and a "limited leakage" steam seal is thus required at the grid plate.

The control rod drives are all mounted at the bottom of the pressure vessel to provide easier access to the core. Both core regions are supported by a single grid-support plate structure located at the bottom of the core. Steam connections for the superheater are made at the combination shield plug-manifold assembly, which is supported by the dryer assembly, both of which can be removed for refueling.

2. Coolant Flow

Forced circulation is used in the boiling region of the core, with the water being circulated by four pumps in as many closed loops. The average quality at the exit from the boiling channels is 8 wt %, and the total recirculation ratio is 13.2 lb of water per lb of steam.

Referring to Figure 5, the coolant water, which is subcooled approximately 14.3 Btu/lb, enters the reactor through the four inlet nozzles located near the bottom of the pressure vessel. It then flows down between the vessel wall and the core support skirt, and up through the bottom plenum, the grid support structure, and the boiler fuel elements. Approximately 6% of the coolant water is bypassed to the region between the boiler elements to cool the control rods.

The steam-water mixture leaving the top of the core is separated with the aid of mechanical steam separators which are located in the upper section of the boiler flow channels. The water emerging from the separators turns and flows downward to the exit nozzles and through the external recirculation loop piping to the suction side of the recirculating pumps. Feedwater at 440°F is added to the coolant water upstream of the pumps to avoid cavitation.

The saturated steam flows up through the ducts which are integral with the steam separators and is discharged into the steam plenum region located between the dryers and the surface of the water. The steam then passes through the dryers, into the hemispherical dome at the top of the pressure vessel, and down through the combination shield plug and manifold assembly into the relatively open region above the superheat core. A two-pass arrangement is used in the superheater, with the first pass being down through the fuel elements located in the outer 24 moderator logs and the second pass up through those in the central 21 logs. A turn-around plenum is located beneath the grid plate, and about 3-1/2% of the first pass steam flow is bypassed to this plenum through orifices to cool the control rods in the superheat region.

The superheated steam flows through the insulated ducts, (which are attached to the second pass moderator logs) into the shield-manifold assembly where it mixes, and then flows out of the reactor through the insulated nozzle located in the top head. The fluid pressures and temperatures at the various stations are shown in Figure 8.

3. Fuel Elements

a. Boiler Elements

Both the boiler and superheat fuel elements are shown in Figure 6. The boiler fuel assembly, which is shown at the top of the figure, consists of a square Zircaloy box containing 5 separate bundles of Zircaloy clad fuel rods. Each bundle is 27.26 in. long and contains 140 fuel rods which are welded to spacer plates at each end. The four center rods in the 12-by-12 bundle are removed to accommodate a 1-in.-diameter tube which is welded to both spacer plates. A thick bushing designed to engage a refueling tool is attached to the upper end of each of the central tubes. The entire box assembly can be removed from the core and transferred to a handling pool for refueling and the separate

bundles can then be removed from the box either for storage or axial rearrangement. The lower nozzle and upper extension to the box are made of stainless steel, since these parts are not in the active core region. The hypothetical mechanical steam separator is shown inserted in the upper extension of the fuel element box assembly.

The fuel rods for the boiler elements consist of cylindrical UO_2 pellets, 0.394 in. in diameter by 0.396 in. long, loaded into 0.450-in.-OD Zircaloy tubing with a cold diametrical clearance of 6 mils. The tube wall thickness is set primarily by the requirement that it withstand a collapsing pressure 50% higher than the system design pressure at a temperature of 650°F. This condition results in a thickness of 21 mils, to which was added a 2-mil allowance for corrosion and a 2-mil manufacturing tolerance to bring the total wall thickness to 25 mils. The room temperature density of the UO_2 pellets was assumed to be 97% of theoretical density for design calculations. Ceramic insulator discs are inserted at each end of the stack of pellets to avoid overheating the end caps, which are welded to each end of the tube. A 0.60-in. space is provided for axial differential expansion of the fuel relative to the clad, and as a reservoir for fission product gases.

b. Superheat Elements

The superheat element, shown in the bottom half of Figure 6, consists of a cluster of seven fuel rods which is designed to fit into a 1,700-in.-diameter flow channel within the moderator log assembly. The rods are welded to and supported from a stainless steel spider at the top end. Short, spiral wires are brazed to the cladding of the outer six rods at 12 in. intervals, with the axial location of the wires staggered to provide lateral spacing between rods every 6 in. The bottom spacer is welded to the center rod, and differential expansion is accommodated by a slip-fit between the lower spacer and the end caps of the outer six rods.

The design of the superheat fuel rods is based on the "thin clad concept" wherein the cladding is allowed to collapse on the fuel under design conditions. The UO_2 fuel is loaded into the thin-walled tubing, which is 0.443 OD, 304 stainless steel with a 20-mil wall thickness. The UO_2 is in the form of sintered powder which is graded into three different particle sizes, mixed, and loaded into the tubes by sonic compaction. The density of the fuel, using this technique,

was assumed to be 10.0 gm/cm^3 . Although pellets could be used as an alternate fabrication method, sonic compaction would avoid the problem of wrinkling and nonuniform collapse, since the cladding is already in contact with the fuel before being exposed to the reactor environment. The 20-mil tube-wall thickness is based on the design condition that the cladding be capable of containing fission-product gas pressures after shutdown (when the reactor is depressurized), with the allowable stress set by the ASME Code for Unfired Pressure Vessels. For the materials and design conditions used, this results in a tube-wall thickness of approximately 15 mils, and to this was added 3 mils for a corrosion allowance and 2 mils for manufacturing tolerance.

The UO_2 extends the full length of the fuel rod, and ceramic insulator discs are used to prevent overheating of the end caps, which are seal-welded to each end. The spring at the top end is included to hold the fuel in place prior to installation in the core, and the void formed around the spring provides sufficient expansion space to accommodate a fission product gas release of approximately 25% without exceeding the reactor operating pressure. The cladding is reinforced in the area around this space to avoid collapsing the tube onto the spring.

4. Moderator Logs

A drawing of the moderator log assembly is shown in Figure 7. Each assembly contains approximately 590 lb of metallic zirconium hydride which is completely canned in stainless steel. The logs are penetrated by 16 flow channels into which the superheat fuel elements are placed. A nozzle is permanently attached to the lower end of the log, and a removable duct and control rod guide structure is bolted to the top end of the assembly, as shown.

There are 192 separate pieces of zirconium hydride in each log assembly, with each piece being a "square tube" 1.92 in. on a side and approximately 1 ft long with a 1.74-in.-diameter hole running through it. The length and degree of subdivision were set by manufacturing considerations. The zirconium metal is first extruded into the shape of the square tube with a slightly smaller hole than the finished block. The extrusions are then cut to 1-ft lengths, hydrided to the desired composition under controlled conditions, and then machined to the final dimensions quoted. After inspection, these pieces are stacked into the stainless steel can, which at this point consists of the outer box and the 16 flow channel

cladding tubes, all of which are welded to the lower end fitting. The inner surfaces of this can are completely coated with an enamel-like hydrogen diffusion barrier. In addition, three axial barriers are required to limit the migration of hydrogen from the cold to the hot end of the log.

The top-plate is welded in place after all of the zirconium hydride has been installed. As presently conceived, the entire assembly would then be placed in a high temperature autoclave to pre-collapse the cladding uniformly and to bond the diffusion barrier material at adjoining surfaces.

Type 431 martensitic stainless steel is used for the moderator cladding material because its thermal expansion coefficient closely matches that of the zirconium hydride. The outer box is a sandwich-type structure with a corrugated core welded to two facing-sheets and vented to the control rod coolant steam atmosphere. It is used here to provide thermal insulation and impart structural rigidity to the moderator assembly. The top duct and control-rod guide structure is bolted to the top plate of the moderator log assembly and sealed to it with a soft metal gasket. The duct is required for the center 21 logs only; but other than this, the logs are completely interchangeable. The entire moderator assembly would be removed from the core and placed in the handling pool for refueling, for which purpose the duct assembly would be removed to gain access to the fuel elements.

5. Control Rods and Drives

The control rod drive mechanisms for the boiling section of the core are of the hydraulic type which scram upward into the core. Thus, the poison section of the boiler control rods is at the bottom and the follower is at the top. This arrangement is based on the need for axial power flattening in the boiling region. The poison material is 2% boron stainless steel and the follower is Zircaloy-2.

The superheat control rod drives would either be steam-actuated or of the mechanical rack and pinion type, since the steam-cooled rods would require a sliding seal if the hydraulic type drive were used. The poison section of the superheat control rods is at the top and the rods scram downward. No follower is used, other than the small, stainless steel push rod which is cruciform shaped to prevent bowing. This arrangement is necessary because of the flow obstruction that would otherwise result if a full-sized cruciform rod guide

structure penetrated the steam turn-around plenum at the bottom of the superheat core. Boron stainless steel is also specified for the superheat control rods, although a poison material with higher resonance absorption characteristics might be required due to the hard thermal spectrum in the superheat region.

6. Pressure Vessel

The pressure vessel is designed to meet the requirements of the ASME Code for Unfired Pressure Vessels. The design pressure and temperature are 1940 psig and 700°F, and the weight is approximately 287 tons, not including the top head which weighs approximately 72 tons. The vessel is lined with 1/4-in. stainless steel on all interior surfaces and the outer surfaces would be insulated with stainless steel wool.

About the only unusual vessel design problem introduced by integral nuclear superheat concerns the high-temperature steam nozzle. The design shown is insulated from the hot steam in the exit duct by bypassing a small amount of saturated steam through the gap between the duct and the vessel wall in the region around the flange. By controlling the bypass flow, it is felt that the thermal stress problem around the flange may be minimized, although careful attention to this problem would be required in a final design. A leak in the high temperature ducting would not pose a vessel design problem, since the pressure gradient is such that the saturated steam would flow into the duct.

TABLE IV

SUMMARY OF DESIGN AND PERFORMANCE DATA FOR ZIRCONIUM HYDRIDE MODERATED REACTOR

GENERAL	
Reactor type	Heterogeneous, boiling water with integral steam superheat
Fuel	Slightly enriched uranium dioxide (UO_2)
Coolant	Water and steam (H_2O)
Moderator	Boiling region: water Superheat region: zirconium hydride ($\text{ZrH}_{1.68}$)

TABLE IV (Cont.)

PLANT DATA

Steam cycle	Direct with superheat
Net electric power (Mwe)	300
Gross Electric power (Mwe)	315
Net cycle efficiency (%)	38.5
Gross cycle efficiency (%)	40.5
Gross thermal power (Mwt)	778
Throttle steam pressure (psig)	1450
Throttle steam temperature (°F)	1000
Reactor operating pressure (psig)	1550
Saturated steam temperature (°F)	600
Feed-water temperature (°F)	440
Steam-flow rate (lb/hr)	2.45×10^6
Coolant-water flow rate (lb/hr)	32.4×10^6

REACTOR CORE

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Location of core region	Peripheral	Central
Equivalent diameter (ft-in.)	9 ft 1/2 in.	5 ft 3 in.
Active height (ft-in.)	11 ft 3 in.	11 ft 3 in.
Thermal power in region (Mwt)	540	238
Volume (liters)	13,487	6,800
Average region power density (kw/l)	40	35
Moderator material	H ₂ O	ZrH _{1.7}
Fuel material	UO ₂	UO ₂
Structural material	Zircaloy	Stainless steel
Moderator-to-fuel volume ratio	2.50	1.50
Number of fuel assemblies	84	720
Number of moderator assemblies	-	45
Number of control rods	68	36
Control rod pitch (in.)	8.50	8.25
Average region specific power (kw/kg U)	19.0	19.0

TABLE IV (Continued)
REACTOR CORE (Continued)

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Composition of cold, unflooded core with control rods withdrawn (volume %)		
Water	0.5742	-
Zirconium hydride	-	0.3029
Stainless steel	-	0.0692
Zircaloy	0.1898	-
UO ₂	0.2288	0.2078
Void	0.0072	0.4201
<hr/>		
FUEL ELEMENTS		
Type	Rod	Rod
Fuel	UO ₂ pellets	UO ₂ sintered powder
Axial segments per assembly	5	1
Rods per segment	140	7
Fuel clad material	Zircaloy-2	Stainless steel, Type 304
Fuel clad OD (in.)	0.450	0.443
Fuel clad thickness (in.)	0.025	0.020
Thermal bond	Helium	
Fuel diameter (in.)	0.394	0.403
<hr/>		
NUCLEAR DATA		
Fuel loading (kg U)	28,400	12,500
Enrichment (at. %)	2.53	3.39
Initial k _{eff} (hot, clean, boiling)		1.15
Reactivity lifetime (Mwd/MTU)	10,000	10,000
Fuel management	Batch	Batch
Initial conversion ratio	0.575	0.548
Gms. Pu/kg U (final)	5.27	5.22
<hr/>		

TABLE IV (Continued)
HEAT TRANSFER DATA

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Average heat flux (Btu/hr-ft ²)	121,000	122,000
Peak-to-average heat Flux ratio		
Radial	1.3	1.24 - (1st pass) 1.06 - (2nd pass)
Axial	1.5	1.5
Local	1.3	1.03
Power overshoot	1.2	1.2
Total	3.05	2.34 - (1st pass) 1.97 - (2nd pass)
Maximum heat flux (Btu/hr-ft ²)	369,000	281,000 (1st pass)
Burnout heat flux (Btu/hr-ft ²)	> 600,000	-
Maximum fuel temperature (°F)	4500	3700 (1st pass)
Maximum fuel-clad surface temperature (°F)	650	1050 - (1st pass) 1230 - (2nd pass)

COOLANT FLOW DATA

Water flow rate (lb/hr)	32.4 x 10 ⁶	
Steam flow rate (lb/hr)	2.45 x 10 ⁶	
Inlet temperature (°F)	593	600
Outlet temperature (°F)	600	1000
Inlet velocity (ft/sec)	9.45	61
Outlet velocity (ft/sec)	15.2	137
Pressure drop (psi)	15	58
Exit quality (lb steam/lb mixture in boiling channel)	0.08	-
Exit void fraction (volume steam/ volume mixture in boiling channel)	0.42	-
Boiling length/total length	0.95	-
Exit slip ratio (velocity steam/ velocity water)	1.4	-
Average void fraction (volume steam/ volume mixture in boiling core)	0.23	-

TABLE IV (Continued)

PRESSURE VESSEL

	<u>Boiler</u>	<u>Core Region</u>	<u>Superheater</u>
Inside diameter (ft-in.)	11 ft 4 in.		
Height (ft)	51 ft		
Wall thickness (in.)	7.625		
Base material	Carbon steel, Type 302B		
Liner	Stainless steel, Type 304		
Weight (tons)			
Body	287		
Head	72		
Total	359		
Operating pressure (psig)	1550		
Design pressure (psig)	1940		
Design temperature (°F)	700

Figure 5. Zirconium-Hydride-Moderated, Boiling, Superheat Reactor

Figure 6. Fuel Elements for Zirconium-Hydride-Moderated Reactor

Figure 7. Moderator Log for Zirconium-Hydride-Moderated Reactor

TURBINE NET HEAT RATE	
$\frac{2.45 \times 10^6 (1491.6 - 419.7)}{300,000}$	$= 8754 \text{ Btu/Kw hr}$
OVER ALL PLANT EFFICIENCY	
$\frac{300,000 \times 3413}{2.45 \times 10^6 (1491.6 - 419.7) 1.01^*}$	$= 38.5 \%$
*1% IS ASSUMED FOR PLANT HEAT LOSS	

Figure 8. Heat Balance for 300-Mwd, Zirconium-Hydride-Moderator, Boiling, Superheat Reactor

B. WATER-MODERATED CONCEPT

1. General

The water-moderated concept described here was developed as a basis for comparison with the zirconium hydride concept described previously. The two designs are basically quite similar, therefore, and only those areas which differ significantly will be described in detail. A summary of the performance and design characteristics is presented in Table V and drawings of the reactor and its fuel elements are shown in Figures 9, 10, and 11.

Actually, two water-moderated concepts were evaluated, with the only difference being in the materials used in the superheater. In one case, stainless steel is used for the process tubes in the superheater and in the other Zircaloy-2 is used. Separate nuclear and cost calculations were made for each case, and a distinction is made between the two designs in the cost section of this report. Since the two designs are identical in other respects, however, both are described in this one section.

The reactor is designed to produce 300 Mwe with the same steam conditions at the turbine throttle as the ZBSR; 1450 psig, 1000°F. The reactor heat balance and schematic flow diagram are shown in Figure 12. Although the coolant water recirculation rate is approximately 7% higher than that for the ZBSR, the effect on net electrical output or cycle efficiency is indiscernible (i.e., less than 0.03% of the net electrical power). Thus, the gross thermal power, rounded off to the nearest megawatt, is 778 Mwt, the same as before.

The power split between regions is noticeably different, however, due to the heat losses in the water-moderated superheater. Thus, the boiling region for this reactor produces 525 Mwt and the superheater produces 253 Mwt (compared to 540/238 in the ZBSR).

The core contains 49 superheat fuel assemblies, 72 boiler fuel assemblies, and 96 control rods set on a 9.25-in.-square pitch as shown in Figure 9. There is no physical boundary between the two regions and the same control rods and drives are used throughout the core. The power density is 28 kw/liter in the superheat region and 40 kw/liter in the boiler. The superheat region is fueled with 13,300 kg of uranium and the boiler contains 27,600 kg. The specific power in each region is 19 kw/kg U, the same as for the ZBSR concept.

The reactor core for the water moderated design is slightly larger than that of the ZBSR, due to the lower power density achievable in the superheater. Thus, the equivalent diameter is 9 ft 6-3/4 in. and the active height is an even 11 ft, compared to the 9 ft 1/2 in. by 11 ft 3 in. ZBSR core. The active length was adjusted to obtain the same average specific power in both designs.

2. Coolant Flow

The coolant flow path differs from that of the ZBSR in two respects, the first being that water must be circulated through the superheat region where it serves as moderator and as a coolant for the control rods located in that region. In passing through the superheater, the moderator water absorbs approximately 6% of the heat generated in that region, about half of which is due to heat transfer through the insulated process tubes and the other half due to neutron and gamma heating. The moderator water is allowed to boil slightly and the flow rate was set by limiting the steam void fraction to 10% of the moderator volume at the core exit. Approximately 1.2% of the steam is generated in the superheat region and the total recirculation ratio is 14.16 lb of water per lb of steam, compared to 13.20 for the ZBSR. The flow characteristics of the boiling region are the same as those of the ZBSR, with the exception that the subcooling is reduced by approximately 1 Btu/lb, due to the higher recirculation ratio.

The other major difference concerns the steam flow path through the superheater. A 2-pass arrangement is used, but both passes are made within the superheat fuel element assemblies in order to avoid the necessity for underwater seals. This results in a higher maximum fuel clad surface temperature, since the maximum heat flux can theoretically occur in both passes. If practical underwater seals can be successfully developed, however, this disadvantage can be circumvented by using a single large turn-around plenum at the bottom of the superheat core and the same flow arrangement as that used in the ZBSR.

3. Fuel Elements

A drawing of the boiler fuel element is shown in Figure 10. It is identical to that used for the ZBSR except that each bundle contains 164 rods instead of 140. The larger bundle (13 by 13 vs 12 by 12) is required to match the lattice spacing of the superheat core. The active length is also 3 in. shorter in order to obtain the same average specific power as the ZBSR.

The superheat fuel assemblies are shown on Figure 11. The complete fuel assembly consists of 16 double-walled process tubes, 2 tube sheets, a lower turn-around plenum, an upper manifold and duct, and sixteen 7-rod fuel element clusters. This assembly is inserted into the square box which helps to align the fuel element assemblies within the core and also serves as a control rod guide structure. The design features the same 7-rod cluster of stainless steel-clad, UO_2 fuel elements as those used in the ZBSR; but in this case the elements are placed in the insulated process tubes, which are assembled into bundles as shown on the drawing. The 7-rod fuel clusters are secured only at the top and are thus free to expand in the axial direction.

The steam enters from the top and flows down through the outer annulus of the concentric duct, which is welded to the top tube sheet. The steam then flows down through the eight fuel clusters which are located in the outer two rows of process tubes and into the steam plenum at the bottom of the assembly. The second pass is made up through the eight fuel clusters in the center two rows of process tubes, after which the steam passes up through the central, insulated duct.

The process tube consists of an outer pressure tube which is in contact with the relatively cold water moderator and an inner flow tube which is in contact with the hot steam. The two tubes are separated by a 0.060 in. gap which is vented to the steam at the exit end of each channel. Stagnant steam thus provides the thermal insulation necessary to avoid excessive heat losses to the moderator.

The outer tube is designed to withstand a collapsing pressure of 100 psi, which is about 30% higher than the pressure drop experienced by the steam in passing through the superheat core. The design shown features stainless steel pressure tubes which are welded to the tube sheets at both ends. The alternate design, which is based on using Zircaloy-2 pressure tubes, would require a sealed mechanical joint between the Zircaloy tubes and short stainless steel tubes which would, in turn, be welded to the tube sheets. The stainless steel tube wall thickness is 0.030 in. and the Zircaloy-2 tubes would be 0.035 in. thick.

The inner flow tube is made of stainless steel Type 304, and is designed to withstand the tensile stress due to the pressure differential between the coolant steam on the inside and the stagnant steam in the annular gap between the two tubes. The ID is 1.70 in., which is the same as that of the flow-tube cladding in the zirconium hydride moderator logs, and the wall thickness is 0.010 in. Each flow tube is welded to the tube sheet at the inlet end and is left free to expand at the exit end. A spiral wire wrap is used in the gap between the flow tube and the pressure tube to maintain concentricity.

The outer box assembly is a semipermanent part of the core structure, but can be removed to facilitate refueling. The portion of the box which is in the active core is made of Zircaloy-2. This is riveted to the lower nozzle and upper extension, which are stainless steel. The sides of the box are slotted to permit the moderator water and the control rod coolant water to mix and flow freely.

4. Pressure Vessel

The pressure vessel is designed for the same requirements and conditions as those specified for the ZBSR pressure vessel. It is 6 in. larger in diameter due to the difference in the size of the two cores. The distance between the equivalent cylindrical core and the vessel wall is the same in each case. The main body of the vessel weighs approximately 305 tons and the head weighs 77 tons.

TABLE V
SUMMARY OF DESIGN AND PERFORMANCE DATA FOR
WATER MODERATED REACTOR

GENERAL	
Reactor type	Heterogeneous, boiling water with integral steam superheat
Fuel	Slightly enriched uranium dioxide (UO ₂)
Coolant	Water and steam (H ₂ O)
Moderator	Light water (H ₂ O)

PLANT DATA	
Steam cycle	Direct with superheat
Net electric power (Mwe)	300
Gross electric power (Mwe)	315
Net cycle efficiency (%)	38.5
Gross cycle efficiency (%)	40.5
Gross thermal power (Mwe)	778
Throttle steam pressure (psig)	1450
Throttle steam temperature (°F)	1000
Reactor operating pressure (psig)	1550
Saturated steam temperature (°F)	600
Feed-water temperature (°F)	440
Steam flow rate (lb/hr)	2.45×10^6
Coolant-water flow rate (lb/hr)	34.73×10^6

	REACTOR CORE	
	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Location of core region	Peripheral	Central
Equivalent diameter (ft-in.)	9 ft 6-3/4 in.	6 ft 1 in.
Active height (ft)	11.0	11.0
Thermal power in region (Mwt)	525	253
Volume (liters)	13,296	9,070

TABLE V (Continued)
REACTOR CORE (Continued)

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Average region power density (kw/l)	40.0	28.0
Moderator material	H ₂ O	H ₂ O
Fuel material	UO ₂	UO ₂
Structural material	Zircaloy	Zircaloy and stainless steel
Moderator-to-fuel volume ratio	2.50	2.35
Number of fuel assemblies	72	49
Number of control rods	52	44
Control rod pitch (in.)	9.25	9.25
Average region specific power (kw/kg U)	19.0	19.0
Composition of cold, unflooded core with control rods withdrawn (volume %)	<u>Pressure Tube Material</u>	
	<u>St. Stl.</u>	<u>Zircaloy</u>
Water	0.5742	0.3858
Stainless steel	-	0.0780
Zircaloy	0.1898	0.0784
UO ₂	0.2288	0.1645
Void	0.0072	0.2933

FUEL ELEMENTS

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Type	Rod	Rod
Fuel	UO ₂ pellets	UO ₂ sintered powder
Segments per assembly	5	1
Rods per segment	164	7
Fuel clad material	Zircaloy-2	Stainless steel Type 304
Fuel-clad OD (in.)	0.450	0.443
Fuel-clad thickness (in.)	0.025	0.020
Thermal bond	Helium	
Fuel diameter (in.)	0.394	0.403

TABLE V (Continued)
FUEL ELEMENTS (Continued)

	<u>Boiler</u>	<u>Core Region</u>	<u>Superheater</u>
Process tubes per assembly	-		16
Flow tube:			
Material	-		Stainless steel, Type 304
Outer diameter (in.)	-		1.720
Inner diameter (in.)	-		1.700
Wall thickness (in.)	-		0.010
<u>Pressure tube:</u>			
Material	-	<u>St. Steel</u>	<u>Zircaloy</u>
Outer diameter (in.)	-	1.910	1.910
Inner diameter (in.)	-	1.850	1.840
Wall thickness (in.)	-	0.030	0.035
Stagnant steam gap width (in.)	-	0.065	0.060

NUCLEAR DATA

	<u>Boiler</u>	<u>Core Region</u>	<u>Superheater</u>
Fuel loading (kg U)	27,600		13,300
Initial k_{eff} (hot, clean, boiling)		1.15	
Reactivity lifetime (Mwd/MTU)	10,000		10,000
Fuel management	Batch		Batch
<u>Stainless steel pressure tube in superheater:</u>			
Initial enrichment (at. %)	2.64		3.50
Initial conversion ratio	0.562		0.475
Finals gm Pu/kg U	5.22		4.73
<u>Zircaloy pressure tube in superheater:</u>			
Initial enrichment (at. %)	2.59		2.62
Initial conversion ratio	0.564		0.530
Finals gm/Pu/kg U	5.22		4.85

TABLE V (Continued)
HEAT TRANSFER DATA

	<u>Boiler</u>	<u>Core Region</u> <u>Superheater</u>
Average heat flux (Btu/hr-ft ²)	121,000	122,000
Region peak-to-average heat flux ratio		
Radial	1.3	1.19
Axial	1.5	1.5
Local	1.3	1.03
Power overshoot	1.2	1.20
Total	3.05	2.21
Maximum heat flux (Btu/hr-ft ²)	369,000	269,000
Burnout heat flux (Btu/hr-ft ²)	>600,000	-
Maximum fuel temperature (°F)	4500	3900
Maximum fuel-clad surface temperature (°F)	650	1320

COOLANT FLOW DATA

Steam flow rate (lb/hr)		2.45 x 10 ⁶
Coolant water flow rate (lb/hr)	34.73 x 10 ⁶	
Inlet temperature (°F)	593	600
Outlet temperature (°F)	600	1000
Inlet velocity (ft/sec)	9.45	51
Outlet velocity (ft/sec)	15.2	160
Pressure drop (psi)	15	48
Exit quality (lb steam/lb mixture in boiling channel)	0.08	-
Exit void fraction (volume steam/volume mixture in boiling channel)	0.42	-
Boiling length (total length)	0.95	-
Exit slip ratio (velocity steam/velocity water)	1.4	-
Average void fraction (volume steam/ volume mixture in boiling core)	0.23	-

TABLE V (Continued)
PRESSURE VESSEL

	<u>Boiler</u>	<u>Core Region</u>	<u>Superheater</u>
Inside diameter (ft-in.)	11 ft 10 in.		
Height (ft)	51		
Wall thickness (in.)	8.0		
Base material	Carbon steel, Type 302B		
Liner	Stainless steel, Type 304		
Weight (tons)			
Body	305		
Head	77		
Total	382		
Operating pressure (psig)	1550		
Design pressure (psig)	1940		
Design temperature (°F)	700

Figure 9. Water-Moderated
Integral Boiling Superheat
Reactor

Figure 10. Boiler Fuel Element for Water-Moderated Reactor

Figure 11. Superheat Fuel Element for Water-Moderated Reactor

TURBINE NET HEAT RATE :	
$\frac{2.45 \times 10^6 (1491.6 - 419.7)}{300,000}$	$= 8754 \text{ Btu/kw - hr}$
OVER ALL PLANT EFFICIENCY	
$\frac{300,000 \times 3413}{2.45 \times 10^6 (1491.6 - 419.7) \times 1.01^*}$	$= 38.5 \%$
*1% IS ASSUMED FOR PLANT HEAT LOSSES	

Figure 12. Heat Balance for 300-Mwe, Water-Moderated, Boiling Superheat Reactor

C. ZIRCONIUM HYDRIDE MODERATED, INTEGRAL FUEL-MODERATOR CONCEPT (ZBSR-IFM)

This concept was investigated only to the extent necessary to obtain an estimate of the fuel cycle costs and an indication of feasibility. The design represents an attempt to improve on the current technology of superheat fuel elements by using the zirconium hydride both as a moderator and as a thick-walled, non-collapsing fuel element cladding material.

Figure 13 shows the IFM element as presently conceived. The superheat core region would be composed of 37 hexagonal bundles of fuel-moderator rods of the type shown. The rods are placed on a triangular pitch to limit the coolant void fraction and to obtain more uniform heat-transfer conditions around the circumference of each rod. A two-pass flow arrangement is required to limit the maximum surface temperature to the range of 1200 to 1300°F. The superheat region would be in a steam environment and separated from the boiling region by a suitable flow baffle similar to that used in the moderator log concept.

The "spacemetal" box enclosing each bundle serves as an insulated flow channel and permits the flow to be orificed to each assembly. This box must also withstand a collapsing pressure of about 75 psi, which is the difference between the control rod coolant (steam) pressure at the top of the core and the superheated steam pressure at the end of the second pass. In the design shown, it was assumed that the load would be transferred from the box to the clustered fuel-moderator rods via the points of contact provided by the continuous wire wrap around each rod. The box will then deflect only in the region between these points, and the expanded metal is used to limit the deflection to a tolerable value without incurring a prohibitively large steel volume fraction.

The IFM assembly shown contains 169 rods. Each rod is divided into 13 axial segments, two of which are reflector plugs. The fueled segments consist of a thick-walled zirconium hydride tube which is loaded with 3/8 in. diameter cylindrical UO_2 pellets, and sealed at both ends with zirconium hydride end caps. The rods are clad with a full-length, thin-walled stainless steel tube which provides corrosion protection and a support surface for the hydrogen diffusion barrier. Diffusion barriers are also required to limit the migration of hydrogen in the axial direction, and this accounts for the rather large number of axial segments used. It might be possible to reduce the number of segments per rod; the number chosen is based on limiting the maximum temperature difference

between two ends of a single, zirconium hydride segment to approximately 100°F in order to insure that the equilibrium axial redistribution of hydrogen would not exceed $\pm 5\%$.

The zirconium hydride tube wall thickness (0.105 in.) is chosen to yield a moderator-to-fuel volume ratio of 1.50, which is about optimum with respect to reactivity. This results in a configuration which could theoretically withstand a collapsing pressure greater than 4000 psi, and the element is thus assuredly "free-standing" in the 1500 psi reactor environment. The UO_2 pellets are free to expand as a column in the axial direction, and an expansion space is provided in each segment. The hydride is in the delta phase with a hydrogen density of 6.0×10^{22} atom/cm³.

The material used for the protective cladding is martensitic stainless steel, Type 431, which has approximately the same thermal expansion coefficient as the zirconium hydride. The clad wall thickness is 5 mils, which is approximately twice the reduction in thickness expected due to corrosion. Thus the steel to UO_2 volume ratio for the rod proper is only 0.086. The additional steel required for the box and the control rod followers (pushrods) increases the overall steel-to- UO_2 volume ratio to approximately 0.143, however.

The boiling region for this concept would be approximately the same as that of the other reactors studied, except for the hexagonal geometry. Consequently, the nuclear calculations were performed using a homogenized core, with the boiling region having the same composition as that of the ZBSR and with the superheat based on the IFM lattice described above. The heat balance and plant data are the same as those for the ZBSR-moderator log design. An abbreviated listing of the major design and performance parameters is shown in Table VI.

TABLE VI
SUMMARY OF DESIGN AND PERFORMANCE DATA FOR
ZIRCONIUM-HYDRIDE-MODERATED, INTEGRAL
FUEL-MODERATOR CONCEPT (ZBSR-IFM)

	Core Region	
	Boiler	Superheater
Equivalent diameter (ft)	9.06	5.2
Active height (ft)	11.0	11.0
Thermal power in region (Mwt)	540	238
Average region power density (kw/l)	40	39
Average region specific power (kw/kg U)	19.0	19.0
Moderator-to-fuel volume ratio	2.50	1.50
Stainless steel-to-fuel volume ratio	-	0.143
Fuel loading (kg U)	28,400	12,500
k_{eff} (hot, clean, boiling)	1.15	
Enrichment (at. %)	2.47	2.79
Initial conversion ratio	0.580	0.639
Final gm Pu/kg U	5.28	5.73
Maximum fuel-clad surface temperature (°F)	-	1277
Coolant pressure drop (psi)	-	~90
Composition (volume fractions)		
Fuel	0.2288	0.2224
Zircaloy	0.1898	-
Stainless steel	-	0.0318
Diffusion barrier	-	0.0192
Zirconium hydride	-	0.3318
Water	0.5742	-
Void	0.0072	0.3948

Figure 13. Zirconium-Hydride
Integral Fuel-Moderator Element

D. 50-Mwe ZIRCONIUM HYDRIDE MODERATED CONCEPT

This concept was to be developed to demonstrate the general characteristics of a prototype ZBSR, assuming that an economic incentive for the large plant was found to exist. Thus, the design to be described is essentially a scaled-down version of the 300-Mwe ZBSR moderator log concept based on the work performed before the results of the cost analysis were obtained. After it became apparent that an economic incentive for developing a large ZBSR did not exist, further work on this concept was discontinued.

A conceptual drawing of the 50-Mwe reactor is shown in Figure 14. Each region of the reactor core has approximately the same power density and specific power as the 300-Mwe ZBSR design. The lattice spacing was reduced to obtain a reasonable core layout and control rod distribution. The steam quality in the boiling region was kept at 8%, and the composition and design of both regions are essentially the same as those of the larger core.

The main difference between the 50-Mwe and the 300-Mwe designs (other than capacity) are:

- 1) The steam pressure and temperature at the turbine throttle were lowered from 1450 psig, 1000°F to 1250 psig, 950°F to permit the use of a preferred standard turbine at the lower power rating.
- 2) The pressure vessel was designed to permit gravity steam-separation.
- 3) The distance between the core and the vessel wall was increased to limit the integrated fast neutron exposure in the latter to 10^{19} nvt.

The last two criteria are felt to be consistent with early construction of a prototype reactor of this type. The surface area required for gravity steam-separation was based on the data presented in Reference 11 normalized to a disengagement velocity of 3 ft/sec at 300 psi. For a design operating pressure of 1350 psig, this results in a superficial disengagement velocity of approximately 1 ft/sec, for which the ID of the vessel must be 10 ft 1 in.

The fast neutron current incident on the pressure vessel was calculated with the aid of experimental data from the Bulk Shielding Reactor. For an integrated dose of 10^{19} nvt over a 30-yr period, the distance required between the core and the vessel was determined to be approximately 23 in., including 3 in. of steel which results in a pressure vessel ID of 8 ft 9 in. in the region around the core.

If the pressure vessel size is to be minimized, these two criteria, namely gravity separation and radiation damage, result in the pear-shaped vessel shown in Figure 14.

The reactor heat balance and schematic flow diagram for the steam conditions chosen is shown in Figure 15, and a brief summary of the design and performance data for this concept is included in Table VII.

TABLE VII
SUMMARY OF DESIGN AND PERFORMANCE DATA FOR
50-Mwe, ZIRCONIUM-HYDRIDE-MODERATED REACTOR

PLANT DATA	
Steam cycle	Direct with superheat
Net electric power (Mwe)	50
Gross electric power (Mwe)	52.5
Net cycle efficiency (%)	35.7
Gross thermal power (Mwt)	140
Thermal power in boiling region (Mwt)	100
Thermal power in superheat region (Mwt)	40
Throttle steam pressure (psig)	1250
Throttle steam temperature (°F)	950
Reactor operating pressure (psig)	1360
Saturated steam temperature (°F)	585
Feedwater temperature (°F)	451
Steam flow-rate (lb/hr)	4.56×10^5
Coolant water flow-rate	5.98×10^6

	REACTOR CORE	
	<u>Boiler</u>	<u>Core Region</u> <u>Superheater</u>
Location of core region	Peripheral	Central
Equivalent diameter (ft)	5.36	3.05
Active height (ft)	5.84	5.84
Thermal power in region (Mwt)	100	40
Average region power density (kw/l)	40	35
Average region specific power (kw/kg U)	19.0	19.0

TABLE VII (Continued)
REACTOR CORE (Continued)

	<u>Core Region</u>	
	<u>Boiler</u>	<u>Superheater</u>
Moderator material	H ₂ O	ZrH _{1.7}
Fuel material	UO ₂	UO ₂
Structural material	Zircaloy-2	Stainless steel
Number of fuel assemblies	52	225
Number of moderator assemblies	-	25
Number of control rods	44	16
Control rod pitch (in.)	6.50	6.33
Moderator-to-fuel volume ratio	2.5	1.5
Fuel loading (kg U)	5,260	2,100
Number of coolant passes	1	2
Maximum fuel cladding temperature (°F)	635	1225

PRESSURE VESSEL

Inside diameter (ft-in.)	10 ft 1 in. (upper body) 8 ft 9 in. (lower body)
Height (ft)	31.0
Wall thickness (in.)	6.00 (upper body) 5.25 (lower body)
Base material	Carbon steel
Liner	Stainless steel
Weight (tons)	
Body	91
Head	19
Total	110
Operating pressure (psig)	1350
Design pressure (psig)	1690
Design temperature (°F)	700

TYPICAL SUPER-HEAT LATTICE

TYPICAL BOILER LATTICE

SECTION A-A

Figure 14. 50-Mwe, Zirconium-Hydride-Moderated, Boiling Superheat Reactor

TURBINE NET HEAT RATE

$$4.56 \times 10^5 \left(\frac{1469 - 432}{50,000} \right) = 9450 \text{ Btu/kw-hr}$$

OVER ALL PLANT EFFICIENCY

$$\frac{50,000 \times 3413}{456 \times 10^5 (1469 - 432) \times 1.01} = 35.7 \%$$

Figure 15. Heat Balance for 50-Mwe, Zirconium-Hydride-Moderated, Boiling, Superheat Reactor

V. ANALYSIS

A. THERMODYNAMICS, HEAT TRANSFER, AND FLUID FLOW

1. Heat Balances

The heat balance and thermodynamic performance characteristics quoted for each of the 300-Mwe reference designs were based upon data obtained from Westinghouse for a 315-Mwe straight condensing turbine-generator plant. At rated capacity, a steam flow-rate of 2.45×10^6 lb/hr is required for turbine throttle conditions of 1450 psig, 1000°F, a condenser vacuum of 1-1/2 in. of mercury, and a feedwater temperature of 440°F, preheated in six feedwater heaters. Auxiliary power requirements were estimated to be 5% of the net plant capacity for each case, based on similar reactor plant designs. A heat loss of one percent of the thermal power was also assumed, and this was added to the thermal power required in each core region in proportion to the total region power level. The results are included in the descriptions of each design, Section IV.

2. Boiling Regions

The design of the boiling region of the cores studied is based on technology presented in Reference 8, in keeping with the ground rules established for the study. The composition is set primarily by nuclear and mechanical considerations, and the core size and fuel loading are dictated by heat transfer requirements. Thus, a basic fuel element configuration employing UO_2 pellets clad with Zircaloy tubes was selected, and the total water-to-fuel ratio was set at 2.50. The usual heat transfer calculations were then performed to determine the maximum heat flux and specific power obtainable as a function of fuel rod diameter.

Assuming that the pellet is concentric with the tube, the heat transfer within the fuel rod is approximated by the following equation:

$$\frac{q}{l} = \frac{2\pi(T_L - T_s)}{\left(\frac{1}{2k}\right)_{\text{UO}_2} + \left(\frac{\ln b/a}{k}\right)_{\text{gas}} + \left(\frac{\ln c/b}{k}\right)_{\text{clad}}}$$

where

q/l = heat generated per unit length of rod (Btu/hr-ft)

T_{CL} = UO_2 centerline temperature ($^{\circ}\text{F}$)

T_s = clad outer surface temperature ($^{\circ}\text{F}$)

a = fuel pellet diameter (ft)

b = clad ID (ft)

c = clad OD (ft)

k = thermal conductivity (Btu/hr-ft- $^{\circ}\text{F}$)

Radiation across the narrow gas gap is not included in the above equation, and if the other parameters and physical properties were known more accurately, this could be claimed as a conservative simplification.

The maximum fuel centerline temperature was set at 4500°F , the clad surface temperature at 650°F , and an effective thermal conductivity of $1.0 \text{ Btu/hr-ft-}^{\circ}\text{F}$ was assumed for the UO_2 . The thermal conductivity of the gas located in the gap between the pellet and the tube was calculated to be $0.07 \text{ Btu/hr-ft-}^{\circ}\text{F}$, based on an atmosphere of helium mixed with fission products. The nominal hot gap width was calculated to be approximately $1/2$ mil, for a cold diametral clearance of 6 mils. The clad thickness was held constant and equal to 0.025 in. , and the conductivity of Zircaloy-2 at 650°F was taken from Reference 12 as $7.4 \text{ Btu/hr-ft-}^{\circ}\text{F}$. The results of this calculation are shown in Figure 16.

A rod diameter of 0.45 in. was chosen after performing a design study on several cores with rod diameters ranging from 0.4 to 0.5 in. The principal considerations underlying this choice were as follows.

- a) The average specific power and power density of the boiling region should be at least as high as current status boiling water reactors.
- b) The average specific power should match that of the superheat core as close as possible to avoid gross power shifts with burnup.
- c) Surface temperature and pressure-drop considerations limit the maximum specific power obtainable in the superheat core.

Figure 16. Maximum Thermal Performance of Boiler Fuel Rods

The average specific power achieved in the boiler is thus dependent on the fuel-rod diameter, the thermal capabilities of the superheater, and the peak-to-average ratios in each region. The values selected are felt to be a reasonable compromise for the two-region cores studied.

The radial peak-to-average ratios were determined directly from the two-group, one-dimensional neutron diffusion calculations described in Section V-B. The value for the boiling region was found to be relatively insensitive to the composition of the superheat region (for cores with the proper power split), and equal to approximately 1.30. The other peak-to-average ratios were selected on the basis that they represent design goals which might reasonably be met in a detailed, final design. The values used for this study are tabulated below, along with the design values used for the Dresden core.¹³

Peak-to-Average Power Density Ratios

	<u>This Study</u>	<u>Dresden</u>
Radial	1.30	1.60
Axial	1.50	1.40
Local	1.30	1.30
Excursion Allowance	1.20	1.25
Product	3.05	3.60

The boiling cores were sized by combining the results of the single-rod heat transfer analysis with the above peaking factors. The results are tabulated in the summary tables which are included in the description of each concept.

The methods used to estimate the steam void fractions in the boiling core are presented in Reference 14. The average exit-steam quality was set at 0.08 lb steam/lb mixture, and the exit-steam volume fraction was then calculated using the following equation:

$$\alpha = \frac{1}{1 + \frac{\nu_f}{\nu_g} \left(\frac{1}{X} - 1 \right) S}$$

where

α = steam volume fraction

ν_f = specific volume of water (ft³/lb)

ν_g = specific volume of steam (ft³/lb)

X = quality $\left(\frac{\text{lb steam}}{\text{lb steam} + \text{lb water}} \right)$

S = slip ratio

The slip ratio for the conditions used in this reactor is estimated to be 1.4, from which the exit-steam volume fraction is 0.42.

The average void fraction over the boiling length was determined from the graphs provided in Reference 14 and corrected for the fraction of water in the core which is nonboiling. The average core void fraction is thus estimated to be 0.23.

The burnout heat flux was checked against data obtained at pressures above and below the reference design conditions. In each case, the burnout heat flux was conservatively estimated to be in excess of $600,000 \text{ Btu/hr-ft}^2$, which is more than 60% higher than the maximum design heat flux used. Therefore, this criterion was not considered to be a limiting condition for the boiling core region, as designed.

The coolant pressure drop for the boiling region core was estimated to be approximately 5 psi, using the methods described in Reference 14 for a cosine axial power distribution. To this was added an allowance of 10 psi for the mechanical separators and for losses in the external piping. The total pressure drop of 15 psi is thus somewhat speculative due to the uncertainties in the performance of the hypothetical steam separators.

3. Superheat Regions

a. General

The thermal analysis for the steam-cooled, superheat region of the cores studied is less straightforward than that of the boiling regions. It includes the calculation of heat transfer and fluid flow conditions for the fuel elements, heat generation rates within the zirconium hydride and water moderators, and heat losses to the water. Each of these will be discussed briefly in this section with respect to the reference designs described previously.

b. Fuel Element Heat Transfer

The choice of the superheater configurations, flow arrangements, and other design parameters used in the reference designs was based in part on a preliminary parameter survey performed early in the study. Designs were analyzed with rod diameters ranging from 0.3 to 0.6 in. and for 1-, 7-, 19-, and 37-rod clusters. The method used in this analysis was to set the number of rods on the basis of maximizing the specific power consistent with a maximum centerline fuel temperature of 4000°F , a surface temperature of 1250°F , and a total peak-to-average ratio of 2.50. Since the total coolant flow rate was set by the thermodynamic cycle, the heat transfer coefficient required to satisfy the surface temperature limit was obtained by varying the flow area in each channel. Using this approach, the only dependent variable was the steam pressure drop. The more significant conclusions of this study follow.

- 1) A fuel element configuration featuring a separate flow channel for each rod was thermally desirable but mechanically impractical for the zirconium-hydride, moderator log concept. The 19- and 37-rod clusters were undesirable from a nuclear standpoint, and this led to the selection of the 7-rod bundle.
- 2) A two-pass flow arrangement was required for the clustered rod design in order to minimize flow maldistributions within the cluster. The single-pass arrangement required extremely tight rod-spacing, which could cause large circumferential variations in the clad surface temperature.
- 3) If a specific power approaching that desired in the boiling region was to be obtained in the superheater, the thermal performance of the fuel elements would be more limited by the maximum clad surface temperature than the fuel centerline temperature.
- 4) The coolant pressure drop through the superheat core ranged from 25 to 100 psi, with the smaller value being obtained with large diameter rods.
- 5) The fuel rod diameter required to obtain an average specific power matching that of the boiler was approximately 0.44 in., which yielded a pressure drop of about 75 psi.

The calculation of the temperatures expected within the stainless steel clad UO_2 fuel rods was based on the same model used for the boiler fuel elements except that the thermal resistance of the gap was eliminated because the fuel will be in close contact with the clad at design conditions. The effective thermal conductivity of the fuel was also reduced to 0.85 Btu/hr-ft-°F in consideration of the lower density expected for the sintered, compacted powder. However, since the size and performance of the superheat cores depend primarily on the clad surface temperatures, the temperatures obtained within the rod are of secondary importance except for estimating fission gas release rates and thermal expansion.

The temperature distribution through the integral fuel-moderator element is of interest due to the thick-walled ZrH_x clad. For a maximum centerline and surface temperature of 4000°F and 1250°F respectively, the temperature drop through the moderator wall was found to depend only on the moderator-to-fuel ratio, with the values being as follows:

$\frac{V_m}{V_{UO_2}}$	$\Delta T_{wall} (^{\circ}F)$
1.0	117
1.5	151
2.0	177

These values were used in the thermal stress analysis which is described in Section V. C.

The maximum fuel-clad surface temperature at design conditions was originally set at 1250°F for the parametric studies but deviates somewhat from this value in the reference 300-Mwe designs. The exact value of this parameter cannot be stated with a high degree of confidence due to uncertainties in the local flow and heat transfer coefficients obtained within the rod cluster. Moreover, the value is not the same for all of the concepts studied due to inherent differences in the radial flux distributions obtained and in the two-pass flow arrangements used. In the water-moderated design, both passes are made within a single fuel bundle assembly in order to avoid the use of water-steam seals; and the maximum heat flux thus occurs in the second pass. In the zirconium-hydride design, the entire superheat core region is (of necessity) in a steam environment, which permits a flow arrangement wherein the second pass is made through logs which are in a region of lower power density. This results in a lower maximum surface temperature.

In each case analyzed, it was assumed that the maximum surface temperature occurs at the position of maximum heat flux in the second pass, and that the enthalpy of the coolant at this point is equal to the value at the end of the first pass plus one-half the total enthalpy rise in the second-pass hot channel. Where applicable, the coolant flow was assumed to be orificed in proportion to the radial power distribution so as to obtain a constant outlet temperature. Deviations from this idealized model were accounted for by appropriate hot channel factors.

The average heat transfer coefficients were calculated using the following heat transfer correlation:

$$Nu = 0.023 (Re)^{0.8} (Pr)^{0.4}$$

where the dimensionless parameters have the usual definitions. The coolant properties used were taken from Reference 15.

The peak-to-average heat flux ratios used in calculating the thermal performance characteristics of the reference designs are given in Table VIII. The radial peaking factors are defined as the ratio of the maximum radial heat flux in the subregion corresponding to the first or second pass to the average radial heat flux in the superheat core region. The values shown are those that would be obtained with a hot, clean core with all control rods withdrawn. The axial peaking factor is that of a "slightly" clipped cosine, and the local factors are due to the flux dip within a seven-rod cluster and the flux peak expected between segments of the integral fuel-moderator element.

TABLE VIII
FLUX PEAKING FACTORS IN SUPERHEATERS FOR REFERENCE DESIGNS

	300-Mwe ZBSR-ML	300-Mwe H ₂ O Moderated	300-Mwe ZBSR-IFM	50-Mwe ZBSR-ML
Axial	1.5	1.5	1.5	1.5
Radial, 1st pass	1.24	1.19	1.17	1.24
Radial, 2nd pass	1.06	1.19	1.04	1.06
Local	1.05	1.05	1.05	1.05

A hot-channel factor of 1.15 was applied to the coolant enthalpy rise to allow for the variation in the flow distribution to each moderator log, IFM assembly, or process tube assembly and the variation between individual flow channels within an assembly, referred to the second pass. As such, it may be considered as a specification for the design of the orifices, plenums, and entrance regions to each flow channel, pending detailed design.

The hot-channel factor applied to the average film temperature drop (F_{θ}) is the product of the following factors:

<u>Cause</u>	<u>F_θ</u>
Uncertainty in flow distribution	1.12
Uncertainty in average heat transfer coefficient	1.20
20% power overshoot (steady state)	1.042
Local inhomogeneity, in fuel-loading and enrichment	1.03

Product = 1.44

The "power overshoot" factor is really a 20% over-design factor and implies that both the steam flow-rate and the power increase simultaneously. The transient case, where the steam generated in the boiler is delayed several seconds before it reaches the superheater, would undoubtedly be more serious, but was not considered in detail here.

In addition to these factors, an attempt was made to estimate the temperature variation expected around the circumference of the individual fuel rods which is caused by the maldistribution of coolant flow within the rod cluster. A theoretical approach to this problem was reported by Deissler and Taylor¹⁶ and an extensive experimental heat transfer program is currently being pursued at ORNL in an attempt to measure the temperature and flow distributions for the seven-rod cluster used in the EGCR.

In the study reported,¹⁶ a geometrical, iterative technique was employed to transform the universal, one-dimensional velocity and temperature profiles to a two-dimensional profile for turbulent flow. This fully developed, two-dimensional profile is a function of only the pitch-to-rod diameter spacing parameter. With these results, Deissler and Taylor were then able to determine quite generally the circumferential variation in rod surface temperature as a function of pitch-to-rod diameter ratio, surface heat flux, rod diameter, and the thermal conductivity and thickness of the cladding. Their results are summarized in a set of curves (Figures 8 and 9 of Reference 16) showing the relationship between the following dimensionless quantities:

$$\frac{bk_T (T_{sm} - \bar{T}_s)}{\left(\frac{Q}{A}\right) r^2} = f\left(\frac{P}{D}\right)$$

where

P = rod pitch

D = rod outer diameter

b = clad thickness

k = thermal conductivity of clad

$\frac{Q}{A}$ = average heat flux

r = rod radius

T_{sm} = maximum circumferential clad temperature

$\bar{T}_s$ = average circumferential clad temperature.

The above relationship was also shown to be independent of both the Prandtl number and the Reynolds number. The analysis was restricted to the case of fully developed flow within an infinite bank of rods, and although the direct application of the results to a finite, seven-rod cluster is certainly open to question, it does serve to illustrate some interesting points. Figure 17, for example, shows the circumferential temperature rise ($T_{sm} - \bar{T}_s$) at the position of maximum heat flux for the water-moderated, superheat fuel rods as a function of the rod pitch-to-diameter ratio for both square and triangular pitch. The circumferential temperature rise at the hot spot for the ZBSR would be about 10% lower, due to the lower maximum heat flux obtained in the second pass. The value of using a triangular pitch is apparent, as is the case for maintaining a reasonably large pitch-to-diameter ratio. The P/D for the seven-rod cluster used in the reference designs is 1.21, for which this analysis would predict a maximum circumferential temperature rise of about 40°F in the water-moderated design, and 36°F in the ZBSR.

Applying the same procedure to the integral fuel moderator element results in a maximum circumferential temperature rise of only 5°F, due to the thick-walled cladding and a lower surface heat flux at the hot spot. The validity of this estimate is highly questionable, however, since the heat transfer model assumed for the calculation of clad temperatures in Reference 16 is valid only for thin-walled tubes. The radial and circumferential conduction in the IFM element is complicated by the fact that it is composed of a thick-walled zirconium

Figure 17. Maximum Circumferential Temperature Rise Above the Average Clad Surface Temperature at the Position of Maximum Heat Flux vs Pitch-to-Diameter Ratio

hydride tube clad with a thin layer of ceramic diffusion barrier and a thin steel jacket. An analytical solution to this two-dimensional, four-region problem would be interesting, but was felt to be beyond the scope of this study.

The foregoing analysis of local temperature variations is based on the assumption of fully developed turbulent flow conditions which would exist well downstream of entrance regions and in the absence of rod spacers. Recent experiments performed at ORNL, however, have shown that the flow distribution and temperature variations within the EGCR seven-rod cluster are strongly dependent on spacer geometry and orientation.^{17,18,19} For the spacer designs tested, the results show that the circumferential ΔT around the rods is largest

immediately downstream of the spacers, but the average heat transfer coefficient is also highest in this region due to the interruption in the boundary layer. Thus, the maximum surface temperatures are obtained immediately upstream of the last spacer in the bundle ($L/d_e = 35$), even though the circumferential ΔT is least at this point.

Despite the uncertainties involved, the theoretical analysis of Deissler and Taylor was tested against the EGCR experimental data reported for test series 4-A at an $L/d_e = 35$. At this point, the difference between the maximum and the average circumferential temperature is approximately 3 to 4°F for one tube and 5 to 6°F for another. (The low values are due to the low heat flux used for the tests: 6,500 Btu/hr-ft².) For these test conditions, the theory predicts a temperature rise of 2.3°F. Although this calculation does not constitute a verification of the theory, the agreement is better than could be expected. It is also noted that the experimental values are higher, probably due to residual maldistribution of flow caused by the midplane spacers which are located at an $L/d_e = 17.5$.

In the seven-rod cluster used for the reference designs of this study, spacing is provided by a series of short, spiral-wire wraps which are brazed to the clad on alternate rods at 12-in. intervals, along the length. Thus, the flow would be disturbed every 6 in. ($L/d_e \approx 19$) in a most unpredictable and undoubtedly nonanalytic manner.

Based upon these considerations, it is concluded that the uncertainty in the maximum clad surface temperature for a clustered rod arrangement is rather large and would probably remain so until definitive tests were run with the actual configurations of interest under conditions closely approximating those expected in the reactor. The theory discussed above does serve to illustrate the approximate magnitude of the problem and to indicate some of the controlling parameters. For this reason, the theoretical circumferential temperature rise calculated according to the theory of Deissler and Taylor has been included in the maximum surface temperatures quoted in the summary tables for each design.

c. Pressure Drop

The steam pressure drop was estimated using standard procedures for the friction and momentum losses. An average steam density was computed

for each pass, based on a cosine axial power distribution. The largest uncertainty in the analysis is felt to be in the losses due to the short, spiral wire spacers. An effective "K" factor of 0.1 was used for each spacer stage, which yields approximately the same fractional increase in pressure drop (over that due to friction alone) as was reported for the EGCR seven-rod cluster and spacer design.¹⁸

Superheat core pressure drop for the reference ZBSR moderator log and the water moderated concepts are shown below.

	<u>ZBSR-ML</u>	<u>H₂O-Moderated</u>
<u>1st Pass</u>		
Friction	17.2	16.8
Momentum	3.8	3.7
Spacers	<u>6.5</u>	<u>6.4</u>
Subtotal	27.5	26.9
<u>2nd Pass</u>		
Friction	30.9	23.4
Momentum	6.8	5.1
Spacers	<u>11.7</u>	<u>8.9</u>
Subtotal	49.4	37.4
TOTAL	76.9	64.3

These values are in reasonable agreement with the 75-psi pressure drop used for the heat balance calculations. An additional allowance of 35 psi has been included in the plant heat balance to account for uncertainties in the analysis and for losses in the steam line, nozzles, shield plug, etc.

d. Moderator Heating

An estimate of the amount of heat absorbed by the moderators in the superheat region of the core is required for both the zirconium-hydride and the water-moderated designs. In the case of the ZBSR concept, the temperature distributions and thermal stresses set up in the moderator logs are directly dependent on the volume heat generation. In the water-moderated design, the

power split and thus the size of the superheat region depends on the amount of heat generated in and transferred to the water.

The fraction of the total fission energy available for heating the two moderators was calculated to be as follows.

<u>Source</u>	<u>Energy (Mev/fission)</u>	
	<u>ZrH_x</u>	<u>H₂O</u>
Fast neutrons	5.0	5.0
Prompt fission gammas	7.4	7.4
Fission product decay gammas	6.0	6.0
Capture gammas	<u>6.5</u>	<u>6.0</u>
TOTAL	24.9	24.4

All of the neutron energy was assumed to be absorbed in the moderators, and the gamma absorption calculations were performed by lumping the gamma source into two energy groups: one at 3 Mev and the other at 1 Mev. The energy absorption coefficients were then determined for these energies. The computational model used to calculate the spatial distribution of gamma heating assumed that the fuel channels served as a uniformly distributed source of gammas. The fraction of this total source that is absorbed in the moderator was then calculated in accordance with the methods presented in Reference 20.

The results of this analysis are presented in Figure 18, which shows the percent of the total superheat power absorbed in zirconium hydride and water as a function of the moderator-to-fuel volume ratio. It will be noted that the heat generated in the zirconium hydride is greater than that generated in the water due to its higher gamma-absorption characteristics. For the moderator-to-fuel volume ratios used in the reference design, the energy absorption is thus estimated to be about 5.4% in the zirconium hydride and about 3.2% in the water.

The heat transferred across the double-walled, insulated process tubes from the hot steam to the relatively cold water moderator was calculated for a range of process tube diameters and steam gap widths. The calculations were based on the assumption that the heat is transferred across the narrow, stagnant steam gap only by radiation and conduction. For the purpose of

Figure 18. Percent of Total Superheat Power Absorbed in Moderator
vs Moderator-to-Fuel Volume Ratio

calculating radiant heat losses, it was assumed that the temperature of the inner flow tube at a given axial position was equal to the temperature of the steam coolant at that point and the temperature of the outer pressure tube was equal to that of the water (600°F). The emissivity of the metal surfaces was set at 0.5, based on the results of experiments reported in Reference 21. Since both the conductivity of the steam and the radiant heat transfer are a function of temperature, the analysis was performed by dividing the process tube into finite, axial segments and obtaining an average conductance for each segment. These were then combined to obtain the average heat loss per foot of process tube, which is shown in Figure 19 as a function of the flow tube diameter with steam gap width as parameter. Using these results, a heat loss of approximately 1.8% of the superheat region power is thus estimated for the reference water-moderated design configuration. When this is added to the 3.2% volume heat generation, a total of approximately 5% is obtained. This was increased by 20% to account for uncertainties in the analysis and for additional heat losses in the ducts, plenums, etc., which brings the total estimated heat loss to the water moderator to 6%.

Figure 19. Average Heat Loss per Foot of Process Tube vs Flow Tube Diameter for Varying Steam Gap Width

e. Temperature Distribution in Moderator Logs

The temperature distribution in the moderator logs was calculated using an average volume heat source of 5.4% of the superheat power as determined from the foregoing analysis. The spatial distribution of the moderator heat source (i.e., peak-to-average ratio) was assumed to be the same as the fission power distribution in the superheat core. For this case, the maximum radial temperature gradient (and thus thermal stress) is obtained in the first pass and the maximum hydride temperature occurs near the outlet of the second pass. The moderator surface temperature follows the local steam coolant temperature rather closely, (ignoring axial conduction) and never exceeds it by more than approximately 45°F. Thus, the most significant parameter is the radial-temperature gradient which determines the maximum thermal stress.

The radial temperature distribution in the moderator logs was estimated by replacing the two-dimensional moderator cell (surrounding each flow channel) with an equivalent, one-dimensional cylindrical cell. Since this approximation could lead to an underestimate of the temperature rise and thermal stress in the moderator if the analytical model is set up so that the cell area is preserved,

two cases were analyzed in an attempt to bracket the problem. For the "worst" case, the radius of the outer cell boundary was set equal to the distance from the center of the flow channel to the corner of the actual square cell. For the "best" case (i.e., optimistic) the radius of the outer boundary was set equal to that of an equivalent cylinder with the same cross-sectional area as the true cell. The same volumetric heat source, or moderator power density, was used for both calculations, but the total heat flux across the inner boundary was held equal to that of the actual cell for the purpose of calculating the temperature drop through the film, clad, and diffusion barrier. With these approximations the temperature rise in the zirconium hydride is given by:

$$T_b - T_a = \frac{qb^2}{4k} \left(\frac{a^2}{b^2} - 1 + 2 \ln \frac{b}{a} \right)$$

where

b = outside radius of the cylinder

a = inside radius of the cylinder

q = moderator power density

k = thermal conductivity.

The results of this analysis are tabulated below for both cases:

	<u>Worst Case</u>	<u>Best Case</u>
Outer cell radius (in.)	1.355	1.084
Inner cell radius (in.)	0.865	0.865
$q_{\max}$ (Btu/hr-in. ³)	691	691
$\Delta T_{\max}$ (°F)		
ZrH _x	88	17
Diff. barrier	20	20
SS Clad	2	2
Film	23	23
Total $\Delta T_{\max}$	133	62

It is noted that the temperature rise in the zirconium hydride differs by a factor of 5, depending on the assumptions used. A more rigorous analysis of the two-dimensional geometry would probably be justified, therefore, in a final design. The analysis of the thermal stresses in the moderator is described in Section V.C.

B. NUCLEAR

1. General

The primary objective of the nuclear analysis of this study was to provide a basis for making valid comparisons between various types of water- and zirconium-hydride-moderated systems. During the study, a large number of fuel element designs was analytically investigated; and several of the more promising core configurations were selected for further examination. These selected cores were optimized to the extent permitted by the ground rules and scope of the study, and were then compared on an economic basis.

Since all of the systems considered were large, well thermalized, and hydrogen-moderated, most of the nuclear calculations were done using relatively standard techniques. The only major problem not amenable to well-established methods was that of adequately describing neutron thermalization in zirconium hydride. Due to strong chemical binding effects, the thermal neutron energy spectrum in ZrH does not resemble the usual hardened Maxwellian or Wigner-Wilkins spectrum.^{22,23,24} Consequently, it was necessary to develop suitable methods for handling the problem. A mathematical model describing the thermalization process in ZrH_x was defined, and the equations were coded in FORTRAN for solution by the IBM-709.

2. Thermalization in Zirconium Hydride

When zirconium metal is hydrided, the hydrogen atoms occupy the interstitial spaces between atoms in the face-centered cubic zirconium lattice. Each hydrogen atom is enclosed by a regular tetrahedron of zirconium atoms, and is free to vibrate approximately isotropically about the center of the tetrahedron. Due to quantum effects, the amount of vibrational energy that the hydrogen atom may possess, relative to the surrounding zirconium lattice, is limited to a few discrete levels. These permissible energy states are uniformly spaced, having a separation of approximately 0.13 ev. The hydrogen atom, in scattering interactions with thermal neutrons, may jump from one of these

energy states to another. This process is the primary mechanism of energy exchange between thermal neutrons and ZrH_x . If this were the only mode of energy transfer, thermal neutrons could change energy only in amounts that are multiples of 0.13 ev. However, in addition to the above-described optical modes of vibration, acoustical vibration also occurs. Local motion of the zirconium lattice is transmitted to the tightly bound hydrogen atoms, and through this mechanism neutrons may exchange energy with the lattice. Energy transfer through acoustic modes involves amounts of energy that are small compared to 0.13 ev. This effect, which tends to smooth out the sharp peaks in the thermal spectrum, has a small effect on neutron thermalization in ZrH_x , and was assumed to be negligible at operating temperature.

Since there was no readily available method for adequately describing neutron thermalization in ZrH_x , a code was written in FORTRAN to numerically solve the space-independent Boltzmann equation in 65 thermal energy groups. The procedure followed by this code is as follows.

- a) Input number densities and microscopic library cross-sections for each group are combined to obtain microscopic group constants.
- b) A 65-by-65 array of temperature-dependent transfer coefficients is formed. The scattering model used is that of an isotropic harmonic oscillator.
- c) A typical ZrH_x spectrum is chosen as a starting solution, and iteration on the flux is continued until the flux in all groups has converged to within a prescribed value. A variable, linear extrapolation technique is used to speed convergence.
- d) Macroscopic and microscopic cross-sections are averaged over the converged spectrum.

The results of calculations done using this code are presented graphically. Figure 20 shows a typical set of spectra in ZrH_x as a function of temperature. The characteristic shape of these curves compares well with other calculations using the same scattering kernel.

The thermal spectrum in a ZrH_x -moderated core is somewhat harder than in a water-moderated core. This hardening seems to be most pronounced at low temperatures. Figure 21 shows a comparison of the spectrum-averaged

Figure 20. Neutron Spectrum in ZrH_x vs Moderator Temperature

Figure 21. Spectrum Averaged $\eta(\text{U-235})$ vs Moderator Temperature for H_2O and ZrH_x

$\eta(\text{U-235})$ as a function of moderator temperature for two moderating materials, H_2O and ZrH_x .

Figure 22 shows the spectrum for a typical ZrH_x -moderated core at operating temperature. Superimposed is a Wigner-Wilkins spectrum for a typical H_2O -moderated core at operating temperature.

Figure 22. Comparison of Typical Thermal Neutron Spectra in H_2O and ZrH_x Moderated Cores

3. Basic Analytical Techniques

All diffusion calculations were done using two neutron energy groups. Extensive use was made of the IBM-709 and 7090 digital computers. All fast group (0.625 ev to 10 Mev) nuclear constants were obtained using the MUFT-4 code²⁵ to generate a fast spectrum and average the cross sections from the AI production MUFT library over this spectrum. The effective U^{238} resonance integral used was taken from Hellstrand's experimental fit²⁶ in $\sqrt{S/M}$. Dancoff corrections were applied to the actual fuel surface to obtain the effective fuel surface. In H_2O moderated regions, the SOFOCATE code was used to generate a Wigner-Wilkins thermal spectrum and to average cross-sections over this spectrum.²⁷

For ZrH_x -moderated regions, the code developed for this study was used to generate the characteristic Zirconium-Hydride Spectrum and to average cross-sections over this spectrum. The thermal group flux disadvantage factor for the cell was determined using the I_2 code, a cylindrical geometry P-3 approximation.²⁸

In the case of clustered-rod fuel elements having negligible moderation internal to the bundle of rods, all materials within the cluster (fuel, clad, spacers, etc) were homogenized together in one region. The core eigenvalue and gross radial flux and power distributions were obtained by using the AIM-5 one-dimensional diffusion theory code.²⁹

Burnup calculations were done using the "zero dimensional" approach.³⁰ The different equations in Reference 31 were coded in FORTRAN and solved using the IBM-709. Control rods were simulated by thermal poison which was adjusted to set $k_{\text{eff}} = 1.00$ at each time-step. Region and group-dependent bucklings were calculated for the hot clean core, and it was assumed that these bucklings would remain constant throughout core life.

4. Fuel Element Survey

After arriving at a calculational method for analyzing ZrH_x -moderated systems, an extensive survey was made of the nuclear properties and characteristics of these systems. The results of this survey are illustrated in Figure 23 which shows the effect on reactivity of enrichment, moderator-to-fuel ratio and rod diameter for an infinite lattice of steel-clad UO_2 fuel pins in a ZrH_x moderator.

The curves shown in Figures 24 and 25 were generated in order to compare ZrH_x and H_2O as moderators. Figure 24 is a plot of $k_{\infty} \text{ vs } V_m/V_{\text{UO}_2}$ for the two moderators; and Figure 25 shows $\text{I.C.R.}_{\infty} \text{ vs } V_m/V_{\text{UO}_2}$. The fuel rod diameter, fuel enrichment, and steel/fuel volume ratio were kept constant for all of these calculations, so that differences are due to moderator effects only.

These graphs show the existence of the following differences in the two moderating materials.

- a) The maximum reactivity occurs at a moderator-to-fuel volume ratio of approximately 1.5 for ZrH_x , as compared to approximately 2.5 for H_2O . This difference is due primarily to the difference in hydrogen density. ZrH_x may be obtained with hydrogen densities at operating temperatures of approximately 0.06×10^{24} atom/cc as compared to approximately 0.04×10^{24} atom/cc for H_2O at typical BWR temperatures and pressures. The major consequence of this difference is to make ZrH_x -moderated cores typically more compact than H_2O cores.

Figure 23. k_{∞} vs V_m/V_{UO_2} in Zr-H_x
Superheat Region for Single Rod
Fuel Element

- b) The peak reactivity obtainable with ZrH_x moderation tends to be less than that obtainable with H₂O (approximately -7.5%Δk) for the representative case shown in Figure 24). Since the H/U atom ratio at the point of maximum reactivity is slightly less for ZrH_x than for H₂O, (due to parasitic capture in zirconium), part of the difference in peak reactivity is due to increased epithermal conversion in the ZrH_x system. However, most of the difference is due to the presence of zirconium as a thermal poison.

A measure of the increased absorption may be obtained by examination of the 2200 m/s cross sections for H₂O and ZrH_x where x is typically 1.7.

Figure 24. Comparison of k_{∞} vs V_m/V_{UO_2} for $ZrH_{1.7}$ and H_2O with Equal Steel and Void Fraction

Figure 25. Comparison of ICR ∞ vs V_m/V_{UO_2} for H_2O and $ZrH_{1.7}$ with Equal Steel and Void Fractions

the product $\eta_f(U-235)$ is only slightly reduced in ZrH_x due to thermal spectrum effects.

Figure 25 shows that the conversion ratio of a typical ZrH_x system is larger than that of a comparable H_2O system (both taken at the point of maximum reactivity). This difference is the result of the smaller H/U atom ratio and the harder thermal spectrum in ZrH_x .

After examining the configuration of a single fuel-pin surrounded by moderator, a pin diameter was selected and clustered-rod-type superheater elements were investigated both for ZrH_x and H_2O moderation. Figure 26 shows k_{∞} vs number of rods in the cluster for both ZrH_x and H_2O . For this case, the enrichment and rod diameter were kept constant and the point at the peak of the reactivity vs moderator-to-fuel ratio was plotted. The peaks in the curves shown in Figure 26 are due to the interplay between fast and thermal effects (as in the case for the k_{∞} vs V_m/V_{UO_2} curve). It is desirable to design a superheater

$$\begin{aligned}\sigma_a(H) &= 0.332 \text{ b} \\ \sigma_a(Zr) &= 0.18 \text{ b} \\ \sigma_a(ZrH_{1.7}) &= 0.438 \text{ b per H atom} \\ \sigma_a(H_2O) &= 0.332 \text{ b per H atom}\end{aligned}$$

This is an increase of 32% in the thermal group moderator absorption.

Another effect that contributes to the reactivity difference is the hard thermal spectrum in ZrH_x . Figure 21 shows the variation of the spectrum-averaged $\eta(U-235)$ with moderator temperature for comparable H_2O and ZrH_x systems. Although $\eta(U-235)$ tends to be smaller in ZrH_x moderated cores, there is a partially compensating effect in the increased thermal utilization caused by a smaller thermal flux disadvantage factor. Thus

fuel element near the peaks in the curves of k_{∞} vs V_m/V_{UO_2} and k_{∞} vs number of rods in cluster. This avoids temperature-void instabilities on one hand and a positive flooding coefficient on the other.

Figure 26. k_{∞} vs Number of Fuel Rods Per Cluster

From these survey calculations, four superheater designs were selected for study when coupled to a boiler region. The same boiler element design was used with each of the four superheater elements.

5. Two-Region Core Analysis and Optimization

Two group constants were generated as a function of enrichment for each of the four superheater compositions and for the single boiler composition. Figure 27 shows k_{∞} vs enrichment, and Figure 28 shows the infinite I. C. R. vs enrichment for all of these compositions.

Each of the four cores that may be made from the above set of compositions was studied for various combinations of boiler and superheater enrichment.

Figure 27. k_{∞} vs Enrichment

Figure 28. Initial Conversion Ratio vs Enrichment

The nuclear and heat transfer coupling of the two regions of an integral boiling-superheating core makes the basic analysis unusually laborious. The power split* between the two regions is fixed by heat-transfer considerations; and thus for each value of k_{eff} of the core, the enrichment of both core regions is uniquely determined.

Nine cases were run for each of the four cores: three enrichments in the superheater region for each of three enrichments in the boiler region. From these nine cases, the proper enrichment for each region was selected to give the desired k_{eff} and power split. This process was done by graphical interpolation in the following manner.

- a) Power split was plotted against boiler enrichment for various superheater enrichments, as is shown in Figure 29. From this graph, E_{SH} vs E_B for the desired power split was taken.

Figure 29. Initial Power Split vs Region Enrichments

*Power split is defined here as the fraction of the total core power that is produced by the superheater.

Figure 30. k_{eff} vs Region Enrichments

Figure 31. Boiler Enrichment vs Superheater Enrichment for Proper Power Split with k_{eff} as Parameter

Figure 32. Radial Power Distribution for Water Moderated Concept

Figure 33. Radial Power Distribution for ZrH_x Moderator Log Concept

- b) K_{eff} was plotted against boiler enrichment for various superheater enrichments, as is shown in Figure 30. From this graph, E_{SH} vs E_{B} for the desired k_{eff} was taken.
- c) The two curves of E_{SH} vs E_{B} were cross-plotted, as is shown in Figure 31. The point of intersection yields the E_{SH} and E_{B} , which give the desired power split and k_{eff} .

The nuclear part of the optimization of these cores consisted of varying the volumes of the two regions so that the calculated radial peak-to-average power ratios would approach those required for satisfactory heat-transfer performance.

The following tables list the nuclear characteristics of the reference design cores. Table IX gives the volume fractions for the various region compositions; Table X is a comparison of the basic nuclear parameters; and Table XI lists the neutron economy of the zirconium-hydride-moderated moderator log concept and the water-moderated core with Zircaloy process tubes. Figures 32 and 33 show typical radial power distributions for each of these two cores.

TABLE IX
VOLUME FRACTIONS FOR SUPERHEAT AND BOILER REGIONS

SUPERHEAT			
<u>H₂O-Moderated</u> <u>Zr-2 Process Tubes</u>		<u>H₂O-Moderated</u> <u>St Stl Process Tubes</u>	
Fuel	0.1645	Fuel	0.1645
Steel	0.0448	Steel	0.0780
ZR-2	0.1170	ZR-2	0.0784
Void	0.2879	Void	0.2933
H ₂ O	0.3858	H ₂ O	0.3858
<u>ZrH_x - Moderated</u> <u>Moderator Log</u>		<u>ZrH_x - Moderated</u> <u>Integral Fuel-Moderator</u>	
Fuel	0.2078	Fuel	0.2224
Steel	0.0692	Steel	0.0318
ZrH _x	0.3029	ZrH _x	0.3318
Void ^x	0.4201	Void ^x	0.4140
BOILER			
Fuel	0.2288		
Zr-2	0.1898		
Void	0.0072		
H ₂ O	0.5742		

TABLE X
NUCLEAR PARAMETERS

	Water-Moderated				Zirconium-Hydride-Moderated			
	Zr-2 Process Tube		Steel Process Tube		Moderator Log		Integral Fuel-Moderator	
	Boiler	Superheater	Boiler	Superheater	Boiler	Superheater	Boiler	Superheater
Initial Enrichment Initial [*] Conversion Ratio	2.59	2.62	2.64	3.50	2.53	3.39	2.47	2.79
Gms Pu Kg U at 10,000 Mwd/MTU	0.564	0.530	0.562	0.475	0.575	0.548	0.580	0.639
Epithermal Fissions (%)	5.22	4.85	5.22	4.73	5.27	5.22	5.28	5.73
Radial Peak-to-Average Power Ratio	17.6	16.5	17.8	19.7	17.4	22.3	17.2	19.5
	1.29	1.19	1.28	1.36	1.31	1.25 [†] 0.99	1.33	1.14 [†] 1.00

*Effect of control rods was taken into account in calculating these initial conversion ratios.
†Two peak-to-average ratios are given for the cores which have a two-pass superheat region.

TABLE XI
NEUTRON BALANCE

	Water ZR-2 Process Tube		Zirconium-Hydride Moderator Log	
	Superheater	Boiler	Superheater	Boiler
Fast Group				
Capture (235)	2.15	2.02	3.15	2.00
Fission (235)	5.40	5.06	7.87	5.02
Capture (238)	18.53	20.19	21.24	20.47
Fission (238)	2.26	2.26	2.39	2.29
Other Absorption	4.30	2.21	5.47	2.24
Leakage	-1.78	9.79	-1.14	8.63
NU-Fission (235)	13.33	12.51	19.44	12.39
NU-Fission (238)	6.06	6.05	6.40	6.13
Thermalized	69.12	58.47	61.02	59.35
Thermal Group				
Capture (235)	7.72	7.86	7.16	7.86
Fission (235)	38.75	39.09	36.11	39.07
Absorption (238)	7.37	7.55	5.45	7.54
Other Absorption	15.32	5.45	12.85	5.76
Leakage	-0.04	-1.48	-0.55	-0.88
NU-Fission (235)	95.72	96.55	89.18	96.50
K_{eff}	1.1511	1.1511	1.1502	1.1502

C. MECHANICAL

1. Boiler Fuel Elements

a. Clad Thickness

The thickness of the Zircaloy-2 tubing used to clad the boiler fuel elements was set by the criterion that it be free-standing when exposed to an external collapsing pressure 50% higher than the nominal reactor operating pressure. The equation used for this calculation is based on Love's formula for the unstable collapse of thin-walled tubes:³²

$$t = D \left[\frac{P(1-\mu^2)}{2CE} \right]^{1/3}$$

where

t = tube wall thickness

D = tube OD

P = external pressure

μ = Poisson's ratio

E = elastic modulus

$C = (t_{\min}/t_{\max})^3 (D_{\min}/D_{\max})^3$.

The mechanical properties of Zircaloy-2 were taken from Reference 12 and are based on an average metal temperature of 650°F. For the 0.450-in.-OD tube, a clad thickness of 21 mils is required, to which was added a 2-mil tolerance and a 2-mil corrosion allowance.

b. Differential Expansion

The differential thermal expansion between the stack of UO₂ pellets and the Zircaloy cladding was calculated assuming that the axial expansion of the fuel is governed by the centerline temperature. The average centerline temperature in the hottest segment is approximately 4000°F, not including the local end-cap peaking factor. For these conditions, the differential expansion is approximately 23 mils/in., or 0.6 in. per segment.

In calculating the radial expansion of the UO_2 , it was assumed that the pellet cracks and thus expands linearly. The average temperature for this case was estimated to be 2500°F, for which the diametral, differential expansion between the pellet and the clad is 5 mils. An initial clearance of 6 mils would thus yield a hot, radial gap of about 1/2 mil.

c. Internal Pressure

The stress on the cladding due to internal fission-product gas release was also checked for conditions that might exist at the end of a fuel cycle when the reactor is depressurized. The maximum burnup expected in a fuel segment is approximately 16,000 Mwd/MTU, for which the internal gas pressure was calculated from the following expression:³³

$$P = T \left[\frac{5 \times 10^{-3} \phi}{V_g} + 1.9 \times 10^{-3} \beta \right]$$

where

ϕ = fractional gas release

T = gas temperature (°F)

V_g = fractional gas space

β = initial helium loading pressure (atmospheres)

P = pressure (atmospheres) .

The gas expansion space in the reference design boiler fuel rods is 5% at the cold, shutdown conditions; and at a temperature of 300°F, it was found that the cladding could safely contain 100% of the fission gasses generated without exceeding two-thirds of the yield strength of Zircaloy-2.

2. Superheat Fuel Elements

a. Clad Thickness

The cladding thickness specified for the UO_2 rod type, superheat fuel elements is based primarily on the requirement that they contain fission-product gas pressure when the reactor is depressurized during a normal refueling

period. This criterion was chosen after considering several other possibilities, such as containment at design temperature but with reduced external pressure. The sequence of events necessary to produce this condition, however, would undoubtedly be more severe than the condition itself. For example, if it is postulated that the main steam line ruptures, the resultant pressure differential created within the reactor vessel would easily collapse the internal steam ducting, thus cutting off the coolant flow to the superheat core. The fuel rods, moderator logs, etc., would also be hurled towards the exit nozzle with an instantaneous acceleration of 25 to 50 g's. Obviously, the cladding could not be made thick enough to insure fission-product containment for this type of accident.

Consideration of less severe transients led to the conclusion that the clad temperature might be as high as 600 to 700°F at the time when the system pressure is down to about 100 psi, assuming that the steam flow-rate is throttled proportionately to the decay heat rate. In this temperature range, the ASME code for unfired pressure vessels specifies a maximum allowable stress of approximately 15,000 psi for Type 304 stainless steel, seamless tubing.

Assuming that the maximum internal gas pressure is equal to the reactor system pressure at operating conditions, it would be down to about 1200 psi at 700°F. The clad thickness required to contain this pressure for these conditions is approximately 16 mils. To this was added a 3-mil corrosion allowance and a 1-mil tolerance which brings the total clad thickness up to 20 mils. The corrosion allowance is based on the data presented in Reference 34.

The expansion space provided in the design of the reference superheat fuel elements is sized to accommodate a release of approximately 25% of the gaseous fission-products formed during the fuel cycle without exceeding an internal pressure of 1500 psi. For this calculation it was assumed that the fission products will diffuse through the fuel and thus establish an equilibrium pressure which is uniform over the length of the rod. Although fission-gas-release data for sintered, compacted UO_2 powder could not be found, the expansion space could be increased without compromising the design if experimental data should indicate that the gas release rate is higher than the value assumed for this study.

b. Thermal Distortion

The temperature difference expected around the circumference of individual rods in the reference design fuel-element is known to create a thermal

distortion problem. This may be illustrated by considering a large bundle of rods, spaced at regular intervals. In the region of closest approach between adjacent rods, the clad surface temperature is higher than the average temperature around the rod, due to the local reduction in flow and heat transfer coefficient. If one rod is slightly off-center, the flow passage on one side is reduced and the surface temperature of the rods facing this narrower passage will increase. Differential expansion causes the rods to bow in the direction of their nearest neighbor, (which aggravates the problem) and the process is thus autocatalytic. In the present case, an attempt was made to limit the amount of bowing by adjusting the distance between the rod spacers.

The maximum deflection of the rods in the seven-rod cluster was calculated using the following expression:¹⁷

$$\epsilon = \frac{\alpha \Delta T_c L^2}{8D}$$

where

α = coefficient of thermal expansion

ΔT_c = maximum circumferential temperature difference

L = unsupported length

D = rod diameter.

The spiral-wire-wrap spacers were arranged so that the rods are supported from each other every six inches. For a maximum ΔT_c of 80°F (which is twice the difference between the maximum and the average circumferential temperature), the maximum deflection would be approximately 10 mils per rod. This reduces the pitch-to-diameter ratio, which increases the ΔT_c , as was shown in Figure 17. By successive iteration, it was found that the bowing would stabilize at a ΔT_c of about 100°F, for which the deflection is approximately 12 mils, assuming both rods deflect equally. Considering the uncertainties inherent in the estimate of the circumferential temperature differences, however, it is impossible to guarantee the thermal stability of the seven-rod cluster as designed. A full-length wire-wrap might be required, although

this would increase the coolant pressure-drop appreciably. An alternate fuel geometry would probably be preferable.

c. Thermal Expansion

The calculation of the thermal expansion of the fuel relative to the clad is complicated by the fact that the clad will collapse on the fuel at operating temperature and pressure. If it is assumed that the clad is completely collapsed so that all of the external pressure is transferred to the fuel-clad interface, the friction force opposing relative movement of the two materials would be about 1 ton per in. of fuel rod. The stress in the clad wall which would be necessary to overcome friction would be approximately 80,000 psi, which is several times the tensile strength of the material at design temperature. Since the pressure required to compact UO_2 powder to densities less than that obtainable with sonic compaction is more than 40 ton/in.², it is obvious that the cladding will yield and stretch with the UO_2 . If it is assumed that the axial expansion of the UO_2 is proportional to the centerline temperature, the maximum differential expansion between the fuel and the clad would be about 1.5%. This is the amount the clad would stretch at the position of the maximum heat-flux in the first pass, which is well beyond the proportional limit for Type 304 stainless steel. The full-length fuel rod would expand by more than an inch. If the axial expansion is governed by the average radial fuel temperature, the maximum strain and elongation would be reduced by about 1/2.

Considering the ductile nature of austenitic stainless steel, however, it cannot be concluded that this type of abuse will cause structural failure, in the usual sense. About all that can be said is that the clad will be stretched beyond its yield point during operation; and that the resultant stresses might be relieved, either by creep or by the fuel contracting to its original size and shape at shut-down when the cladding is called upon to contain the internal gas pressure. It is believed that the differential expansion problem would be more severe if pellets were used in place of compacted powder. In this case, the clad might be stretched only in the region between pellets and the local strain would thus be much larger.

From this discussion it can be seen that analysis cannot be used to guarantee the integrity of superheat fuel elements designed according to the thin-clad concept. This assurance must await in-pile operation and testing.

3. Process Tubes

The thickness of the outer pressure-tube and the inner flow-tube which together make up the process-tube assembly for the water-moderated concept was at first thought to be relatively important for this comparison study, since much of the difference in the parasitic neutron absorption between the two concepts is at stake if stainless steel is used for both tubes. When it was determined that Zircaloy-2 could probably be used for the pressure-tube, however, the importance of this parameter diminished somewhat.

The process-tube assemblies are designed so that the flow-tube is always in tension and subject to a maximum pressure differential which is equal to the coolant pressure drop for one pass. The maximum pressure differential will occur at the entrance to the second pass, at which point the steam temperature is approximately 800°F. The ASME pressure-vessel code specifies a maximum allowable stress of 8500 psi for Type 304 stainless steel, welded tubing at this temperature. Using this value and applying a safety factor of 1.5 on the pressure differential to account for the increased pressure drop that would occur in a power overshoot results in a required thickness of about 6 mils for the 1.70-in.-diameter flow tube. An additional 4 mils was added to account for corrosion on both surfaces and mechanical tolerances which brings the total nominal thickness to 10 mils.

The thickness of the outer pressure-tube was calculated using the equation described previously for the design of the boiler fuel-cladding. In this case, the pressure-tube at the exit from the second pass is subject to a collapsing pressure equal to the total steam pressure drop through the superheat core, which is estimated to be about 65 psi for the reference design. This was increased to 100 psi to provide a safety factor of about 50%. Since the pressure-tube is in direct contact with the water, the material properties were evaluated at 600°F. For the reference designs, this analysis resulted in a wall thickness of 30 mils of stainless steel or 35 mils of Zircaloy-2, including an allowance for corrosion and mechanical tolerances.

It should be noted that the thickness of the pressure-tube varies as the cube root of the design pressure, and thus a factor-of-two increase in ΔP would theoretically require an increase of only 26% in wall thickness.

4. Moderator Logs

a. Thermal Stress

The calculation of the radial thermal stress in the ZrH_x moderator was based on the following formula for the elastic thermal stress in a hollow cylinder with internal heating:

$$\sigma_{\max} = \frac{E}{1 - \mu} \left[\frac{q}{8k} \left(b^2 + a^2 - \frac{b^2 - a^2}{\ln \frac{b}{a}} \right) + \left(T_b - T_a \right) \left(\frac{b^2}{b^2 - a^2} - \frac{1}{\ln \frac{b}{a}} \right) \right]$$

where

b = outside radius of the cylinder

a = inside radius of the cylinder

q = moderator power density

k = thermal conductivity

α = coefficient of thermal expansion

E = elastic modulus

μ = Poisson's ratio

T_b = moderator temperature at outer boundary, and

T_a = moderator temperature at surface.

The maximum thermal stress occurs at the inner boundary which is in tension. Using the two values for the maximum moderator temperature rise calculated previously, the maximum thermal stress predicted for the "worst" and "best" cases are 6450 and 1340 psi, respectively. The higher value is about 80% of the lowest estimate for the ultimate tensile strength of delta-phase zirconium hydride, which is not exactly a comfortable margin for a brittle material. A generalized, two-dimensional analysis of the thermal stresses in cylindrical and hexagonal cells performed at Batelle,³⁵ however, indicates that this value is probably too high. For example, if the present moderator cell were hexagonal, the maximum thermal stress would only be about 17% higher

than that calculated for the equivalent cylinder with equal cross-sectional area, or approximately 1570 psi. Thus, a maximum thermal stress in the range of 3000 to 4000 psi would appear to be a conservative estimate for the square cell. If a more detailed analysis of this problem indicated that the thermal stress is larger than this, a strong incentive would exist for changing the geometry from that shown to a hexagonal lattice.

b. Migration Stress

The diffusion of hydrogen which occurs in the presence of a temperature gradient in zirconium hydride will eventually result in a "migration" stress which is opposite in sign to the thermal stress, and which may be greater in absolute magnitude, as mentioned previously. A theory for the hydrogen migration phenomenon has been described,³⁶ in terms of the heat of transport, for which a value has been determined experimentally. The mathematical solution for the equilibrium hydrogen distribution, however, is intractable for the two-dimensional geometry of interest here. An alternate approach (which is known to be overly conservative) was used, therefore, to estimate the migration stress on the basis of an equivalent "migration expansion" coefficient which was compared with the thermal expansion coefficient and thermal stress. If it is assumed that the hydrogen partial pressure will be equal throughout the material after an infinite period of time, the variation in H/Zr ratio can be determined from Figure 2 for a given ΔT . The corresponding change in moderator volume can then be obtained from the known variation in ZrH_x density with H/Zr ratio.

Using this method, an effective migration-expansion coefficient of -21×10^{-6} in./in. °F is estimated for small temperature differentials in delta phase $ZrH_{1.7}$. This is about 2.8 times the thermal expansion coefficient and approximately four times higher than would be inferred from hydrogen distribution data measured after 1344 hr.³⁶ Since the migration stress would be opposed by the thermal stress during operation, the worst case would occur (after many years) when the reactor is shut down. Thus, if the thermal stress is in the range of 3 to 4,000 psi, the maximum migration stress could be as high as 8,000 to 11,000 psi. This is in the same range as the ultimate tensile strength reported for pure delta-phase zirconium hydride. The uncertainty in this estimate is large, however, and cannot be confirmed experimentally at this time. If a more rigorous analysis and/or experiments indicate a need, the strength of the material could be improved by alloying, as mentioned previously.

c. Moderator Cladding

The moderator logs must be clad for corrosion protection, as stated earlier, and the clad material must provide a suitable support surface for the ceramic diffusion barrier. Differential expansion considerations suggest the use of either austenitic or ferritic stainless steel, which have approximately the same coefficient of expansion as zirconium hydride. These alloys contain from 12 to 27% chrome, and less than 2-1/2% nickel. Corrosion data for this material in a steam environment are sparse, but the following static test data were found for Martensitic stainless steels containing 12% chrome and 1/2% nickel.

<u>Material Type (12-chrome)</u>	<u>Temperature (°F)</u>	<u>Corrosion Rate (mils/yr)</u>	<u>Reference</u>
410	800	0.060	37
420	925	0.024	38
403	925	0.016	38
410	1000	0.120	37
420	1100	0.040	38
403	1100	0.008	38
416	1100	0.024	38

The highest corrosion rate indicated from these tests is 0.12 mils/yr at 1000°F, which is approximately the maximum temperature expected in the flow channels inside the logs. After 30 years, a total penetration of about 3.6 mils would be expected under static conditions. Since the penetration rate could be several times this high under dynamic conditions, (which could cause the protective oxide film to flake off), the clad thickness in the inner-flow tubes was increased to 10 mils. The corrosion resistance is known to increase with chrome and nickel content, which led to the choice of Type 431 Martensitic stainless steel, containing 15 to 17% chromium and 1.25 to 2.50% nickel. Dynamic corrosion tests would still be required to verify the suitability of this material for the present application, however.

The outer box consists of a core of expanded, corrugated metal sandwiched between and welded to two face-plates. This box is exposed to steam which is essentially static at a maximum temperature of about 800°F. The thickness of the core and the face-plates may be varied, and the total equivalent

thickness required for corrosion protection is estimated to be approximately 20 mils, based on a total penetration of one-half the initial thickness. If tests should indicate that this is inadequate, a single metal surface or a suitable ceramic filler (to protect the internal surfaces) could be used to avoid increasing the steel volume fraction beyond that assumed for this study.

From this discussion, it can be seen that the thickness of the steel cladding required for the moderator logs is somewhat speculative. The values chosen are felt to be near-minimal for the present application, however, both from a corrosion and a mechanical standpoint.

5. Integral Fuel Moderator Elements

The feasibility of the IFM elements depends primarily on the structural integrity of the thick-walled zirconium-hydride tube. Based on an ultimate tensile strength of 8000 psi, the element described in Section IV.C could theoretically contain an internal pressure approximately four times that expected due to fission product gases released during the fuel cycle. The external pressure required to cause compressive yield is calculated to be about three times the reactor system pressure. The largest stress calculated is that due to the radial thermal gradient across the tube wall at the position of maximum heat-flux. In this case, the maximum thermal stress is a function only of the moderator-to-fuel volume for which the following values were calculated:

V_m/V_f	1.0	1.5	2.0
σ_{th} (psi)	5200	6500	7250

For the reference design ($V_m/V_f = 1.5$), the maximum thermal stress is thus estimated to be 6500 psi, which is about 80% of the lower estimate of the ultimate strength. This is the tensile stress at the outer surface, however; and at design conditions, it would be partially relieved by a compressive stress of approximately 4300 psi. Thus, the net tensile stress on the outer surface of the tube is estimated to be only 2200 psi.

While these numbers are reassuring, it is questionable whether the usual failure criteria can be applied to zirconium hydride since it is a brittle material. Thus, a more complete analysis considering transient thermal stresses, local stresses at end cap joints, etc., followed by experimental verification would probably be required to guarantee structural integrity.

VI. ECONOMICS

The economic analysis of the reference designs includes estimates of the fuel cycle and capital costs. The fuel cycle costs were developed on an absolute basis for each of the four 300-Mwe concepts studied, using the same procedures and data for each design, where applicable. Capital costs were estimated on a differential basis for the two concepts demonstrating the lowest fuel-cycle costs. The methods, data, and results of this analysis are described below:

A. FUEL CYCLE COSTS

Fuel cycle costs were estimated for each core region of the four reference designs described previously. The estimates were all based on an average burnup of 10,000 Mwd per metric ton of uranium in each core region, using a one-batch refueling scheme. Since the average specific power is the same in each region, both the superheater and the boiler would be refueled at the same time. Current cost data were used for fabrication, reprocessing, shipping, etc. The major fuel cost input data are summarized in Table XII, and the methods used to calculate the various fuel cycle cost components are discussed briefly in the following paragraphs.

TABLE XII
FUEL CYCLE COST DATA

Reactor Description	Water-Moderated				Zr-Hydride-Moderated			
	SS Process Tubes		Zr-2 Process Tubes		Moderator Log		I.F.M.	
Core Region	B	S-H	B	S-H	B	S-H	B	S-H
Thermal Power (Mwt)	525	253	525	253	540	238	540	238
Fuel Loading (kg U)	27,600	13,300	27,600	13,300	28,400	12,500	28,400	12,500
Aver. Exposure (Mwd/MTU)	10,000	10,000	10,000	10,000	10,000	10,000	10,000	10,000
Initial Enrichment (at. %)	2.64	3.50	2.59	2.62	2.53	3.39	2.47	2.79
Final Enrichment (at. %)	1.62	2.40	1.57	1.60	1.53	2.35	1.47	1.82
Pu Discharge (gm/kg U)	5.22	4.73	5.22	4.85	5.27	5.22	5.28	5.73
Fabrication Cost (\$/kg U)	135	115	133	146	137	92	133	250

1. Burnup

This charge accounts for the depletion in quantity and reduction in value of the fuel due to burnup. The unit costs for the enriched uranium were based on the published AEC prince schedule. The initial enrichments used are those required to obtain the proper initial power split and a $k_{\text{eff}} = 1.15$ in the hot, clean, boiling core. The final enrichments were obtained from the results of the burnup analysis for an average exposure of 10,000 Mwd/MTU.

2. Fabrication

The fuel element fabrication costs are based on the designs described in Section IV. Interest on the capital investment during fabrication are included in the costs shown in Table XII, but the use charges are lumped with those incurred during the rest of the fuel cycle. The fabrication costs are not the same for each concept due to differences in design and, to a lesser extent, in enrichment. The unit costs for the boiler elements are approximately equal for each design and are close to the Euratom guaranteed price of \$140/kg U for Zircaloy-clad UO_2 elements. The unit price for the stainless steel-clad UO_2 , seven-rod cluster element used in the ZBSR-moderator log concept is substantially lower than that of the water-moderated concepts, due to the additional cost of the process tube assembly hardware required for the latter. The difference in the costs of the superheat elements for the two water-moderated designs reflects the higher cost of the Zircaloy process tubes compared to stainless steel. The semi-permanent Zircaloy boxes are not included in either estimate, but are included as a capital cost item.

The extremely high fabrication cost estimated for the integral fuel moderator elements is a result of the large number of manufacturing processes required to produce this element plus the high material costs and relatively low yields, as compared to more conventional designs.

The sequence envisioned for manufacturing the IFM rods would be essentially as follows.

- 1) Purchase zirconium in form of thick-walled tubing and solid rods (for end caps and reflector plugs) extruded to approximately the same cross-sectional dimensions as those of finished product.
- 2) Cut tubing and reflector plugs to required length and machine end-caps.

- 3) Weld one end-cap onto each tube segment.
- 4) Load UO_2 pellets and insulator discs into zirconium tubes.
- 5) Weld other end-cap in helium environment.
- 6) Hydride the fueled segments and reflector plugs.
- 7) Machine hydrided segments to final dimensions, using centerless grinders with diamond wheels.
- 8) Weld end-fittings onto one end of stainless steel-clad tubes.
- 9) Coat inside surface of clad tubes with diffusion barrier.
- 10) Assemble reflector plugs, fueled segments and axial diffusion barriers into clad tubes.
- 11) Weld other end-fitting to clad tube.
- 12) Braze wire wraps around each rod and bake at high temperature and pressure to precollapse cladding onto segments and to bond diffusion-barrier joints.
- 13) Assemble into bundles.

The base cost of the thick-walled zirconium tubing (based on a vendor estimate of \$32/lb) amounts to more than \$100/kg of uranium, before hydriding.

3. Shipping

This charge includes the cost of shipping new fuel from the fabricator to the site and irradiated fuel from the site to the reprocessing plant. The rate used for these two operations is equivalent to \$3/kg U for the new fuel and \$12.45/kg U for spent fuel.

4. Use Charges

The use charges were calculated at 4% per year, with the inventory time and corresponding fuel value per core distributed as follows:

Process	Time (Months)	Fuel Value
Pre-core (fabrication, shipment and storage)	6	initial
In-core + spares	21.6	$\left[\frac{\text{initial} + \text{final}}{2} \right] + \left[0.1 \times \text{initial} \right]$
Post-core (cooling, shipping, storage and reprocessing)	7.0 (superheater) 7.5 (boiler)	
		final

It will be noted that the in-core time was based on an 80% plant factor and the fuel value during this time was based on the arithmetic average of the initial and final values plus 10% spares assessed at the initial value.

5. Reprocessing and Conversion

This item includes chemical reprocessing of the spent fuel, conversion of uranyl nitrate to UF_6 , and conversion of plutonium nitrate to metal. The reprocessing of spent fuel was based on a plant capacity of one metric ton per day, an 8-day turn-around time, and a charge of \$16,400 per day. The conversion of uranyl nitrate to UF_6 and plutonium nitrate to metal was evaluated at \$5.60/kg U and \$1.50/gm Pu respectively.

6. Plutonium Credit

The standard government buy-back price of \$12/gm (independent of isotopic composition) was credited to the Pu produced during the cycle.

The results of the fuel cycle cost analysis are shown on Table XIII. It is noted that the ZBSR-moderator log concept has the lowest, total-core fuel cycle costs, and the water moderated concept with Zircaloy process tubes is the closest competitor. The net difference in fuel cycle costs between these two concepts is approximately 0.12 mills/kwh. Close examination of the individual cost components reveals that this difference is due entirely to the higher fabrication cost of the process tube assembly for the water-moderated, superheat fuel elements. Also, the total cost incentive for using Zircaloy instead of stainless steel process tubes appears to be small for the conditions of this study (~0.04 mills/kwh). Although the Integral Fuel-Moderator concept has the lowest net burnup and use-charge costs, this advantage is outweighed by the high fabricating costs estimated for this type of superheat element.

TABLE XIII
FUEL CYCLE COSTS
(mills/kwhr)

Reactor Description	Water-Moderated				ZRH _x - Moderated			
	SS Process Tube		Zr-2 Process Tube		Moderator Log		I. F. M.	
Core Region	B	S-H	B	S-H	B	S-H	B	S-H
Burnup	1.1275	0.6039	1.1238	0.5404	1.1271	0.5439	1.1271	0.4939
Fabrication	0.9956	0.4061	0.9801	0.5159	1.0378	0.3070	1.0060	0.8343
Shipping	0.1139	0.0546	0.1139	0.0546	0.1169	0.0516	0.1169	0.0516
Use Charge	0.2306	0.1654	0.2241	0.1074	0.2218	0.1151	0.2139	0.1141
Reprocessing and Conversion	0.2545	0.1374	0.2545	0.1381	0.2609	0.1342	0.2610	0.1368
Pu Credit	(0.4616)	(0.2003)	(0.4616)	(0.2056)	(0.4784)	(0.2090)	(0.4793)	(0.2295)
Total (Region)	2.2605	1.1671	2.2348	1.1508	2.2861	0.9788	2.2510	1.4012
Total (Core)	3.4276		3.3856		3.2649		3.6522	

It should be mentioned that the absolute level of the fuel cycle costs for all of the concepts is fairly high. This is due primarily to the assumption of batch-type refueling and an average burnup of 10,000 Mwd/MTU.

B. DIFFERENTIAL CAPITAL COSTS

The direct costs estimated for the major reactor components which are different in the ZBSR-Moderator Log Concept and the water-moderated concepts (as designed) are listed below:

Item	H ₂ O-Moderated	ZBSR-ML
Reactor Pressure Vessel	\$1,470,000	\$1,405,000
Shield Plug-Manifold	87,500	57,000
Flow Baffle and Plenum	-	32,200
Extra Control Rods and Drives	-	176,000
Semipermanent Superheat Fuel Boxes	376,000	-
Semipermanent Boiler Fuel Boxes	550,000	645,000
Moderator Logs	-	2,340,000
Total Direct Cost	\$2,483,500	\$4,655,200
Direct Cost Difference	\$2,171,700	

The small difference in pressure vessel costs is due to the fact that a large part of the total vessel cost is incurred in manufacturing processes which are essentially the same in each design. Differences in the cost of the internal core structure, control rods and drives, and the semipermanent fuel boxes are also small (less than 5% of the total capital cost differential), and are subject to design changes and improvements which could be effected with either concept. The zirconium-hydride moderator logs represent the major differential capital cost item. These are estimated to cost approximately \$52,000 each, which is close to \$90 per lb of finished zirconium hydride. The starting cost of the extruded zirconium metal is only \$14 per lb based on a vendor quotation. The numerous fabrication processes, canning hardware, and relatively low yield increase the cost of the finished product substantially.

The direct capital cost penalty for the ZBSR, using a capital charge of 14% per yr and an 80% plant factor is thus estimated to be 0.145 mill/kwh. If the indirect costs are added as a fixed percentage of the direct capital costs as specified in the AEC's "Ground Rules for Evaluating Steam-Cooled Power Reactor Plants," the total capital cost penalty for the ZBSR would increase to approximately \$3,330,000, or 0.222 mills/kwh. The validity of using this procedure, when one of the major cost items — the moderator logs — are to be installed in much the same fashion as the fuel elements, is considered to be highly questionable. The indirect cost differential was not estimated, however, since the capital cost penalty without top charges is already slightly larger than the fuel cost saving for the ZBSR and the issue would thus appear to be somewhat academic.

VII. CONCLUSIONS

On the basis of this evaluation study, the following conclusions have been reached.

- a) There appears to be no economic incentive for using zirconium hydride as a substitute for water as moderator in an integral, boiling water superheat reactor. Although a fuel-cycle cost savings of approximately 0.12 mills/kwh, was obtained with the reference ZBSR-moderator log concept, this is more than compensated by a capital cost penalty of approximately 0.22 mills/kwh, including top charges.
- b) The fuel-cycle costs for the integral fuel-moderator concept are approximately 0.27 mills/kwh, higher than those of the water-moderated concept, due to the higher fabrication costs. Although this disadvantage would decrease if a higher burnup could be achieved, the development of a concept with fuel fabricating costs of approximately \$250/kg U would not appear to be a fruitful approach for reducing the costs of nuclear power.
- c) The use of zirconium hydride introduces several difficult problems not encountered in the water-moderated concepts, such as thermal stresses in the moderator, hydrogen migration and leakage, and the questionable long-term integrity of the thin-walled moderator cladding. The problem of emergency decay heat removal is also more critical, and the core structure is slightly more complex.

Since firming up the reference designs, several changes have been thought of which could improve the performance of both the zirconium hydride and the water-moderated concepts, but it is doubtful that these would alter the results. The main reasons for this conclusion follow.

- a) The cost of manufacturing canned zirconium-hydride assemblies is extremely high and adds from 8 to 12 \$/kwe to the cost of the plant.
- b) The need for canning the solid moderator with stainless steel results in a steel volume fraction comparable to that of the water-moderated core design. When this is combined with the inherently inferior neutron economy of zirconium hydride relative to water, a higher burnup and use charge results for the zirconium-hydride-moderated core.

- c) The cost savings attributable to achieving a higher power density in the superheat core are slight. The superheat region occupies less than half of the core volume and a 25% increase in its power density reduces the vessel diameter by less than 5%. The resultant cost savings amount to less than \$1/kwe.

Even if the cost of the moderator logs could somehow be reduced by a factor of two, the power cost for the reference design ZBSR would only break even with that of the water-moderated concept. If all of the steel canning for the logs could be eliminated, the net savings would only be about 0.07 mills/kwh, whereas redesign of the Zircaloy-process tube assembly to make it a semipermanent part of the water-moderated core structure would reduce the fuel element fabricating costs for that concept by about 0.2 mills/kwh.

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