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SAFETY ANALYSIS OF B&W STANDARD PWR
USING THORIUM-BASED FUELS

— Final Report —

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by

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June 1980

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Prepared for

Oak Ridge National Laboratory
Operated by Union Carbide for
the U.S. Department of Energy

Contract No. W-7405-eng-26

by

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Under

Subcontract No. 7496

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ACKNOWLEDGMENT

In addition to the principal authors listed on the title page, significant contributions were made to various phases of this work by the following individuals: M. V. Bonaca (Safety Analysis), B. J. Buescher (Materials properties), J. C. Moxley and R. L. Harne (Thermal Analyses), W. A. Wittkopf (Nuclear Methods), and T. A. Coleman (RD&D Requirements).

Babcock & Wilcox
Power Generation Group
Nuclear Power Generation Division
Lynchburg, Virginia

Report BAW-1609

May 1980

Safety Analysis of B&W Standard PWR
Using Thorium-Based Fuels

V. O. Uotinen, W. P. Carroll, H. M. Jones, E. C. Toops

Key Words: Thorium, Thoria, Pressurized Water Reactors,
Denatured Uranium, Thorium Fuel Cycle,
Safety Analysis

ABSTRACT

A study was performed to assess the safety and licenseability of the Babcock & Wilcox standard 205-fuel assembly PWR when it is fueled with three types of thoria-based fuels denatured ($^{233}\text{U}/^{238}\text{U-Th}$) O_2 , denatured ($^{235}\text{U}/^{238}\text{U-Th}$) O_2 , and (Th-Pu) O_2 . Selected transients were analyzed using typical PWR safety analysis calculational methods. The results support the conclusion that it is feasible from a safety standpoint to utilize either of the denatured uranium-thoria fuels in the standard B&W plant. In addition, it appears that the use of thoria-plutonia fuels would probably also be feasible. These tentative conclusions depend on a data base that is more limited than that available for UO_2 fuels. Thus, it is concluded that a broader data base of nuclear and material properties should be acquired and that fuel demonstration irradiations and associated post-irradiation examinations should be conducted prior to making decisions regarding the wide-scale use of these fuels in PWRs.

Nomenclature

ATWS	Anticipated transients without scram
B&W	Babcock & Wilcox
BOC	Beginning of cycle
BOL	Beginning of life
BWC	B&W critical heat flux correlation; one generation newer than the BXC (DNBR < 1.17 → fuel damage)
BXC	B&W critical heat flux correlation (DNBR < 1.25 → fuel damage)
CHF	Critical heat flux
CRA	Control rod assembly
DNB	Departure from nucleate boiling
DNBR	Departure from nucleate boiling ratio
EOC	End of cycle
EOL	End of life
ESFAS	Engineered safety features actuation system
FA	Fuel assembly
LOFW	Loss of feedwater
LOOP	Loss of offsite power
MDNBR	Minimum departure from nucleate boiling ratio
NSSS	Nuclear steam supply system
PWR	Pressurized water reactor
RC	Reactor coolant
RCS	Reactor coolant system
RPS	Reactor protection system

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1. INTRODUCTION AND BACKGROUND

During the past few years, interest in the thorium fuel cycle for pressurized water reactors (PWRs) has been renewed because of two important incentives. The primary incentive has been the hope that the thorium fuel cycle (using "denatured" uranium) would reduce the risks of nuclear weapons proliferation. The second important incentive has been the fact that the thorium fuel cycle appears to have the potential to reduce the amount of uranium ore and enrichment required over a period of time.

Fuel cycle evaluations have been carried out to determine the potential benefits of using thorium-based fuels in PWRs.^{1,2} These evaluations indicate that the use of thorium could indeed result in considerable savings in uranium ore requirements if the bred ^{233}U were recycled.

As an important part of the recent re-evaluation of alternative fuel cycles, the United States Department of Energy (DOE) conducted the Nonproliferation Alternative Systems Assessment Program (NASAP).

It was recognized that in order to meaningfully assess the technical practicality of employing thorium-based fuels in PWRs of current design, the fuel cycle studies must be supplemented by evaluations of the safety and licenseability of such PWRs fueled with thorium-based fuels. With this motivation, DOE contracted with The Babcock & Wilcox Company (B&W) for a study to assess the safety and licenseability of B&W's standard 205-fuel assembly PWR with the following three thorium-based fuel types:

1. Denatured ($^{235}\text{U}/^{238}\text{U}-\text{Th}$) O_2 with self-generated uranium recycle and plutonium storage.
2. Denatured ($^{233}\text{U}/^{238}\text{U}-\text{Th}$) O_2 with self-generated uranium recycle and plutonium storage.
3. (Pu-Th) O_2 with plutonium recycle and ^{233}U storage.

B&W's standard plant design³ and 17 x 17 fuel assembly mechanical design make-up the reference design for the study, the only change being a modified fuel

composition. Details of the 17×17 fuel assembly and standard plant are presented in the appendix.

The approach to be taken in this study was first to identify the changes in reactor physics, thermal-hydraulics, and material parameters that result from substituting the thorium-based fuels and that impact the safety and licensing characteristics of the plant. The implications of these changes with regard to the various anticipated operational occurrences and design basis accidents would then be assessed. An initial qualitative assessment would first address the broad range of postulated events (those events typically covered in Chapter 15 of a Safety Analysis Report). On the basis of this preliminary assessment, some postulated events would be eliminated from further consideration, while others would be identified as more limiting and would be examined in greater detail. Selected detailed analyses would then be performed for events identified as having potentially more adverse consequences with thorium-based fuel. Emphasis would be on the denatured $(^{233}\text{U}/\text{Th})\text{O}_2$ fuel, and somewhat more qualitative treatment would be given the other fuel types. The overall objective was to conduct and document a fairly thorough preliminary assessment of the safety and licenseability of B&W's standard plant using these thorium-based fuels.

This is the final report of this contract. Section 2 summarizes the results obtained in the study. Section 3 presents information on safety-related parameters that formed the basis for the subsequent safety analysis. These parameters covered the areas of reactor physics, thermal-hydraulics, and material properties. Section 4 discusses in detail the safety assessments and analyses performed under this program. Section 5 gives a preliminary definition of a fuel-related RD&D program that would be required to ensure the licenseability of PWRs fueled with denatured $(^{233}\text{U}/\text{Th})\text{O}_2$.

2. SUMMARY AND CONCLUSIONS

A study has been conducted to assess the safety and licenseability of the Babcock & Wilcox (B&W) standard 205-fuel assembly PWR fueled with three types of thorium-based fuels: denatured ($^{233}\text{U}/^{238}\text{U-Th}$) O_2 , denatured ($^{235}\text{U}/^{238}\text{U-Th}$) O_2 , and (Th-Pu) O_2 . The safety analysis was based on available material property data for the thorium-based fuels and on reactor physics and thermal calculations performed as part of the program. This data base was used to select postulated events that were judged to be potentially more limiting with the thorium-based fuels than with standard UO_2 fuel. These selected transients were analyzed using typical safety analysis calculational methods to obtain quantitative assessments of the key safety questions.

In most cases, the transient consequences were less severe for the thorium fuel mixtures than for the standard UO_2 core. For transients that yielded worse consequences for the thorium fuels, those consequences were generally still within acceptable limits. Furthermore, simple alterations of reactor protection system (RPS) setpoints would make the response of the system equivalent to that of the UO_2 cases. The only exception to this conclusion is the ATWS case. The peak pressure for this transient exceeded the corresponding UO_2 value by 270 psia, and RPS modification cannot be invoked to mitigate this peak. This problem is generic to all fuel types, and generic resolution is under investigation.

A general evaluation of the results of this study supports the conclusion that it is feasible from a safety standpoint to utilize either of the denatured uranium-thorium fuels in the standard B&W 205-FA plant. Furthermore, it appears that the use of thorium-plutonium fuels would probably also be feasible. While the results of this study are encouraging, it should be recognized that it is based on a rather limited data base compared with the data base available for licensing standard UO_2 cores. This is especially true for the thorium-plutonium fuel but also significant for the thorium-uranium fuels. Thus, if utilization of these thorium fuels were to be seriously pursued, it would be prudent to

first implement a transition phase for which their limited use would be licensable with existing data. At the same time, a broader data base could be acquired to support a broader licensing basis.

In particular, it is important to demonstrate the satisfactory performance of such fuels in modern PWRs through a series of fuel demonstration programs, including test rod, lead test assembly, and batch implementation projects. These irradiation programs should be supplemented by others, such as one aimed at acquiring more precise information related to basic nuclear and material properties, a program of critical experiments, and a program of transient fuel performance experiments.

3. PHYSICS, THERMAL, AND MATERIAL-RELATED PARAMETERS FOR SAFETY ANALYSIS

This section describes the generation and accumulation of pertinent information on safety-related parameters in the areas of reactor physics, thermal-hydraulics, and material properties in relation to the B&W standard plant fueled with the thorium-based fuels of interest. These parameters form the basis for the safety analysis of any reactor design and largely determine the behavior of the system under various postulated accident conditions. Thus, the information presented in this section is the basic input data for the subsequent safety assessments and analyses.

3.1. Reactor Physics Parameters

A broad range of safety-related reactor physics parameters were calculated to permit the safety evaluation of the three thorium-based fuel types. The following specific parameters were calculated:

1. Fuel temperature coefficient of reactivity (Doppler coefficient).
2. Moderator temperature coefficient of reactivity.
3. Soluble boron reactivity worth.
4. Neutron lifetime.
5. Effective delayed neutron fraction.
6. Control rod reactivity worths.

Before these calculations could be performed, however, certain changes were required in B&W's lattice cell code, NULIF.⁴ These changes are discussed below, and the generation of the various reactor physics parameters is discussed in subsequent paragraphs.

3.1.1. Modifications to NULIF Code

Two standard nuclear design computer codes were used to generate the required reactor physics parameters. One was the NULIF code, which is B&W's standard lattice cell code.⁴ It is a fundamental-mode spectrum generation and fuel depletion program which is used to calculate reactivity; conversion ratio; and

burnup-dependent, spectrum-weighted, few group neutron cross sections for use in diffusion theory spatial calculations. The other nuclear code was B&W's version of the PDQ-7 code, which is a multi-dimensional diffusion theory depletion code.⁵

NULIF was developed for use with uranium- and plutonium-fueled reactor systems and has been demonstrated to be a reliable tool for predicting the neutronic behavior of such systems. However, at the start of this contract, NULIF lacked the capability to perform calculations for thorium-fueled systems. Thus, one of the preliminary tasks performed under the contract involved implementing certain modifications in the NULIF code. These modifications included (1) generation of a multigroup neutron cross section library for the thorium chain isotopes, and (2) incorporation of extended depletion chains for the thorium chain isotopes, and (3) addition of resonance integral formulation for thorium cycle isotopes.

Using the ETOG⁶ (epithermal fine-group generation) and THOR⁷ (thermal fine-group generation) codes, the desired multigroup cross sections were generated for the NULIF library from the ENDF/B-IV data files.

The thorium cycle depletion chain incorporated in NULIF may be represented schematically by

The time-dependent thorium cycle depletion equations are solved using the same numerical methods as are applied for the uranium cycle chains. This method has checked well against the exponential methods used in PDQ07 and CINDER⁸ and is faster with respect to computer time.

The resonance shielding in thorium cycle isotopes is based on the single-level Breit-Wigner representation and the Adler-Nordheim model, this is the model used for the uranium cycle isotopes. It neglects resonance-resonance interference and approximates resonance-potential scattering interference with a smooth resonance contribution.

The modified NULIF code and newly generated multigroup cross sections were checked by calculating the effective multiplication factor of a number of

($^{233}\text{U-Th}$) $_2$ exponential experiments.⁹ These results, presented in Table 3-1, are satisfactory and are generally consistent with values reported by others.

3.1.2. General Assumptions and Description of Physics Methodology

As mentioned above, B&W's standard 205-fuel assembly core and 17×17 fuel assembly dimensions formed the basic framework for this study. The three thorium-based fuel types specified in the contract (and for which physics parameters were generated) are as follows:

1. Denatured ($^{233}\text{U}/^{238}\text{U-Th}$) $_2$ with self-generated U recycle and Pu storage.
2. Denatured ($^{235}\text{U}/^{238}\text{U-Th}$) $_2$ with self-generated U recycle and Pu storage.
3. (Pu-Th) $_2$ with Pu recycle and ^{233}U storage.

The source of ^{233}U in fuel type 1 was assumed to be a fast breeder reactor blanket, as suggested by ORNL. This feed material contained 97 wt % ^{233}U and 3 wt % ^{234}U . The source of ^{235}U for fuel type 2 was assumed to be high-enriched uranium (93 wt % ^{235}U , 6 wt % ^{238}U and 1 wt % ^{234}U). Denatured uranium was produced by diluting the fissile isotope with depleted uranium (0.2 wt % ^{235}U) to reduce the fissile isotope content to specified, proliferation-resistant levels. These specified levels were 12 wt % for ^{233}U and 20 wt % for ^{235}U . In cases that contained both ^{233}U and ^{235}U , a weighted average enrichment limit was used according to the following formula:

$$\frac{0.12 M_{233} + 0.20 M_{235}}{M_{233} + M_{235}} \geq \frac{M_{233} + M_{235}}{M_T}$$

where

M_{233} = mass of ^{233}U ,

M_{235} = mass of ^{235}U ,

M_T = total mass of uranium ($M_{233} + M_{234} + M_{235} + M_{238}$).

For fuel type 3 the initial plutonium isotopic distribution was taken from a typical cycle 3 discharged fuel batch calculated for TVA's Bellefonte plant. This Pu contained 59.6 at. % ^{239}Pu , 21.0 at. % ^{240}Pu , 13.7 at. % ^{241}Pu , and 5.7 at. % ^{242}Pu .

Physics parameters were also calculated for a reference low-enriched uranium (LEU) fuel, which was assumed to be 3 wt % ^{235}U in uranium. This is the average enrichment of a typical first core design for the Bellefonte plant.

Each fuel was depleted for three cycles using the NULIF code as depicted schematically in Figure 3-1. The residual fuel was then re-enriched as necessary and recycled according to the recycling sequence depicted in Figure 3-2. A total of nine cycles were run for each fuel.

The NULIF code models the fuel pin, surrounding cladding, and moderator. A "subregion x" contribution was included to account for the control rod guide tubes, instrument tube, and assembly gap. No representations of LBP, APSRs, or control rods were provided. Radial and axial leakage effects were approximated by $B^2 = 0.0235 \times 10^{-3} \text{ cm}^{-2}$. Volume fractions for the fuel pin and assembly (17×17) are shown in Table 3-2.

The same depletion sequence and moderator boron dilution schedule were used during the depletion of all fuel types. At BOC, the boron concentration in the moderator was taken as 1000 ppm. This was reduced in eight steps to 10 ppm at EOC. The cycle design exposure (about 11,000 MWd/mtM) was achieved for each fuel after 274 effective full power days (EFPD), followed by a 50-day shutdown for refueling.

At the end of three cycles, the enrichment suitability was tested. The enrichment must satisfy an EOC reactivity criterion defined with respect to the reference urania fuel:

$$\bar{k}_{\text{EOC}} = 1/3[k(11,000) + k(22,000) + k(33,000)]$$

where $\bar{k}_{\text{EOC}}$ is the average EOC value of k after cycles 1, 2, and 3. Each cycle has 10 ppm moderator boron at this point in the boron depletion schedules. For the reference urania fuel, $k_{\text{EOC}} = 1.027$.

The enrichments of the thorium fuels were adjusted to give the same $\bar{k}_{\text{EOC}}$ after three cycles of exposure. Initial enrichments arrived at in this study are shown in Table 3-3.

3.1.3. Soluble Boron Reactivity Worth

The reactivity worth of soluble boron is different for each fuel type under consideration. An indication of this difference is given in Table 3-4, which shows values of ppm boron/percent reactivity. As can be seen from the table, there is a considerable difference between the four cases — soluble boron worth in a thorium/plutonium core is about a factor of 2.5 smaller than in a standard

LEU core. Soluble boron worths in denatured urania/thoria cores lie between these two extremes.

3.1.4. Fuel Temperature Coefficient of Reactivity

The fuel coefficient of reactivity (Doppler coefficient) was determined for each fuel type at various temperatures at several points in time in the recycling sequence. Doppler coefficients are shown in Tables 3-5 and 3-6 at the beginning and end of cycle 1, respectively. Similarly, Tables 3-7 and 3-8 show Doppler Coefficients for the beginning and the end of the equilibrium cycle, respectively.

In every case the Doppler coefficient is more negative for thorium-based fuels than for the LEU UO_2 fuel; furthermore, it is consistently more negative for the denatured UO_2 - ThO_2 fuels (which contain two strong resonance absorbers ^{232}Th and ^{238}U) than it is for the PuO_2 - UO_2 fuel.

It was also found that the Doppler coefficient is only weakly dependent on the concentration of boron in the moderator. For example, a change in boron concentration from 1665 to 10 ppm changed the Doppler coefficient by only 10% for the reference LEU urania core and only 3% for the thoria/plutonium core.

3.1.5. Moderator Temperature Coefficient of Reactivity

Moderator temperature coefficients of reactivity were determined for all the fuel types of interest at the beginning and end of cycle 1 and again at the beginning and end of the equilibrium cycle. Results are presented in Table 3-9 for both hot operating and hot standby conditions. The moderator coefficient is seen to be consistently more negative for the thoria-based fuel cases than for the standard UO_2 case except at very low soluble boron concentrations.

As additional information, the variation of the moderator coefficient was determined as a function of moderator temperature. These results are tabulated in Tables 3-10 through 3-13.

3.1.6. Total Reactivity of Moderator Density Change

The changes in reactivity associated with changes in moderator density were determined for the fuel types of interest. These results are presented in

Tables 3-14 through 3-17 for the beginning and end of cycle 1 and for the beginning and end of an equilibrium cycle.

3.1.7. Fuel Isotopic Concentrations

Initial isotopic concentrations were calculated for the various fuel types using the standard pellet, rod, and assembly dimensions of the B&W Mark C design. Pellets were assumed to have a density equivalent to 94% of theoretical density. Values of theoretical density assumed for UO_2 , PuO_2 , and ThO_2 were 10.96, 11.45, and 10.00 gm/cc, respectively. Fuel isotopic concentrations for the beginning and end of cycle 1 and beginning and end of cycle 8 (representing the equilibrium cycle, n) are listed in Tables 3-18 through 3-21.

3.1.8. Prompt Neutron Lifetime

The prompt neutron lifetime was determined for all fuel types of interest at the beginning and end of cycle 1 and again at the beginning and end of the equilibrium cycle.

The neutron lifetime was determined from NULIF calculations using the standard "1/v method."¹⁰ In this method, a fictitious neutron absorber whose absorption cross section varies as the inverse of the velocity is added to every region of the NULIF unit cell at a concentration of α/v_0 , where v_0 equals 2200 m/s and α is a constant that can be adjusted to yield a desired significant change in the multiplication factor k . The neutron lifetime ℓ is then given by

$$\ell = \frac{k_1 - k_2}{k_1 k_2 \alpha}$$

where k_1 and k_2 are the multiplication factors before and after the addition of the 1/v absorber.

The lifetimes calculated using this method are listed in Table 3-22. The lifetimes for all thorium-based fuels are seen to be shorter than for the standard UO_2 fuel, with the PuO_2 - ThO_2 fuel having the shortest lifetime of all the fuels investigated.

3.1.9. Delayed Neutron Fractions

The effective delayed neutron fraction, β_{eff} , is also based on NULIF calculations and is represented by the following expression:

$$\beta_{\text{eff}} = \frac{k_d}{k_p} \bar{\beta}.$$

In this equation, k_d is the multiplication factor calculated using a delayed neutron source spectrum in NULIF, k_p is the multiplication factor using a prompt neutron source spectrum, and $\bar{\beta}$ is the delayed neutron fraction averaged over all the neutron sources in the unit cell. The value of $\bar{\beta}$ in turn is determined by the expression

$$\bar{\beta} = \frac{\sum_i \beta_i^{\text{abs}} f_i}{\bar{\nu}}$$

where β_i^{abs} is the absolute delayed neutron fraction for isotope i (i.e., delayed neutrons/fission), f_i is the fraction of total fissions contributed by isotope i , and $\bar{\nu}$ is the average number of neutrons per fission, defined as

$$\bar{\nu} = \sum_i \nu_i f_i.$$

Values of β_{eff} calculated using the methodology described above are listed in Table 3-23 for the beginning and end of cycle for cycles 1 and n . The delayed neutron fractions for the $\text{D}^{233}\text{U}/\text{ThO}_2$ and $\text{PuO}_2/\text{ThO}_2$ fuels are much smaller than for the reference UO_2 case, whereas for the $\text{D}^{235}\text{U}/\text{ThO}_2$ fuel the delayed neutron fractions are not appreciably different from the reference case.

The kinetic parameters discussed above are strongly governed by fission fractions. Fission fractions have been determined for each fissionable isotope and for each fuel type of interest, again at the beginning and end of cycles 1 and n . Table 3-24 lists the fission fractions for the reference uranium fueled case, and Tables 3-25 through 3-27 show the fission fractions for the thorium-based fuel cases.

The average group yields of delayed neutrons (for the six delayed neutron groups) were determined by

$$\bar{\alpha}_j = \frac{\sum_i a_{ji} \beta_i^{\text{abs}} f_i}{\bar{\nu} \bar{\beta}}.$$

The values of average group yields are given in Table 3-28.

Average group decay constants were determined by the expression

$$\bar{\lambda}_j = \frac{\sum_i \lambda_j^i a_{j\beta}^i \text{abs } f_i}{\bar{a}_j \bar{v} \bar{\beta}}.$$

The average decay constants are given by group in Table 3-29.

3.1.10. Relative Control Rod Worth

The relative control rod worth was calculated for each of the fuels of interest at BOC-1 and EOC-1 conditions. These calculations were performed with the diffusion theory code PDQ in two dimensions for a geometric configuration representing four quarter-assemblies and the associated water gap. Each fuel pin cell was represented by a spatial mesh block, and reflecting boundary conditions were applied on all sides so that the calculation effectively represented an infinite array of 17×17 assemblies.

For each fuel type, the multiplication factor was calculated with and without control rods inserted in two diagonal assemblies of the array. This was done in each for hot full power (HFP), hot zero power (HZP), and hot standby (HSB) conditions. Calculated control rod worths were normalized to the worth obtained for the reference UO_2 case. For comparison, the relative worth was also calculated for a conventional $\text{PuO}_2\text{-UO}_2$ (Pu recycle) case. The results are presented in Table 3-30. For all the thorium-based fuels, the relative control rod worth is less than it is for the standard UO_2 case.

3.1.11. Core Calculations of Control Rod Worth

In addition to the calculations to determine relative control rod worth on an assembly basis, PDQ07 calculations were also performed in quarter-core and full-core geometry to determine control rod pattern worths, shutdown margins, ejected rod worths, and dropped rod worths. These calculations were performed at BOC conditions for a denatured $(^{233}\text{U}/^{238}\text{U-Th})\text{O}_2$ core and for a $(\text{Th-Pu})\text{O}_2$ core. The results were compared with corresponding results obtained for a reference UO_2 core.

The core loading configuration, which was the same for all fuel types analyzed, included fuel assemblies of three enrichments distributed as shown in Figure 3-3. In each case, the core contained 72 control rod assemblies arranged in

the pattern shown in Figure 3-4. The power distribution was fine-tuned for each core by appropriate adjustments of lumped burnable poison content in selected assemblies to ensure acceptable BOL power peaking.

The net control rod pattern worth, control requirement, and shutdown margin are shown in Table 3-31 for each of the cores analyzed. These results indicate that the shutdown margin is expected to be sufficient for either a denatured ($^{233}\text{U}/^{238}\text{U}\text{-Th}$) O_2 core or a $(\text{Th-Pu})\text{O}_2$ core. If required, additional margin could be obtained by increasing the number of control rod assemblies (up to 81 full-length control rods can be accommodated by a B&W plant of the reference size).

Ejected rod worths and dropped rod worths were also calculated to provide input to the analytical assessments of these accidents. Calculated ejected rod worths are shown in Table 3-32 for the three fuel types. It should be noted that the scope of the study did not permit worst-case searches to be made for the cores using thorium-based fuels. In these cases the ejected rod of maximum worth was assumed to be located in the same core position as for the standard UO_2 core. This was thought to be a reasonable first approximation since the loading pattern of the three kinds (enrichments) of assemblies was identical for all fuel types. Limiting dropped rod worths were also assumed to occur in the same location for each core, and the results are presented in Table 3-33.

As a general observation, the core calculations indicate the same qualitative conclusion regarding comparative rod worth as was indicated by the assembly calculations reported in section 3.1.10. Of the cores investigated, the standard UO_2 core consistently has larger control rod worths than the cores using thorium-based fuels.

3.2. Thermal Analysis of Fuel Pins

The design philosophy for safe operation of pin-type nuclear reactor cores requires heat rates to be established using conservative calculations to preclude central fuel melting. The buildup of pressure within the pin from gases produced during the fission process must also be predicted conservatively.

The TACO program¹¹ was developed by B&W to conservatively predict fuel pin temperatures and pressures. TACO includes models for fuel densification and swelling, fuel restructuring, gas release, cladding creep, and gap closure.

For this work, the code was modified to properly account for the fuel thermal expansion coefficient and the porosity correction factor for thermal conductivity of thorium-based fuels.

The fuel densification-swelling, fission gas release, fuel relocation, and restructuring models were kept intact in TACO, assuming that the existing urania models are also valid for the thoria-urania mixtures. Other important parameters, notably the thermal conductivity, can be accounted for through code input.

TACO was used to analyze the three thorium-based fuel types, and for comparison a standard UO_2 case was included. Standard TACO analytical procedures were used. Materials properties data used in these analyses are described in section 3.3. The cladding temperatures (average and surface) are shown in Figure 3-5 as a function of linear heat rate. These temperatures are applicable to all fuel types analyzed and are based on a coolant temperature of 597.5F.

The average fuel temperatures are shown as a function of burnup in Figures 3-6 through 3-9. Fuel temperatures are seen to be similar for all fuel types, with denatured $(^{233}\text{U}/^{238}\text{U-Th})\text{O}_2$ showing slightly higher temperatures than the other fuels. This is explained by the slightly lower thermal conductivity of the $(^{233}\text{U}/^{238}\text{U-Th})\text{O}_2$ fuel type for temperatures of about 2200F and above. The temperature rise early in life is typical and is due to densification. The subsequent gradual decrease in temperature can be attributed to the swelling of the pellets as they are exposed to radiation during reactor operation.

The results of the TACO analyses also indicated the fuel pin internal pressure as a function of burnup is similar for all the fuels analyzed. These results are shown in Figure 3-10.

3.3. Material Properties

A literature survey was conducted to determine the available thermal-physical properties and the irradiation behavior of thoria-based fuel mixtures. The thermal physical properties reviewed were melting point, thermal expansion, emissivity, thermal conductivity, and the porosity correction factor to the thermal conductivity. The irradiation effects reviewed include densification, swelling, fuel pellet relocation, release of fission gases, and restructuring.

The following subsections briefly discuss the thorium-based fuel properties used in this study.

3.3.1. Melting Point

The melting point is an important characteristic in evaluating the performance of reactor fuels. Standard practice in LWR fuel design is to avoid fuel melting during normal operation and abnormal operation transients. The melting point for UO_2 - ThO_2 mixtures is shown in Figure 3-11, which includes data from Christensen¹², Latta, et al.¹³, and Lambertson, et al.¹⁴ The melting point curve for ThO_2 - PuO_2 mixtures, developed by Freshley and Mattys¹⁵, is shown in Figure 3-12. Both of these melting point curves are recommended in the most recent compilation of the Alternate Fuels Properties Group of Argonne National Laboratory.¹⁶

3.3.2. Thermal Expansion Coefficients

Data on the thermal expansion of UO_2 are reported by Conway, et al.¹⁷, and data compiled for UO_2 , ThO_2 and ThO_2 - UO_2 mixtures is reported by Kittel et al.¹⁶ At temperatures below 1000K, the thermal expansion of all these oxides is essentially identical. Above 1500K the thermal expansion is higher for UO_2 than for ThO_2 and between these extremes for mixtures of UO_2 and ThO_2 . Empirical fits are recommended by Conway for UO_2 and by Kittel for ThO_2 and $(Th_{0.8}U_{0.2})O_2$. These formulas were combined by linear interpolation to obtain the appropriate expressions for thermal expansion of the fuel mixtures used in this study. The interpolation was performed as described below.

For mixtures with ThO_2 content up to 80%, use the interpolated value between UO_2 and $(Th_{0.8}U_{0.2})O_2$ as follows:

If $T < 1520F$,

$$\alpha = \frac{W_{th}}{80} (3.7778 \times 10^{-6} + 8.9506 \times 10^{-10}T) + \frac{W_u - 20}{80} \times 5.669 \times 10^{-6};$$

if $1520 < T \leq 1941F$,

$$\alpha = \frac{W_{th}}{80} \times 6.944 \times 10^{-6} + \frac{W_u - 20}{80} \times 5.669 \times 10^{-6};$$

If $1941F < T$,

$$\alpha = \frac{W_{th}}{80} \times 6.944 \times 10^{-6} + \frac{W_u - 20}{80} (4.005 \times 10^{-6} + 8.575 \times 10^{-10}T)$$

where

α = linear thermal expansion coefficient of thorium mixture,
in./in.-°F,

W_{th} = ThO_2 weight percentage, input,

W_u = UO_2 weight percentage, input.

For mixtures with ThO_2 content above 80%, use the interpolated value between $(Th_{0.8}U_{0.2})O_2$ and ThO_2 , as follows:

If $T \leq 1520F$,

$$\alpha = \frac{W_u}{20} (3.7778 \times 10^{-6} + 8.9506 \times 10^{-10} T) + \frac{W_{th} - 80}{20} (4.527 \times 10^{-6} + 3.611 \times 10^{-10}T);$$

if $1520F < T$,

$$\alpha = \frac{W_u}{20} \times 6.944 \times 10^{-6} + \frac{W_{th} - 80}{20} (4.527 \times 10^{-6} + 3.611 \times 10^{-10}T).$$

No data were found for thorium-plutonium mixtures. Thus, it was recommended that the thermal expansion of thorium-uranium mixtures be used.

3.3.3. Thermal Conductivity

A thorough compilation of thoria-urania conductivity data is reported by Berman, et al., of Bettis Atomic Power Laboratory.¹⁸ This report gives an analytical expression for thermal conductivity that is thought to be the best available in the literature today. The mathematical model recommended for temperatures less than or equal to 1800K is as follows:

$$K_{100} = [A_0 + A_1U + A_2U^2 + (B_0 + B_1U + B_2U^2)T]^{-1}$$

where

A,B = coefficients,

T = temperature, degrees Kelvin,

U = mole fraction of UO_2 in the mixture.

Above 1800K the conductivity is assumed to be constant. Very limited work was found for thoria-plutonia mixtures¹⁹, and the recommendation was to use the expression developed for thoria-urania.

3.3.4. Porosity Correction Factor

In determining the effect of density on thermal conductivity, reference 16 recommends that the conductivity be calculated by the Maxwell-Eucken equation²⁰:

$$k_p = \frac{1 - p}{1 + \beta p} k_0$$

where

p = porosity fraction = $1 - \rho$ (where ρ = fraction of TD),

k_p = conductivity of fuel with porosity p ,

k_0 = conductivity of 100% dense fuel,

β = a correction factor.

In addition, a temperature-dependent β is recommended.

To facilitate computation of the heat flow equation, the factor β was set constant at 1.4 corresponding to an average fuel temperature of 1000C. Further refinement was not thought to be necessary due to the fact that the porosity correction factor has a small effect on thermal performance.

3.3.5. Densification and Swelling

Fuel densification is an important parameter in the thermal analysis of PWR fuels. It has been observed to occur early in life in UO_2 fuel and results in a decrease in pellet diameter. Since thoria and urania are both oxide ceramics with similar structures, it can be anticipated that they will exhibit a similar type of densification behavior. This has been demonstrated to some extent in work done by Bettis²¹ in which a densification model developed for use on thoria LWBR fuels described the densification kinetics observed in Halden UO_2 fuels. Since the kinetics of thoria based fuels is expected to be similar to UO_2 fuels, the densification kinetics equation used by B&W and incorporated in the NRC-approved TACO code¹¹ was recommended for thoria-based fuels.

A second irradiation phenomenon that affects fuel density is fuel swelling. During irradiation, an ordered fuel structure is perturbed in a number of ways. High-energy neutrons and fission fragments can collide with fuel atoms and thereby cause defects, vacancies, interstitials, dislocation loops, etc.

The fission products generated, some of which are noble gases, may be incompatible with the fuel structure and precipitate or coalesce as inclusions or gas pores. Highly localized fission temperatures may cause crystallographic changes in the fuel.

The densification equation used in the TACO code also includes a term to account for swelling. The swelling rate used for thorium-based fuel is 0.007 $\Delta v/v$ per fission/cm³, as given in reference 21.

3.3.6. Fuel Relocation

Another important phenomenon affecting thermal performance is fuel pellet cracking and relocation. The examination of thorium-based fuels irradiated in Indian Point 1 indicated that these pellets do not exhibit cracking to the degree seen in UO₂ fuels;²² however, pellet cracking did occur in test rods irradiated by Bettis at high linear heat rates.²³ In the absence of other data, the relocation model developed for UO₂ pellets¹¹ was used for the steady state thermal analysis.

3.3.7. Fission Gas Release

Little data were found on the release of fission gas for thorium-based fuels. Because of this the UO₂ gas release model in the TACO code¹¹ was used with the burnup correction factor recommended by the NRC.²⁴

3.3.8. Restructuring

Restructuring is not generally observed in LWR fuels under steady-state conditions, nor is it considered in the analysis of transients. Since the restructuring temperature of thorium is generally accepted to be higher than that of UO₂, no restructuring was accounted for in the thermal analysis of thorium based fuels.¹⁶

Table 3-1. $(^{233}\text{U-Th})\text{O}_2$ Exponential Experiment⁹
Benchmark Comparison

<u>Moderator/fuel vol ratio</u>	<u>H₃BO₃ concentration, mg/cm³</u>	<u>"Best" value of measured B_m², m⁻²</u>	<u>Calculated k_{eff}</u>
0.9970	0	75.88	0.9923
1.3840	0	86.06	0.9941
1.7134	0	89.34	0.9982
2.1943	0	90.35	1.0005
3.0043	0	85.54	1.0015

Calculated critical $\bar{k}_{\text{eff}} = 0.9973 \pm 0.004$.

Table 3-2. Volume Fractions

<u>Material</u>	<u>Cell basis</u>	<u>Assembly basis</u>
Fuel	0.328	0.295
Moderator	0.548	0.584
Clad	0.105	0.103
Grid	0.003	0.003
Void	<u>0.016</u>	<u>0.015</u>
	1.000	1.000

Table 3-3. Initial Enrichment for Same Average EOC K

<u>Fuel type</u>	<u>Enrichment</u>	<u>Avg EOC K</u>
Std UO ₂	3.0 wt%	1.027
²³³ Du/ThO ₂	3.1	1.027
²³⁵ Du/ThO ₂	4.5	1.027
PuO ₂ -ThO ₂	4.15	1.027

$$\bar{K}_{EOC} = 1/3[K(11,000) + K(22,000) + (33,000)].$$

Table 3-4. Inverse Soluble Boron Worth

<u>Fuel type</u>	<u>Inverse worth, ppm/% Δρ</u>
Std UO ₂	102
²³³ Du-ThO ₂	129
²³⁵ Du-lhU ₂	137
PuO ₂ -ThO ₂	267

Table 3-5. Doppler Coefficient at BOC-1

Fuel temp, F	Doppler coeff, $10^{-5}/^{\circ}\text{F}$			
	Std UO_2	^{233}Du ThO_2	^{235}Du ThO_2	PuO_2 - ThO_2
Hot Operating Temperature, $T_m = 600\text{F}$, 1665 ppm boron				
3000	-0.89	-1.45	-1.47	-1.17
2700	-0.93	-1.52	-1.55	-1.25
2400	-0.98	-1.62	-1.66	-1.35
2100	-1.03	-1.74	-1.78	-1.44
1800	-1.06	-1.86	-1.90	-1.54
1600	-1.11	-1.94	-1.98	-1.56
1400	-1.16	-2.05	-2.11	-1.63
1200	-1.24	-2.23	-2.29	-1.81
900	-1.37	-2.51	-2.60	-2.07
600				

Hot Standby Temperature, $T_m = 549\text{F}$,
1863 ppm boron

900	-1.29	-2.33	-2.39	-1.90
750	-1.36	-2.48	-2.55	-2.03
600	-1.34	-2.56	-2.64	-2.06
549				

Table 3-6. Doppler Coefficient at EOC-1

Fuel temp, F	Doppler coeff, $10^{-5}/^{\circ}\text{F}$			
	Std UO_2	^{233}Du ThO_2	^{235}Du ThO_2	PuO_2 - ThO_2
<u>Hot Operating Temperature, $T_m = 600\text{F}$, 10 ppm boron</u>				
3000				
	-0.98	-1.52	-1.49	-1.21
2700				
	-1.03	-1.60	-1.56	-1.30
2400				
	-1.08	-1.70	-1.67	-1.40
2100				
	-1.14	-1.83	-1.80	-1.49
1800				
	-1.17	-1.94	-1.91	-1.60
1600				
	-1.24	-2.04	-2.00	-1.60
1400				
	-1.30	-2.16	-2.13	-1.71
1200				
	-1.39	-2.34	-2.31	-1.88
900				
	-1.53	-2.64	-2.62	-2.14
600				
<u>Hot Standby Temperature, $T_m = 549\text{F}$, 10 ppm boron</u>				
900				
	-1.40	-2.39	-2.36	-1.95
750				
	-1.48	-2.54	-2.52	-2.08
600				
	-1.48	-2.64	-2.62	-2.09
549				

Table 3-7. Doppler Coefficient at BOC-n

<u>Fuel temp, F</u>	<u>Doppler coeff, 10⁻⁵/°F</u>			
	<u>Std UO₂</u>	<u>²³³Du ThO₂</u>	<u>²³⁵Du ThO₂</u>	<u>PuO₂- ThO₂</u>
<u>Hot Operating Temperature, Tm = 600F, 1630 ppm boron</u>				
3000	-1.06	-1.69	-1.67	-1.32
2700	-1.11	-1.79	-1.76	-1.40
2400	-1.17	-1.91	-1.88	-1.53
2100	-1.23	-2.04	-2.03	-1.62
1800	-1.27	-2.18	-2.15	-1.73
1600	-1.35	-2.29	-2.28	-1.75
1400	-1.42	-2.44	-2.43	-1.87
1200	-1.52	-2.65	-2.64	-2.05
900	-1.67	-2.99	-3.00	-2.33
600				
<u>Hot Standby Temperature, Tm = 549F, 1863 ppm boron</u>				
900	-1.56	-2.76	-2.76	-2.15
750	-1.64	-2.95	-2.95	-2.29
600	-1.67	-3.05	-3.07	-2.33
549				

Table 3-8. Doppler Coefficient at EOC-n

Fuel temp, F	Doppler coeff, $10^{-5}/^{\circ}\text{F}$			
	Std UO_2	^{233}Du ThO_2	^{235}Du ThO_2	PuO_2 - ThO_2
<u>Hot Operating Temperature, $T_m = 600\text{F}$, 10 ppm boron</u>				
3000	-1.19	-1.76	-1.71	-1.36
2700	-1.24	-1.86	-1.80	-1.45
2400	-1.31	-1.98	-1.93	-1.57
2100	-1.37	-2.12	-2.07	-1.67
1800	-1.43	-2.26	-2.21	-1.78
1600	-1.50	-2.37	-2.33	-1.79
1400	-1.58	-2.53	-2.48	-1.91
1200	-1.69	-2.74	-2.70	-2.10
900	-1.85	-3.08	-3.06	-2.38
600				
<u>Hot Standby Temperature, $T_m = 549\text{F}$, 10 ppm boron</u>				
900	-1.70	-2.79	-2.76	-2.18
750	-1.79	-2.96	-2.94	-2.31
600	-1.77	-3.10	-3.07	-2.34
549				

Table 3-9. Moderator Coefficient of Reactivity

	Coeff of reactivity, $10^{-4}/^{\circ}\text{F}$			
	<u>Urania ^{235}U</u>	<u>Thoria ^{233}Du</u>	<u>Thoria ^{235}Du</u>	<u>Thoria- Plut</u>
<u>Hot Operating Temperature, $T_m = 600\text{F}$, $T_f = 1400\text{F}$</u>				
BOC-1, 1665 ppm	0.57	0.33	-0.95	-1.11
BOC-n, 1665 ppm	0.61	-0.13	-0.97	-0.88
BOC-1, 800 ppm	-0.78	-0.83	-2.01	-1.70
BOC-n, 800 ppm	-1.18	-1.35	-2.10	-1.54
EOC-1, 600 ppm	-1.40	-1.31	-2.45	-1.66
EOC-n, 600 ppm	-1.55	-1.81	-2.45	-1.63
EOC1, 10 ppm	-2.44	-2.26	-3.26	-2.17
EOC-n, 10 ppm	-2.73	-2.81	-3.35	-2.19
<u>Hot Standby Temperature, $T_m = 549\text{F}$, $T_f = 549\text{F}$</u>				
BOC-1, 1863 ppm	0.95	1.04	0.01	-0.65
BOC-n, 1863 ppm	0.85	0.84	0.16	-0.46
BOC-1, 1200 ppm	0.21	0.38	-0.58	-1.00
BOC-n, 1200 ppm	0	0.13	-0.49	-0.84
EOC-1, 600 ppm	-0.66	-0.30	-1.21	-1.07
EOC-n, 600 ppm	-0.71	-0.57	-1.09	-1.04
EOC-1, 10 ppm	-1.46	-1.03	-1.81	-1.47
EOC-n, 10 ppm	-1.63	-1.34	-1.77	-1.47

Table 3-10. Moderator Temperature Coefficient Vs Temperature
Near 600F, Cycle 1

Moderator temp, F	$10^{-4}/^{\circ}\text{F}$			
	BOC-1		EOC-1	
	1665 ppm	800 ppm	600 ppm	10 ppm
<u>Urania</u>				
610	0.51	-0.93	-1.68	-2.78
600	0.57	-0.78	-1.40	-2.44
590	0.62	-0.62	-1.12	-2.09
570	0.63	-0.46	-0.96	-1.83
<u>Thoria/²³³U denatured</u>				
610	0.27	-0.98	-1.54	-2.56
600	0.33	-0.83	-1.31	-2.26
590	0.38	-0.68	-1.08	-1.96
570	0.59	-0.38	-0.74	-1.53
<u>Thoria/²³⁵U denatured</u>				
610	-1.15	-2.29	-2.79	-3.66
600	-0.95	-2.01	-2.45	-3.26
590	-0.75	-1.73	-2.10	-2.85
570	-0.52	-1.38	-1.72	-2.39
<u>Thoria/plutonia</u>				
610	-1.38	-2.01	-1.99	-2.53
600	-1.11	-1.70	-1.66	-2.17
590	-0.83	-1.39	-1.32	-1.80
570	-0.10	-1.49	-1.38	-1.81

Table 3-11. Moderator Temperature Coefficient Vs Temperature
Near 600F, Equilibrium Cycle

Moderator temp, F	$10^{-4}/^{\circ}\text{F}$			
	BOC-n		EOC-n	
	1665 ppm	800 ppm	600 ppm	10 ppm
<u>Urania</u>				
610	0.76	-1.44	-1.91	-3.16
600	0.61	-1.18	-1.55	-2.73
590	0.46	-0.92	-1.19	-2.29
570	0.43	-0.79	-1.04	-2.06
<u>Thoria/²³³U denatured</u>				
610	-0.27	-1.58	-2.13	-3.19
600	-0.13	-1.35	-1.81	-2.81
590	0.01	-1.11	-1.49	-2.42
570	0.27	-0.76	-1.10	-1.95
<u>Thoria/²³⁵U denatured</u>				
610	-1.20	-2.41	-2.82	-3.78
600	-0.97	-2.10	-2.45	-3.35
590	-0.74	-1.78	-2.07	-2.91
570	-0.46	-1.40	-1.66	-2.42
<u>Thoria/plutonia</u>				
610	-1.15	-1.86	-1.97	-2.57
600	-0.88	-1.54	-1.63	-2.19
590	-0.60	-1.22	-1.28	-1.81
570	-0.08	-1.36	-1.36	-1.83

Table 3-12. Moderator Temperature Coefficient Vs Temperature
Near 549F, Cycle 1

Moderator temp, F	$10^{-4}/^{\circ}\text{F}$			
	BOC-1		EOC-1	
	1863 ppm	1200 ppm	600 ppm	10 ppm
<u>Urania</u>				
587	1.01	0.10	-0.94	-1.89
562	0.97	0.19	-0.72	-1.55
549	0.95	0.21	-0.66	-1.46
537	0.93	0.23	-0.61	-1.36
512	0.88	0.25	-0.55	-1.34
<u>Thoria/²³³U denatured</u>				
587	0.89	0.12	-0.73	-1.58
562	0.99	0.31	-0.40	-1.17
549	1.04	0.38	-0.30	-1.03
537	1.08	0.45	-0.19	-0.89
512	1.16	0.57	-0.02	-0.69
<u>Thoria/²³⁵U denatured</u>				
587	-0.22	-0.95	-1.72	-2.45
562	-0.04	-0.66	-1.33	-1.96
549	0.01	-0.58	-1.21	-1.81
532	0.06	-0.49	-1.08	-1.65
512	0.13	-0.37	-0.91	-1.44
<u>Thoria/plutonia</u>				
587	-0.58	-0.99	-1.15	-1.62
562	-0.60	-0.96	-1.06	-1.47
549	-0.65	-1.00	-1.07	-1.47
532	-0.70	-1.04	-1.08	-1.46
512	-0.84	-1.14	-1.16	-1.51

Table 3-13. Moderator Temperature Coefficient Vs Temperature
Near 549F, Equilibrium Cycle

Moderator temp, F	$10^{-4}/^{\circ}\text{F}$			
	BOC-n		EOC-n	
	1863 ppm	1200 ppm	600 ppm	10 ppm
<u>Urania</u>				
587	0.86	-0.14	-1.02	-2.09
562	0.85	-0.02	-0.77	-1.72
549	0.83	0.00	-0.71	-1.62
537	0.81	0.02	-0.66	-1.52
512	0.89	0.02	-0.62	-1.42
<u>Thoria/²³³U denatured</u>				
587	0.60	-0.23	-1.09	-1.98
562	0.78	0.04	-0.70	-1.50
549	0.84	0.13	-0.57	-1.34
537	0.90	0.23	-0.45	-1.18
512	1.00	0.36	-0.26	-0.95
<u>Thoria/²³⁵U denatured</u>				
587	-0.16	-0.93	-1.66	-2.47
562	0.08	-0.60	-1.22	-1.94
549	0.16	-0.49	-1.09	-1.77
532	0.24	-0.38	-0.95	-1.60
512	0.34	-0.23	-0.76	-1.37
<u>Thoria/plutonia</u>				
587	-0.33	-0.79	-1.10	-1.61
562	-0.39	-0.79	-1.02	-1.47
549	-0.46	-0.84	-1.04	-1.47
532	-0.53	-0.89	-1.05	-1.47
512	-0.70	-1.04	-1.14	-1.52

Table 3-14. Total Reactivity of Moderator Density Change
at Beginning of Cycle 1, 1630 ppm

Relative density	Total reactivity, %			
	Urania ²³⁵ U	Thoria ²³³ U denatured	Thoria ²³⁵ U denatured	Thoria- plutonia
1.30	-1.61	-1.56	0.36	2.22
1.20	-0.91	-0.88	0.45	1.66
1.10	-0.36	-0.34	0.35	0.93
1.0 ^(a)	0	0	0	0
0.9	0.12	0.10	-0.67	-1.19
0.8	-0.08	-0.13	-1.77	-2.69
0.7	-0.72	-0.82	-3.46	-4.62
0.6	-1.98	-2.16	-6.0	-7.1
0.4	-7.9	-8.6	-15.7	-14.7
0.2	-25.8	-30.2	-42.4	-30.8

(a) Reference relative density, $d/d_o = 1.0$ at hot full power,
600F, 2250 psi.

Table 3-15. Total Reactivity of Moderator Density Change
at End of Cycle 1, 10 ppm

Relative density	Total reactivity, %			
	Urania ²³⁵ U	Thoria ²³³ U denatured	Thoria ²³⁵ U denatured	Thoria- plutonia
1.30	3.0	2.4	3.7	3.8
1.20	2.3	1.9	2.7	2.7
1.10	1.3	1.0	1.5	1.5
1.0 ^(a)	0	0	0	0
0.9	-1.58	-1.31	-1.88	-1.74
0.8	-3.59	-3.02	-4.25	-3.83
0.7	-6.2	-5.3	-7.3	-6.4
0.6	-9.6	-7.6	-11.3	-9.5
0.4	-20.8	-18.9	-24.7	-18.7
0.2	-46.3	-46.8	-56.1	-37.3

(a) Reference relative density, $d/d_o = 1.0$ at hot full power,
600F, 2250 psi.

Table 3-16. Total Reactivity of Moderator Density Change
at Beginning of Cycle N, 1630 ppm

Relative density	Total reactivity, %			
	Urania ²³⁵ U	Thoria ²³³ U denatured	Thoria ²³⁵ U denatured	Thoria plutonia
1.30	-0.59	-0.79	0.36	2.2
1.20	-0.17	-0.32	0.47	1.6
1.10	0.04	-0.04	0.37	0.9
1.0 ^(a)	0	0	0	0
0.9	-0.36	-0.26	-0.72	-1.15
0.8	-1.15	-0.94	-1.91	-2.59
0.7	-2.51	-2.18	-3.73	-4.39
0.6	-4.7	-4.25	-6.5	-6.7
0.4	-13.4	-12.9	-14.5	-13.2
0.2	-36.4	-38.7	-44.5	-26.7

(a) Reference relative density, $d/d_o = 1.0$ at hot full power, 600F, 2250 psi.

Table 3-17. Total Reactivity of Moderator Density Change
at End of Cycle N, 10 ppm

Relative density	Total reactivity, %			
	Urania ²³⁵ U	Thoria ²³³ U denatured	Thoria ²³⁵ U denatured	Thoria plutonia
1.30	3.9	3.3	3.9	4.0
1.20	2.8	2.4	2.9	2.9
1.10	1.6	1.4	1.6	1.5
1.0 ^(a)	0	0	0	0
0.9	-1.97	-1.70	-1.98	-1.8
0.8	-4.45	-3.89	-4.48	-3.93
0.7	-7.7	-6.7	-7.71	-6.5
0.6	-11.9	-10.5	-11.9	-9.6
0.4	-25.6	-23.3	-25.9	-18.2
0.2	-56.0	-55.1	-58.4	-34.8

(a) Reference relative density, $d/d_o = 1.0$ at hot full power,
600F, 2250 psi.

Table 3-18. Fuel Isotopic Concentrations at Beginning of Cycle 1

	Isotopic conc, nuclei/barn-cm			
	Urania 3.0 wt % ²³⁵ U	Thoria 3.1 wt % ²³³ Ud	Thoria 4.5 wt % ²³⁵ Ud	Thoria 4.15 wt % ²³⁹ Pu+ ²⁴¹ Pu
²³² Th	--	0.16057-1	0.16683-1	0.20064-1
²³³ U	--	0.65868-3	--	--
²³⁴ U	0.51552-5	0.20323-4	0.96345-5	--
²³⁵ U	0.68736-3	0.96017-5	0.95932-3	--
²³⁶ U	--	--	--	--
²³⁸ U	0.21944-1	0.47308-2	0.37794-2	--
²³⁹ Pu	--	--	--	0.69684-3
²⁴⁰ Pu	--	--	--	0.24625-3
²⁴¹ Pu	--	--	--	0.16046-3
²⁴² Pu	--	--	--	0.66829-4

Table 3-19. Fuel Isotopic Concentrations at End of Cycle 1

	Isotopic conc, nuclei/barn-cm			
	Urania 3.0 wt % ²³⁵ U	Thoria 3.1 wt % ²³³ Ud	Thoria 4.5 wt % ²³⁵ Ud	Thoria 4.15 wt % ²³⁹ Pu+ ²⁴¹ Pu
²³² Th	--	0.15920-1	0.16552-1	0.19935-1
²³³ Pa	--	0.16562-4	0.15460-4	0.15289-4
²³³ U	--	0.51332-3	0.90742-4	0.92572-4
²³⁴ U	0.42849-5	0.46193-4	0.12509-4	0.40631-5
²³⁵ U	0.44163-3	0.10752-4	0.68503-3	0.25228-6
²³⁶ U	0.44701-4	0.74350-6	0.52719-4	0.36589-8
²³⁸ U	0.21778-1	0.46649-2	0.37225-2	0.1-9
²³⁹ Pu	0.80122-4	0.34841-4	0.32067-4	0.42951-3
²⁴⁰ Pu	0.14941-4	0.60754-5	0.47749-5	0.25122-3
²⁴¹ Pu	0.66043-5	0.26664-5	0.23221-5	0.16478-3
²⁴² Pu	0.63505-6	0.22332-6	0.16418-6	0.73419-4

Table 3-20. Fuel Isotopic Concentrations at Beginning of Equilibrium Cycle

	Isotopic conc, nuclei/barn-cm			
	Urania 3.0 wt % ^{235}U	Thoria 3.2 wt % ^{233}Ud	Thoria 4.1 wt % ^{235}Ud	Thoria 4.35 wt % $^{239}\text{Pu}+^{241}\text{Pu}$
^{232}Th	--	0.15878-1	0.16266-1	0.19601-1
^{233}Pa	--	0.44807-5	0.42350-5	0.40718-5
^{233}U	--	0.52849-3	0.31241-3	0.10135-3
^{234}U	0.42849-5	0.15607-3	0.75199-4	0.37876-5
^{235}U	0.44163-3	0.37153-4	0.40857-3	0.23311-6
^{236}U	0.44701-4	0.10518-4	0.21292-3	0.32073-8
^{238}U	0.21728-1	0.45599-2	0.38631-2	0.1-10
^{239}Pu	0.82046-4	0.35944-4	0.33529-4	0.40375-3
^{240}Pu	0.14941-4	0.59047-5	0.49116-5	0.39212-3
^{241}Pu	0.65632-5	0.25667-5	0.21987-5	0.25674-3
^{242}Pu	0.63505-6	0.20571-6	0.15318-6	0.22490-3

Table 3-21. Fuel Isotopic Concentrations at End of Equilibrium Cycle

	Isotopic conc, nuclei/barn-cm			
	Urania 3.0 wt % ^{235}U	Thoria 3.2 wt % ^{233}Ud	Thoria 4.1 wt % ^{235}Ud	Thoria 4.35 wt % $^{239}\text{Pu}+^{241}\text{Pu}$
^{232}Th	--	0.15730-1	0.16126-1	0.19467-1
^{233}Pa	--	0.17719-4	0.16795-4	0.15837-4
^{233}U	--	0.4299-3	0.30081-3	0.17356-3
^{234}U	0.34767-5	0.16377-3	0.82221-4	0.10786-4
^{235}U	0.27459-3	0.40768-4	0.29074-3	0.12562-5
^{236}U	0.72103-4	0.12759-4	0.22654-3	0.37560-7
^{238}U	0.21592-1	0.44876-2	0.37961-2	0.1-9
^{239}Pu	0.10749-3	0.47355-4	0.45237-4	0.23408-3
^{240}Pu	0.34157-4	0.13012-4	0.11139-4	0.34108-3
^{241}Pu	0.19577-4	0.88707-5	0.79589-5	0.22949-3
^{242}Pu	0.43865-5	0.16683-5	0.13052-5	0.23005-3

Table 3-22. Neutron Lifetime

	Lifetime, 10^{-6} seconds			
	Std UO_2	$^{233}\text{DU}/\text{ThO}_2$	$^{235}\text{DU}/\text{ThO}_2$	$\text{PuO}_2/\text{ThO}_2$
BOC-1	20.4	15.9	15.9	7.45
EOC-1	21.5	18.7	17.0	9.05
BOC-n	20.1	16.6	15.7	7.75
EOC-n	24.0	19.3	18.0	9.65

Figure 3-23. Delayed Neutron Fractions

	β_{eff}			
	Std UO_2	$^{233}\text{DU}/\text{ThO}_2$	$^{235}\text{DU}/\text{ThO}_2$	$\text{PuO}_2/\text{ThO}_2$
BOC-1	0.00740	0.00316	0.00725	0.00323
EOC-1	0.00592	0.00320	0.00615	0.00348
BOC-n	0.00592	0.00332	0.00483	0.00300
EOC-n	0.00535	0.00341	0.00451	0.00398

Table 3-24. Fission Fractions for Standard UO_2

Nuclide	MeV/fiss	BOC-1	EOC-1	BOC-n	EOC-n
^{233}U	198.8	--	--	--	--
^{235}U	201.3	0.94	0.635	0.635	0.445
^{238}U	204.7	0.06	0.065	0.065	0.075
^{239}Pu	209.5	--	0.275	0.275	0.40
^{241}Pu	211.8	--	0.025	0.025	0.08

Table 3-25. Fission Fractions for Denatured $^{233}\text{U}/\text{ThO}_2$

<u>Nuclide</u>	<u>BOC-1</u>	<u>EOC-1</u>	<u>BOC-n</u>	<u>EOC-n</u>
^{232}Th	0.01	0.01	0.01	0.01
^{233}U	0.97	0.84	0.81	0.73
^{235}U	0.01	0.015	0.045	0.06
^{238}U	0.01	0.015	0.015	0.015
^{239}Pu	--	0.11	0.11	0.155
^{241}Pu	--	0.01	0.01	0.03

Table 3-26. Fission Fractions for Denatured $^{235}\text{U}/\text{ThO}_2$

<u>Nuclide</u>	<u>BOC-1</u>	<u>EOC-1</u>	<u>BOC-n</u>	<u>EOC-n</u>
^{232}Th	0.01	0.01	0.01	0.01
^{233}U	--	0.13	0.44	0.465
^{235}U	0.98	0.75	0.44	0.35
^{238}U	0.01	0.01	0.01	0.01
^{239}Pu	--	0.095	0.095	0.14
^{241}Pu	--	0.005	0.005	0.025

Table 3-27. Fission Fractions for $\text{PuO}_2/\text{ThO}_2$

<u>Nuclide</u>	<u>BOC-1</u>	<u>EOC-1</u>	<u>BOC-n</u>	<u>EOC-n</u>
^{232}Th	0.015	0.015	0.015	0.015
^{233}U	--	0.10	0.10	0.195
^{239}Pu	0.760	0.60	0.49	0.365
^{240}Pu	0.005	0.005	0.01	0.01
^{241}Pu	0.22	0.28	0.38	0.41
^{242}Pu	--	--	0.005	0.005

Table 3-28. Relative Yields of Delayed Neutron Groups

Fuel type		Group number					
		1	2	3	4	5	6
Urania, ^{235}U	BOC-1	0.03438	0.20199	0.18424	0.40427	0.14207	0.03311
	EOC-1	0.03267	0.20699	0.18601	0.39413	0.14440	0.03579
	BOC-n	0.03269	0.20699	0.18601	0.39413	0.14440	0.13579
	EOC-n	0.02989	0.20891	0.18574	0.38728	0.15037	0.03790
Thoria, ^{233}U den.	BOC-1	0.07725	0.25637	0.21750	0.33178	0.08960	0.02739
	EOC-1	0.06937	0.25227	0.21366	0.33657	0.09853	0.02961
	BOC-n	0.06708	0.24936	0.21177	0.34122	0.10090	0.02956
	EOC-n	0.06201	0.24704	0.20886	0.34598	0.10646	0.02956
Thoria, ^{235}U den.	BOC-1	0.03724	0.20911	0.18632	0.40772	0.11384	0.02778
	EOC-1	0.03981	0.21492	0.18941	0.39917	0.12842	0.02818
	BOC-n	0.04899	0.22635	0.19690	0.38113	0.11863	0.02830
	EOC-n	0.04951	0.23014	0.19812	0.37578	0.11824	0.02828
Thoria, plutonia	BOC-1	0.02711	0.24945	0.19449	0.36169	0.13879	0.02855
	EOC-1	0.02841	0.24598	0.19234	0.36486	0.14216	0.02625
	BOC-n	0.02511	0.24146	0.18801	0.37077	0.15039	0.02421
	EOC-n	0.02726	0.24005	0.18768	0.37151	0.15053	0.02247

Table 3-29. Decay Constants for Delayed Neutron Groups

Fuel type		Group number					
		1	2	3	4	5	6
Urania, ^{235}U	BOC-1	0.01273	0.03185	0.1181	0.3175	1.402	3.92
	EOC-1	0.01276	0.03160	0.1216	0.3224	1.398	3.85
	BOC-n	0.01276	0.03160	0.1216	0.3224	1.398	3.85
	EOC-n	0.01280	0.03145	0.1244	0.3283	1.408	3.82
Thoria, ^{233}U den.	BOC-1	0.0126	0.03333	0.1306	0.3077	1.286	3.30
	(Same for EOC-1, BOC-n and EOC-n) ^(b)						
Thoria, ^{235}U den.	BOC-1	0.0127	0.0317	0.115	0.311	1.40	3.87
	(Same for EOC-1, BOC-n and EOC-n) ^(b)						
Thoria, plutonia	BOC-1	0.01283	0.03081	0.1297	0.3385	1.427	3.28
	EOC-1	0.01277	0.03089	0.1289	0.3383	1.457	3.29
	BOC-n	0.01276	0.0307	0.1279	0.3408	1.492	3.32
	EOC-n	0.01272	0.03086	0.1274	0.3399	1.504	3.33

(a) Fissions are predominately those from ^{233}U .

(b) Fissions are either ^{235}U or ^{233}U with negligible error.

Table 3-30. Relative Rod Worth on Assembly Basis (a)

	<u>Std UO₂</u>	<u>²³³DU ThO₂</u>	<u>²³⁵DU ThO₂</u>	<u>PuO₂-ThO₂</u>	<u>PuO₂-UO₂</u>
<u>BOC-1 — 1650 ppm boron in moderator</u>					
HFP	1.00	0.857	0.951	0.589	0.617
HZP	0.989	0.837	0.930	0.579	0.609
HSB	0.952	0.812	0.895	0.567	0.597
<u>EOC-1 — 10 ppm boron in moderator</u>					
HFP	1.025	0.963	0.983	0.665	
HZP	1.013	0.941	0.961	0.654	
HSB	0.975	0.909	0.922	0.639	

(a) Rods are in alternate assemblies (50% coverage).

Table 3-31. Reactivity Shutdown Analysis

	Reactivity worth, % $\Delta k/k$		
	<u>UO₂</u>	<u>(²³³U/Th)O₂</u>	<u>(Pu/Th)O₂</u>
Net pattern worth (a)	6.74	5.96	5.49
Requirements	<u>1.70</u>	<u>2.50</u>	<u>2.60</u>
Available shutdown margin	5.04	3.46	2.89

(a) Most reactive rod stuck out.

Table 3-32. Ejected Rod Worth

<u>Fuel</u>	<u>Power level, %</u>	<u>Ejected rod worth, % $\Delta k/k$</u>
UO ₂	100	0.46
UO ₂	0	0.91
(²³³ U/Th)O ₂	100	0.33
(²³³ U/Th)O ₂	0	0.75
(Th/Pu)O ₂	100	0.20
(Th/Pu)O ₂	0	0.41

Table 3-33. Limiting Dropped Rod Worth^(a)

<u>Fuel</u>	<u>CRA banks inserted</u>	<u>Rod worth, % $\Delta k/k$</u>
UO ₂	None	0.12
(²³³ U/Th)O ₂	None	0.12
(Th/Pu)O ₂	None	0.11

(a) Hot full power, location L-10.

Figure 3-1. Schematic of 3-Cycle In-Reactor Residence Time

Figure 3-2. Recycling Sequence Used in Fuel Cell Analysis Task

Figure 3-3. Core Loading Diagram

BATCH NO.	URANIA W/O U235	THORIA W/O U233	THORIA W/O PLUT (FISS.)
1	2.49	2.0	2.325
2	2.88	2.5	3.100
3	3.45	3.0	3.875

Figure 3-4. Control Rod Locations for Rod Worth Calculations

Figure 3-5. Clad Temperatures (Average Pin)
Thorium Fuel Analysis*

*For all fuel compositions analyzed.

Figure 3-6. Average Fuel Temperature Thorium Fuel Analysis $\text{ThO}_2(72.5\%) \text{UO}_2(27.5\%)$

Figure 3-7. Average Fuel Temperature Thorium Fuel Analysis $\text{ThO}_2(58\%) \text{UO}_2(42\%)$

Figure 3-8. Average Fuel Temperature Thorium Fuel Analysis $\text{ThO}_2(82.5\%) \text{PuO}_2(17.5\%)$

Figure 3-9. Average Fuel Temperature Thorium Fuel Analysis UO_2 (100%)

Figure 3-10. Fuel Pin Internal Pressure (Average Pin) Thorium-Based Fuels Plus 100% UO_2
 Fuel TACO Code Analysis — NRC Fission Gas Release Model

Figure 3-11. Melting Curves for UO_2/ThO_2 System

Figure 3-12. Melting Point of $\text{ThO}_2\text{-PuO}_2$ ¹⁵

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4. SAFETY ANALYSIS OF THORIUM-BASED FUELS

4.1. Introduction

The safety and licenseability of the B&W Standard 205-FA (fuel assembly) NSS are reported for thorium-based fuels in this section based on the analysis of selected reactor transients. The transients and methods of analysis used in this study are consistent with those of a Final Safety Analysis Report prepared for a B&W 205-FA NSS,²⁵ which conforms to NRC guidelines²⁶ and review plans.²⁷ The properties of the alternate thorium-based fuels were incorporated into an existing B&W plant design model in order to perform the transient analyses. The reactor system responses for these transients were compared to those of transients that have been analyzed with the same model for the standard UO₂ fuel in order to assess the feasibility of operating a B&W NSS with the thorium-based fuels.

The existing plant design chosen for this study was the standard 205-FA plant with a rated core power of 3800 MWt. This is a two-loop PWR comprising two hot legs, one pressurizer, two integral economizer once-through steam generators (OTSGs, 16,000 tubes each), four cold legs (two per loop, one RC pump in each), and the reactor vessel. The system is designed to maintain a constant core average coolant temperature of 597F during operation above 15% full power. The core fuel assembly design is described below.

Type	Mark C
No. of fuel assemblies	205
Standard fuel	UO ₂ sintered pellets
FA array	17 by 17
Fuel rods/assemblies	264
No. of control rod assemblies	72
No. of APSR assemblies	8

The computer code models developed for the thorium-based fuel differ from the standard fuel model only in their description of the fuel characteristics. This premise dictates that variations of the reactor system's response to a given transient for the various fuels result from either material or kinetic property differences in the fuel. For this reason, the safety-related physics parameters (presented in section 3.1 of this report) are assessed here with respect to their impact on transient accident conditions. The results of this initial assessment are used to formulate a systematic approach to the analysis of transients for the alternate fuels.

4.2. Accident Analysis Description

This section addresses abnormal operations in the systems and components within the scope of supply of the reactor vendor. The events examined are taken from the listing of accidents required by the NRC in Regulatory Guide 1.70, "Standard Format and Content of Safety Analysis Reports for Nuclear Power Plants," Revision 2, issued in September 1975.

The accidents are presented in the SAR in a format that reflects the manner in which the accident affects the reactor system. Typically, the accidents are grouped into the following eight categories:

1. Increase in heat removal by secondary system.
2. Decrease in heat removal by secondary system.
3. Decrease in reactor coolant system flow rate.
4. Reactivity and power distribution anomalies.
5. Increase in reactor coolant inventory.
6. Decrease in reactor coolant inventory.
7. Radioactive release from a subsystem or component.
8. Anticipated transients without scram.

All the accidents within these categories are conservatively analyzed with respect to time in life of the reactor core. In each accident analysis, the cause and effect and the order of occurrence for the postulated events are described. The degree of dependence on reactor protection system (RPS) operation and the conservative values of important design parameters are indicated in each analysis. Each accident analysis states the RPS, emergency core cooling system (ECCS), and/or safety-related system functions that would terminate the transient.

The transient conditions resulting from all accidents are analyzed to the extent that they are shown to be either:

1. Inherently terminated,
2. Terminated by operation of the RPS, which is designed to maintain the integrity of the fuel and/or the reactor coolant system (RCS), or
3. Terminated by other conditions that result in operation of the engineered safety features or other safety-related systems that are designed to maintain the integrity of the core and/or the containment and to reduce the potential offsite doses to the public when one or more of the protective barriers are ineffective.

4.3. Accident Selection Process

The initial assumption that the alternate fuels are to be incorporated in a standard B&W PWR system reduces the scope of transients that need to be analyzed. Only those events in which the system response is sensitive to the core characteristics require analysis in order to assess the safety and licenseability of a plant operating with a fuel other than the standard UO_2 composition. The categories chosen for analysis are as follows:

1. Increase in heat removal by secondary system.
2. Decrease in heat removal by secondary system.
3. Decrease in RCS flow rate.
4. Reactivity and power distribution anomalies.
5. Anticipated transients without scram (ATWS).

These accident categories provide a means to assess the safety-related physics parameters of the alternate fuels provided in section 3.1 of this study. As a result of the manner in which these categories were formed, they respond differently to changes in fuel characteristics, and the controlling physics parameters for each transient within these categories are known. The physics parameters that are of major concern to the transient analyses are the moderator temperature reactivity coefficient (α_m), the Doppler or fuel temperature reactivity coefficient (α_D), the delayed neutron fraction (β), and the prompt neutron lifetime (ℓ). These parameters affect both the timing and magnitude of the core response, which is representative of changes in the times of reactor trip and engineered safety features actuation system (ESFAS) initiation.

These interrelated effects necessitate computer analysis of most transients by computer codes incorporating time-dependent feedback of the integrated RCS. The accidents involving changes (increases or decreases) in reactor coolant inventory were not selected for analysis in this study. An increase in reactor coolant inventory is referred to as a "moderator dilution accident" in B&W Safety Analysis Reports. This accident results in uncontrolled boron dilution due to the inadvertent addition of unborated water. The reason for not including this accident category in the analyses was that the reactivity change due to a given amount of moderator dilution is less severe for the thorium-based cores than for the UO_2 core (due to the smaller reactivity worth of boron in the thorium-based case). A decrease in reactor coolant inventory is known generically as a loss-of-coolant accident (LOCA). Qualitatively, it can be noted that the smaller neutron lifetimes and delayed neutron fractions of the thorium-based cores would tend to cause a more rapid reduction of the fission rate during a LOCA, which would reduce the residual fission power. In addition, both the melting temperatures and thermal conductivities of the thorium-based fuels are somewhat higher than those of corresponding UO_2 -based fuels. Since these characteristics tend to reduce the severity of the consequences of a LOCA, it was decided to forego expensive LOCA analyses in the current preliminary assessment of safety and licenseability.

A summary of the accidents in each of the first four categories of accidents selected for analysis is given in Table 4-1. The ATWS category is not included because it encompasses all transients. The accidents, for which the Doppler coefficient is indicated as a controlling parameter, are sensitive to changes in the delayed neutron fraction and the prompt neutron lifetime. Figures 4-1 through 4-4 compare the core physics parameters for the three thorium fuel mixtures to those of a standard UO_2 core and a plutonium-uranium mixed oxide core studied by B&W. The mixed oxide comparison was included because sensitivity studies performed in the mixed oxide study for three reactivity transients help to indicate the importance of the physics parameters and the results that may be expected from their variance.

Figure 4-1 shows that the range of the moderator coefficient of the thorium fuel mixtures is similar to the α_m range for UO_2 . The limiting values shown for UO_2 are those of the Bellefonte plant. Based on comparative values, this parameter is not expected to greatly impact any transient results comparison.

The Doppler coefficient values (Figure 4-2), however, cover a range that is more negative for the thorium fuels than for the UO_2 . The UO_2 values are again those calculated for the Bellefonte reactor, and the values for the thorium-based fuels are from Tables 3-5 through 3-8. BOL values are BOC-1 results at a fuel temperature of 1500F, and EOL values are the EOC-n results at 1145F fuel temperature. From the standpoint of kinetics only, a more negative Doppler coefficient is a desirable safety feature in containing transient power excursions. However, a more negative Doppler coefficient will also delay high-power and high-pressure trips, and the final transient consequences cannot be predicted for all cases based on UO_2 data.

The delayed neutron fraction ranges are shown in Figure 4-3. The UO_2 values are once again those calculated for the Bellefonte reactor. The values for the thorium mixtures are outside the UO_2 range for this parameter also. In addition, the time-in-life relationship for the β range of the thorium fuel mixtures varies. The β_{eff} value is smallest at BOL for the Pu and ^{233}U cases, whereas it is smallest at EOL for the ^{235}U and standard UO_2 cases. This behavior reflects the different isotopic mixtures in these fuels and how the various important nuclides build-in or are depleted as a function of irradiation. These differences would affect the reactivity transients and may shift the worst time-in-life of their occurrence.

Figure 4-4 illustrates the relationships of the prompt neutron lifetime ranges for the various fuels. It is readily seen that the thorium mixture ranges are well outside the uranium range (Bellefonte values), especially for the Th/Pu fuel mixture. This parameter should influence the short-lived reactivity transients, but the extent is unknown, especially since the effects of this parameter cannot be totally isolated from the effects of β .

Figures 4-5 through 4-7 show the results of sensitivity studies performed on α_m , α_D , and β , respectively, for a Pu/U mixed oxide fuel for three reactivity transients. For each transient the physics parameter is plotted versus the most limiting system response. These figures illustrate the dependence of the different reactivity transients on each parameter. The trend of a transient's results depends on whether or not the termination of the transient depends on a reactor trip or on the physics feedback alone.

Table 4-2 summarizes the relationship of the physics parameters for each of the thorium fuels to those of the standard uranium fuel. The most limiting fuel

is $(^{233}\text{U}/\text{Th})\text{O}_2$, and the results of a safety analysis study of this fuel should bound those of the other two fuels in most circumstances. For this reason, most of the analytical effort was expended on the $(^{233}\text{U}/\text{Th})\text{O}_2$ fuel, and a secondary analysis was performed on the $(\text{Th}/\text{Pu})\text{O}_2$ fuel because of the large difference between the prompt neutron lifetimes of that fuel and the $(\text{U}/\text{Th})\text{O}_2$ fuels.

Based on the preceding assessment of the safety-related physics parameters and the relative importance of each transient with respect to licensing concerns, the following accidents were analyzed in detail in this study for the $^{233}\text{U}/\text{Th}$ mixed oxide fuel:

1. Steam line break.
2. Feedwater line break.
3. Loss of coolant flow.
4. Locked rotor.
5. Startup accident.
6. Uncontrolled CR group withdrawal at power.
7. CRA drop.
8. Rod ejection.
9. Loss of normal feedwater (ATWS).

Accidents 2, 6, 7, and 8 of this list are also analyzed for the Th/Pu mixed oxide fuel.

4.4. Conservative Assumptions

The initial design conditions applicable to all the accident analyses are given in Tables 4-3 and 4-4. These tables summarize the fuel-independent and fuel-dependent parameters, respectively. The evaluation of average system responses for each accident is supplemented with conservative engineering assumptions to provide a margin for uncertainties in calculational methods. Because of allowable operating ranges and instrument uncertainties, the accident analyses are performed so that the set of initial offset conditions that results in the most limiting transient is always accounted for in the analysis. This is done by incorporating the operational ranges and uncertainties in the initial conditions and incorporating the maximum RPS uncertainty in the setpoint in the direction resulting in the most limiting transient.

As shown in Table 4-3, 102% of rated core power is used as the initial condition for the full-power cases (in accordance with Reg. Guide 1.49), while nominal design values are used as initial conditions for RCS flow. The initial condition values for the RCS pressure and inlet temperatures include offset values of ± 45 psi and $\pm 2^\circ\text{F}$, respectively, to account for control band and instrumentation errors. The reactor power, pressure, and inlet temperature parameter values are chosen to produce the minimum DNB initial condition for each transient unless specific exception is taken in the individual sections. In addition, the RPS trip values used in the accident analyses for these parameters are inclusive of maximum measurement errors. For example, the high-pressure trip value of 2470 psia used in the accident analysis is 115 psi larger than the actual trip setpoint value of 2355 psia. This difference accounts for the pressure tap error and for the maximum pressure difference between the pressure tap and the core exit. The 115 psi, then, represents the maximum expected difference in the direction that will most delay the action of the high-pressure trip, thus making the transient more severe.

Other conservatisms are provided in individual analyses through the use of clean or fouled (whichever is more conservative) steam generator inventory, minimum or maximum system volumes, etc. For hot channel DNBR assessment, transient pressures and temperatures are based on initialization at offset conditions and are corrected by -45 psi and $+2^\circ\text{F}$, respectively. The control rod worths used for transients caused by their inadvertent motion were limiting values based on calculations for a UO_2 core. Physics calculations show that the worths determined for the UO_2 core are more conservative than those that would be used in a thorium core. The UO_2 peaking factors were also used for the thorium core calculations.

For accidents resulting in reactor trip, a minimum tripped rod worth for either beginning of life (BOL) or end of life (EOL) is assumed. The minimum tripped rod worth accounts for the moderator deficit, Doppler deficit, and a 1% $\Delta k/k$ subcritical margin at hot shutdown with the rod of maximum worth stuck out of the core.

The normalized control rod worth insertion curves used in the analysis of events resulting in reactor trip are shown in Figure 4-8. Figures 4-9 and 4-10 show the core reactivity as a function of control rod position and as a function of time following trip, respectively, and were used to generate Figure

4-8. Several factors that influence the axial power profile and hence the integral rod worth were investigated to determine the appropriate curves for use in the transient analysis for the standard UO_2 core. Such factors as (1) number and worth of rods dropped, (2) effects of flux redistribution, (3) initial offset conditions, (4) power levels, and (5) time in core life were considered and, in conjunction with operating plant test data, formed the basis for the integral rod worth curves shown in Figure 4-9. The curve of control rod position versus time (Figure 4-8) was conservatively established from supportive test data on the control rod design.

4.5. Terminology

"Neutron power" is used to describe the total energy release from fission. "Thermal power" refers to the total core heat transferred from the fuel to the coolant. "RCS pressure" is used synonymously with core outlet pressure. A trip setpoint value designates a core condition, inclusive of maximum calibration and instrument errors, that must be exceeded before a reactor trip is assumed to occur. After the trip setpoint value has been exceeded, there is a trip delay time before the rods begin moving into the core. The trip delay time for each reactor trip mode used in the analyses, shown in Table 4-3, is composed of sensor delay time, RPS delay time, circuit breaker delay time, and a CRD release delay time. The CRD release delay time and the circuit breaker delay time are constant regardless of the mode of reactor trip. However, the sensor and RPS delay times, vary for the different trip modes depending on the parameters being measured and on the function to be performed with the monitored parameter. Therefore, the trip delay time is different for each mode of reactor trip. The trip delay times listed in Table 4-3 represent the maximum expected values; therefore, no error analysis is associated with them.

4.6. Acceptance Criteria

The criterion adopted in these accident analyses to ensure that the integrity of the RCS boundary is maintained for postulated moderate-frequency classified transients is that the system pressure shall remain below Code pressure limits. The ASME Code, Section III, pressure limit is 110% of the RCS design pressure of 2500 psig. The safety limit thus established is 2750 psig. The criterion adopted in these accident analyses for postulated moderate-frequency classified transients to ensure that no fuel damage occurs is that a DNBR greater

than 1.25 as calculated by the BXC correlation or 1.14 as calculated by the BWC correlation must be maintained throughout the transient. The DNBR analyses for the thoria fuel mixtures used the BWC correlation. (This was the latest correlation available at the time of this report.) Most of the corresponding UO_2 cases were analyzed for DNBR using the BXC correlation. It could have been used for the thoria fuels after extensive reworking of existing computer codes and input decks. However, it was economical to use the BWC correlation for the thoria fuels. Although no direct comparison can be made between the results of the two correlations, each DNBR analysis provides specific information about the fuel integrity, which is the primary function of the analysis. Furthermore, the BWC correlation is based on more complete experimental data and provides a superior measure of fuel performance. A DNBR of more than 1.25 (BXC) or 1.14 (BWC) is required for the 112% design overpower steady-state condition. Thus, if the core thermal power during the transient does not exceed 112%, there will be no fuel damage provided the reactor coolant flow, system pressure, and core outlet temperature remain near their normal operating values or vary in a direction resulting in increased margin in DNBR. This includes neither accidents with local peaking concerns nor infrequent and limiting faulted transients which assess fuel damage and RCS boundary damage on a case-by-case basis.

If the DNBR goes below 1.25 (BXC) during a transient, the cladding temperature is evaluated to confirm the structural integrity of the fuel rod. An additional fuel damage criterion is used in the rod ejection accident because of the fast rate of energy addition. The fuel integrity is ensured if the peak enthalpy of the hottest rod is less than 280 cal/g; above which prompt fuel failure is assumed to occur for the standard UO_2 core. This fuel failure value is conservative for the alternate thoria fuels because their melting points are higher than that of the UO_2 fuel. The safety margin for any transient is the difference between the peak value of the limiting parameter and the 112% thermal power, 1.25 (BXC) or 1.14 (BWC) DNBR, 280 cal/g, or the 2750 psig limit.

4.7. Analysis of Accidents

4.7.1. Increase in Heat Removal by Secondary System — Steam Line Break

The accident selected for analysis in this category is the steam line break. This accident is highly sensitive to the moderator temperature coefficient of

the core and produces the most severe consequences of the three accidents in this category (see Table 4-1). This accident has been analyzed for the ($^{233}\text{U}/\text{Th}$) O_2 alternate fuel.

4.7.1.1. Causes and Frequency Classification

This is a non-mechanistic postulated design basis event classified as a limiting fault.

4.7.1.2. Sequence of Events and System Operation

The loss of secondary coolant due to a failure of a steam line between the steam generator and the turbine causes steam pressure to decrease and thus places a demand on the normal control system for increased feedwater flow. This action, accompanied by steam flow through the turbine stop valves and the break, lowers the average reactor coolant temperature and pressure. Core protection is provided by reactor trip on low reactor coolant pressure or high neutron flux, depending on the break size. Analyses have been performed to determine the effects and consequences of a loss of coolant as a result of a double-ended rupture (DER) of a 42-inch steam line between the main steam isolation valves (MSIVs) of a steam generator and the turbine at BOL conditions, and several split ruptures between a steam generator and its MSIV at EOL conditions for the ($^{233}\text{U}/\text{Th}$) O_2 fuel mixture. The conditions of the steam line rupture analyzed have been chosen to obtain the most severe minimum DNBR and core overcooling. These conditions are tabulated in Tables 4-5 and 4-6.

Worst DNB — BOL conditions yield the worst DNB consequences at the time of reactor trip because the power density is greater than for EOL conditions. There is maximum depressurization of the RCS at time of trip with the DER of the largest steam line. The largest steam line in a standard B&W 205-FA plant is a 42-inch-OD pipe. The combination of high power density and maximum depressurization produces the most severe DNB consequences for a steam line break. In the analysis of this case it is assumed that offsite power is lost at the time of rupture. The RC pumps begin to coast down immediately. Although gravity insertion will occur when power is lost to the control rods, it is assumed that the RC pump monitors trip the reactor. The ESFAS initiates closure of the MSIVs and the main feedwater isolation valves (MFWIVs) and actuates auxiliary feedwater flow to the unaffected steam generator on a low steam generator pressure signal. The unaffected steam generator then repressurizes, and

subsequent heat removal is provided by auxiliary feedwater flow to the unaffected generator with steam relief through the safety valves. Table 4-7 summarizes the sequence of events for this case.

Worst Overcooling — Reactor overcooling following a steam line break is maximized during EOL conditions. The rate of reactor system cooling following a steam line break accident is a function of the steam generator water inventory available for cooling. The largest inventory, which occurs at rated power in a fouled steam generator, results in the greatest mass available for cooling. The fouled steam generator inventory plus 5% is assumed in this analysis.

Table 4-8 summarizes the three steam line ruptures analyzed at EOL conditions. The most severe combination of core depressurization and cooldown is caused by an intermediate break size (6.0 ft²). The reactor trips on high flux, and a low-RC-pressure signal initiates steam generator isolation, auxiliary feedwater, and high-pressure injection. After this, the sequence of events (summarized in Table 4-9) proceeds as in the case above. The MFWIV of the affected steam generator was assumed to fail. This was the worst single failure for overcooling (i.e., causes the least subcritical margin).

4.7.1.3. Core and System Performance

Safety Evaluation Criteria — The criteria with which this study concerns itself are those pertaining to fuel integrity and the radiation release barriers. The three protective barriers are the fuel cladding, the reactor coolant system, and the containment. If any of these barriers failed, the radiological consequences for a core composed of the alternate fuels would be similar to those for a UO₂ core.

The safety evaluation of this accident is based on core thermal power, DNBR, cladding temperature, subcritical margin and return to power following reactor trip, and RCS pressure. The behavior of these parameters is compared to UO₂ core transient results and analyses in order to assess the accident consequences for the (2³³U/Th)O₂ and the other two alternate fuel cores.

Method of Analysis — The TRAP2 digital computer program was used to determine the characteristics of this accident.²⁸ The multinode model (72 nodes) included a detailed description of both the reactor coolant system and the steam generator.

The TRAP2 model provides simulation of most of the secondary system valves (with appropriate flow rates and opening and closing times) including the main and startup feedwater valves, auxiliary feedwater valves, steam dump and safety valves, MSIVs, and turbine stop valves.

The model also includes energy balances for the principal steam generator components, the entire reactor coolant system (core, loops, and steam generator), and the pressurizer (with both mass and energy transfer). The reactor kinetics, trip logic and action, and a fuel pin simulation with Doppler and moderator temperature feedback are also features of the model. The control rod worth that satisfies the criterion for a shutdown margin of 1% $\Delta k/k$ at the hot shutdown condition is used for trip.

Results of Analysis — Figure 4-11 shows the reactor system response for the 42-inch steam line rupture. The DNBR drops to a minimum of 1.18 using the BWC DNB correlation, which indicates that no fuel failure occurs.

Figures 4-12 and 4-13 show the reactor system response for the worst overcooling case. The reactor remains subcritical throughout the transient; hence, no fuel damage is expected to occur.

The most severe steam line break with respect to overcooling occurs for the break that combines an early reactor trip with a long time interval between the trip and ESFAS initiation. Both conditions depend on the size of the break. If a break is small enough, the feedwater flow increases to make up the loss of secondary fluid and the reactor will continue to operate under new equilibrium conditions. As the break size increases, the reactor trips on high neutron flux and the ESFAS initiates on low RCS pressure. As the break size increases still further, the ESFAS initiates on low steam generator pressure and the reactor trips on low RCS pressure. Reactor overcooling consequences are most severe for a break size near the transition of ESFAS initiation on low RCS pressure and low steam generator pressure. Table 4-8 indicates the trend of the transient consequences versus reactor trip and ESFAS conditions caused by the break size for the $(^{233}\text{U}/\text{Th})\text{O}_2$ core.

Conclusions on Barrier Performance — The RCS pressure for the steam line breaks is shown in Figures 4-11 and 4-13. In each case the pressure remains well below the boundary pressure limit of 2750 psig, ensuring that the pressure boundary remains intact. The 42-inch steam line break does not experience DNB and

the cladding remains intact. Figure 4-14 illustrates the DNBR response for this transient.

4.7.2. Decrease in Heat Removal by Secondary System — Feedwater Line Break

The accident selected for analysis in this category is the feedwater line break (FWLB). Of the four accidents included in this category (see Table 4-1), the FWLB poses the greatest threat to core integrity and is extremely sensitive to changes in the moderator coefficient. This accident was analyzed for both the (Th/²³³U)O₂ and (Th/Pu)O₂ alternate fuel.

4.7.2.1. Causes and Frequency Classification

This is a non-mechanistic postulated design basis event classified as a limiting fault.

4.7.2.2. Sequence of Events and System Operation

The DER of a main feedwater line between the first feedwater line upstream check valve and the steam generator was considered using a conservative effective blowdown area corresponding to the cross-sectional flow area of a 22-inch feedwater pipe. Using the maximum feasible blowdown area provides the fastest blowdown of the affected and unaffected steam generators. Maximizing the blowdown rate provides a rapid loss of heat sink, which is a conservative assumption from the standpoint of an overheating event.

The discharge flow rate from the maximum break size is based on Moody critical flow parameters for the time when the flow is critical; otherwise, the flow rate is based on normal orifice equation flow rate. No credit is taken for frictional losses in the feedwater piping between the generator and the break; this provides a conservative discharge rate.

The reactor is initially operating at 102% of rated core power, BOL conditions, and the steam generators are operating with a clean generator inventory. These conditions result in the least amount of generator inventory available for cooling. All feedwater flow from the main feedwater pumps is assumed to be forced out through the break; therefore, loss of all feedwater to the unaffected generator also occurs at the time of rupture. Table 4-10 summarizes the sequence of events for the (Th/²³³U)O₂ case, and Table 4-11 summarizes the sequence of events for the (Th/Pu)O₂ case.

4.7.2.3. Core and System Performance

Safety Evaluation Criteria — The safety evaluation criteria for this accident are as follows:

1. The core thermal power will not exceed 112% of rated power.
2. Reactor coolant system pressure will not exceed the Code pressure limits.

Methods of Analysis — B&W's TRAP2, a digital computer code, was used to determine the characteristics of this accident.²⁸ Included were a complete kinetics model, pressure model, average fuel rod model, complete multinode modeling of the secondary system and steam generators, and a simulation of the instrumentation for pressure and flux trip. The initial conditions were 102% of rated core power operation without automatic control and an initial system pressure of 2250 psia. Only the Doppler and moderator coefficients of reactivity were used as feedback. For trip, the minimum control rod worth that satisfies the criterion for a shutdown margin of 1% $\Delta k/k$ at the hot standby condition is used throughout the analysis.

Results — The transient system responses for neutron power, thermal power, moderator temperature, and system pressure are shown in Figures 4-15 and 4-16 for the $(Th/^{233}U)O_2$ and $(Th/Pu)O_2$ cases. These figures demonstrate that neither case violates the safety evaluation criteria. The addition of auxiliary feedwater to the good steam generator will permit continued heat removal with steam relief through the safety valves in both cases.

4.7.2.4. Barrier Performance

The system pressure and thermal power are plotted in Figures 4-15 and 4-16 for the $(Th/^{233}U)O_2$ and $(Th/Pu)O_2$ cases. The integrity of the RCS pressure boundary is maintained since the safety limit of 2750 psig is not exceeded. Fuel damage should not occur since core thermal power does not exceed 112% of rated power.

4.7.2.5. Discussion of Results

The system response of this accident for a UO_2 core is shown in Figure 4-17. The sequence of events for this case is identical to the thorium core cases up to and including the MSIV closure time. From this point, the UO_2 case differs from the $(Th/^{233}U)$ case in that the moderator temperature increases faster and the system pressure reaches its peak value sooner and then levels off. The

RCS pressure response variance in these two cases is the result of a combination of the pressure-relieving capability of the primary safety valves and the rate of pressure buildup within the primary system. The lower specific heat of the thorium fuel allows it to cool off more quickly while there is greater secondary cooling capacity and does not allow it to sustain heat transfer to the RCS for as long a period of time as does the UO_2 fuel. Since heat transfer to the RCS is the forcing function for the increase in RCS pressure, the system pressure for the thorium fuel tends to be more cyclic than for the UO_2 fuel when interacting with the primary safety valves. The faster release of core heat for the thorium case occurs during a time of greater secondary system cooling capacity and accounts for the less severe moderator temperature response.

The system response for the $(\text{Th/Pu})\text{O}_2$ case differs from the UO_2 case in that the power levels decrease faster and the moderator temperature and system pressure remain at significantly lower values.

The initial decrease in neutron power followed by thermal power for the $(\text{Th/Pu})\text{O}_2$ case is a result of the negative BOL moderator coefficient. The UO_2 and $(\text{Th}/^{233}\text{U})\text{O}_2$ moderator coefficients are positive at BOL. The responses of these power levels are responsible for the comparatively low moderator temperature and RCS pressure results for $(\text{Th/Pu})\text{O}_2$.

The $(\text{Th}/^{235}\text{U})\text{O}_2$ fuel may be expected to behave in much the same manner as the $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel because of the similar specific heat values. The moderator coefficient for this fuel, however, is negative and would influence the transient response in a manner similar to the influence exhibited for the $(\text{Th/Pu})\text{O}_2$ fuel. These combined effects indicate that the consequences of this transient for the $(\text{Th}/^{235}\text{U})\text{O}_2$ fuel would be less severe than those of the $(\text{Th}/^{233}\text{U})\text{O}_2$ case.

4.7.3. Decrease in Reactor Coolant System Flow Rate

Of this category of accidents (see Table 4-1), both the loss-of-coolant-flow and locked-rotor accidents are analyzed. These accidents are analyzed for the $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel only.

4.7.3.1. Loss of Coolant Flow

Causes and Frequency Classification

The reactor coolant flow rate is reduced if one or more of the RC pumps fail. A pumping failure can occur from mechanical failures or from a loss of electric power. With four independent pumps available, a mechanical failure in one pump will not affect operation of the others.

Each RC pump receives power from one of two electrically separate busses, and one pump in each loop is connected to each bus. Faults in an individual pump motor or its power supply could cause a reduction in flow, but a complete loss of forced flow (the more conservative case) is extremely unlikely and would occur only if all offsite power or both unit auxiliary transformers were lost. The loss of all offsite power would cause gravity insertion of control rods independent of reactor trip from the RPS. The four-pump coastdown case is an event of infrequent occurrence.

Sequence of Events and Systems Operation

The reactor is protected from the consequences of RC pump failures by the RPS. The reactor is tripped by the RPS if insufficient reactor coolant flow exists for the power level. Normal control action may initiate a power reduction upon pump failure to prevent reactor power from exceeding that permissible for the available flow. Even though this action may be available to prevent or mitigate this accident, it was not considered in the accident analysis. The time sequence of events is listed in Table 4-12 for the four-pump coastdown case. The reactor is initially operating at 102% rated core power, BOL conditions. The initial reactor coolant pressure and temperature are adjusted from normal by -45 psi and +2°F, respectively, to account for control band and instrument uncertainties.

Core and System Performance

Safety Evaluation Criteria — The safety evaluation criteria for this accident are as follows:

1. Pressure in the RCS remains below 110% of the design pressure.
2. The minimum DNBR shall not be less than 1.25 (BXC) or 1.14 (BWC).

Methods of Analysis — The loss-of-coolant-flow accident is analyzed using a combination of analog and digital computer programs.

The PUMP analog simulation, which is used to determine the reactor flow rate following a loss of pumping power, includes descriptions of flow-pressure drop relationships in the reactor coolant loops.²⁹ Pump flow characteristics are determined from manufacturers' zone maps. Flow-speed, flow-torque, and flow-head relationships are solved by pump laws. A pump inertia of 100,000 lb-ft² is used.

The power, inlet temperature, and system pressure are calculated by a B&W digital code, CADDs, which includes a point kinetics model with a closed-loop simulation.³⁰ The loop simulation includes a pressurizer model and a steam generator model, the whole of which is connected through time delays to the kinetics calculations. The point kinetics model used to determine the neutron power during the coastdown and following reactor trip includes six delayed neutron groups, control rod worth and rod insertion characteristics, and trip delay times.

B&W has developed the RADAR digital computer code to calculate fuel temperature, cladding temperature, and DNB ratio as a function of time for reactor system transients.³¹ Input to the code consists of flow, power, inlet temperature, and system pressure as functions of time. The core transient analysis code simulates the reactor core with a two-channel model. Each channel consists of one fuel rod with its associated flow area and spacer grid geometry. Given the necessary input as stated above, the code will calculate a pressure drop across a typical reactor channel (average channel) as a function of time. This pressure drop is then imposed on the second channel (usually a hot channel) to determine hot channel flow and DNBR in addition to fuel and cladding temperatures. Compared to the average channel, the hot channel has greater heat generation and reduced flow areas as well as statistical hot channel factors. The analytical fuel pin model contains a transient response calculation, while the hydraulic model considers the steady-state solutions of energy, mass, and momentum balances at each time step.

The transient response is obtained by applying the changing flow, power, inlet temperature, and system pressure to the initial conditions of the average channel. This calculation yields the average channel pressure drop as a function

of time, along with the hot channel power, inlet temperature, and system pressure. From these results, the response of the hot channel in terms of cladding temperature, fuel temperature, and DNB ratio is calculated as a function of time.

Results of Analysis — The results of this analysis show that the loss of all four RC pumps can be sustained from 102% rated core power (2% allowance for heat balance error) without damage to the fuel. Figure 4-18 shows the reactor coolant flow, neutron power, and system pressure as a function of time after the loss of all pump power. The DNBR as a function of time for the four pump coastdown is shown in Figure 4-19. The degree of core protection during coastdown is indicated by comparing the minimum DNBR for the coastdown with the criterion of 1.14 (BWC). As seen in the figure, the minimum DNBR (1.51) is greater than the criterion value.

The RCS is capable of providing natural circulation flow immediately after pump coastdown. Its natural circulation characteristics have been calculated with conservative values for all resistance and form loss factors. Resistance and form loss factors were determined using the system design (maximum) resistances with additional conservatism applied, including the use of locked-rotor resistances for all primary pumps. No voids are assumed to exist in the core or the reactor outlet piping. The natural circulation flow provides more than adequate heat transfer capability for cooling of the standard UO_2 core and decay heat removal by the RCS. The use of alternate thorium fuels is not expected to compromise this capability.

Barrier Performance

The loss-of-coolant-flow accident does not result in fuel damage or excessive pressure in the RCS since the minimum DNBR is not less than 1.14 (BWC) and the peak system pressure never exceeds the ASME Code allowable limits.

Discussion of Results

Figure 4-20 shows the reactor coolant flow, neutron power, and system pressure as a function of time after the loss of all pump power for the UO_2 core case. The RC flow coastdown is identical to that of the $(Th/^{233}U)O_2$ case. The thorium fuel case shows a faster rate of decrease in reactor power than the UO_2 case as a result of its more negative BOL Doppler coefficient under the condition of increased fuel temperature from the reduction of RC flow and core heat transfer.

The power level at 5.0 s is less for the thorium fuel mixture because it uses a greater shutdown worth for the reactor trip curve. Although both cases utilize a tripped rod worth based on a 1% $\Delta k/k$ hot shutdown margin, this transient occurs rapidly enough that the moderator and Doppler temperature feedback effects are not totally accounted for in its time frame. The minimum DNBR concern occurs in less than the RCS loop transient time and the fuel and moderator temperatures do not reach equilibrium values.

The BXC DNBR as a function of time for the UO_2 four-pump coastdown is shown in Figure 4-21. The minimum DNBR of 1.39 (BXC) leaves a margin of 14 DNB "points" above the BXC design limit of 1.25. The 1.51 minimum DNBR (BWC, Figure 4-19) for the $(Th/^{233}U)O_2$ four-pump coastdown is a decrease of 15 DNB points from the initial steady-state conditions and 37 DNB points above the BWC design limit. Since different CHF correlations are used for the two cases, it is not clear which fuel maintains a greater margin to DNB, but the following can be concluded:

1. No fuel damage occurs for either fuel mixture.
2. The DNB recovery for the thorium fuel mixture begins earlier than for the UO_2 fuel, which seems to indicate a larger margin to DNB.

4.7.3.2. Locked Rotor

Causes and Frequency Classification

The RC flow rate is reduced if one or more of the RC pumps fail. A pumping failure can be caused by mechanical failures or a loss of electric power. With four independent pumps available, a mechanical failure in one will not affect operation of the others. The loss of flow due to mechanical malfunction from any cause has been considered and analyzed as the locked-rotor accident. This is a non-mechanistic postulated design basis event classified as a limiting fault; therefore, only one pump is assumed to be affected.

Sequence of Events and System Operation

The reactor is protected from the consequences of an RC pump failure(s) by the RPS. The reactor is tripped if insufficient reactor coolant flow exists for the power level. Normal control action may initiate a power reduction upon pump failure to prevent reactor power from exceeding that permissible for the available flow. Even though control action may be available to prevent or mitigate this accident, the accident was analyzed without control action.

The locked-rotor accident is a rapid decrease in flow caused by the instantaneous seizure of an RC pump rotor. A pump seizure may be caused by any of the operating configurations of the four RC pumps, but the locked-rotor accident from the BOL four-pump operating condition is generally the most limiting case with respect to DNBR consequences. The time sequence of events for the locked-rotor transient from the four-pump condition is listed in Table 4-13. The reactor is initially operating at 102% rated core power, BOL conditions. The initial RC pressure and temperature are adjusted from their normal values by -45 psi and +2°F, respectively, to account for control band and instrument uncertainties.

Core and System Performances

Safety Evaluation Criteria — The safety evaluation criteria for this accident are as follows:

1. No fuel cladding failure will occur.
2. The core geometry will remain intact so there is no loss of core cooling capability.

Method of Analysis — The RC pump shaft seizure accident is analyzed using a combination of analog and digital computer programs. The PUMP analog simulation used to determine the reactor flow rate following a loss of pumping power includes descriptions of flow-pressure drop relations in the reactor coolant loops.²⁹ Pump flow characteristics are determined from manufacturers' zone maps. Flow-speed, flow-torque, and flow-head relationships are solved by pump laws.

B&W has developed the RADAR digital computer code to calculate fuel temperature, cladding temperature, and DNB ratio as a function of time for reactor system transients.³¹ The input to RADAR consists of flow, power, inlet temperature, and system pressure as functions of time as determined by the CADDs code. The core transient analysis code simulates the reactor core with a two-channel model. Each channel comprises one fuel rod with its associated flow area and spacer grid geometry. Given the necessary input as stated above, the code will calculate a pressure drop across a typical reactor channel (average channel) as a function of time. This pressure drop is then imposed on the second channel (usually a hot channel) to determine hot channel flow and DNBR in addition to fuel and cladding temperatures. Compared to the average

channel, the hot channel has greater heat generation and reduced flow areas, as well as statistical hot channel factors. The analytical fuel pin model contains a transient response calculation, while the hydraulic model considers the steady-state solutions of energy, mass, and momentum balances at each time step.

The transient response is obtained by applying the changing flow, power, inlet temperature, and system pressure to the initial conditions of the average channel. This calculation yields the average channel pressure drop as a function of time, along with the hot channel power, inlet temperature, and system pressure. From these results, the response of the hot channel in terms of cladding temperature, fuel temperature, and DNBR is calculated as a function of time.

Results of Analysis — The results of this analysis show that the reactor can sustain a reactor coolant pump shaft seizure during four-pump operation without damage to the fuel. Figure 4-22 shows the reactor coolant flow, neutron power, and system pressure after the seizure. The peak reactor coolant pressure is 2275 psia at the core outlet. Figure 4-23 shows the DNBR as a function of time after the seizure.

Although the DNB ratio drops below 1.14, Figure 4-24 shows that the fuel cladding surface temperature does not increase excessively. Only 0.9% of the fuel pins experience a DNBR less than 1.14.

Barrier Performance

The locked-rotor accident does not result in fuel cladding damage or excessive pressure in the RCS since the maximum cladding temperature is an acceptable value of 1332F and the peak pressure does not exceed the 2750 psig Code allowable limits.

Discussion of Results

Figure 4-25 shows the reactor coolant flow, neutron power, and system pressure for the UO_2 locked-rotor transient from the same operating conditions as the $(\text{Th}/^{233}\text{U})\text{O}_2$ case. The results of the cases for the two fuels are indiscernable up to 1.2 s when the power drops off for the $(\text{Th}/^{233}\text{U})\text{O}_2$ case while it continues to rise for the UO_2 case. This result is similar to that obtained for the four-pump coastdown accident. The more negative $(\text{Th}/^{233}\text{U})\text{O}_2$ BOL Doppler

coefficient maintains a lower power level as the fuel temperature rises as a result of the reduced RC flow and core heat transfer. Also similar to the four-pump coastdown case is the lower power level at 5.0 s for the thorium mixture. This is also explained by the time frame of the transient and the inability of the core to see the total effects of moderator and Doppler temperature feedback. The fuel and moderator temperatures do not reach equilibrium values in this time frame because the system loop transient time is longer than 5.0 s. This condition allows the thorium core to see a greater tripped rod worth because the magnitude of the shutdown worth is greater for the thorium case as a result of its more negative moderator and Doppler temperature coefficients which are used to obtain the minimum tripped rod worth for a 1% $\Delta k/k$ minimum hot shutdown margin. This effect results from the conservative assumptions used to derive the shutdown worth and may not occur in the real situation.

Figure 4-26 shows the DNBR and maximum clad and fuel temperatures as a function of time for the UO_2 locked-rotor case from four-pump operation. Using the BXC correlation, this case results in 3.5% of the fuel pins experiencing DNB. Based on these results, the $(Th/^{233}U)O_2$ fuel produces less severe consequences for this type of transient than the UO_2 fuel. The difference in DNBR results for these two fuels is attributed to the greater specific heat of UO_2 for fuel temperatures greater than 3000F. The similarity of specific heats for the three thorium fuels addressed in this study indicate that the consequences of the locked rotor transient for the $(Th/^{235}U)O_2$ and $(Th/Pu)O_2$ fuels would be similar to those for the $(Th/^{233}U)O_2$ fuel.

4.7.4. Reactivity and Power Distribution Anomalies

The accidents selected for analysis in this category are the startup accident, uncontrolled control rod group withdrawal at power, control rod assembly (CRA) misoperation, and rod ejection accident. All of these accidents were analyzed for the $(Th/^{233}U)O_2$ core for BOL and EOL conditions except the startup accident; it was analyzed for BOL conditions only because it is mitigated by Doppler feedback and an EOL case would be less severe than the BOL case. Analysis of these accidents provides a comprehensive study of the kinetics effects of $(Th/^{233}U)O_2$ on reactivity-forced transients. The remaining two accidents in this category, startup of inactive RC pumps and chemical addition system malfunctions (see Table 4-1) are not included in the study because their

analysis would not contribute significant additional information about the core and system response to the alternate fuels.

The fastest reactivity transients in this category are the uncontrolled control rod group withdrawal at power, CRA misoperation, and rod ejection accidents. For this reason, these transients are most sensitive to changes in core kinetics and provide the best means to evaluate the (Th/Pu)O₂ core, which varies substantially with respect to fuel kinetics (e.g., prompt neutron lifetime) from the (Th/U)O₂ fuels. The uncontrolled control rod group withdrawal at power and rod ejection accidents were analyzed for the (Th/Pu)O₂ core at BOL and EOL conditions, while the CRA misoperation accident was analyzed only at BOL conditions, which is sufficient to make a judgment of its consequences for this fuel within the scope of this report.

4.7.4.1. Startup Accident

Causes and Frequency Classification

The objective of a normal startup is to bring a subcritical reactor to the critical or slightly supercritical condition, and then to increase power in a controlled manner until the desired power level and system operating temperature are obtained. During a startup, an uncontrolled reactivity addition could cause a power excursion. This excursion is terminated by the negative Doppler effect if no other protective action operates. No power excursion will result from such an uncontrolled reactivity addition during refueling (control rod removal) because the boron concentration in the core during reactor vessel head removal and while loading and unloading fuel is maintained at not less than the boron concentration required to shut down the reactor to a 1% $\Delta k/k$ subcritical condition with all control rods removed. Transients occurring before reactor vessel head removal are adequately considered under the following analysis of the startup accident. Uncontrolled control rod group withdrawal from a subcritical condition is considered to be an incident of moderate frequency that results in no radioactive release beyond that of normal operating levels.

Sequence of Events and System Operation

The startup accident transients are terminated by either flux-core ΔT or high RC pressure trips depending on the associated reactivity insertion rate. A delay time is accounted for in the flux-core ΔT and pressure trip, and following reactor trip, insertion of control rods begins after the appropriate delay

time. The RC pressure reaches a peak value within seconds after the trip is initiated. If the pressurizer pressure increases enough, the safety valves are actuated and they open and then close according to their respective set-points. No operator action is required to terminate this accident. The following design provisions minimize the possibility of inadvertent continuous rod withdrawal and limit the potential power excursions:

1. The CRD control system is designed so that only one control rod group can be withdrawn at a time, except that there is a 25% overlap in travel between two regulating rod groups successively withdrawn. This overlap occurs near the minimum worth positions for each group since one group is near the end of travel and the other is near the beginning of travel. The minimum calculated worth of any single control rod regulating group is specified in Table 4-14.
2. Control rod withdrawal rate is limited.
3. A high startup rate withdrawal inhibit and alarm are provided in the source range.
4. A high startup rate withdrawal inhibit and alarm are provided in the intermediate range.

Even though these design provisions are available, they are not used in the analysis. Only the flux-core ΔT and high RC pressure trip of the RPS were used in the analysis.

Core and System Performance

Safety Evaluation Criteria — The safety evaluation criteria for this accident are as follows:

1. The minimum DNBR will not be less than 1.25 (BXC) or 1.14 (BWC).
2. The reactor coolant system pressure will not exceed the Code pressure limits.

Method of Analysis — A B&W digital point kinetics model of the reactor core and coolant system, CADDs, was used to determine the characteristics of this accident.³⁰ This model used full reactor coolant flow but no heat transfer out of the system to maximize system conditions. The use of sprays in the pressurizer was also not considered since the effect would not be significant during the time frame of the transient. The rods were assumed to be moving

outward along the steepest part of the curve of rod worth versus rod travel. The values of the principal parameters used in this analysis are listed in Table 4-14. The criterion for minimum movable control rod worth is that a shutdown margin of 1% $\Delta k/k$ at the hot shutdown conditions is required. The startup accident has been analyzed using the minimum tripped rod worth required for the 1% subcritical hot shutdown condition. This minimum tripped rod worth provides for the maximum-worth stuck rod. The startup accident was analyzed from 1% $\Delta k/k$ subcritical at the hot, pressurized condition.

Results of Analysis — Figure 4-27 shows the results of withdrawing the minimum-worth control rod group at the maximum rod speed from 1% subcritical. The Doppler effect terminates the neutron power rise, but the heat input to the reactor coolant increases the system pressure past the trip point, and the transient is terminated by the high-pressure trip.

Figure 4-28 shows the results of withdrawing all control rod assemblies at the maximum speed from 1% subcritical. This results in a maximum possible reactivity addition rate. The power rise is terminated by the negative Doppler effect and the flux-core ΔT trip terminates the transient. The peak thermal heat flux is significantly less than the peak thermal heat flux at 102% of rated power.

Figures 4-29 and 4-30 show the effect of varying the reactivity addition rate on the peak neutron power and peak thermal power. The reactivity insertion rate was varied from half an order of magnitude below the single rod group rate used for the analysis to the rate for simultaneous withdrawal of all rods. The slower rates result in the pressure trip being actuated, whereas the faster rates actuate the flux-core ΔT trip. A DNBR analysis was not performed for this transient because of the well behaved reactor system response. This accident does not produce a transient that threatens to violate the DNB criterion. It is bounded by the rod withdrawals at full-power operating conditions.

Barrier Performance

Table 4-14 summarizes the results of the postulated startup accident. It is concluded that the reactor is completely protected against any startup accident involving the withdrawal of any or all control rods since the thermal power never approaches the design overpower condition, the peak pressure never exceeds the Code allowable limits, and the minimum DNBR criterion of 1.25 (BXC) or 1.14 (BWC) is not violated.

Discussion of Results

Figure 4-31 compares the results of the minimum control rod withdrawal for the $(\text{Th}/^{233}\text{U})\text{O}_2$ and UO_2 cases.

The more negative Doppler coefficient of the thorium fuel results in a quickly terminated rise in power for this transient, which reduces the thermal output of the core and results in a smaller rate of increase of the fuel and moderator temperature and system pressure than in the UO_2 case. The reduced rate of increase of the system pressure in the thorium case delays the high-pressure reactor trip and final termination of the transient.

Figure 4-32 compares the results of the all-rod-groups withdrawal for the $(\text{Th}/^{233}\text{U})\text{O}_2$ and UO_2 cases. Both cases trip on the $\phi/\Delta T$ function because of the early power spike. There is less integrated power for the thorium case, however, because of the more negative Doppler coefficient, and the reactor system response compares to the UO_2 case in a manner similar to that of the normal control rod withdrawal cases.

The comparisons of neutron and thermal power versus reactivity insertion rates in Figures 4-29 and 4-30 for $(\text{Th}/^{233}\text{U})\text{O}_2$ and UO_2 cores clearly illustrate the effects of the fuel Doppler coefficient. The peak neutron power for the sensitivity study is equivalent for the all-rod-group withdrawal case for both fuels, but for smaller reactivity insertion rates the more negative Doppler coefficient of the thorium core plays a role of increasing importance and suppresses the peak neutron power. The peak thermal power of this sensitivity study is 22% for the thorium fuel versus a peak of 44% for the UO_2 study. Therefore, the consequences of the startup accident are less severe for a $(\text{Th}/^{233}\text{U})\text{O}_2$ core than for a UO_2 core.

The startup accident transient response for a $(\text{Th}/^{235}\text{U})\text{O}_2$ core would be equivalent to that for the $(\text{Th}/^{233}\text{U})\text{O}_2$ core because of their similar Doppler coefficient values. The Doppler coefficient of $(\text{Th}/\text{Pu})\text{O}_2$ is not quite as negative as those for the thorium-uranium fuels, but it is more negative than that of UO_2 . Thus, the startup accident consequence for the $(\text{Th}/\text{Pu})\text{O}_2$ core is expected to be acceptable and even less severe than the consequences of the transient for the UO_2 core.

4.7.4.2. Uncontrolled Control Rod Group Withdrawal at Power

Causes and Frequency Classification

A rod bank withdrawal accident presupposes operator error or equipment failure resulting in accidental withdrawal of a control rod group while the reactor is at power. As a result, the power level increases, the reactor coolant and fuel rod temperatures increase, and if the withdrawal were not terminated by the operator or the RPS, core damage would eventually occur. Uncontrolled control rod assembly withdrawal at power is considered to be an incident of moderate frequency that results in no radioactive releases above those of normal operating levels.

Sequence of Events and System Operation

The rod bank withdrawal accident transients are terminated by either high flux or high RC pressure trips depending on the reactivity insertion rate. A delay time following reactor trip is accounted for in the flux and pressure trip, and control rod insertion begins after the appropriate delay time. The reactor coolant pressure reaches a peak value usually within seconds after the trip is initiated. If the pressurizer pressure increases sufficiently, the safety valves are actuated, and they open and then close according to their respective setpoints. No operator action is required to terminate this accident.

The following provisions are made in the design to indicate this accident:

1. High reactor coolant outlet temperature alarms.
2. High reactor coolant system pressure alarms.
3. High pressurizer level alarms.

Even though these design provisions are available, they were not used in the analysis. Only the high RCS pressure and neutron power level trips of the RPS were used in the analysis.

Core and System Performance

Safety Evaluation Criteria — The safety evaluation criteria for this accident are as follows:

1. The minimum DNBR will not be less than 1.25 (BXC) or 1.14 (BWC).
2. The RCS pressure will not exceed the Code pressure limits.

Method of Analysis — B&W's CADDs digital computer code was used to determine the characteristics of this accident.³⁰ Included were a point kinetics model, pressure model, average fuel rod model, steam demand model with secondary coastdown to decay heat level, coolant transport model, and a simulation of the instrumentation for pressure and flux trip. The initial conditions were 102% of rated power operation without automatic control. Only the Doppler and moderator coefficients of reactivity were used as feedback. The values used for the main parameters in evaluating this accident are given in Table 4-15. For trip, the minimum control rod worth that satisfies the criterion for a shutdown margin of 1% $\Delta k/k$ at the hot standby condition, assuming that the maximum worth rod remains out of the core, is used throughout the analysis. In addition, corrections of +2°F and -45 psi were included to account for control band and instrument uncertainties in all cases. This set of initial conditions provides the limiting DNBR analysis.

Results of Analysis — $(Th/^{233}U)O_2$: Figures 4-33 and 4-34 show the results of the nominal single rod group and all-rod group withdrawal from 102% of rated core power BOL conditions. These rod group withdrawals are assumed to be the minimum single group worth and the maximum worth regulating rod groups for BOL, 102% of rated power operation. The transients are terminated by a high neutron flux level trip.

A sensitivity analysis of reactivity addition rate was performed for both BOL and EOL conditions. The salient results are summarized in Table 4-16. In no case of this study did the thermal power exceed 112% overpower. Figure 4-35 shows the pressure variation for the two time-in-life conditions for a very wide range of rod withdrawal rates, about an order of magnitude smaller and greater than the nominal single-rod-group case for BOL conditions. The reactor trip function is dependent on both the time-in-life of the core and the reactivity insertion rates. For each time-in-life case, the neutron flux trip is the primary core protective device for the more rapid reactivity insertion rates, and it also protects the RCS against high pressure. The high-pressure trip is relied upon for the slower transients for both BOL and EOL conditions. Consistent with past UO_2 analyses, BOL conditions produce the worst consequences for this accident. As seen in Figure 4-35, in no case of this study does the system pressure exceed the Code pressure limit.

(Th/Pu)O₂: Figures 4-36 and 4-37 show the results of the nominal single rod group and all rod group withdrawal from 102% of rated core power, BOL conditions. These rod group withdrawals have the same reactivity worths as those in the (Th/²³³U)O₂ analysis. These two transients are also terminated by a high neutron flux level trip.

A sensitivity analysis of reactivity addition rate was performed for both BOL and EOL conditions. The salient results are summarized in Table 4-17. In no case of this study did the thermal power exceed 112% overpower. Figure 4-38 shows the pressure variation for the two time-in-life conditions for a wide range of rod withdrawal rates. As seen in this figure, in no case did the system pressure exceed the Code pressure limit. The transients of this study exhibit the same relationship of trip function to reactivity addition rate as the (Th/²³³U)O₂ and UO₂ studies.

Barrier Performance

Figures 4-39 and 4-40 illustrate the DNBR response for the (Th/²³³U)O₂ nominal single rod group and all-rod group withdrawal transients. The nominal rod withdrawal case reaches a minimum DNBR of 1.53 (BWC) at 13.0 seconds. The all-rods withdrawal case reaches a minimum DNBR of 1.57 (BWC) at 1.0 second. Neither case violates the minimum DNBR safety criterion of 1.14 (BWC). Based on these results and the similarity of the specific heats of (Th/²³⁵U)O₂ and (Th/Pu)O₂ to that of (Th/²³³U)O₂, none of the thorium fuel mixtures should present a DNBR safety concern for this type of transient.

In no case of this analysis did the thermal power approach the 112% overpower condition. Furthermore, the DNBR remained above the safety limit criterion, and the peak pressure never exceeded the Code allowable limits.

Discussion of Results

Figures 4-33, 4-34, 4-36, and 4-37 provide a comparison of the magnitude and time of occurrence of peak parameter values for the UO₂ core case, which assumes the same reactivity addition rate as the case presented for the thorium mixed oxide core. These comparisons show that the relative timing and magnitude of the results for the alternate fuels versus UO₂ are not consistent for a given fuel; their dependence on the reactivity addition rate varies with the characteristics of each fuel. This timing and magnitude shift is also illustrated

by Figures 4-35 and 4-38. The time and magnitude of system response parameters in this type of transient are primarily functions of time of trip. For large reactivity additions, the thorium fuels experience a faster trip on high neutron flux level than the UO_2 fuel. This is caused by the smaller delayed neutron fractions of the thorium fuels (see Figure 4-3), which cause a faster core response to reactivity additions. As the reactivity addition rate is reduced for these transients, the trip time is delayed and the importance of the Doppler coefficients is increased. The Doppler coefficients of the thorium fuels are more negative than that of the UO_2 fuel (see Figure 4-2) and depress the rise in neutron flux longer for a given reactivity addition than does the feedback of the UO_2 fuel.

Figure 4-35 shows that the BOL conditions produce the worst consequences for this accident for a $(\text{Th}/^{233}\text{U})\text{O}_2$ core. This is the same trend observed for UO_2 cases. The maximum peak pressure experienced for this thorium fuel mixture is greater than that indicated for the UO_2 core but is still considerably within acceptable safety limits. The larger peak pressure for this fuel is a result of its more negative Doppler coefficient, which delays the transition of the trip function from high RCS pressure to high neutron flux as the reactivity addition rate is increased. It is this transition that terminates the rise in peak system pressures as a function of reactivity addition rate.

Figure 4-38 shows that BOL and EOL conditions produce equivalent peak pressure response but for different reactivity addition rates. The negative BOL moderator coefficient for $(\text{Th}/\text{Pu})\text{O}_2$ suppresses the rate of pressure increase prior to trip and causes less pressure increase following a high RCS pressure trip. As in the previous analyses, the maximum peak pressure for EOL occurs for a greater reactivity addition rate than for BOL. This reflects the more negative reactivity coefficients characteristics of EOL conditions.

This analysis indicates that the overall results of rod bank withdrawal accidents for a thorium fuel mixture are acceptable from a safety evaluation standpoint. The consequences of this type of accident are dependent on the interrelationships of several reactor system characteristics. These interrelationships do not allow a simple trend to develop in changing from a UO_2 core to a thorium fuel mixture core, but all of the safety evaluation criteria are satisfied.

4.7.4.3. Control Rod Assembly (CRA) Misoperation

Single CRA Drop

This accident differs from the other reactivity transients in that it entails the addition of negative reactivity into the core. The analysis of this type of accident is necessary to completely evaluate the changes in core kinetics for the alternate fuels.

Causes and Frequency Classification – A control rod assembly (CRA) is defined as dropped when it is inserted more than an indicated 9 inches below its group reference position. This definition then covers both the actions of dropping a CRA and leaving a CRA behind while moving a group out. Actually, the action of leaving a CRA behind is potentially less limiting than dropping a CRA, since 278 seconds are needed to pull out a bank from full insertion in the "run" mode, and as soon as the CRA deviation from its group reference position exceeds 9 inches, the operator will receive 5 visual alarms and two audible alarms. The control rod assembly drop accident is considered to be an accident of moderate frequency.

Sequence of Events and Systems Operation – The drop of a single CRA in the core during reactor operation will cause an excursion in RCS parameters and an increase in hot channel peaking factor. Such conditions must be examined to ensure that fuel design limits are not exceeded.

The negative reactivity insertion due to a single CRA drop causes neutron flux, thermal power, inlet temperature, and system pressure to decrease. At BOL conditions, Doppler and moderator coefficients are not sufficiently negative to provide enough feedback to stop the system excursion, and the reactor trips on low RCS pressure. The reactor trip is followed by a rapid recovery in DNB. Later in the cycle, the increasingly negative Doppler and moderator temperature coefficients cause system pressure to decrease more slowly, delaying the time of trip. Somewhere in midlife there exists a combination of Doppler and moderator temperature coefficients that will stop the pressure decrease just above the low-pressure setpoint, so that a system trip will not occur. From this point in life to the end of life, a low-pressure trip will not occur, but the pressure, temperature, and power excursions resulting from the CRA drop will become increasingly smaller, due to the increasingly strong feedback effects with lifetime. Assuming that the worst CRA drop results in the same

reactivity insertion and the same peaking factor throughout the cycle, minimum DNBR will change with life due to the different system response throughout the cycle, unless it occurs in all cases at the initiation of the transient. In this case, RCS response will not affect minimum DNBR, which will be constant as a function of core life.

No operator action is required to terminate this accident. Normal control action is available in the case of a dropped rod to inhibit all rod out-motion and run back the steam generator load demand to 60% of rated load. Even though normal control action is available to prevent or mitigate this accident, it was not used in the accident analysis.

Since this analysis shows that the maximum reactivity addition due to a single CRA drop does not exceed $0.13\% \Delta k/k$, adequate hot shutdown margin is ensured by requiring subcriticality of $1\% \Delta k/k$ with the control rod of greatest worth fully withdrawn from the core. In all cases, the available rod worth is sufficient to provide a safe shutdown in the event of a reactor trip.

Core and System Performances —

Safety Evaluation Criteria: The safety evaluation criteria for this accident are as follows:

1. Minimum DNB ratio shall not be less than 1.25 (BXC) or 1.14 (BWC).
2. The maximum centerline fuel temperature shall not exceed the fuel melt limit.

Method of Analysis: The single-CRA drop accident analyses have been performed in three steps, involving nuclear calculations to determine peaking factors and rod worths, system calculations to determine core average response, and thermal-hydraulic calculations to determine transient hot channel DNB ratio. The first step of determining peaking factors and rod worths was performed for a UO_2 core. The conservative applicability of these values to the thorium cores has been confirmed by physics analyses performed within the scope of this study.

1. Nuclear Analyses — The nuclear calculations were performed with a three-dimensional nodal code with thermal-hydraulic feedback. Cycle 1 was depleted from 0 to 460 EFPD in order to analyze single CRA drop as a function of burnup. Only CRA drop from 102% of rated power was considered since this condition yields the most limiting results. The nuclear analysis identified the largest CRA worth that can be dropped at any time in the cycle and the highest increase in radial peaking factor resulting from a rod drop at any time in the cycle. These values are given in Table 4-18.

The superposition of this CRA worth and power peaking increase is conservative since the actual drop of the CRA of highest worth yielded a lower peaking factor.

2. System Analyses — CADDs has been used to analyze the transient response to a CRA drop. The CRA reactivity worth was input in CADDs as a function of time along the CRA insertion curve of Figure 4-8 (1.7 seconds to 2/3 insertion).

Since the nuclear analysis has identified a combination of dropped rod worths and resultant power peaking values covering the whole cycle, the minimum DNBR in the cycle is a function of only the pressure, temperature, and power excursions. The drop of the CRA at BOL causes a rapid trip on low pressure when core power is low. The trip terminates the DNBR decrease. At EOL, the decrease in neutron power caused by the dropped CRA is counteracted by the negative Doppler and moderator coefficients in such a manner that a reactor trip will not occur. Neutron power returns toward its initial value before pressure and inlet temperature have decreased much below their initial values.

Therefore, the worst-case CRA drop is the one that will cause RCS pressure to decrease the closest to the low-pressure trip setpoint without causing a trip. Neutron power will then rebound toward its initial value under the action of the feedback effects, while system pressure will remain at its minimum value reached in the transient. The low pressure and inlet temperature caused by this transient will in turn lower the RCS average temperature. The ΔT across the core must increase, and mass flow rate must increase due to the density changes, which in turn will drive core power back to its initial value of 102% of rated power.

The system transient resulting from a CRA drop is most sensitive to moderator temperature coefficient variations and least sensitive to other point kinetics model parameters that change with burnup, including the Doppler coefficient. Therefore, in order to identify the worst case in life for $(\text{Th}/^{233}\text{U})\text{O}_2$, the CRA drop is analyzed several times with BOL conditions but with increasingly negative moderator temperature coefficients, including the EOL value. The EOL moderator temperature coefficient case was also run with the EOL CADDs model.³⁰ A case was identified which, although it did cause a low RCS pressure trip, occurred very late in the transient and was considered to be very close to the case that would cause the reactor pressure to decrease asymptotically to the trip setpoint without causing a trip. The BOL CADDs model for $(\text{Th}/\text{Pu})\text{O}_2$ is then run for standard BOL conditions to determine how this fuel behaves. For all cases analyzed, the system conditions are assumed to be nominal plus uncertainties, or within the control band. Initial inlet temperature is nominal plus 2°F, initial system pressure is nominal minus 45 psi uncertainty, and initial power is 102% of rated.

3. Thermal-Hydraulic Analyses — The hot channel thermal-hydraulic analysis was performed with RADAR to study transient DNBR.³¹ The system parameters calculated with CADDs, together with the percent increase in radial peak from Table 4-18, were input in RADAR to determine the hot channel DNBR (BWC) as a function of time. The increase in peak due to the drop is

applied in RADAR at time zero into the transient. The initial peaking factors for all CRA drop cases were assumed to be the UO_2 reference design values, 1.67 axial and 1.48 radial, which bound the three thorium fuels. Then, the maximum increase in peaking factor calculated for any CRA drop analyzed was conservatively superimposed on this already conservative initial condition.

Results of Analysis — The results of the analyses are presented in Figures 4-41 through 4-48. The CADDs CRA drop analysis was performed repeatedly for the $(Th/^{233}U)_2O_2$ core with a BOL model, but using several values of moderator coefficients between HFP BOL and EOL values.³⁰ For $\alpha_m = -0.50 \times 10^{-4} \Delta k/k/^\circ F$, system pressure decreases at a rate that prolongs the low RCS pressure trip long enough to identify this case as presenting the worst transient conditions.

The analysis with $\alpha_m = -2.81 \times 10^{-4} \Delta k/k/^\circ F$ was repeated with an EOL CADDs model to demonstrate that the system responses traced as a function of time are only slightly affected by parameters other than moderator temperature coefficient. This effect is illustrated in Figures 4-41 through 4-43. Figure 4-44 includes the pressure response of a system using the $(Th/Pu)_2O_2$ core. This figure shows the strong dependence of this transient's response on moderator coefficient regardless of the fuel mixture contained in the core. Figures 4-45 and 4-46 provide further information on the reactor system response to the $(Th/Pu)_2O_2$ CRA drop transient.

Figures 4-47 and 4-48 show the minimum DNBR transient response for the $(Th/^{233}U)_2O_2$ core HFP, BOL and EOL cases, respectively. Both the BOL and EOL cases experienced their minimum DNBR of 1.66 (BWC) at the beginning of the transient. Neither case experienced fuel failure. The EOL case used the $-2.81 \times 10^{-4} \Delta k/k/^\circ F$ EOL moderator coefficient. The BOL case depicted in Figure 4-47 uses a moderator coefficient of $-0.50 \times 10^{-4} \Delta k/k/^\circ F$, which is not the typical BOL value but was chosen to maximize the centerline fuel temperature. In each case, the centerline fuel temperature was 4685F at time zero, decreased during the transient, and then increased until trip occurred. The final fuel centerline temperature was 4668F for the BOL case and 4682F for the EOL case. At no time did the centerline fuel temperature exceed the melting point.

Barrier Performance — No fuel heat generation limits were exceeded, no DNB occurred, and no operator action was required to terminate the transient. Therefore, the safety criteria for this postulated accident are met for the $(Th/^{233}U)_2O_2$ fuel.

Discussion of Results — The behavior of this transient is dominated by the moderator temperature reactivity feedback as shown in Figure 4-44. The system pressure response shows a dependence on the moderator coefficient of the core regardless of fuel type. The single cases for the UO_2 core and $(Th/Pu)O_2$ core fit neatly within the trend of results produced by the moderator coefficient sensitivity study for the $(Th/^{233}U)O_2$ core. Based on these results and the relative ranges of the three thorium fuels, all of these fuels would produce acceptable consequences in this accident situation.

4.7.4.4. Rod Ejection Accident

Causes and Frequency Classification

Reactivity excursions initiated by uncontrolled rod withdrawal (section 4.7.4.2) were shown to be safely terminated without harming the reactor core or the integrity of the reactor coolant system. In order for reactivity to be added to the core more rapidly, physical failure of a pressure barrier component in the control rod drive assembly must occur. Such a failure could cause a pressure differential to act on a control rod assembly and rapidly eject it from the core region.

The rod ejection accident is considered to be a limiting fault with the potential for releasing significant amounts of radioactive material.

Sequence of Events and Systems Operation

The power excursion due to the rapid increase in reactivity from the rod ejection accident is limited by the Doppler effect and terminated by RPS trips. These trips may be either high flux, flux-core ΔT , or high RC pressure depending on the reactor power level and the reactivity insertion rate. A delay time is accounted for in the flux, flux-core ΔT , and pressure trips. Following a reactor trip and the appropriate delay time, insertion of the control rods begins and the fuel temperature reaches its peak value, usually within a few seconds after trip. No operator action is required to terminate this accident.

Since control rod assemblies are used to control load variations and deboration is used to compensate for fuel depletion, only a few control rod assemblies are partially inserted at rated power level. Thus, the severity of a rod ejection accident is inherently limited because the amount of reactivity available in the form of control rod worth is relatively small.

Core and Systems Performance

Safety Evaluation Criteria — The safety evaluation criteria for this assumed accident are as follows:

1. A control rod ejection accident will not cause further violation of the RCS integrity.
2. The maximum fuel rod enthalpy will not exceed 280 cal/g.

Accident Bases — Using analytical methods based on diffusion theory, the worth of the most reactive control rod assembly in each rod group was determined for various control rod axial configurations. The tripped rod worth corresponds to the minimum worth available with the rod of maximum worth stuck out at BOL and EOL. The severity of the rod ejection accident depends on the worth of the ejected rod and the reactor power level. The control rod group of greatest worth is the first in the entire rod pattern to be withdrawn. When the reactor is subcritical, the boron concentration is maintained at a level that ensures that the reactor is at least 1% subcritical with the control rod of greatest worth fully withdrawn from the core. Thus, a rod ejection will not cause a nuclear excursion when the reactor is subcritical and all the other rods are in the core. The rod position limits ensure that inserted rod groups do not contain single rod worths greater than 0.65% $\Delta k/k$ at rated power and 1.0% $\Delta k/k$ at hot zero power.

A maximum rod worth of 0.65% $\Delta k/k$ at 102% of rated power has been considered a limiting value to demonstrate the inherent ability of the system to safely terminate this postulated transient. A single 1.0% ejected rod worth at BOL, hot zero power would result in a lower transient peak thermal power and thus less severe environmental consequences than 0.65% $\Delta k/k$ ejected rod worth at rated power. A detailed analysis has been performed at 102% of rated power and 10^{-3} of assumed full power for a range of rod worths.

A rod must be fully inserted in the core to have its greatest reactivity worth. Assuming that the failure occurs so that the pressure barrier no longer offers restriction to the ejection and that there is no viscous drag force limiting the rate of ejection, the control rod travel time to the top of the active region of the core is calculated to be 0.13 second. Since most of the reactivity is added during the middle 75% of this travel, only this distance is used in the analysis, resulting in an ejection time of 0.10 second for the analysis.

Fuel Rod Damage — A fuel failure threshold of 280 cal/g (at the pellet radius corresponding to the average temperature of the hottest fuel pellet) has been used in UO_2 studies to define the extent of fuel failure. Although there are not enough experimental data to arrive at a comparable threshold value for the thorium fuel mixtures, the 280 cal/g value is conservative for these fuels based on a comparison of the melting point temperatures of the $(\text{Th/U})\text{O}_2$ and $(\text{Th/Pu})\text{O}_2$ mixtures with that of UO_2 considering the densities of the mixtures.

In computing the average enthalpy of the hottest fuel pellet during the excursion for the cases at 102% rated power, it is assumed that no heat is transferred from the fuel rod between the time the accident is initiated and the time the neutron power returns to the 102% of rated power level. For the cases at 10^{-3} of assumed full power, the enthalpy increase was based on the peak value of the average fuel temperature. In all cases the average enthalpy rise from the integrated energy or the fuel temperature traces was multiplied by the maximum peaking factor to obtain the enthalpy increase in the hottest fuel pellet.

The latest correlation of the ANL TREAT data for the meltdown experiments on Zircaloy-2-clad uranium dioxide fuel rods show the zirconium-water reaction to become significant for 210 to 220 cal/g energy input.³² In calculating the percentage of fuel rods experiencing burnout in a given rod ejection accident, it was assumed that any DNB condition results in burnout for each rod where the DNB occurs. DNB was assumed to occur in a rod ejection transient whenever the peak thermal power of a given fuel rod exceeded the peak at steady-state conditions that would result in a DNB, which in turn was assumed to occur for a DNBK of 1.25 using the BXC correlation or 1.14 using the BWC correlation. B&W's CADDs digital computer program has been used to analyze the rod ejection accident.³⁰

Results of Analysis — $(\text{Th}/^{233}\text{U})\text{O}_2$: Table 4-19 summarizes the rod ejection analysis for the $(\text{Th}/^{233}\text{U})\text{O}_2$ core. Analyses were performed for BOL and EOL time-in-life conditions and for 102% rated core power and 10^{-3} of rated core power (i.e., low power).

Figure 4-49 shows the peak neutron power as a function of ejected rod worth. The low power curves show two distinct parts corresponding to worths less than and values near to and greater than β . Figure 4-50 shows the corresponding results for the peak thermal power. For the low-power cases and for rod worth

values near prompt critical, the period was short enough to carry the transient through the flux-core ΔT trip. For lower rod worths, the high RC pressure trip was relied on.

As shown in Figure 4-50, the peak thermal power for the high-power cases showed relatively little change with increased rod worth. The peak neutron power curves for the high power cases in Figure 4-49 do show a marked change with increased rod worths, but the thermal effect was small because the transients were rapidly terminated by the Doppler reactivity feedback effect.

(Th/Pu)O₂: A summary of the rod ejection analysis for the (Th/Pu)O₂ core is given in Table 4-20. Analyses were performed for BOL and EOL conditions and high and low power levels.

Figure 4-51 shows the peak neutron power as a function of rod worth, and Figure 4-52 shows the corresponding results for the peak thermal power. These figures demonstrate the same behavior as low-power cases with respect to rod worth values near β as the (Th/²³³U)O₂ fuel. The results for the high-power-level cases also exhibit trends similar to those of the (Th/²³³U)O₂ fuel.

Barrier Performance

The hypothetical rod ejection accident has been investigated in detail at two initial reactor power levels: 3876 MWt and 10⁻³ of 3876 MWt. Both BOL and EOL conditions were considered at each power level for both (Th/²³³U)O₂ and (Th/Pu)O₂ cores. Calculations were performed for the peak enthalpy of the hottest fuel rod in the (Th/²³³U)O₂ core.

The most severe transient with respect to these calculations was the ejection of a 0.65% $\Delta k/k$ control rod from HFP, BOL conditions. This case resulted in an enthalpy of 173.2 cal/g for the hottest fuel rod in the core, which is well below the fuel failure criterion of 280 cal/g. The results of this analysis prove that the reactivity transient resulting from this accident will be limited by the Doppler effect and terminated by the RPS with no serious core damage or additional loss of RCS integrity.

Discussion of Results — Figures 4-53 and 4-54 show the trends of peak neutron power and peak thermal power, respectively, versus control rod worth for a UO₂ core. These curves demonstrate the same trends as seen in the corresponding figures for the thorium fuels. The most striking difference is the control

rod worth value, for which a transition occurs between the trip functions. This difference in transition worths reflects the differences in the β of each core. Cases of rod ejections with rod worths equal to or greater than β exhibit prompt critical behavior, whereas cases with rod ejection worths less than β do not experience neutron flux spikes and rely on the high-pressure trips for termination. The maximum values of neutron and thermal power for the three cores illustrated are of similar magnitudes. Although equivalent results for each fuel analyzed are biased toward different ejected control rod worths, the overall consequences are similar and all the fuels analyzed produce acceptable results for this postulated accident.

4.7.5. Anticipated Transients Without Scram

Although the ATWS events are outside the normal Chapter 15 FSAR scope, one accident was analyzed to assess differences in reactor system response for a thorium core versus a UO_2 core. Analytical evaluation of ATWS events was performed only for those transients for which the probability of occurrence is greater than 10^{-7} per reactor year per plant. The analysis of ATWS events for B&W reactors is presented in reference 33. The ATWS analysis reports that the peak system pressure calculated for the postulated case of no control rod motion with the complete loss of normal feedwater exceeds the emergency pressure limit. This accident was chosen for analysis because of its severe consequences. The $(Th/^{233}U)O_2$ core was chosen because of its greater relative importance in the scope of this study.

4.7.5.1. Complete Loss of Normal Feedwater

Causes and Frequency Classification

The loss-of-feedwater accident was assumed to occur as a result of the complete loss of normal feedwater. This accident results from the loss of main feedwater pumps or the inadvertent closure of all feedwater control or isolation valves. This accident is considered to be a moderate-frequency event.

Sequence of Events and System Operation

The reactor is assumed to be operating initially at 100% of rated power, BOL conditions, and at normal system pressure when a complete loss of normal feedwater occurs (5-second rampdown). As the level in the steam generator falls, a severe mismatch is developed between core heat generation and secondary system heat demand, resulting in a rapid increase in primary coolant pressure and

temperature. A reactor trip occurs on high system pressure, but it is assumed that the control rods are not inserted. As the coolant temperature increases, negative moderator density feedback reduces the power level, while positive (but smaller) fuel temperature feedback tends to delay power reduction as the fuel cools. Reactor coolant loop transit effects are very pronounced with the power level rising and falling as the coolant completes cycles through the steam generator and back to the core. The turbine is assumed to trip 3 seconds after the loss of main feedwater. System pressure increases, and the pressurizer begins to fill. The pressurizer safety relief valves open to provide relief for coolant expansion, but the pilot relief valve is assumed to fail shut. The pressurizer spray is assumed to operate normally. Pressure increases as the steam generator begins to dry out and continues to increase through pressurizer fill. The pressure peaks when core heat transfer is reduced to a level at which pressurizer relief and auxiliary feedwater heat removal are adequate to terminate the coolant temperature rise. As liquid and steam relief from the pressurizer continues, the pressurizer pressure decreases to the setpoint value of its safety valves (2515 psia). The RCS is maintained at or below this pressure until the operator implements AIWS recovery procedures. The chronological sequence of events is presented in Table 4-21.

Methods of Analysis

The B&W digital computer code CADDs was used to determine the characteristics of this accident.³⁰ Included were complete kinetics, pressure, average fuel rod, steam generator, and coolant transport models. Only the Doppler and moderator coefficients of reactivity were used as feedback.

Results of Analysis

The transient responses of the important system parameters are presented in Figures 4-55 through 4-59. The system pressure response illustrated in Figure 4-58 shows that the system pressure exceeds the emergency pressure limit. The peak system pressure is 4856 psia, an increase of 270 psi above the same case for the UO_2 case analyzed for a revision of reference 33 results.

Conclusions

The comparison of the LOFW transient sequence for the $(\text{Th}/^{233}\text{U})\text{O}_2$ and UO_2 in Table 4-21 indicates that the RCS pressure and temperature increase at a slightly faster rate for the thorium case. This response is a result of less net

negative reactivity feedback from the combination of moderator density and fuel temperature effects.

The RC density decreases during this transient, which causes negative moderator reactivity feedback. The fuel temperature also decreases, which causes positive reactivity feedback. The magnitude of fuel temperature change is sufficient to provide fuel temperature feedback of the same order of magnitude as the moderator density feedback. Table 4-22 compares the moderator density reactivity feedback for the three thorium fuel mixtures at BOL. This comparison indicates that the other two thorium fuels, $(\text{Th}/^{233}\text{U})\text{O}_2$ and $(\text{Th}/\text{Pu})\text{O}_2$, would experience greater negative reactivity feedback than the $(\text{Th}/^{235}\text{U})\text{O}_2$ fuel for this transient. The BOL Doppler coefficients for the two Th/U fuels are very similar. They are both more negative than that of UO_2 and provide a more positive reactivity feedback. The combination of the Doppler coefficient and moderator density feedback for the $(\text{Th}/^{235}\text{U})\text{O}_2$ indicates that this fuel would experience less severe results for this transient than the $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel. The BOL Doppler coefficient for the $(\text{Th}/\text{Pu})\text{O}_2$ fuel is less negative than that of the $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel. The results of this transient for $(\text{Th}/\text{Pu})\text{O}_2$ fuel should thus be less severe than those of $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel. Hence, the results presented in this section for $(\text{Th}/^{233}\text{U})\text{O}_2$ fuel present the most severe consequences for the thorium fuels.

The consequences of an ATWS loss of feedwater for the alternate thorium fuels are either equivalent to or more severe than those for the standard UO_2 core. This problem is generic to all fuel types, and generic resolution of this problem is currently under investigation.

4.8. Safety Analysis Conclusions

The analysis of selected reactor transients demonstrates that it is feasible to license and operate safely a B&W 205-FA NSS fueled with denatured $(^{233}\text{U}/^{238}\text{U}-\text{Th})\text{O}_2$ or denatured $(^{235}\text{U}/^{238}\text{U}-\text{Th})\text{O}_2$. The safety analysis also indicates that the plant could probably be licensed when fueled with $(\text{Th}/\text{Pu})\text{O}_2$ fuel although the data base on which this tentative conclusion is based is more limited than it is for the other fuel types.

In most cases, the transient consequences were less severe for the thorium fuel mixtures than for the standard UO_2 core. For the transients that yield worse consequences for the thorium fuels, the consequences were generally still within

acceptable limits. Furthermore, simple alterations of RPS setpoints could limit the response of the system to the equivalent of the UO_2 cases. The only exception to this conclusion is the ATWS case. The peak pressure for this transient exceeded the corresponding UO_2 value by 270 psia, and RPS modification cannot be invoked to mitigate this peak. This problem is generic to all fuel types. A generic resolution of this problem is presently under investigation.

All in all, the results of this study lead to the conclusion that it is feasible from a safety standpoint to utilize either of the denatured urania-thoria fuels in the standard B&W 205-FA plant. Furthermore, it appears that the use of thorium-plutonium fuels would also be feasible. Transient responses with these fuels in the core are so close to the responses with UO_2 fuel that it appears that existing hardware, analytical methods, and even topical analyses could be modified quite easily to address these fuels in a valid manner.

Table 4-1. Accident Summary

<u>Accident</u>	<u>Controlling parameter</u>	<u>Time in life when effect is maximized</u>
Increase in heat removal by secondary system:		
Feedwater system malfunctions resulting in decrease in FW temperature	Moderator coeff	EOL
FW system malfunctions resulting in increase in FW flow	Moderator coeff	EOL
Steam line break	Moderator coeff	EOL
Decrease in heat removal by secondary system:		
Loss of external electric load and/or turbine trip	Moderator coeff	BOL
Loss of non-emergency a-c power to station auxiliaries	Decay Heat rate	EOL
Loss of normal FW flow	Moderator coeff	BOL
FW line break	Moderator coeff	BOL
Decrease in RC system flow rate:		
Single and multiple RC pump trips (loss of coolant flow)	Moderator coeff	BOL
RC pump shaft seizure (locked rotor)	Moderator coeff	BOL
Reactivity and power distribution anomalies:		
Uncontrolled CR group withdrawal from subcritical condition (startup acc.)	Moderator coeff	BOL
	Doppler coeff	BOL
Uncontrolled CR group withdrawal at power	Moderator coeff	BOL
	Doppler coeff	BOL
CRA misoperation:		
CRA withdrawal	Moderator coeff	EOL
CRA drop	Moderator coeff	MOL
	Doppler coeff	MOL
Startup of inactive RC pumps	Moderator coeff	EOL
	Doppler coeff	EOL
Chemical addition system malfunction	Boron Concent	BOL
Rod ejection accident	Moderator coeff	BOL
	Doppler coeff	BOL
	Gap activity	EOL

Table 4-2. Most Limiting Fuels

Thoria mixtures	Physics parameters							
	α_m		α_D		β		ℓ	
	<u>BOL</u>	<u>EOL</u>	<u>BOL</u>	<u>EOL</u>	<u>BOL</u>	<u>EOL</u>	<u>BOL</u>	<u>EOL</u>
Th/U ²³³	⊕	+	-	⊗	⊗ ¹	⊗	x	⊗
Th/U ²³⁵	-	⊗	-	x	⊗ ²	x	x	x
Th/Pu	-	-	-	x	x	x	⊗	x

x Values which exceed UO₂ range

+ Values comparable to UO₂ range endpoints

- Values within UO₂ range

⊗ Most extreme Th mixture value

1 Smallest value of Th mixture range

2 Largest value of Th mixture range

Table 4-3. Fuel-Independent Parameters Applicable
to All Accidents in Accident Analysis

Core power level for accident analysis, MWt	3876 ^(a)
Initial RC system pressure, psia	2250 ^(b)
Initial RC system flow, 10 ⁶ lb/h	157.44
Initial reactor coolant inlet temperature, F	
Rated power	568.5 ^(b)
Hot shutdown	548.8 ^(b)
High flux trip value, % rated power (3600 MWt)	112
High flux trip delay, s	0.4
High-pressure trip value, psia	2470
High-pressure trip delay, s	0.65
Low-pressure trip value, psia	2000
Low-pressure trip delay, s	0.65
RC pump monitor trip delay, s	0.62
Flux/flow trip value	1.10
Flux/flow trip delay, s	1.5
Control rod travel time to 2/3 insertion, s	1.7
Low steam generator pressure ESFAS trip setpoint, psia	600
Low RCS pressure ESFAS trip setpoint, psia	1600
DNB correlation for UO ₂ transient analysis	BXC
DNB correlation for thoria transient analysis	BWC
DNBR at steady-state design overpower (112% rated power) at full coolant flow	
BXC	1.35
BWC	1.43
DNBR design limit (onset of film boiling)	
BXC	1.25
BWC	1.14
Fouled steam generator inventory +5%, lbm	43,300
Feedwater temperature, inlet nozzles, F	438.2
Steam conditions, outlet nozzles	
Total flow at 102% rated power, 10 ⁶ lb/h	16.5
Temperature, F	590.5
Pressure, psia	1065

(a) 102% of 3800.

(b) ±45 psi, ±2F was used in the accident analysis.

Table 4-4. Fuel-Dependent Parameters Applicable to All Accidents in Accident Analysis

	UO_2	$(Th/^{233}U)O_2$	$(Th/^{235}U)O_2$	$(Th/Pu)O_2$
Nom. mod coeff @ BOL(HFP,HZP), $\Delta k/k/^\circ F$	$+0.55 \times 10^{-4}$	$+0.33 \times 10^{-4}$	-0.95×10^{-4}	-1.11×10^{-4}
Nom. mod coeff. @ EOL(HFP), $\Delta k/k/^\circ F$	-3.3×10^{-4}	-2.81×10^{-4}	-3.35×10^{-4}	-2.19×10^{-4}
Nom. Doppler @ BOL(HFP), $\Delta k/k/^\circ F$	-1.23×10^{-5}	-1.94×10^{-5}	-1.98×10^{-5}	-1.56×10^{-5}
Nom. Doppler @ EOL(HFP), $\Delta k/k/^\circ F$	-1.93×10^{-5}	-2.66×10^{-5}	-2.62×10^{-5}	-2.03×10^{-5}
Min tripped rod worth at BOL, % $\Delta k/k$	2.26	2.68	3.37	3.05
Min tripped rod worth at EOL, % $\Delta k/k$	3.68	4.02	5.20	3.27

Table 4-5. Conditions Applicable to DNB Case

Parameter	Assumption
Break size	42-in. OD (16.9 ft ²) double-ended rupture
Break location	Between MSIVs and turbine
Power level, %	102 ^(a)
Time in life	BOL
Turbine trip	Occurs at rupture
Offsite power	Lost at time of rupture
No. of RC pumps operating at time of rupture	Four

(a) 102% of rated core power + 20 MWt RC pump power = 1.02×3800 MWt
+ 20 MWt = 3896 MWt.

Table 4-6. Conditions Applicable to Worst Overcooling Case

<u>Parameter</u>	<u>Assumption</u>
Break size	6.0-ft ² split rupture
Break location	Outside containment, upstream of MSIV
Power level, %	102 ^(a)
Time in life	EOL
Turbine trip	Occurs at rupture
Offsite power	Available
No. of RC pumps operating	Four
Single failure	Main feedwater isolation valve

(a) 102% of rated core power + 20 MWt RC pump power = $1.02 \times 3800 \text{ MWt} + 20 \text{ MWt} = 3896 \text{ MWt}$.

Table 4-7. Sequence of Events for 42-Inch Steam Line Break, Worst-DNB Case

<u>Event</u>	<u>Time, s</u>
Rupture/loss of offsite power	0.0
Pump monitors initiate reactor trip	0.0
Rods begin to drop	0.62
MDNBR of 1.18 reached (BWC correlation)	1.3
Low SG pressure MSIV and AFWAS setpoint reached	1.6
Main steam isolation valves close	9.1
Auxiliary FW assumed to flow to unaffected steam generator	9.1
FW isolation valves complete closure	16.1

Table 4-8. Summary of Overcooling Cases

	Break size, ft ²	Trip Fn, time(a)	ESFAS Fn, time(a)	Max return to power time(a)	Min subcritical margin, time(a)
Th/U ²³³ O ₂	5.0	High ϕ 5.7 s	Low RCS press. 11.3 s	15.7% 41.5 s	$2.7 \times 10^{-3} \Delta k/k$ 42.0 s
	6.0	High ϕ 5.3 s	Low RCS press. 10.6 s	17.9% 39.0 s	$2.2 \times 10^{-3} \Delta k/k$ 41.0 s
	7.25	High ϕ 5.0 s	Low SG press. 5.9 s	15.3% 31.6 s	$3.9 \times 10^{-3} \Delta k/k$ 32.1 s
UO ₂	7.25	High ϕ 3.9 s	Low RCS press. 9.0 s	32.0% 3.15 s	$3.2 \times 10^{-4} \Delta k/k$ 31.4 s

(a) Time after rupture.

Table 4-9. Sequence of Events for a 6.0 ft² Steam Line Break Overcooling Case

Event	Time, s
Rupture	0
High flux trip setpoint reached	4.9
Rods begin to drop	5.3
Low RC system pressure HPI, MSLIS, and AFWAS setpoint reached	10.6
Main steam isolation valves close	18.1
Auxiliary FW begins to flow to unaffected steam generator	18.1
FW isolation valve on unaffected generator completes closure	19.1
MFVCV and MFWSUCV on affected generator complete closure	20.1, 25.1
Minimum subcritical margin of 0.22% $\Delta k/k$ reached	41.0
HPI flow begins	35.6

Table 4-10. Sequence of Events for (Th/U²³³)O₂
Feedwater Line Break

Time, s	Event
0.0	Rupture — turbine stop valves assumed closed
2.5	Low steam generator ESFAS setpoint reached
5.0	Initiation of MFIV closure (17 s ramp)
5.42	High RCS pressure trip setpoint reached
6.07	Rods begin to drop
10.0	MSIVs close
13.0	Primary safety valves open
26.0	Primary safety valves close
34.0	Peak RCS pressure occurs (2580 psig)
34.0	Secondary steam safety valves open
42.5	Auxiliary FW initiated
45.0	Primary safety valves open

Table 4-11. Sequence of Events for (Th/Pu)O₂
Feedwater Line Break

Time, s	Event
0.0	Rupture — turbine stop valves assumed closed
2.5	Low steam generator ESFAS setpoint reached
5.0	Initiation of MFIV closure (17 s ramp)
5.42	High RCS pressure trip setpoint reached
6.07	Rods begin to drop
10.0	MSIVs close
11.0	Peak RCS pressure occurs (2498 psig)
13.0	Primary safety valves open
27.0	Primary safety valves close
42.5	Auxiliary FW initiated
45.0	Primary safety valves open

Table 4-12. Sequence of Events — Four-Pump Coastdown

<u>Event</u>	<u>Time, s</u>
Pumps begin coasting down	0
Control rods begin to drop	0.62
Minimum DNBR occurs	1.2

Table 4-13. Time Sequence of Events for RC Pump Shaft Seizure Accident From Four-Pump Operation

	<u>Time, s</u>
One pump locked rotor	0
Flux/flow trip setpoint reached	0.42
Film boiling occurs	0.9
Control rods begin to drop	1.92
Minimum DNBR occurs	2.0
Maximum fuel centerline temperature (4725F) occurs	2.5
Peak system pressure occurs	3.88
Maximum cladding surface temperature (1332F) occurs	4.1

Table 4-14. Startup Accident Parameters

Max rod speed, in./min.	30
Max worth of all rods, % $\Delta k/k$	12.04
Max reactivity addition rate (all rods at maximum speed), 10^{-4} ($\Delta k/k$)/s	8.66
Min rod worth of single group, % $\Delta k/k$	1.24
Reactivity addition rate for nominal single rod group, 10^{-5} ($\Delta k/k$)/s	8.95
Delay for flux-core ΔT trip, s	0.40

Table 4-15. Rod Withdrawal Accident Parameters

Control rod speed, in./min.	30
Rod worth of nominal single group, % $\Delta k/k$	1.46
Nominal single rod group reactivity addition rate, 10^{-4} ($\Delta k/k$)/s	1.05
Maximum rod worth of all rods, % $\Delta k/k$	13.04
Maximum reactivity addition rate of all rods, 10^{-4} ($\Delta k/k$)/s	9.38

Table 4-16. Summary of (Th/²³³U)₂ Rod Withdrawal Accident Analysis

<u>Time in life</u>	<u>Reactivity rate, ($\Delta k/k$)/s</u>	<u>Trip function (a)</u>	<u>Peak thermal power, % of 3876 MWt</u>	<u>Peak system pressure, psia</u>
BOL	9.38×10^{-4}	1	102.4	2214.0
	1.05×10^{-4}	1	107.3	2324.1
	6.0×10^{-5}	1	108.2	2435.3
	4.5×10^{-5}	2	108.3	2502.6
	4.0×10^{-5}	2	107.8	2500.9
	2.0×10^{-5}	2	105.6	2491.2
	1.0×10^{-5}	2	104.2	2484.8
EOL	9.38×10^{-4}	1	103.0	2212.4
	4.0×10^{-4}	1	106.5	2254.1
	2.0×10^{-4}	1	109.3	2463.5
	1.05×10^{-4}	2	105.3	2485.5

(a) ₁ = high neutron flux level trip, 2 = high RCS pressure level trip.

Table 4.17. Summary of (Th/Pu)O₂ Rod Withdrawal Accident Analysis²

Time in	Reactivity rate, $(\Delta k/k)/s$	Trip function (a)	Peak thermal power, % of 3876 MWt	Peak system pressure, psia
BOL	9.38×10^{-4}	1	102.4	2213.5
	2.0×10^{-4}	1	105.9	2261.5
	1.05×10^{-4}	1	108.6	2437.7
	9.0×10^{-5}	2	108.4	2501.3
	8.0×10^{-5}	2	107.7	2498.2
	5.7×10^{-5}	2	106.0	2491.4
	2.0×10^{-5}	2	102.5	2477.7
EOL	9.38×10^{-4}	1	102.1	2213.4
	4.0×10^{-4}	1	104.0	2229.6
	2.0×10^{-4}	1	107.3	2310.0
	1.8×10^{-4}	1	108.7	2416.5
	1.4×10^{-4}	2	108.1	2501.2
	1.05×10^{-4}	2	106.3	2493.2
	7.0×10^{-5}	2	104.5	2485.6

(a)₁ = high neutron flux level trip, 2 = high RCS pressure level trip.

Table 4-18. Single CRA Drop Accident

Maximum worth CRA dropped, % $\Delta k/k$	Initial peaks radial/axial	Increase in total peak, %
0.13	1.48/1.67	7.05

Table 4-19. Summary of (Th/²³³U)O₂ Rod Ejection Accident Analysis

Initial power level, % rated/ time in life	Ejected rod worth, % Δk/k	Peak power, % rated	
		Neutron	Thermal
0.1, BOL	1.0	6830.0	43.1
0.1, EOL	1.0	4767.1	54.3
0.1, BOL	0.65	5005.7	28.0
0.1, EOL	0.65	3424.6	32.9
0.1, BOL	0.4	415.6	10.6
0.1, EOL	0.4	153.6	9.1
0.1, BOL	0.3	11.0	10.3
0.1, EOL	0.3	7.4	6.9
102.0, BOL	0.65	3914.1	135.8
102.0, EOL	0.65	1968.0	130.2
102.0, BOL	0.4	1034.9	120.0
102.0, EOL	0.4	585.2	116.5

Table 4-20. Summary of (Th/Pu)O₂ Rod Ejection Accident Analysis

Initial power level, % rated/ time in life	Ejected rod worth, % Δk/k	Peak power, % rated	
		Neutron	Thermal
0.1, BOL	1.0	5737.7	68.9
0.1, EOL	1.0	4828.2	49.2
0.1, BOL	0.65	3078.4	25.4
0.1, EOL	0.65	3032.1	20.4
0.1, BOL	0.4	803.5	11.9
0.1, EOL	0.4	30.0	10.2
0.1, BOL	0.3	13.3	9.3
0.1, EOL	0.3	7.0	6.3
102.0, BOL	0.65	3562.5	135.7
102.0, EOL	0.65	2244.9	125.2
102.0, BOL	0.4	1211.1	120.3
102.0, EOL	0.4	547.4	114.1

Table 4-21. Chronological Sequence of Events for Complete Loss of Normal Feedwater Accident With No Rod Motion

Event	Time, s	
	(Th/ ²³³ U) _{0₂} core	UO ₂ core
1. FW flow begins to decrease	0.0	0.0
2. Turbine trips on loss of FW	3.0	3.0
3. Pressurizer spray actuated by high system pressure	3.0	3.5
4. FW flow rate is zero	5.0	5.0
5. Pressurizer pilot relief valve setpoint reached, but valve fails closed	5.1	5.0
6. High-pressure trip setpoint reached, but no rod motion	6.85	7.0
7. Pressurizer code safety valves open	8.5	8.5
8. Steam generators dry out	21.0	21.0
9. Auxiliary FW begins to enter generators	25.0	25.0
10. Pressurizer becomes full of liquid	30.8	31.7
11. Auxiliary FW reactor full flow	30.0	30.0
12. Peak system pressure occurs	36.5 ^(a)	38.5 ^(b)
13. System pressure decreases as relief rate exceeds fluid expansion rate	>36.5	>38.5

(a) Peak pressure = 4856 psia.

(b) Peak pressure = 4586 psia.

Table 4-22. Moderator Density Reactivity
Feedback at BOL

Relative density	$(\text{Th}/^{233}\text{U})\text{O}_2$	$(\text{Th}/^{235}\text{U})\text{O}_2$	$(\text{Th}/\text{Pu})\text{O}_2$
1.3	-1.56	0.36	2.22
1.2	-0.88	0.45	1.66
1.1	-0.34	0.35	0.93
1.0	0.0	0.0	0.0
0.9	-0.10	-0.67	1.19
0.8	-0.13	-1.77	-2.69
0.7	-0.82	-3.46	-4.62
0.6	-2.16	-6.0	-7.1
0.4	-8.6	-15.7	-14.7
0.2	-30.2	-42.4	-30.8

Figure 4-1. Moderator Coefficients ($\alpha_m \times 10^{-4} \Delta k/k/F$)

Figure 4-2. Doppler Coefficients ($\alpha_D \times 10^{-5} \Delta k/k/F$)

Figure 4-3. Delayed Neutron Fractions (β_{eff})

Figure 4-4. Prompt Neutron Lifetime ($\ell \times 10^{-6}$ s)

Figure 4-5. Moderator Coefficient Sensitivity

Figure 4-6. Doppler Coefficient Sensitivity

Figure 4-7. Sensitivity of Delayed Neutron Fraction

Figure 4-8. Percent Control Rod Worth Vs Time After Initiation of Control Rod Motion

Figure 4-9. Percent Control Rod Worth Vs Percent Control Rod Insertion Applicable to All Accidents Analyzed

Figure 4-10. Percent Control Rod Insertion
Vs Time After CRDM Release

Figure 4-11. 42-Inch DER Steamline Break
DNB Case System Response

—— (Th/U²³³) O₂
 ---- UO₂

Figure 4-12. 7.25 ft² Steamline Break Overcooling
Case System Response

Figure 4-13. 7.25 ft² Steamline Break Overcooling Case System Response

— UO₂
 --- (Th/U²³³) O₂

Figure 4-14. Minimum DNB Ratio Vs Time for a
42-Inch DER Steam Line Break

Figure 4-15. Feedwater Line Break Between Steam Generator and First Upstream Check-Valve for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-16. Feedwater Line Break Between Steam Generator and First Upstream Check Valve for (Th/Pu) O_2 Case

Figure 4-17. Feedwater Line Break Between Steam Generator and First Upstream Check Valve for UO₂ Case

Figure 4-18. System Response of DNBR Parameters for
(Th/²³³U)O₂ Four-Pump Coastdown

Figure 4-19. Minimum DNB Ratio Vs Time After Loss of All Pump Power for $(\text{Th}/^{233}\text{U})\text{O}_2$ Four-Pump Coastdown

Figure 4-21. Minimum DNB Ratio Vs Time After Loss of all Pump Power for UO_2 Four-Pump Coastdown

Figure 4-22. Locked Rotor From BOL Four-Pump
Operation for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-23. Minimum DNB Ratio Vs Time for Locked Rotor
From Four-Pump Operation for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-24. Maximum Fuel Centerline Temperature and Clad Surface Temperature as a Function of Time for a Locked Rotor From Four-Pump Operation for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-25. Locked Rotor From BOL Four-Pump
Operation for UO_2

Figure 4-26. MDNBR, Maximum Clad and Fuel Temperature for a Locked Rotor From Four RC Pump Operation for UO_2

Figure 4-27. Startup Accident Using a 1.24% $\Delta k/k$ Control Rod Group for $(Th/^{233}U)O_2$

Figure 4-28. Startup Accident Using All Control Rod Groups With a Worth of 12.04% $\Delta k/k$ for $(Th/^{233}U)O_2$

Figure 4-29. Startup Accident Peak Neutron Power Vs Reactivity Insertion Rate

Figure 4-30. Startup Accident Peak Thermal Power Vs Reactivity Insertion Rate

Figure 4-31. Startup Accident Comparison Using a 1.24% $\Delta k/k$ Control Rod Group

Figure 4-32. Startup Accident Comparison Using All Control Rod Groups With a Worth of 12.04% $\Delta k/k$

Figure 4-33. Rod Withdrawal Accident Using a
1.46% $\Delta k/k$ Rod Group for (Th/
 ^{233}U) O_2

Figure 4-34. Rod Withdrawal Accident Using All Rod Groups Worth of 13.04% $\Delta k/k$ for $(Th/^{233}U)O_2$

Figure 4-35. Rod Withdrawal Accident Peak System Pressure Response Vs Reactivity Addition Rate Comparison for $(\text{Th}/^{233}\text{U})\text{O}_2$

— $(\text{Th}/\text{U}^{233})\text{O}_2$, BOL
 --- $(\text{Th}/\text{U}^{233})\text{O}_2$, EOL
 ----- UO_2 , BOL

Figure 4-36. Rod Withdrawal Accident Using a 1.46% $\Delta k/k$ Rod Group for (Th/Pu)O₂

Figure 4-37. Rod Withdrawal Accident Using All Rod Groups Worth of 13.04% $\Delta k/k$ for (Th/Pu)O₂

Figure 4-38. Rod Withdrawal Accident Peak System Pressure Response Vs Reactivity Addition Rate Comparison for $(\text{Th/Pu})\text{O}_2$

— $(\text{Th/Pu})\text{O}_2$, BOL
 - - - $(\text{Th/Pu})\text{O}_2$, EOL
 - - - UO_2 , BOL

Figure 4-39. Minimum DNB Ratio Vs Time for the $(Th/^{233}U)O_2$
Nominal Single Rod Group Withdrawal

Figure 4-40. Minimum DNB Ratio Vs Time for the $(\text{Th}/^{233}\text{U})\text{O}_2$
All Rod Groups Withdrawal

Figure 4-41. RCS Pressure Vs Time for CRA Drop From 102% Power as a Function of Moderator Temperature Coefficient for $(Th/^{233}U)O_2$

Figure 4-42. Neutron Power Vs Time for CRA Drop From 102% Power as a Function of Moderator Temperature Coefficient for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-43. Inlet Temperature Vs Time for CRA Drop From 102% Power as a Function of Moderator Temperature Coefficient for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-44. RCS Pressure Vs Time Comparison for CRA Drop From 102% Power as a Function of Moderator Temperature Coefficient

- (a) UO₂
- (b) (Th/Pu) O₂
- (c) (Th/U²³³) O₂

Figure 4-45. Neutron Power Vs Time for
CRA Drop for (Th/Pu)O₂

Figure 4-46. Inlet Temperature Vs Time for CRA Drop From 102% Power for (Th/Pu)O₂

Figure 4-47. Minimum DNB Ratio Vs Time for a HFP, BOL, (Th/²³³U)O₂ Core CRA Drop

Figure 4-48. Minimum DNB Ratio Vs Time for a HFP, EOL, (Th/²³³U)₂ Core CRA Drop

Figure 4-49. Peak Neutron Power as a Function of Control Rod Worth for $(\text{Th}/^{233}\text{U})\text{O}_2$

- (a) FLUX $\sim \Delta T$ TRIP
- (b) HIGH PRESSURE TRIP
- (c) HIGH FLUX TRIP

Figure 4-50. Peak Thermal Power as a Function of Control Rod Worth for $(\text{Th}/^{233}\text{U})\text{O}_2$

Figure 4-51. Peak Neutron Power as a Function of Control Rod Worth for (Th/Pu)₂

Figure 4-52. Peak Thermal Power as a Function of Control Rod Worth for (Th/Pu) O_2

- (a) FLUX- ΔT TRIP
- (b) HIGH PRESSURE TRIP
- (c) HIGH FLUX TRIP

Figure 4-53. Peak Neutron Power as a Function of Ejected Control Rod Worth for UO_2

Figure 4-54. Peak Thermal Power as a Function of Ejected Control Rod Worth for UO_2

Figure 4-55. $(\text{Th}/^{233}\text{U})\text{O}_2$ LOFW ATWS —
Fraction of Total Power

Figure 4-56. $(\text{Th}/^{233}\text{U})\text{O}_2$ LOFW ATWS —
Average Coolant
Temperature

Figure 4-57. (Th/²³³U)₂ LOFW ATWS —
Pressurizer Liquid Level

Figure 4-58: $(Th/^{233}U)_2$ LOFW ATWS —
System Pressure

Figure 4-59. (Th/²³³U)₂O₂ LOFW ATWS —
Steam Generator
Inventory

5. RESEARCH AND DEVELOPMENT NEEDS FOR LICENSING

One of the questions addressed under this NASAP contract concerned the resource requirements and schedule for the research and development effort required for full-scale use of denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$ fuels in commercial pressurized light water reactors. This question has been addressed from the perspective of a fuel supplier, focusing on R&D activities that relate directly to licensing the use of this new fuel form for full-scale use in commercial PWRs.

The performance of $\text{ThO}_2\text{-UO}_2$ fuel was successfully demonstrated in a commercial PWR in the 1960's when fuel designed and fabricated by B&W was used as the complete first core in the Indian Point 1 reactor. However, that fuel was clad with stainless steel and operated at lower temperatures and with different control techniques than is typical in today's PWRs. The UO_2 content in that early fuel was also significantly lower than is currently considered for denatured fuel. Furthermore, licensing requirements have changed substantially in the past decade, and today's ground rules and licensing requirements are considerably more demanding. Thus, it is felt that a substantial R&D effort will be required to permit the licensing of full-scale use of denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$ fuels in modern commercial PWRs. B&W has made preliminary estimates of these R&D requirements and associated schedules, which are presented in this section.

The estimates are based on engineering judgment using the experience obtained from licensing today's typical low-enriched UO_2 fuel for PWRs, experience with thorium fuel in the Indian Point 1 reactor, and experience gained in planning R&D programs for plutonium recycle in B&W's commercial reactors.

The R&D program includes a critical experiment project, and out-of-reactor transient fuel performance project, and three in-reactor irradiation projects in order to make a prudent, logical and orderly progression toward commercial implementation of thorium based fuels. The three irradiation projects are a

test rod project, a lead test assembly project, and a batch implementation project.

The structure of the R&D program into three progressive, integrated in-reactor irradiation projects is suggested because this kind of orderly progression is consistent with the cautious, prudent approach that the NRC has adopted for introducing new fuel types into commercial reactors.

The initial irradiation project (the test rod project) would represent the first step. For in-reactor irradiation, all the test rods would be contained in a single fuel assembly and would constitute either the whole assembly or a part of it. The number of denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$ rods in this test would depend on the availability of materials and licensing considerations. At the conclusion of the in-reactor irradiation phase, segments of rods would be used to perform out-of-reactor transient fuel performance experiments on irradiated fuel. Fuel performance data from these projects would provide assurance of the integrity of the basic fuel rod design and would be used to support the licensing effort for later use of the thorium-based fuels.

Another project would be a critical experiment project, which will be required early in the program to support the nuclear design effort. In the licensing process the utility and/or the fuel supplier will be required to demonstrate that the calculational methods used in the nuclear design are capable of predicting such parameters as criticality, power peaking, control rod worth, and temperature coefficients with essentially the same degree of accuracy as is typical for standard low-enriched UO_2 -fueled cores. Thus, such parameters as those above will be measured in the critical experiment project. These measured results will support the anticipated methods development activities and the nuclear design effort for the subsequent irradiation projects.

The next project would involve the demonstration irradiation of several full assemblies of denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$ fuel. This represents the second step in the progressive introduction of increasing quantities of this new fuel form into commercial operation. The fuel assembly and core design, and the licensing effort required for this project would be supported by the results of the earlier test rod irradiation project and also by the results of the critical experiment project. The successful performance of these lead test assemblies will lay the groundwork for full-batch implementation.

Some design iteration will probably be desirable as a result of the information gained from both the test rod project and lead test assembly project. Therefore, small changes to the assembly design and core configuration are likely for full batch implementation. Current experience with new fuel designs indicates that the NRC will require surveillance of the first full-core implementation regardless of the lead test assembly project.

The schedule that has been estimated for carrying out the total R&D program represents a somewhat optimistic viewpoint. It assumes considerable overlap between various projects and also assumes that no large "surprises" will be introduced by any of the irradiation projects or by post-irradiation examinations. It also assumes that the NRC will introduce no surprises that would delay the rapid sequential implementation of the progressive R&D projects. Further assumptions are identified in the detailed work outline presented below. Thus, the schedule shown in this report represents what could be considered an optimistic, "as-fast-as-reasonably-possible" situation.

In summary, an orderly R&D program that can be considered as minimally required for full-scale employment of denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$ fuel in commercial PWRs is described as a 13-year effort achievable by the early 1990's. The cost (in 1978 dollars) of the items shown in the attached schedule is approximately \$23 million. It should be noted that this total does not include costs for the items enumerated in the following list of assumptions.

RESEARCH AND DEVELOPMENT PROGRAM
WORK OUTLINE FOR
FULL-SCALE UTILIZATION OF
DENATURED $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$

Assumptions

1. Basic materials data measurements will have been conducted in a time frame sufficient to support the design of the batch loading.
2. The required raw materials for the program (^{233}U) will be supplied by Department of Energy.
3. A suitable fabrication facility for thorium fuel is operational. Capital cost for this facility are not included in these costs.
4. An existing, licensed uranium fuel assembly design will be used as a base for thorium fuel design.
5. Utilities will want complete backup on licensing for all thorium demonstration programs. This means two separate analyses for the fuel cycle and licensing--one involving thorium and one for uranium only.
6. Basic nuclear data measurements will have been taken in a time frame sufficient to support the neutronics design of the batch loading.
7. Schedules were estimated based on annual reactor operating cycles. Pursuit of this program in operating cycles of 18 months or longer would increase the schedule because of the increased time between outages.
8. Four cycles irradiations are assumed because recent low-enriched UO_2 experience shows that there is a large resource incentive for developing fuel assemblies with longer lifetimes.
9. The use of a reconstitutable end fitting for the test rod project is required. A portion of the fuel rods from the assembly will be replaced during each refueling outage. The assembly will then be reinserted in the core for irradiation and the extracted rods examined to obtain performance data.
10. Post-irradiation equipment for poolside use already exists.
11. Define the following:
 - Small: 0-\$100,000
 - Medium: \$100,000-\$1,000,000
 - Large: more than \$1,000,000

12. Structure of outline:

PROGRAM

I. Projects

1. Phases

a. Tasks

PROGRAM OUTLINE

Scope

I. Test Rod Project

1. Engineering Phase

Medium

a. Task 1 — Design

(1) Model Development

- (a) Nuclear
- (b) Mechanical
- (c) Thermal
- (d) Hydraulic

(2) Fuel Pellet Study

- (a) Composition
- (b) Size: dimensions, geometry
- (c) Rod internals optimization

(3) Dimensional Analysis

- (a) Assume standard OD and length
- (b) Assume standard FA components except end fitting
- (c) Reconstitutable end fitting and supporting equipment

b. Task 2 — Specifications and Drawings

(1) Fuel Pellet Specifications

(2) Fuel Assembly Drawings

2. Material Selection Phase

Small

a. Literature Survey

b. Procurement

c. Testing

3. Fabrication Phase (a) Medium
- a. Process Development
 - b. Material Development
 - c. Test Production
 - d. Quality Assurance
 - e. Characterization
 - f. Shipping
4. Out-of-Core Test Phase Large
- a. Flow Testing for End Fitting
 - b. Transient Fuel Performance Tests
5. Incore Demonstration Phase (b) Large
- a. Licensing
 - (1) Fuel Cycle
 - (2) Reload Report
 - (3) On-Line Computer Changes
 - (4) Physics Test Manual
 - (5) Startup Procedures
 - (6) Fuel Shuffle
 - b. Incore Performance Monitoring
 - (1) Radiochemistry
 - (2) Power History
 - c. Examinations
 - (1) Destructive
 - (2) Nondestructive

II. Load Test Assembly Project

1. Engineering Phase Medium
- a. Design
 - (1) Model Development

(a) Executed four times, once for each irradiation cycle.

(b) All tasks executed four times. Assume that design is input from step I.1.

- (a) Nuclear
 - (b) Mechanical
 - (c) Thermal
 - (d) Hydraulic
 - (e) Safety
 - (f) ECCS
- (2) Fuel Pellet Study
 - (a) Composition
 - (b) Size: dimensions, geometry
- (3) Assembly Optimization Study
- b. Specifications and Drawings
 - (1) Fuel Pellet Specifications
 - (2) Fuel Assembly Drawings
- 2. Material Selection Phase Small
 - a. Literature Survey
 - b. Procurement
 - c. Testing
- 3. Fabrication Phase^(a) Medium
 - a. Process Development
 - b. Material Procurement
 - c. Test Production
 - d. Quality Assurance
 - e. Characterization
 - f. Shipping
- 4. Out-of-Core Test Phase Large
 - a. Critical Experiments
 - b. Flow Tests
 - c. CHF Correlation^(b)
 - d. FLECHT Tests
- 5. Incore Demonstration Phase^(c) Large
 - A. Licensing

 (a) Nine assemblies.

(b) If optimum pin OD is different from that of UO_2 fuel.

(c) Each task executed four times. Assume that design is input from step II.1.

- ### III. Batch Implementation Project

Medium

(1) Model Development

- ### b. Specifications and Drawings

- Small**

c. Testing

Large

- (a) Destructive examination executed once, nondestructive four times.

- c. Test Production
- d. Quality Assurance
- e. Characterization
- f. Shipping
- 4. Out-of-Core Test Phase Large
(Assume none required other than that from step II.4.)
- 5. Incore Demonstration Phase ^(a) Large
 - a. Licensing
 - (1) Fuel Cycle
 - (2) Reload Report
 - (3) On-Line Computer Changes
 - (4) Physics Test Manual
 - (5) Startup Procedures
 - (6) Fuel Shuffle
 - b. Incore Performance Monitoring
 - (1) Radiochemistry
 - (2) Power History
 - c. Examinations ^(b)
 - (1) Destructive
 - (2) Nondestructive

(a) Executed four times. Assume that design is input from step II.1.

(b) Destructive examination executed once, nondestructive four times.

RD&D Program for Implementation of Denatured $^{233}\text{U}/\text{UO}_2\text{-ThO}_2$

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APPENDIX
Babcock-205 NSS Design Features

Table A-1. Babcock-205 NSS Data Summary

	<u>Design rating</u>	<u>Alternate rating</u>
<u>Plant</u>		
Total NSS power, MWt	3,820	3,620
Gross electrical output, MWe ^(a)	1,357	1,297
Net electrical output, MWe ^(a)	1,296	1,237
Net plant heat rate, Btu/kWh ^(a)	10,010	9,931
<u>Reactor Coolant System</u>		
Design temperature, F (650F for RC pump)	670	
Design pressure, psig	2,500	
Hydrostatic test pressure, psig	3,125	
Operating pressure, psig	2,235	
Coolant volume, ft ³ (650F metal temperature)	12,813	
Total reactor flow, gpm (four RC pumps operating)	434,000	
Reactor inlet temperature, F	569	574
Reactor outlet temperature, F	626	627
Reactor vessel OD including nozzles, in.	275	
Reactor vessel ID, in.	182	
Height of reactor vessel and head, ft	43	
Reactor vessel wall thickness, in.	9.125	
Reactor vessel-OTSG centerline spacing, ft	34	
Hot leg pipe ID, in.	38	
Cold leg pipe ID, in.	28	
Pump suction pipe ID, in.	32	
Pump capacity, gpm	108,500	
Pump total developed head, ft	376	
Pump motor rating (nameplate), hp	12,500	
OTSG overall height, ft	75.3	
OTSG shell ID, in.	139	
Number of OTSG tubes	16,016	
OTSG effective tube length, ft	52.1	
OTSG tube OD, in.	0.625	
OTSG tube wall thickness, in.	0.034	
Total steam flow rate, 10 ⁶ lb/h	16.7	15.5
Steam pressure, psia	1060	
Steam superheat, (min), F	35	50
Feedwater temperature, F	465	

(a) At 2-in. Hg condenser backpressure.

Table A-2. Babcock-205 Core Data Summary

	<u>Design rating</u>	<u>Alternate rating</u>
Core heat output, MWt	3,800	3,600
Average thermal output, kW/ft	5.73	5.43
Design overpower, %	112	
Fuel assembly		
Number	205	
Rods	264	
Array	17×17	
Assembly pitch, in.	8.587	
Weight, lb	1,537	
Overall length, in.	165.7	
No. of spacer grids	8	
Fuel rod		
Total No. per core	54,120	
Outside diameter, in.	0.379	
Cladding thickness, in.	0.0235	
Active fuel length, in.	143	
Rod pitch, in.	0.501	
Fuel pellet		
Density, % theoretical	94	
Diameter, in.	0.324	
Length, in.	0.375	
CRA		
Maximum number	81	
Number (uranium core)	64	
Pins per assembly	24	
Length of B ₄ C poison section, in.	139	
APSRs		
Number	8	
Pins per assembly	24	
Length of B ₄ C poison section, in.	36	
BPRAs		
Number (first core/reload)	116/68	
No. of pins per assembly	24	
Length of B ₄ C-Al ₂ O ₃ poison section, in.	126	

Table A-3. Babcock-205 NSS ECCS Features

Number of reactor internals vent valves	8
Number of reactor vessel core flooding nozzles	2
Core flooding tanks	
Number	2
Volume, ft ³	1,800
Operating pressure, psig	600
High-pressure injection pumps	
Number	3 (a)
Capacity, gpm	700
Head, ft	2,600
Low-pressure injection pumps	
Number	2
Capacity, gpm	5,000
Head, ft	385

(a) Two required for ECCS service.

Figure A-1. Babcock-205 NSS

Figure A-2. Once-Through Steam Generator

*Length of regions
vary with load.

Figure A-3. Pressurizer for Babcock-205 NSS

Plenum Assembly

Control Rod Guide Tube

Inlet Nozzle

Outlet Nozzle

Upper Grid

Fuel Assembly

Core Support Assembly

Core Basket

Lower Grid Assembly

Guide Lugs

Incore Instrument Guide Tubes

Flow Distributor Head

Figure A-5. Babcock & Wilcox Mark-C Fuel Assembly

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