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STEAM COOLED FAST MODULE

FEBRUARY 1969

**AEC RESEARCH &
DEVELOPMENT REPORT**

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STEAM COOLED FAST MODULE (SCFM)

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STEAM COOLED FAST MODULESummary

A unique design for a fast flux test module evolves from the combination of a fast-thermal system with the steam cooled fast reactor concept. In this design, each module provides an intense ($\sim 2(10)^{15}$ n/cm²/sec) fast neutron flux in a test region 4 to 5 inches in diameter and 3 to 4 feet in length. The total core size of each module is about 30 inches in diameter, and this core is in turn surrounded by a neutron moderating reflector such as graphite.

An important feature of this steam cooled fast test module is the inherent fail-safe design. The results of the calculations indicate that a module design is attainable with the maximum reactivity condition near the operating steam density so that there will be: (1) a negative change in reactivity on loss of steam coolant, due to increased fast neutron leakage; (2) a negative change in reactivity on water flooding, due to the presence of a thermal neutron absorber. By adding gadolinium oxide to the fuel, it is possible to assure that the reactor is always sub-critical in the water flooded condition. This safety feature also simplifies the mechanisms for discharge and handling of the irradiated fuel and allows the use of standard water basin systems. Thus, although the Steam Cooled Fast Reactors are not being considered for the Power Breeder Reactors, the use of steam cooling for a test reactor is still very attractive. Such a system could provide an inexpensive facility for general fast reactor testing work, including tests of interest to the LMFBR Programs.

When the Steam Cooled Fast Test Module is surrounded by a thermal neutron reflector, some further advantages are obtained. First, the thermal neutrons cause fissions in a band around the outer edge of the module, thus creating a driver region for the module. This thermal driver tends to make the operating characteristics (neutron lifetime and reactor control) of the module

more nearly that of a thermal reactor. The dependence on the thermal region causes a decrease in reactivity on compaction that might occur, for example, by core melting. Second, the designed combination of the size of the module and the pressure vessel thickness can be used to give a nearly flat radial power distribution with a smooth transition between the fissioning in the thermal neutron driver to that in the fast neutron core. Finally, the thermal neutron flux peaks in the graphite a few inches outside of the module, providing a large region of intense thermal neutron flux ($\sim 10^{15}$ n/cm²/sec) for production of isotopes or advanced thermal neutron research. Combinations of the test modules surrounding a thermal neutron trap might be used to drive the flux intensity even higher while providing several fast test regions in the same overall reactor.

General Design

Interest in the design of the Steam Cooled Fast Module arose as a part of the project to search for possible uses of the shutdown Hanford Production Reactors. One earlier concept for a fast test function was to use the reactor loading to drive a subcritical fast flux conversion fuel region that surrounded the test section. A brief study of such a gas-cooled conversion region was made.⁽¹⁾ It was determined that the fast flux in the test section could only be about the same intensity as the thermal flux in the driving reactor region and this did not appear to be high enough to justify the expenditure required to construct the facility.

The reason for considering only a subcritical converter region was based on safety considerations. Since the main avenue left for raising the test region flux was to let the conversion region become a driver, it was decided to review the various possible fast reactor systems for salient safety

features. Further, it was considered important to utilize as much of the existing equipment as possible. The production reactors are graphite moderated and water cooled. The existing pumps can supply ample water coolant at pressures up to 600 psi. Some steam capability exists, from fossil fuel boilers, at 225 psi.

Consideration of all of these features led to the Steam Cooled Fast Module (SCFM) design study, with the basic idea to:

- A. Provide a usable fast neutron flux ($\sim 2(10)^{15}$ n/cm²/sec, $E > 0.1$ Mev) for a test loop to test fast reactor components;
- B. Design the Fast Module with SAFETY as the prime factor with features such as:
 - 1. Small or negative change in reactivity on loss of steam cooling;
 - 2. Thermal absorber such as Gd added so that the core is inherently shut-down on water flooding and can be discharged in pieces into the water basin;
 - 3. Operating condition is near the maximum reactivity;
 - 4. Core meltdown accident does not cause a reactivity increase;
- C. Design the core cooling within the capacity of the available water pumps;
- D. A design for a flat flux in as long a region as possible;
- E. Utilize several advantages that accrue from locating the Fast Module in the graphite reflector, such as:
 - 1. The operating characteristics tend to be influenced by the thermal neutrons, thus lengthening the prompt neutron lifetime and possibly allowing a regulating control system to be located in the region where thermal fissions predominate;

2. The thermal flux peaks in a relatively large region of the graphite reflector surrounding the module. This intense thermal flux might be used for isotope production and to provide neutron beams for basic research;

F. Consider installing several basic Fast Modules in one production reactor.

The use of steam cooling takes advantage of the present water pumping systems and, by adding Gd to the fuel, the core can be safely handled in the water flooded condition, thus allowing utilization of the present fuel discharge, storage, and handling systems. In fact, this fast-thermal system with steam cooling seems to provide a unique combination for an inherently fail-safe fast test reactor design.

CORE PHYSICS

D. D. Lanning, S. J. Altschuler, and G. J. Busselman

Introduction

The present concept of the SCFM is a vertical core, as schematically shown in Figure 1. Core physics and thermal hydraulics calculations leading to this general design have only been made in the form of parametric studies. The results of these calculations give sufficient information for a general feasibility study, and also, the final optimum SCFM design work can be guided by these general results. Suggestions to be used in further design studies are listed in the Conclusions.

Results to Date and Future Work

The neutronic calculations are an extension of steam cooled fast reactor calculations previously made as a part of the Steam Cooled Task Force Study.⁽²⁾ The calculations consist of group cross sections obtained from the computer codes HRG and THERMOS, and used in the one-dimensional transport theory code DTF-IV. A further description of these calculations is given in Appendix A. For these initial parameter studies the graphite region surrounding the 30-inch diameter Fast Module was assumed to be a reflector only (holes in the graphite but no fuel). The test region at the center of the Fast Module was taken to be 4.0 inches in diameter.

Initially, it was thought that the low steam content (steam volume fraction of the core $\sim$ 25%) case would be the most interesting and would give the desired safety features of negative reactivity for voiding or flooding. However, it was found that the maximum reactivity occurred upon flooding and adding a reasonable amount of gadolinium did not suffice since

even when flooded, the system was essentially still a fast reactor. Some thought was then given to running such a system on water cooling rather than moving to a new volume fraction of steam. However, the water-cooled case does not have a fast neutron energy spectrum that closely represents the spectrum to be expected in future fast reactors (LMFBR). Returning to the steam-cooled systems, preliminary heat transfer calculations were made. These calculations also indicated that the desired power could not be attained in the low steam density cores. In fact, a steam volume fraction of at least 55% seemed indicated from the heat transfer review. Thus, for the core physics studies, the volume percents are 55% coolant, 15% 304 stainless steel structure, and 30% fuel. Fuel is considered to be 0.25 inch diameter pins clad in 0.028 inch thick stainless steel. The final design may include Incoloy-800 for the fuel clad rather than 304 stainless steel.

Core Diameter

The 30-inch SCFM core diameter was originally chosen to be compatible with design considerations in the Hanford Production Reactors. However, later studies indicate that much smaller cores will be difficult to make critical at any enrichment, and much larger diameters increase the total power without significant improvement in the desired performance characteristics (i.e., fast flux intensity and spectrum shape).

Core Length

Calculations presented here are based upon a four-foot long core. In the thermal hydraulics section, the parametric studies indicate a core length of three feet is required to meet the desired power density (of 0.3 MW/liter) to give $\sim 2(10)^{15}$ n/cm²/sec fast flux (> 0.1 Mev) in the test region. The general physics results are not strongly sensitive to the core length, although

the final enrichment for criticality obviously is core length dependent. In any case, the core length can be as long as four feet if the inlet pressure is allowed to be greater than 600 psi. Thus, another iteration between the thermal hydraulic design and the core physics might make a more consistent design, but it is not expected to change the general results presented here.

Core Fuel

Oxide fuels were assumed for the calculations and studies were made on $^{238}\text{UO}_2$ systems with either $^{235}\text{UO}_2$ enrichments or plutonium enrichments. It was found that the enriched uranium systems had a peak reactivity in a condition between operating steam densities and water flooding that is about 10% in k_{eff} above the operating reactivity. Although schemes such as the use of zirconium hydride in the fuel were considered for moving the operating steam point to the maximum reactivity, the design seemed somewhat complicated and less desirable due to the shift in neutron spectrum. Lining the inside of the pressure vessel with zirconium hydride does not control the reactivity peaking; thus, it would be necessary to include the hydride with the materials in the core, thus reducing the ^{238}U and shifting the spectrum.

On the other hand, the reactivity peaking in plutonium fueled systems is less pronounced and the desired operational design seems attainable. Therefore, most of this design study is concerned with the plutonium enriched cases. For these calculations, oxide fuels were assumed to have 85% of theoretical density. The plutonium isotopes were taken in the proportion 57/27/12/4 for ^{239}Pu , ^{240}Pu , ^{241}Pu , and ^{242}Pu , respectively. The high exposure Pu was used assuming the source of fuel might be from commercial power reactors.

Parametric Calculational Results

Variations in fuel enrichment, steam density, gadolinium oxide in the fuel, and pressure vessel thickness were considered in this parametric study. Figure 2 shows the relation between the smeared steam density for 55 vol% coolant, and the actual steam condition. In the figures following, the smeared steam density is used as the parameter that is allowed to vary from 0 (no coolant) to 33.9 (water flooding). The operating condition is taken to be an average smeared steam density of 0.56, based on core inlet saturated steam at 620 psia and exit condition steam at 520 psia with a temperature of 900°F.

The fuel enrichment was varied to obtain a critical system with a reasonable value of Gd_2O_3 in the system and with k_{eff} greater than unity by a sufficient amount for operation requirements such as temperature coefficients and fuel burnup. This occurred at about 60% for the ^{235}U in the $^{235}UO_2$ - $^{238}UO_2$ mixture and at 50% plutonium in the PuO_2 - $^{235}UO_2$ mixture.

The important part of the neutronics design search was to find an operating system to satisfy the conditions of inherent safety over the full range of smeared steam densities. The results shown in Figure 3 indicate that such a system does exist near the 18% Gd_2O_3 in the PuO_2 - UO_2 fuel loading. It is of interest to note that there is a small positive reactivity change indicated from the operating condition to the 0 or no coolant condition. However, the shape of the curve is strongly dependent upon the pressure vessel thickness as indicated in Figure 4. Since the vessel thickness can be in the range indicated by these curves (i.e., 1.67" to 2.5"), it seems possible to combine the decoupling effect of a thicker pressure vessel and the absorption effect of the Gd_2O_3 to obtain an operable system. This is verified by the results of another calculation, shown in Figure 5, where the reactivity change on

coolant loss is negative, the maximum rise above the operating condition is less than 1%, and the rate of change of reactivity with smeared steam density near the operating condition is zero.

A few words should be included about the physics trends observed as a part of these parametric studies. It is clear from the calculations that the negative change in reactivity on loss of steam coolant is controlled by the change in neutron leakage. The calculated values of the change in k_{∞} on loss of steam coolant give an increase in reactivity, thus the design of the core (such as the core diameter and pressure vessel thickness) must allow the leakage change to dominate the reactivity effect in order to obtain the desired inherent safety feature. Toward the water flooded end of smeared steam density curve the Gd_2O_3 absorption can be made large enough to dominate the reactivity effects, thus providing another desired inherent safety feature. The effect of the Gd_2O_3 on the k_{eff} for low smeared steam densities comes from the effect on the thermal fissions at the core reflector boundary and is also in part caused by the physical reduction in the PuO_2-UO_2 in each fuel pin to allow room for the Gd_2O_3 .

For comparison, Figure 6, similar to Figure 4, shows the values of k_{eff} for an enriched uranium core. Again, a different design is required to find the operable condition for enriched uranium cores.

Neutron Flux and Core Power Distribution

Next, it is of interest to study the flux and power distributions. Figures 7 through 10 give information about the neutron spectrum in the central 4-inch (empty) test region for the various parameters. Figures 7 and 8 show that the effect on the test spectrum is small for both the addition of Gd_2O_3 and for changes in the pressure vessel thickness. In Figure 9, a

plot of the Fast Test Reactor (FTR) spectrum is also given for comparison. It is interesting to note that the SCFM operating curve (0.560) gives a higher fraction of neutrons above 0.1 Mev. Figure 10 shows the enriched uranium fueled cases.

The radial neutron flux distribution is shown in Figure 11 indicating the rise of the fast flux toward the center and the rise of the thermal flux in the reflector. Figures 12, 13, and 14 show the fission distributions as a function of radius; Figure 12 shows the relative fissions caused by the fast and thermal fluxes.

Control System Concept

The currently envisioned control system for the fast module consists of two portions: in-core and out-of-core control.

Figure 15 is a schematic of the horizontal cross section of the vertically oriented module. The inlet steam flows down around the core in the area denoted by "Steam Downcomer," and then flows up through the power producing annular core. The central target region may or may not be cooled by reactor steam.

The out-of-core control is regarded as the primary system and consists of a venetian blind type of affair. Figure 16 shows the type of control blade motion which is envisioned. Preliminary calculations show a possible control strength of $0.045 \Delta k/k$ using one-inch thick B_4C shutters enriched to 100% ^{10}B . One quarter inch thick shutters have a strength of $0.032 \Delta k/k$. The transport theory calculational model used here compared the control blade closed position, see Figure 16, to totally removed blades. In reality, the worth of blade movement may be substantially less; however, selective shaping of the control blade might be utilized to maximize the reactivity swing.

Assuming a shutter width of 4 inches yields about 25 shutter blades needed to surround the core $0.032/25 = 0.0013 \Delta k/k$ for one blade or about \$0.20 which is the correct order of magnitude for a regulating rod. The current thought is to gang all of the blades except those needed for the regulating or load following function. Thus, only 4 drive motors (2 operating - 1 reg., 1 gang - and 2 stand-by) would be subjected to the inlet steam environment.

The in-core control rods or pins are envisioned as either seldom moved safety rods, curtain-type rods which would have no capability for movement during reactor operation or a combination of the two types. The curtain rods could be removed from the core in steps to make up reactivity lost by fuel depletion.

A simple calculation was carried out assuming a 1/4-inch shell of B_4C enriched to 100% ^{10}B , placed at the radial midpoint in the annular core. The worth of this ring, relative to the condition where steam occupied this location, is $0.052 \Delta k/k$. Any rods placed within the reactor core will tend to dilute the fuel and detract from usable heat transfer area. Both of these effects will lower the specific power of the core for a given set of conditions. Therefore, the number of these in-core rods must be minimized. The strength of the in-core rods stated above is by no means a maximum, but is simply an estimate of what might be rather easily attained.

The ^{10}B used in the above calculations is not necessarily the optimum control material from either a cost or reactivity standpoint. Other elements that should be examined either singly or in combination are natural B, Europium, Tantalum, Gadolinium, and Dysprosium.

It seems clear at this point that at least \$10 in control is available from these two systems. This should be sufficient for a module operated using a steam density which corresponds to near maximum reactivity.

Fuel Burnup Effect

A preliminary calculation of the effect of fuel burnup has been made. The computer program ZODIAC⁽³⁾ was used. This program consists of one-dimensional multigroup diffusion theory calculations (HFN) with cross sections derived from THERMOS and HRG all chained to the burnup code ALCHEMY. Comparison between the HFN diffusion theory results and the DTF-IV transport theory results show some differences that will take further study. The results of these preliminary burnup calculations are shown in Figure 17, which indicate that the reactivity will decrease by about 2% after 100 days of full power operation.

Checking the Calculational Techniques

The results reported above are all based on calculations and the obvious question arises concerning the validity of the calculational techniques. There are very few experimental critical assembly measurements in the SCFM range of interest. However, some experimental results are available and the calculational techniques used have been compared with these experiments as described in Appendix A. In summary: (1) the values of k_{eff} calculated for uranium systems seem to be slightly high, perhaps 1%, depending upon assumed reflector savings; (2) the values of k_{eff} calculated for plutonium systems seem to be 1 to 5% high, presumably due to the use of a ^{239}Pu capture-to-fission ratio (which may be due to the fact that the error in $\bar{\alpha}$ is due to incorrect $\alpha(E)$ values for the intermediate neutron energies) that causes the

discrepancy between theory and experiment to be largest at the lower H/Pu cases. However, earlier correlations with the ZPR experiments where H/Pu = 0 indicated reasonable agreement. Thus, the reactivity peaking indicated for the plutonium systems in Figure 3 may not be as high as calculated while the end points at no coolant and at water flooding may be close to the correct value. If anything, these corrections would be a benefit in the design, for example, by reducing the required amount of Gd_2O_3 . Therefore, it is expected that any improved calculational techniques will still lead to the same conclusions that an inherently safe system can be designed. It is clear that mockup critical experiments will be required for the final SCFM design.

FIGURE 1

Schematic Design of SCFM (Surrounding Moderator not Shown)

Figure 2. Relationship Between Temperature, Pressure, and Smeared Steam Density for a 55 vol% Coolant Core

Figure 3. Effect of Gadolinium on the Relationship Between k_{eff} and Smeared Steam Density for Mixed Oxide Core with a 1.67" Pressure Vessel Thickness.

Figure 4. Effect of Pressure Vessel Thickness on the Relationship between k_{eff} and Smeared Steam Density for Mixed Oxide Cores

Figure 5. $\Delta\rho = \frac{k_{eff} - k_{eff}^{operating}}{k_{eff}}$ as a Function of Smeared Steam Density for a Mixed Oxide Core with a 20% Gd_2O_3 in the Fuel and a 2-1/2" Thick Pressure Vessel

Figure 6. Effect of Pressure Vessel Thickness on the Relationship Between k_{eff} and Smeared Steam Density for Uranium Cases

Figure 7. Flux as a Function of Energy for Various Gadolinium Concentrations - Plutonium Core

Figure 8. Flux as a Function of Energy for Various Pressure Vessel Thicknesses - Mixed Oxide Core

Figure 9. Flux as a Function of Energy for Various Smeared Steam Densities, Mixed Oxide Core (50% Pu, 18% Gd)

Figure 10. Flux as a Function of Energy for Various Smeared Steam Densities - Uranium Core

Figure 11. Flux as a Function of Radius for a Mixed Oxide Core with 18% Gadolinium, 1.67" Pressure Vessel, $\bar{\rho} = 0.739$ #/ft³ Core = 600 psi Saturation.

Figure 12. Relationship Between Power Density and Radius (1.67" Thick Pressure Vessel) - Mixed Oxide Core

Figure 13. Radial Power Profiles as a Function of Gadolinium Content in Mixed Oxide Cores

Figure 14. Radial Power Profiles for Typical Uranium & Plutonium Fueled Cores and Pressure Vessel Thicknesses

STEAM COOLED FAST MODULE

Figure 15

EXTERIOR CONTROL SYSTEM

Figure 16

Figure 17. Required Control vs Core Lifetime
Mixed Oxide Core - 18% Gd - 2.5" Pressure Vessel

$$\Delta\rho = \frac{k_{(0)} - k_{(t)}}{k_{(t)}}$$

THERMAL HYDRAULICS STUDY

D. T. Aase

A parametric study was performed on the Steam-Cooled Test Cavity core to determine its thermal-hydraulic performance capability. The primary objective of this study was to show the relationship between the core design and operating parameters and the power density that can be achieved when a consistent set of design constraints is maintained. Three core design parameters were considered independently. They were core diameter, core length, and test cavity diameter. In addition, the steam pressure at the core inlet and steam temperature at the core outlet were considered independent operating parameters.

For each combination of the independent design and operating parameters considered, the following constraints were maintained:

1. The maximum clad temperature was held constant by adjusting the fuel pin diameter;
2. By varying the number of fuel pins in the core, the maximum fuel center-line temperature was held below incipient melting ($\leq 4950^{\circ}\text{F}$);
3. The maximum steam velocity was used as a constraint to indirectly limit the core power.

The thermal-hydraulic model which interconnected all the independent parameters and design constraints was based on the model developed by PNL to study Steam Cooled Fast Breeder Power Reactors for the AEC Alternate Coolant Fast Breeder Task Force.⁽²⁾ The thermal properties of the fuel, however, were based on 93.5% theoretical density oxide pellet fuel used in superheat reactors rather than the 88% dense fuel used in Breeder Reactors where high burnups are

necessary for economic reasons. The other fuel and clad design parameters; i.e., clad material, allowable temperature, allowable burnup, clad thickness to diameter ratio, and required fission gas plenum were based on the proposed EVESR Mark-IV fuel design.⁽⁴⁾ The clad material is Incoloy-800 with a thickness-to-diameter ratio of 0.0715. The maximum clad temperature, to achieve $\sim 40,000$ MWD/T fuel exposure, is 1250°F . A fission gas plenum equal to 0.375 of the core length was used in determining core pressure drop.

The maximum steam velocity was limited to values of 280 fps and 350 fps for comparison purposes. The EVESR Mark-IV fuel had a proposed steam velocity of 280 fps.

The calculations were programmed and performed by H. P. Foote of the Process Simulation and Analysis Section on the PNL Hybrid computer.

Study Results

The affect of the design constraints on the allowable power density was examined. The maximum coolant velocity proved to be the most restricting constraint in the range of design and operating parameters of interest. Figure 18 shows the effect of allowed coolant velocity on the achievable power density. This sensitivity study was performed on the core described by the parameters listed on the Figure. The range of practical interest for maximum coolant velocity is 250 fps - 350 fps and there appears to be sufficient incentive to evaluate the overall affect of higher velocities.

The maximum allowed clad temperature also restricts the core power density. This is shown in a slightly different manner in Figure 19. Only two values of maximum clad temperature were considered. The value of 1250°F has been proposed for superheat application, and 1350°F was used for the same alloy (Incoloy-800) for Steam Cooled Fast Breeder application. The higher

temperature introduces additional uncertainty into the clad design which does not appear to be justified by the modest gain in power density that can be obtained.

Figure 20 shows that the power density is not restricted by the allowed fuel centerline temperature. This indicates that the number of fuel pins should be an independent design parameter. There is, however, no direct restriction on number of fuel pins so it would be most practical to consider fuel pin diameter as an independent parameter and vary the number of fuel pins to keep the clad temperature constant. Current fuels technology considers fuel pin diameters of 0.250-inch practical for large-scale production. This was set as the lower limit on fuel pin diameter. The upper limit was not bounded but the maximum achievable power density is obtained at 0.250-inch, or just slightly above for most design and operating conditions considered.

The reason higher fuel centerline temperatures do not increase the power density is that the higher fuel temperature allows fewer pins in the core. However, to maintain the clad temperature constant the pins must be large. This results in a net loss of coolant flow cross section. To maintain constant flow velocity the power must be cut back to allow lower steam flows.

The study of constraints shows that the allowed steam velocity has the most effect on the power density at which a given core can be operated. Maximum clad temperature is of secondary importance, and fuel centerline temperature is not a restriction in maximizing the power density. Minimum fuel pin diameter will, instead, have a secondary limiting effect on the achievable power density.

Only one of the gross core design parameters had a significant effect on the power density. Figures 21 and 22 show the achievable power density as a function of core length for different coolant velocities and core inlet pressures. To obtain 0.3 MW/l in a four-foot core requires velocities higher than 350 fps and/or pressures higher than 600 psi.

Varying the core diameter between 24-40 inches did not change the power density significantly, nor did changing the test section diameter between 1 and 6 inches. Perhaps a test hole slightly larger than 5 inches could be accommodated, at little or no sacrifice in power density, if a marketing survey shows that is desirable.

Figures 23 through 25 show the sensitivity of power density to core inlet pressure. The trend is as expected with highest pressures yielding highest power density. The designs shown are not optimum because a high fuel centerline temperature was used. The slopes of the curves are, however, valid.

Figure 26 shows an interesting trend for power density as a function of steam temperature at the core outlet. In the analytical model, the core inlet temperature is set at the saturation value for the steam pressure considered. As the core outlet temperature is raised, the ΔT across the core increases and the steam flow tends to drop proportionately. The power is then adjusted to increase the coolant velocity to the limiting value. This trend is approximately linear with core outlet temperature. Two other factors are occurring simultaneously. The heat transfer properties of the steam are not as good at higher temperatures so the heat transfer coefficient is getting smaller, thus requiring larger pins which tends to limit the steam flow and power density. The bulk coolant temperature is also approaching the maximum clad temperature which causes an increase in the required heat transfer area.

Eventually, at the power density peak, the latter two factors began indirectly controlling the achievable power density, thus driving it down.

Figures 27 through 33 show how this peak varies for different design and operating conditions. The maximum power density occurs between 710°F and 790°F depending upon the design being considered.

Considering the results of the parametric study, the core parameters shown in Table I resulted in a power density of 0.3 MW/l. Longer cores required pin diameters of less than 0.25 inches to achieve 0.3 MW/l within reasonable coolant velocities and clad temperatures.

The core in Table I was used as a reference core to study the possibility of starting the test module with steam from available boilers. There is approximately 0.5×10^6 lbs/hr of 225 psi steam available from existing boilers. Figure 34 shows the maximum power at which the test module can be run for steam conditions between 100 and 200 psi. About one-third full power can be achieved with the 200 psi steam at 0.5×10^6 lbs/hr flow.

Since it may be economically attractive to operate the test module with steam supplied from existing boilers, some design variations were considered. Of primary interest is power density as a function of core length as shown in Figure 35. The core design you would select does not vary a great deal from the reference design selected at higher pressure. It should possibly have a different outlet temperature. This was not determined in this study. The power density achievable with the low pressure system appears to be about one-third of that achievable at 600 psi. This emphasizes the linearity of the pressure curves shown in Figures 23 through 25.

The parametric studies presented here are perhaps adequate for establishing a preliminary reference design. Some continued study should be made in determining an optimum pin diameter. Some experimental work should precede the final design to determine allowable maximum steam velocities. Of particular interest is possible flow-induced vibration at very high velocities. It is a very important constraint in establishing the desired power density.

An additional point of interest is the fuel fabrication cost. The unit cost for a 0.250 inch fuel pin from the work of the Alternate Coolant Fast Breeder Task Force would be about 153 \$/Kg of heavy metal. The reference design in Table I has approximately 859 Kgs of U and Pu in the core. At a burnup of 40,000 MWD/T, the core will last 34,342 MWD, or 381 days.

Assuming 270 full power days per year of operation yields,

$$\frac{270}{381} = 0.71 \text{ cores/year}$$

and

$$859 (153) = 131,427 \text{ \$/core}$$

or

$$\text{Fabrication cost} = 93,313 \text{ \$/year.}$$

TABLE I
Typical Core Design

<u>Parameter</u>	<u>Units</u>	<u>Value</u>
Core Length	ft.	3.0
Core Diameter	in.	30
Test Section Diameter	in.	5
Core Volume	ℓ	290
Volume Fractions:		
Fuel		.256
Steam		.521
Structural (Incoloy-800)		.223
Number of Fuel Pins		3361
Fuel Pin Diameter	in.	0.25
Clad Thickness	in.	0.018
Fuel Pin Pitch	in.	0.403
Fission Gas Plenum Length	ft.	1.125
Core Power	MW _t	90
Steam Flow	lbs/hr	1.6x10 ⁶
Core Inlet Pressure	psi	600
Core Inlet Temperature	°F	486
Core Pressure Drop	psi	71.0
Core Outlet Pressure	psi	529
Core Outlet Temperature	°F	784
Steam Density (core inlet)	lbs/ft ³	1.29
Steam Density (core outlet)	lbs/ft ³	0.74
Steam Density (core average)	lbs/ft ³	1.02
Smeared Steam Inventory	lbs/ft ³ of core	0.53
Average Coolant Velocity	fps	255
Maximum Coolant Velocity	fps	350
Axial Power Peaking Factor		1.10
Radial Power Peaking Factor		1.00
Engineering Peaking Factor		1.10
Maximum Clad Temperature	°F	1250
Maximum Fuel Centerline Temperature	°F	4160
Heat Transfer Coefficient at Hot Spot	B/hr-ft ² -F	1022
Linear Heat Rating (maximum)	Kw/ft	10.8
Power Density	MW/ℓ	0.31

PIN	=	--	psi	LC	=	4.0	ft.	TC max.	=	1250	F
TOUT	=	780	F	DCI	=	5	in.	VCM	=	--	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	2.16	ft.

Figure 18. Sensitivity of Core Power Density to Maximum Coolant Velocity

PIN = --- psi
TOUT = 750 F
DCO = 30 in.

LC = 4.0 ft.
DCI = 5 in.
TCL max = 4950 F

TC max. = --- F
VCM = 280 fps
REFS = 2.16 ft.

Figure 19. Sensitivity of Core Power Density to Maximum Clad Temperature and Core Inlet Pressure

PIN	=	--	psi	LC	=	4.0	ft.	TC max.	=	1250	F
TOUT	=	--	F	DCI	=	5	in.	VCM	=	280	fps
DCO	=	30	in.	TCL max	=	---	F	REFS	=	2.16	ft.

Figure 20. Sensitivity of Core Power Density to Maximum Fuel Centerline Temperature and Core Inlet Pressure

PIN = 600 psi
TOUT = 750 F
DCO = 30 in.

LC = -- ft.
DCI = 5 in.
TCL max = 4950 F

TC max. = 1250 F
VCM = -- fps
REFS = -- ft.

Figure 21. Sensitivity of Core Power Density to Core Length and Maximum Coolant Velocity

PIN	=	--	psi	LC	=	--	ft.	TC max.	=	1250	F
TOUT	=	750	F	DCI	=	5	in.	VCM	=	350	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	--	ft.

Figure 22. Sensitivity of Core Power Density to Core Length and Inlet Pressure

PIN	=	---	psi	LC	=	--	ft.	TC max.	=	1250	F
TOUT	=	750	F	DCI	=	5	in.	VCM	=	350	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	--	ft.

Figure 23. Sensitivity of Core Power Density to Core Length and Inlet Pressure

PIN	= ---	psi	LC	= 4.0	ft.	TC max.	= 1250	F
TOUT	= 900	F	DCI	= 5	in.	VCM	= --	fps
DCO	= 30	in.	TCL max	= 4950	F	REFS	= 2.16	ft.

Figure 24. Sensitivity of Core Power Density to Maximum Steam Velocity and Core Inlet Pressure

PIN	=	--	psi	LC	=	4.0	ft.	TC max.	=	1250	F
TOUT	=	--	F	DCI	=	5	in.	VCM	=	280	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	2.16	ft.

Figure 25. Sensitivity of Core Power Density to Core Outlet Temperature and Inlet Pressure

PIN	=	600	psi	LC	=	4.0	ft.	TC max.	=	1250	F
TOUT	=	--	F	DCI	=	5	in.	VCM	=	280	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	2.16	ft.

Figure 26. Sensitivity of Core Power Density to Core Outlet Temperature

PIN	= 600	psi	LC	= 4.0	ft.	TC max.	= 1250	F
TOUT	= --	F	DCI	= 5	in.	VCM	= --	fps
DCO	= 30	in.	TCL max	= 4950	F	REFS	= 2.16	ft.

Figure 27. Sensitivity of Core Power Density to Maximum Steam Velocity and Outlet Steam Temperature for 600 psi Steam in a 4.0 ft Core

PIN = 600 psi
 TOUT = -- F
 DCO = 30 in.

LC = -- ft.
 DCI = 5 in.
 TCL max = 4950 F

TC max. = 1250 F
 VCM = 350 fps
 REFS = -- ft.

Figure 28.

Sensitivity of Core Power Density to Core Length and Outlet Steam Temperature for a Steam Pressure of 600 psi and Velocity of 350 fps

PIN	= 600	psi	LC	= --	ft.	TC max.	= 1250	F
TOUT	= --	F	DCI	= 5	in.	VCM	= 280	fps
DCO	= 30	in.	TCL max	= 4950	F	REFS	= --	ft.

Figure 29. Sensitivity of Core Power Density to Core Length and Outlet Steam Temperature for a Steam Pressure of 600 psi and Velocity of 280 fps

PIN	=	---	psi	LC	=	3.0	ft.	TC max.	=	1250	F
TOUT	=	---	F	DCI	=	5	in.	VCM	=	350	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	1.62	ft.

Figure 30. Sensitivity of Core Power Density to Steam Pressure and Temperature in a 3.0 ft Core With a Steam Velocity of 350 fps.

PIN	=	--	psi	LC	=	4.0	ft.	TC max.	=	1250	F
TOUT	=	--	F	DCI	=	5	in.	VCM	=	350	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	2.16	ft.

Figure 31. Sensitivity of Core Power Density to Steam Pressure and Temperature in a 4.0 ft Core With a Steam Velocity of 350 fps

PIN = -- psi
TOUT = -- F
DCO = 30 in.

LC = 4.0 ft.
DCI = 5 in.
TCL max = 4850 F

TC max. = 1250 F
VCM = 280 fps
REFS = 2.16 ft.

Figure 32.

Sensitivity of Core Power Density to Steam Pressure and Temperature in a 4.0 ft core With a Steam Velocity of 280 fps

PIN	=	-	psi	LC	=	3.0	ft.	TC max.	=	1250	F
TOUT	=	-	F	DCI	=	5	in.	VCM	=	280	fps
DCO	=	30	in.	TCL max	=	4950	F	REFS	=	1.62	ft.

Figure 33. Sensitivity of Core Power Density to Steam Pressure and Temperature in 3.0 ft. Core with a Steam Velocity of 280 fps

PIN = — psi
 TOUT = 784 F
 DCO = 30 in.

LC = 3.0 ft.
 DCI = 5 in.
 VCM = 350 fps

REFS = 1.62 ft.
 NP = 3333 Pins
 DP0 = .250

Figure 34

Maximum Power as a Function of Steam Pressure In
 a Core Designed for High Pressure Operation

PIN = 200 psi
TOUT = 730 F
DCO = 30 in.

LC = -- ft.
DCI = 5 in.
TCL max = 3000 F

TC max. = 1250 F
VCM = 350 fps
REFS = -- ft.

Figure 35 Sensitivity of Core Power Density to Core Length at a Steam Pressure of 200 psi

FAST FLUX MODULE EFFECTS ON PRODUCTION REACTOR

G. L. Gelhaus

Introduction

A prime consideration in the design of a fast flux module will be its effect on the operating production reactor. In particular, one must be assured that the higher flux generated by the module does not cause melting in the production reactor fuel. Therefore, it was necessary to determine the thickness of a zone needed to buffer the fast flux module and the fueled portion of the production reactor.

Results

The results of these calculations indicated that the buffer zone must be approximately five lattice units thick and the necessary thickness was found to be relatively insensitive to having the tubes in the buffer zone filled with air, H_2O , or H_2O and depleted UO_2 fuel. The buffer zone easily accommodated a flooded fast flux module.

Calculational Methods

The reactor effects were calculated, as in the Core Physics Section, using DTF-IV (a one-dimensional multigroup transport code). Fast and epi-thermal cross sections (lower broad group energy boundaries of 8.21×10^5 , 1.83×10^5 , 5.25×10^4 , 1.17×10^4 , 9.66×10^2 , 4.79×10^1 , and 0.638 eV) were calculated using HRG. These HRG calculations accounted for anisotropic scatterings and used a P-1 leakage approximation. BATTELLE-THERMOS was used to calculate the thermal cross sections.

The DTF-IV calculations used a module of 30 vol% fuel, 15 vol% stainless steel, and 55 vol% coolant (saturated steam at an inlet pressure = 600 psi with an outlet pressure = 500 psi at 900°F). The fast flux module fuel used

in the calculations had 59% ^{235}U and 41% ^{238}U at 88% theoretical density mixed with approximately 20 atoms of Gd (Gd_2O_3) per 100 atoms of uranium. The module itself had a 4-inch diameter test hole with a stainless steel wall thickness of 0.91 cm. The pressure vessel had an external diameter of 30 inches and was 2.5 inches thick.

The production reactor was assumed to consist of a 8-3/8 inch square H_2O -cooled graphite lattice, fueled with natural uranium. The fuel rod had a diameter of 1.7 cm clad in 0.13 cm of aluminum. A water gap of 0.28 cm separated the cladding from the 0.19 cm thick aluminum process tube sheath. The graphite made up the rest of the fuel cell.

The graphite cell in the buffer zone was essentially the same as the fuel cell except the clad fuel and coolant were removed. The investigations were made with the graphite cell containing (1) a void, (2) H_2O , and (3) depleted UO_2 fuel and H_2O coolant.

The relative flux calculations that were made are independent of the power level of either the production reactor or the SCFM. To evaluate the perturbation due to the SCFM, the radius of the outer production reactor boundary for the calculation was made large compared to the SCFM perturbation. A reflecting boundary was assumed at this outer edge and the results were analyzed on the basis of searching for the distance required to return the original flat flux distribution that is attained before reaching the outer calculational boundary. This flat distribution is then extended back to the origin and is marked on the Figures as the unperturbed flux.

The calculations of the flux shapes were made with varying thicknesses (in units of lattice spaces) of the graphite buffer zone to determine how much of the adjacent operating production reactor would have to be unfueled. The

calculations were made for 2, 3, 4, and 5 lattice space thicknesses of graphite. The flux was sufficiently reduced with the five lattice spaces of graphite, as seen in Figures 36 and 37, but a fast flux peak still existed in the fueled production reactor (Figure 38). This indicated that not all of the module's fast flux was thermalized in the 106 cm of graphite.

After the buffer zone thickness was determined, attempts were made to reduce it by (1) adding H_2O in the empty process tubes, and (2) inserting depleted UO_2 (0.2 wt% UO_2) fuel with H_2O coolant in the process tubes. As seen in Figure 39, the necessary zone thickness is relatively insensitive to these changes.

Calculations were also made for the case of a flooded fast flux module. The investigations clearly revealed that the buffer zone was large enough to keep the production reactor operating normally (Figure 40).

After the final fast module design is determined, calculations should again be made to determine if the new design will have any significant changes on the module's effect on the operating reactor.

Figure 36. Thermal Flux as a Function of Graphite Buffer Zone Thickness Between SCFM & Operating Reactor

Figure 37. Thermal Flux for SCFM as Operating Reactor with Graphite Buffer Thickness of Five Lattice Units

Figure 38. Fast Flux for SCFM in Operating Reactor with Graphite Buffer Thickness of Five Lattice Units

Figure 39. Thermal Flux as a Function of Material Content in Process Tubes of Buffer Zone

Figure 40. Thermal Flux for Operating and Flooded SCFM in Operating Reactor

CONCLUSION

The results of the work to date are briefly reviewed in the opening Summary Section. Specifically, the calculations indicate that the SCFM system is attainable with the basic ideas as listed in the General Design Section. If one assumes the specific power to be 0.3 MW/l, the total flux at the center of the test hole is about 2.5×10^{15} n/cm²/sec. The spectra analysis indicates that about 77% of the neutrons are above 0.1 MeV; hence, this fast flux is about 1.9×10^{15} n/cm-sec. The power density is quite flat across the core with the outer edge dropping off to about 80% of the central value. Control blades were not included in these calculations.

A very interesting point is that by using the existing Hanford Production Reactor steam plant on a once-through basis, a power density of between 0.1 and 0.15 MW/l should be obtainable. This would produce a flux of between 0.63×10^{15} and 0.95×10^{15} n/cm²/sec above 0.1 MeV. It is possible that a detailed optimization of the core could raise this value even higher. It would seem that such a system has a cost advantage as both the steam circulator and the steam generator are eliminated. Module startup would then be a simple matter so far as the thermal hydraulics are concerned.

There are many remaining questions about the neutronic design and operating characteristics of the SCFM. The next obvious step in the design study is to incorporate the improvements of the calculational techniques required by the theory-experiment correlations; to settle on a control system design; and to iterate again between the design - the thermal hydraulics - and the core physics, including such decisions as the fuel density (note physics used 85% while thermal hydraulics used 93.5% T.D.). These calculations will start with

the results of the parametric calculations discussed in this report and build toward a firmer design by including two and three dimensional calculational analyses.

As soon as the design is reasonably firm, a critical mockup experiment is needed to confirm and improve the design calculations for both the steam density effects and the control system effectiveness.

In addition, studies need to be made on the general characteristics of the reactor startup and the cold-to-hot reactivity swing from the flooded reactor condition to the operating steam condition. These studies must include a time dependent operational stability study and an improved fuel burnup study. Other core design parameters should be considered, such as the use of different thermal and epithermal neutron absorbers in place of the Gd_2O_3 .

Before a firm reference design is established, some preliminary flow tests must be made for typical fuel configurations to determine critical steam velocities. Of prime concern in these tests would be the onset of flow-induced vibrations.

Equally important is the initiation of the safety analysis study. The standard and emergency methods of reactor shutdown and decay heat removal must be designed to prevent excessive shock to the fuel assemblies and still allow final water flooding. Reactor transient analysis studies must be made, including feedback between the reactivity and the thermal dynamics. The reactivity change in the event of fuel melting must be studied in detail to look at both compaction or dispersion of the core material.

Finally, an overall reactor study could be made containing several Steam Cooled Fast Modules. This interaction between modules would be studied, including the thermal flux enhancement and the mode of overall operation.

The work in this report has been mainly concerned with the physics and thermal hydraulics of the SCFM. The other important areas of Engineering Design related to the Hanford Production Reactors are being considered in a separate report by Douglas United Nuclear, Incorporated.

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APPENDIX AASSESSMENT OF ANALYTICAL METHODS BY CORRELATION WITH EXPERIMENT

V. O. Uotinen

Introduction

Analytical correlations of some fast and intermediate energy critical experiments have been made to assess the adequacy of the methods and/or data being used in calculating the neutronics of the fast module.

The critical experiments that were chosen for this assessment fall into two groups: (1) a series of three intermediate-energy, bare, homogeneous assemblies of PuO_2 -Polystyrene compacts measured at Battelle-Northwest;^(1, 2) and (2) two simulated steam-cooled fast reactor experiments (SNEAK 3A-1⁽³⁾ and SNEAK 3B-2⁽⁴⁾) performed at Karlsruhe, Germany.

The first group - namely the bare homogeneous systems - can be considered to be primarily a test of the adequacy of the basic cross sections, because the simple geometry involved in both the cell calculation and the reactor calculation minimizes the problems associated with the calculational model. The Karlsruhe experiments, on the other hand, provide a test of the adequacy of the calculational methods because their heterogeneous fuel assemblies and multi-zoned reactor loadings introduce additional complexities into the calculation. The various assemblies are described in Tables A-I, A-II, A-III, and Figures A-1 and A-2.

The Basic Calculational Scheme

The theoretical methods and the cross sections, which constitute the basic calculational scheme, are the same as those used to obtain the results presented in the section on Core Physics. Briefly, the calculational method consists of using the multigroup transport theory codes HRG and THERMOS to calculate cell-average group constants for seven fast groups and one thermal

group (Table A-IV); the eight-group constants are then used in a one-dimensional S_4 , P_1 transport theory calculation with the DTF-IV code. The basic cross section set used for the calculation of non-thermal effects (in the HRG code) was obtained from ORNL and is basically the GAM-III set from GA, updated by L. L. Bennett of ORNL. The plutonium constants in this set are the preliminary ENDF/B values by Greebler. For the thermal group calculation, the ^{239}Pu cross sections were from the 1965 evaluation by Sher. Cross sections of the other heavy nuclei were from the Leonard evaluation.

Results of Calculations Using the Basic Computational Scheme

A. Karlsruhe Experiments

The main difference between the two Karlsruhe experiments that have been analyzed is that assembly SNEAK 3B-2 contained a central plutonium zone, whereas assembly 3A-1 contained only uranium fuel. The geometry of the fuel assemblies and of the reactor loadings was very similar in these two assemblies. Thus, any difference in calculated k_{eff} values can be attributed to the presence of plutonium in assembly 3B-2.

Heterogeneity within the fuel assemblies was neglected; i.e., the cell calculations were done assuming a homogeneous mixture of nuclei. The reactor calculations were performed assuming concentric annular rings of fuel. A reflector savings of 10 cm was assumed for each end of the assembly to account for axial leakage in the reactor calculation. Calculated macroscopic cross sections are listed in Tables A-V and A-VI.

Calculated values of k_{eff} for the Karlsruhe experiments are 1.0128 and 1.0127, respectively, for the 3B-2 and 3A-1 assemblies. The k_{eff} can thus be calculated equally well for both the all-uranium assembly and the assembly containing a central plutonium zone.

Both values of k_{eff} are somewhat higher than the measured value of unity. It is estimated, on the basis of calculations performed at Karlsruhe,⁽³⁾ that taking into account the heterogeneity of the fuel assemblies will increase the calculated k_{eff} values by at most 0.005. After this correction, the calculated k_{eff} values are high by 1.5 - 2.0%.

We must keep in mind, however, that the calculated k_{eff} is sensitive to the axial leakage, which is based upon an estimated reflector savings of 10 cm. Measured values of the reflector savings are not available, so there is an area of uncertainty present here. If the reflector savings is changed from 10 cm to 8 cm, the calculated k_{eff} values decrease by $\sim 1\%$. Without experimental information on the reflector savings involved, it is not possible to decide how accurately our one-dimensional calculations predict the k_{eff} values of these assemblies. We can say, however, that if one uses reasonable estimates of the reflector savings, calculated values of k_{eff} appear to be reasonable.

B. Homogeneous PuO₂-Polystyrene Experiments

The three bare PuO₂-polystyrene assemblies have calculated median fission energies of about 30 ev, 33 ev, and 0.7 kev. Therefore, they are not fast assemblies, but they are far from being thermal assemblies, and do serve to test the adequacy of the cross sections (and to some extent, also the calculational methods). Calculated macroscopic cross sections are listed in Table A-VII.

Calculated values of k_{eff} range from 1.0121 to 1.0518, as shown in Table A-VIII. Two general trends may be observed: (1) the first two assemblies, each of which has a hydrogen-to-plutonium atom ratio of 15, indicate that as the amount of ²³⁹Pu increases, the calculated k_{eff} increases

and the agreement with experiment gets worse. (2) The second and third assemblies differ in both H/Pu ratio and ^{239}Pu content; the larger change is the factor of three in H/Pu ratio. Here, as the H/Pu ratio decreases, the calculated k_{eff} increases and the agreement with experiment again gets worse.

These results indicate that there are rather large discrepancies between calculation and measurement. In particular, the large trends as a function of ^{239}Pu concentration and H/Pu ratio are disturbing.

Investigation of Reasons for Discrepancies

A. Cross Sections

The discrepancies observed in the correlation of the Karlsruhe experiments have been discussed in Section III-A.

The trends observed in the correlation of the PuO_2 -polystyrene experiments are largely attributable to low values of α (ratio of capture cross section to fission cross section) for ^{239}Pu . Several recent measurements⁽⁸⁻¹⁰⁾ in the ev and kev energy range indicate values of α^{239} higher (by a factor as great as 2.0) than earlier accepted values. For a thermal reactor, and for most fast reactors, this energy range (a few ev to about 40 kev) contains only a small fraction of the total flux. However, for the intermediate-energy PuO_2 -polystyrene assemblies the median fission energy lies within this energy range. Thus, α^{239} in the ev and kev range has a significant effect on the calculated behavior of these assemblies.

Calculations were performed in which the macroscopic fission or capture cross sections in group number five (0.966 kev to 11.7 kev) were changed so as to increase α^{239} in this group by a factor of 1.5, which is approximately the factor indicated by the recent measurements. These changes resulted in

better agreement between calculated and measured k_{eff} values, and also reduced slightly the trend in k_{eff} versus ^{239}Pu concentration (Table A-IX). If α^{239} were changed over the entire questionable energy range (instead of only in our group number 5), the changes in k_{eff} would be somewhat larger.

As to how the newer, higher α^{239} values will affect results calculated for plutonium-fueled fast assemblies, we can qualitatively reason as follows: For a fast reactor, in which the questionable energy range (up to ~ 40 kev) is well below the mean fission energy, the new α^{239} values will lower the calculated k_{eff} values by a small amount, if at all. However, in the case of the steam-cooled fast module, where one is considering various states of flooding, the new α^{239} values may have a significant effect on the calculated results. As more hydrogen is introduced into the system, the spectrum will be softened, thus building up the neutron flux in the ev and kev range. For these cases, using the new values of α^{239} will result in considerably lower values of k_{eff} than those previously calculated.

In order to evaluate the importance of these changes, it is suggested that the recent α^{239} measurements be evaluated, and that future calculations for the fast module be performed using the new, higher α^{239} values.

B. Computational Methods

Calculations have been performed to evaluate the effects of changing the degree of detail in the calculational methods. These studies included changing the number of energy groups, changing the quadrature order, and a comparison between diffusion theory and transport theory (Table A-X).

1. The number of energy groups was increased to 18 (Table A-XI).

The additional groups were at the high energy end (to represent leakage effects better), and in the ev-kev range (to give more

detail near the median fission energies of the PuO_2 -polystyrene assemblies). This increase in the number of groups lowered the calculated k_{eff} values by small amounts. The improvement, however, is not great enough to warrant the extra expense involved in performing the more detailed calculation.

2. The quadrature order used in the DTF-IV transport theory calculations was increased from four to eight, and in a few cases to twelve. No significant changes were obtained in calculated k_{eff} values.
3. One dimensional diffusion theory calculations were performed using the code HFN with both 8 and 18 energy groups. The diffusion theory calculations gave k_{eff} values lower than those obtained with transport theory by 2 - 3%. Increasing the number of energy groups from 8 to 18 in the diffusion theory calculation resulted in small changes (< 1%) in calculated values of k_{eff} for the homogeneous assemblies; for the SNEAK 3B-2 assembly, k_{eff} changed by 1.6%.
4. A more detailed slowing down calculation has been attempted using the GAF/GAR code, but the results obtained thus far have been inconclusive.

Conclusions

This study has revealed some problem areas in the basic calculational scheme used in this "first round" study. It is suggested that, before future design calculations are performed, these areas be investigated further. The calculational scheme that is decided upon should be able to predict the behavior of these critical assemblies more accurately than the scheme used in this "first round" study.

Specific areas that need further study are the following:

1. Cross Sections: Recent measurements of heavy element cross sections have indicated that some previously accepted values may be inaccurate. Specifically, there is evidence⁽⁸⁻¹⁰⁾ that currently accepted values of σ^{239} (in the ev and kev range) are too low. (There is also evidence that currently accepted values of the capture cross section of ^{238}U below 10 kev are too large.⁽⁴⁻⁷⁾) The recent measurements have not been adequately evaluated yet. The ENDF/B cross sections, for example, do not yet include the influence of these recent measurements. An evaluation of all the recent measurements needs to be performed before a cross section set can be chosen which is truly up-to-date.
2. Slowing Down Model: Different slowing down models should be compared to evaluate the adequacy of the model to calculate the neutron spectrum. The HRG calculation could be compared, for example, with the more detailed GAF/GAR scheme developed by Gulf General Atomics, or with codes developed under the FFTF Program (FCC and/or 1D-X codes).
3. Heterogeneity Correction: Studies conducted by Karlsruhe have shown that the heterogeneity correction is not very large ($< .005 \Delta k/k$). Nevertheless, this effect should be evaluated for the specific cases of interest.

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TABLE A-1

Description of PuO₂-Polystyrene Assemblies

H/Pu wt% ²⁴⁰ Pu	15 8.08	5 2.2	5 11.46
Hydrogen	3.9431×10^{-2}	4.2348×10^{-2}	2.920×10^{-2}
Carbon	3.9431×10^{-2}	4.2348×10^{-2}	2.920×10^{-2}
Oxygen	5.2753×10^{-3}	5.6464×10^{-3}	1.159×10^{-2}
²³⁹ Pu	2.4245×10^{-3}	2.7614×10^{-3}	4.986×10^{-3}
²⁴⁰ Pu	2.1312×10^{-4}	6.1860×10^{-5}	6.623×10^{-4}
²⁴¹ Pu	- - -	- - -	1.382×10^{-4}
²⁴² Pu	- - -	- - -	1.026×10^{-5}

Experimental Data:

Measured thick. of inf. slab	18.74 cm	16.09 cm	16.83 cm
Buckling	173.3 m^{-2}	222.7 m^{-2}	194 m^{-2}

TABLE A-II

Atom Densities (in 10^{20} cm^{-3}) of SNEAK 3A-1 Used for Homogeneous Calculations

	Al	C	Cr+Mn	Fe	H	Ni	O	Si	Ti	^{235}U	^{238}U	Mg
Core Zone 1	128.8	4.12	36.6	123.1	7.40	19.0	144.7	1.84	0.53	20.314	81.023	0.37
Core Zone 2	128.9	4.13	36.7	123.3	7.42	19.0	144.8	1.88	0.55	20.221	81.066	0.37
Blanket Zone 3	- -	0.14	11.9	40.1	- -	10.2	- -	0.46	0.2	1.54	399.3	- -

TABLE A-III

Particle Densities for SNEAK 3B-2

	Pu-Zone	U-Type-1 Zone	U-Type-2 Zone	Blanket
^{239}Pu	14.76			
^{240}Pu	1.33			
^{241}Pu	0.11			
^{242}Pu	0.0			
^{235}U	0.56	20.404	20.25	1.625
^{238}U	81.86	81.39	81.26	399.414
Al	125.6	129.66	129.47	
C	9.73	8.76	9.00	0.14
Co	0.14	0.18	0.19	
Cr	33.72	34.09	34.31	11.08
Fe	119.7	120.47	121.16	39.55
H	18.49	16.81	17.29	
Mg	1.31	0.65	0.65	
Mn	2.23	1.99	1.97	0.87
Ni	17.55	18.43	18.23	9.84
O	122.2	146.28	145.80	
Si	2.54	1.84	1.86	0.46
Ti	0.30	0.38	0.39	
Mo + Nb	0.38	0.40	0.40	0.19

TABLE A-IVEnergy Group Structure for Basic 8-Group Analysis

<u>Group</u>	<u>Upper</u>	<u>Lower</u>
1	10 Mev	0.821 Mev
2	0.821 Mev	0.183 Mev
3	0.183 Mev	52.5 kev
4	52.5 kev	11.7 kev
5	11.7 kev	0.966 kev
6	0.966 kev	47.9 ev
7	47.9 ev	0.683 ev
8	0.683 ev	0

TABLE A-V
Macroscopic 8-Group Cross Sections

SNEAK 3B-2

Group	Diff. Coef.	Σ_r	Σ_{out}	Σ_a	Σ_c	Σ_f	$U\Sigma_f$
				Pu-Zone			
1	2.2134142	4.2336679-2	3.5676543-2	6.6601353-3	1.1429977-3	5.5171396-3	1.6409732-2
2	1.5853188	2.2820737-2	1.8819193-2	4.0015447-3	1.5933376-3	2.4082071-3	7.0351280-3
3	1.1095543	2.9816049-2	2.4500395-2	5.3156540-3	2.9089562-3	2.4066978-3	6.9089986-3
4	9.5082022-1	3.1712814-2	2.4117576-2	7.5952384-3	4.8196475-3	2.7755909-3	7.9287282-3
5	8.0581785-1	2.9890131-2	1.5842714-2	1.4047417-2	9.1167453-3	4.9306716-3	1.4042674-2
6	6.6591508-1	5.4855009-2	7.0963482-3	4.7758662-2	2.6928804-2	2.0819858-2	5.9436017-2
7	6.7587472-1	1.2227024-1	4.4015922-3	1.1786864-1	7.4948523-2	4.2920122-2	1.2244240-1
8	4.6750 -1			3.0843 -1		1.6676 -1	4.8161 -1
				U-1 Zone			
1	2.1542896	4.3356886-2	6.2879355-3	1.2586610-3	1.2586610-3	5.0292746-3	1.3621569-2
2	1.5352612	2.3787092-2	4.2990927-3	1.7373535-3	1.7373535-3	2.5617392-3	6.3655662-3
3	1.0807705	3.0836081-2	6.7548114-3	3.3806484-3	3.3806484-3	3.3741631-3	8.2751265-3
4	9.3642108-1	3.2912523-2	1.0381704-2	5.4580942-3	5.4580942-3	4.9236099-3	1.2037055-2
5	8.2215981-1	3.3249108-2	1.9775508-2	9.7469198-3	9.7469198-3	1.0028588-2	2.4493758-2
6	6.6830850-1	6.6373764-3	5.9697881-2	2.7745420-2	2.7745420-2	3.1952460-2	7.8027906-2
7	6.4229006-1	4.0785054-3	1.5596477-1	7.6142909-2	7.6142909-2	7.9821860-2	1.9492497-1
8	5.9597 -1			1.8664 -1		1.4618 -1	3.5522 -1
				U-2 Zone			
1	2.1543043	4.3382164-2	3.7122852-2	6.2593116-3	1.2531598-3	5.0061518-3	1.3559552-2
2	1.5353537	2.3908562-2	1.9634274-2	4.2742877-3	1.7321493-3	2.5421384-3	6.3169263-3
3	1.0807523	3.1127946-2	2.4409898-2	6.7180476-3	3.3696479-3	3.3483996-3	8.2119548-3
4	9.3709584-1	3.3297914-2	2.2969478-2	1.0328436-2	5.4410344-3	4.8874022-3	1.1948527-2
5	8.2220277-1	3.3539026-2	1.3842937-2	1.9696089-2	9.7257711-3	9.9703182-3	2.4351417-2
6	6.6795570-1	6.6438459-2	6.9083441-3	5.9530114-2	2.7711350-2	3.1818765-2	7.7701423-2
7	6.4288214-1	1.5925073-1	4.2100947-3	1.5504064-1	7.5804364-2	7.9236273-2	1.9349497-1
8	6.5260 -1			1.7046 -1		1.3331 -1	3.2395 -1
				Blanket			
1	1.5900979	8.4300195-2	6.9804537-2	1.4495659-2	3.4685837-3	1.1027075-2	3.0510660-2
2	1.2679734	2.1111456-2	1.4864211-2	6.2472447-3	6.0298810-3	2.1736365-4	5.3857096-4
3	7.5230717-1	1.9118425-2	8.1359467-3	1.0982479-2	1.0715978-2	2.6650067-4	6.5368434-4
4	5.9060782-1	1.8646601-2	2.9658748-3	1.5680727-2	1.5300896-2	3.793018 -4	9.2872529-4
5	5.2235202-1	1.9582152-2	4.0492205-4	1.9177230-2	1.8512012-2	6.6521810-4	1.6249191-3
6	4.2650156-1	2.0939697-2	8.4588326-5	2.0855109-2	1.8765634-2	2.0894737-3	5.1024945-3
7	7.3911064-1	1.9859134-2	1.1620768-8	1.9859123-2	1.3560762-2	6.2983616-3	1.5380599-2
8	7.6616 -1			4.0054 -2		1.0127 -2	2.4609 -2

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TABLE A-VI
Macroscopic 8-Group Cross Sections
for SNEAK 3A-1 Assembly

Group	Diff. Coef.	Σ_r	Σ_{out}	Σ_a	Σ_c	Σ_f	$N\Sigma_f$
				Core Zone-1			
1	2.1901116	4.1449296-2	3.5213245-2	6.2360512-3	1.2614518-3	4.9745995-3	1.3461306-2
2	1.5738121	2.0307266-2	1.6008696-2	4.2985700-3	1.7408815-3	2.5576886-3	6.3537602-3
3	1.1085065	2.4083444-2	1.7342269-2	6.7411757-3	3.3730908-3	3.3680848-3	8.2598313-3
4	9.6116085-1	2.4001534-2	1.3714157-2	1.0287377-2	5.3971082-3	4.8902690-3	1.1955657-2
5	8.6031432-1	2.5299739-2	6.3901920-3	1.8909547-2	9.3219592-3	9.5875878-3	2.3417181-2
6	7.1399874-1	5.7402073-2	2.7126893-3	5.4689384-2	2.5052959-2	2.9636426-2	7.2372150-2
7	6.4698017-1	1.5496151-1	1.7876581-3	1.5317386-1	7.3700776-2	7.9473079-2	1.9407325-1
8	6.3777 -1			1.7749 -1		1.3869 -1	3.3701 -1
				Core Zone-2			
1	2.1887431	4.1457666-2	3.5232514-2	6.2251526-3	1.2613138-3	4.9638388-3	1.3432514-2
2	1.5729173	2.0308074-2	1.6022237-2	4.2858367-3	1.7398238-3	2.5460129-3	6.3247506-3
3	1.1079243	2.4091823-2	1.7369016-2	6.7228067-3	3.3700976-3	3.3527092-3	8.2221224-3
4	9.6079502-1	2.4005508-2	1.3745806-2	1.0259701-2	5.3914484-3	4.8682530-3	1.1901829-2
5	8.5981643-1	2.5275335-2	6.4170174-3	1.8858318-2	9.3099549-3	9.5483628-3	2.3321369-2
6	7.1369907-1	5.7243256-2	2.7248315-2	5.4518425-2	2.4998973-2	2.9519454-2	7.2086506-2
7	6.4721964-1	1.5439475-1	1.7934593-3	1.5260129-1	7.3478352-2	7.9122942-2	1.9321822-1
8	6.4471 -1			1.7687 -1		1.3813 -1	3.3565 -1
				Blanket Zone-3			
1	1.5899892	8.4266362-2	6.9793003-2	1.4483359-2	3.4722167-3	1.1011143-2	3.0466910-2
2	1.2679496	2.1093059-2	1.4860828-2	6.2322315-3	6.0256746-3	2.0655681-4	5.1176140-4
3	7.5214067-1	1.9101933-2	8.1413877-3	1.0960545-2	1.0707969-2	2.5257599-4	6.1952849-4
4	5.9077630-1	1.8625094-2	2.9806559-3	1.5644439-2	1.5284511-2	3.5992762-4	8.8006159-4
5	5.2497630-1	1.9483958-2	3.7440310-4	1.9109555-2	1.8479703-2	6.2985165-4	1.5385293-3
6	4.3026516-1	2.0563540-2	8.4089208-5	2.0479450-2	1.8497647-2	1.9818038-3	4.8395647-3
7	7.4126238-1	1.8723679-1	1.4790880-8	1.8723664-2	1.2735837-2	5.9878268-3	1.4622273-2
8	6.2535 -1			4.1152 -2		1.1626 -2	2.8251 -2

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TABLE VII
Macroscopic 8-Group Cross Sections
Pu-O₂ Polystyrene Compacts

Group	Diff. Coef.	Σ_r	Σ_{out}	Σ_a	Σ_c	Σ_f	$v\Sigma_f$
$H/Pu = 15, {}^{240}Pu = 2.2 \text{ wt\%}$							
1	1.7375416	8.4586614-2	7.9108775-2	5.4778400-3	1.1424936-4	5.3635906-3	1.7192149-2
2	1.2055147	1.6693087-1	1.6226094-1	4.6699233-3	3.6537595-4	4.3045473-3	1.2638214-2
3	9.2701251-1	3.2377779-1	3.1886777-1	4.9100249-3	6.6218234-4	4.2478425-3	1.2254348-2
4	7.6901393-1	3.9919515-1	3.9275192-1	6.4432249-3	1.6103068-3	4.8329182-3	1.3888899-2
5	6.6414712-1	3.2884353-1	3.1410905-1	1.4734482-2	5.6741371-3	9.0603453-3	2.6006640-2
6	6.0479122-1	3.3597995-1	2.4335025-1	9.2629701-2	3.4698725-2	5.7930976-2	1.6626190-1
7	5.3834171-1	3.6272907-1	1.6000383-1	2.0272524-1	1.0632342-1	9.6401816-2	2.7667712-1
8	1.6722 -1			9.7277 -1		6.1400 -1	1.7794
$H/Pu = 15, {}^{240}Pu = 8.08 \text{ wt\%}$							
1	1.8898997	7.8749443-2	7.3670299-2	5.0791443-3	1.1614349-4	4.9630009-3	1.5961436-2
2	1.3003737	1.5542626-1	1.5121605-1	4.2102099-3	3.6284042-4	3.8473696-3	1.1313108-2
3	9.9647477-1	3.0147884-1	2.9707398-1	4.4048572-3	6.6471419-4	3.7401430-3	1.0792391-2
4	8.2550576-1	3.7170126-1	3.6588451-1	5.8167481-3	1.5670396-3	4.2497084-3	1.2214462-2
5	7.1264670-1	3.0619179-1	2.9278330-1	1.3408495-2	5.4490609-3	7.9594346-3	2.2846727-2
6	6.4618072-1	3.1241005-1	2.2735991-1	8.5050136-2	3.4008304-2	5.1041833-2	1.4649006-1
7	5.7616988-1	3.4404642-1	1.3561396-1	2.0843247-1	1.2358494-1	8.4847526-2	2.4352029-1
8	2.2962 -1			9.1964 -1		5.5101 -1	1.5968
$H/Pu = 5, {}^{240}Pu = 11.46 \text{ wt\%}$							
1	1.9332274	7.1714402-2	6.0634646-2	1.1079756-2	2.6991475-4	1.0809841-2	3.4841531-2
2	1.3066511	1.2412988-1	1.1503067-1	9.0992109-3	8.4319827-4	8.2560126-3	2.4341890-2
3	1.0343465	2.3090577-1	2.2124387-1	9.6618999-3	1.5626998-3	8.0992001-3	2.3447714-2
4	8.8158704-1	2.8456583-1	2.7165906-1	1.2906766-2	3.5378610-3	9.3689054-3	2.7049568-2
5	7.6848000-1	2.4114244-1	2.1245264-1	2.8689802-2	1.1802886-2	1.6886916-2	4.8612779-2
6	6.7348216-1	3.0182294-1	1.4430328-1	1.5751965-1	6.4703045-2	9.2816611-2	2.6691432-1
7	5.5985676-1	4.4239710-1	7.8994122-2	3.6340297-1	1.9715124-1	1.6625174-1	4.7990789-1
8	1.5341 -1			1.3819		7.6931 -1	2.2307

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TABLE A-VIII

Calculated k_{eff} for Critical PuO_2 -Polystyrene AssembliesBasic 8-Group Analysis

H/Pu wt% ^{240}Pu	15 8.08	15 2.2	5 11.46
k_{eff} (calc)	1.0121	1.0355	1.0518

TABLE A-IX

Effect of Changing Σ_f or Σ_c in Group Number 5 to Change
 α^{239} in Group Number 5 by a Factor of 1.5

H/Pu	wt% ^{240}Pu	Normal	$\Delta\Sigma_{f5}$	$\Delta\Sigma_{c5}$
15	8.08	1.0121	1.0041	1.0052
15	2.2	1.0355		1.0292
5	11.46	1.0518	1.0373	1.0391

TABLE A-X

Effects of Changing the Number of Energy Groups, Quadrature Order,
and Reactor Calculation

	Normal	S8	S12	S4 17gps	HFN 8gps	HFN 18gps
PuO_2 -Poly H/Pu = 15 wt% ^{240}Pu = 8.08	1.0121	1.0087	1.0094	1.0074	0.9873	0.9872
PuO_2 -Poly H/Pu = 15 wt% ^{240}Pu = 2.2	1.0355	1.0324	1.0332	1.0309	1.0073	1.0067
PuO_2 -Poly H/Pu = 5 wt% ^{240}Pu = 11.46	1.0518	1.0486	1.0496	1.0481	1.0213	1.0220
SNEAK 3B-2	1.0218	1.0122	- -	- -	0.9919	0.9760

TABLE A-XI18-Energy Group Structure

<u>Group</u>	<u>Upper</u>	<u>Lower</u>
1	10 Mev	6.07 Mev
2	6.07 Mev	3.68 Mev
3	3.68 Mev	1.74 Mev
4	1.74 Mev	821 kev
5	821 kev	183 kev
6	183 kev	52.5 kev
7	52.5 kev	11.7 kev
8	11.7 kev	3.36 kev
9	3.36 kev	996 ev
10	966 ev	445 ev
11	445 ev	101 ev
12	101 ev	47.9 ev
13	47.9 ev	37.3 ev
14	37.3 ev	13.7 ev
15	13.7 ev	5.03 ev
16	5.03 ev	2.38 ev
17	2.38 ev	0.683 ev
18	0.683 ev	0.0

Figure A-1. Configuration and Dimensions of Sneak Assembly 3A-1

T = Trim Rod X = Position Contains Thermocouples
 S = Safety Rod
 R = Regulating Rod F-F = "Window" Elements

Height of Assembly = 80.39 cm

Figure A-2. Configuration and Dimensions of SNEAK 3B-2 Assembly

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