TENNESSEE VALLEY AUTHORITY
CHATTANOOGA, TENNESSEE 37401
400 Chestnut Street Tower II

December 19, 1979

Director of Nuclear Reactor Regulation
Attention: Mr. L. S. Rubenstein, Acting Chief
Light Water Reactors Branch No. 4
Division of Project Management
U.S. Nuclear Regulatory Commission
Washington, DC 20555

Dear Mr. Rubenstein:

In the Matter of the Application of	)	Docket Nos. 50-327
Tennessee Valley Authority	)	50-328

Enclosed are proposed revisions to the Sequoyah Nuclear Plant Final Safety Analysis Report (FSAR). These revisions are required to resolve inconsistencies between the draft technical specifications and the FSAR. Amendment 64 will incorporate these revisions in the Sequoyah FSAR.

Very truly yours,

TENNESSEE VALLEY AUTHORITY

L. M. Mills, Manager
Nuclear Regulation and Safety

Enclosure

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design basis will be met for transients associated with Condition II events including overpower transients. There is an additional large DNBR margin at rated power operation and during normal operating transients.

4.4.1.2 Fuel Temperature Design Basis

Basis

During modes of operation associated with Condition I and Condition II events, the maximum fuel temperature shall be less than the melting temperature of UO_2. The UO_2 melting temperature for at least 95% of the peak kW/ft fuel rods will not be exceeded at the 95% confidence level. The melting temperature of UO_2 is taken as 5080°F (Reference 1) unirradiated and reducing 58°F per 10,000 MWD/MTU. By precluding UO_2 melting, the fuel geometry is preserved and possible adverse effects of molten UO_2 on the cladding are eliminated. To preclude center melting and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700°F has been selected as the overpower limit. This provides sufficient margin for uncertainties in the thermal evaluations as described in Subparagraph 4.4.2.10.1.

Discussion

Fuel rod thermal evaluations are performed at rated power, maximum overpower and during transients at various burnups. These analyses assure that this design basis as well as the fuel integrity design bases given in Section 4.2 are met. They also provide input for the evaluation of Condition III and IV faults given in Chapter 15.

4.4.1.3 Core Flow Design Basis

Basis

A minimum of ~~95.5%~~ 92.5% of the Thermal Flow Rate will pass through the fuel rod region of the core and be effective for fuel rod cooling. Coolant flow through the thimble tubes as well as the leakage from the core barrel-baffle region into the core are not considered effective for heat removal.

Discussion

Core cooling evaluations are based on the Thermal Flow Rate (minimum flow) entering the reactor vessel. A maximum of ~~4.5%~~ 7.5% of this value is allotted as bypass flow. This includes RCC guide thimble cooling flow, head cooling flow, baffle leakage, and leakage to the vessel outlet nozzle.

4.4.1.4 Hydrodynamic Stability Design Bases

Basis

Modes of operation associated with Condition I and II events shall not lead to hydrodynamic instability.

distributions are shown for the 4 foot elevation (1/3 of core height) in Figure 4.4-13, and 8 foot elevation (2/3 of core height) in Figure 4.4-14 and at the core exit in Figure 4.4-15. These distributions are for the full power conditions as given in Table 4.4-1 and for the radial power density distribution shown in Figure 4.3-8. The THINC code analysis for this case utilized a uniform core inlet enthalpy and inlet flow distribution.

4.4.2.7 Core Pressure Drops and Hydraulic Loads

4.4.2.7.1 Core Pressure Drops

eight grid

The analytical model and experimental data used to calculate the pressure drops shown in Table 4.4-1 are described in Paragraph 4.4.2.8. The core pressure drop includes the fuel assembly, core support plate, and holddown plate pressure drops. The full power operation pressure drop values shown in the Table are the unrecoverable pressure drops across the vessel, including the inlet and outlet nozzles, and across the core. These pressure drops are based on the Best Estimate Flow (most likely value for actual plant operating conditions) as described in Subsection 5.1.1. Subsection 5.1.1 also defines and describes the Thermal Design Flow (minimum flow) which is the basis for reactor core thermal performance and the Mechanical Design Flow (maximum flow) which is used in the mechanical design of the reactor vessel internals and fuel assemblies. Since the Best Estimate Flow is that flow which is most likely to exist in an operating plant, the calculated core pressure drops in Table 4.4-1 are based on this best estimate flow rather than the Thermal Design Flow. 29

Uncertainties associated with the core pressure drop values are discussed in Subparagraph 4.4.2.10.2.

The pressure drops quoted in Table 4.4-1 are based on seven grids and conservatively estimated grid pressure loss coefficients. Phase 1 of the D-loop tests (Reference 5) resulted in a measured core pressure drop of a magnitude sufficiently lower than the predicted pressure drop that the pressure drops quoted in Table 4.4-1 will be conservative even with the addition of an eighth grid. The estimated pressure drop compared to the measured pressure drop in Reference 5 uses the same conservatively estimated grid pressure loss coefficients used for the Table 4.4-1 pressure drop calculations. Thus, it was expected that the calculated pressure drop would be conservative (larger) relative to the measured value. The Sequoyah fuel assembly grids, top nozzle, and bottom nozzle designs are the same as in the prototype assembly tests and the hydraulic resistances measured during the test are therefore directly applicable to the Sequoyah analysis. Further verification of the 17x17 core pressure drops including uncertainties will be obtained from Phase 2 of the D-loop tests. 29

4.4.2.7.2 Hydraulic Loads

The fuel assembly hold down springs, Figure 4.2-2, are designed to keep the fuel assemblies resting on the lower core plate under transients associated with Condition II and II events. Maximum flow conditions are limiting because hydraulic loads are a maximum. The most adverse flow conditions occur during a LOCA. These conditions are presented in Subsection 15.4.1. 29

Hydraulic loads at normal operating conditions are calculated based on the Mechanical Design Flow which is described in Section 5.1 and accounting for the minimum core bypass flow based on manufacturing tolerances. Core hydraulic loads at cold plant startup conditions are also based on this flow but are adjusted to account for the coolant density difference. Conservative core hydraulic loads for a pump overspeed transient, which create flow rates 20% greater than the Mechanical Design Flow, are evaluated to be greater than twice the fuel assembly weight.

Core hydralic loads were measured during the prototype assembly tests described in Section 1.5. Reference 5 contains a detailed discussion of the results.

Lift forces are directly proportional to the pressure drop. The lift force on an eight grid assembly is thus less than 5 percent greater than the lift force on a seven grid assembly for the same flow rate. Reference 5 shows that lift off of an eight grid fuel assembly (5 percent greater than the seven grid assembly shown) is not predicted during a postulated pump overspeed transient even though it is not necessary to preclude lift off. Additionally, the flow
29 rates used in the test are based on a plant with flow rates ~~about 6.7 percent~~ greater than Sequoyah. This results in additional margin for Sequoyah since the lift force is reduced.

The hydraulic loads during normal operation can be obtained from Reference 5 by adjusting the loads for the Sequoyah pressure drop and flow rate. The effect of startup and shutdown transients are shown to be inconsequential in Reference 5.

4.4.2.8 Correlation and Physical Data

4.4.2.8.1 Surface Heat Transfer Coefficients

Forced convection heat transfer coefficients are obtained from the familiar Dittus-Boelter correlation (Reference 53), with the properties evaluated at bulk fluid conditions:

$$\frac{hD_e}{K} = 0.023 \left(\frac{D_e G}{\mu}\right)^{0.8} \left(\frac{C_p \mu}{K}\right)^{0.4} \qquad (4.4\text{-}15)$$

where:

- h = heat transfer coefficient, BTU/hr-ft^2-°F
- De = equivalent diameter, ft
- K = thermal conductivity, BTU/hr-ft-°F
- G = mass velocity, lb/hr-ft^2
- μ = dynamic viscosity, lb/ft-hr
- C_p = heat capacity, BTU/lb-°F

This correlation has been shown to be conservative (Reference 54) for rod bundle geometries with pitch to diameter ratios in the range used by PWRs.

The onset of nucleate boiling occurs when the clad wall temperature reaches the amount of superheat predicted by Thom's (Reference 55) correlation. After this occurrence the outer clad wall temperature is determined by:

through 27), and by measurements of the fuel and clad dimensions during fabrication. The resulting uncertainties are then used in all evaluations involving the fuel temperature. The effect of densification on fuel temperature uncertainties is presented in Reference 6.

In addition to the temperature uncertainty described above, the measurement uncertainty in determining the local power, and the effect of density and enrichment variations on the local power are considered in establishing the heat flux hot channel factor. These uncertainties are described in Subparagraph 4.3.2.2.1.

Reactor trip setpoints as specified in the Technical Specifications Subsection 16.2.3 include allowance for instrument and measurement uncertainties such as calorimetric error, instrument drift and channel reproducibility, temperature measurement uncertainties, noise, and heat capacity variations.

Uncertainty in determining the cladding temperature results from uncertainties in the crud and oxide thicknesses. Because of the excellent heat transfer between the surface of the rod and the coolant, the film temperature drop does not appreciably contribute to the uncertainty.

4.4.2.10.2 Uncertainties in Pressure Drops

Core and vessel pressure drops based on the Best Estimate Flow, as described in Section 5.1, are quoted in Table 4.4-1. The uncertainties quoted are based on the uncertainties in both the test results and the analytical extension of these values to the reactor application. ~~The magnitude of the uncertainties will be confirmed when the experimental data on the prototype fuel assembly (Section 1.5) is obtained.~~

A major use of the core and vessel pressure drops is to determine the primary system coolant flow rates as discussed in Section 5.1. In addition, as discussed in Paragraph 4.4.4.1, tests on the primary system prior to initial criticality will be made to verify that a conservative primary system coolant flow rate has been used in the design and analyses of the plant.

4.4.2.10.3 Uncertainties Due to Inlet Flow Maldistribution

The effects of uncertainties in the inlet flow maldistribution criteria used in the core thermal analyses is discussed in Subparagraph 4.4.3.1.2.

4.4.2.10.4 Uncertainty in DNB Correlation

The uncertainty in the DNB correlation (Paragraph 4.4.2.3) can be written as a statement on the probability of not being in DNB based on the statistics of the DNB data. This is discussed in Subparagraph 4.4.2.3.2.

4.4.2.10.5 Uncertainties in DNBR Calculations

The uncertainties in the DNBR's calculated by THINC analysis (see Subparagraph 4.4.3.4.1) due to uncertainties in the nuclear peaking factors are accounted for by applying

conservatively high values of the nuclear peaking factors and including measurement error allowances. In addition, conservative values for the engineering hot channel factors are used as discussed in Subparagraph 4.4.2.3.4. The results of a sensitivity study (Reference 52) with THINC-IV show that the minimum DNBR in the hot channel is relatively insensitive to variations in the core-wide radial power distribution (for the same value of $F^N_{\Delta H}$).

The ability of the THINC-IV computer code to accurately predict flow and enthalpy distributions in rod bundles is discussed in Subparagraph 4.4.3.4.1 and in Reference 63. Studies have been performed (Reference 52) to determine the sensitivity of the minimum DNBR in the hot channel to the void fraction correlation (see also Subparagraph 4.4.2.8.3); the inlet velocity and exit pressure distributions assumed as boundary conditions for the analysis; and the grid pressure loss coefficients. The results of these studies show that the minimum DNBR in the hot channel is relatively insensitive to variations in these parameters. The range of variations considered in these studies covered the range of possible variations in these parameters.

4.4.2.10.6 Uncertainties in Flow Rates

The uncertainties associated with loop flow rates are discussed in Section 5.1. For core thermal performance evaluations, a Thermal Design Loop Flow is used which is less than the Best Estimate Loop Flow (approximately 4% for the four-loop plant and 5% for the three-loop plant). In addition another 7.5% ~~4.5%~~ of the Thermal Design Flow is assumed to be ineffective for core heat removal capability because it bypasses the core through the various available vessel flow paths described in Subparagraph 4.4.3.1.1.

4.4.2.10.7 Uncertainties in Hydraulic Loads

As discussed in Subparagraph 4.4.2.7.2, hydraulic loads on the fuel assembly are evaluated for a pump overspeed transient which create flow rates 20% greater than the Mechanical Design Flow. The Mechanical Design Flow as stated in Section 5.1 is greater than the Best Estimate or most likely flow rate value for the actual plant operating condition (by approximately 4.5%).

4.4.2.10.8 Uncertainty in Mixing Coefficient

The value of the mixing coefficient, TDC, used in THINC analyses for this application is 0.038. The mean value of TDC obtained in the "R" grid mixing tests described in Subparagraph 4.4.2.3.1 was 0.042 (for 26 inch grid spacing). The value of 0.038 is one standard deviation below the mean value; and ~90% of the data gives values of TDC greater than 0.038 (Reference 46).

The results of the mixing tests done on 17 x 17 geometry, as discussed in Subparagraph 4.4.2.3.3, had a mean value of TDC of 0.059 and standard deviation of $\sigma = 0.007$. Hence the current design value of TDC is almost 3 standard deviations below the mean for 26 inch grid spacing.

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3. Leakage flow from the vessel inlet nozzle directly to the vessel outlet nozzle through the gap between the vessel and the barrel.

4. Flow entering into the core from the baffle-barrel region through the gaps between the baffle plates.

The above contributions are evaluated to confirm that the design value of core bypass flow is met. The design value of core bypass flow for Sequoyah is equal to 4.5% of the total vessel flow. Of the total allowance, 2.5% is associated with the internals (Items 1, 3, and 4 above) and 2.0% for the core. Calculations have been performed using drawing tolerances on a worst case basis and accounting for uncertainties in pressure losses. Based on these calculations, the core bypass flow for Sequoyah is ≤ 4.5%. This design bypass value is also used in the evaluation of the core pressure drops quoted in Table 4.4-1, and the determination of reactor flow rates in Section 5.1.

Flow model test results for the flow path through the reactor are discussed in Section 4.4.2.8.2.

4.4.3.1.2 Inlet Flow Distributions

Data has been considered from several 1/7 scale hydraulic reactor model tests (References 56, 57, and 64) in arriving at the core inlet flow maldistribution criteria to be used in the THINC analyses (See Subparagraph 4.4.3.4.1). THINC I analyses made using this data have indicated that a conservative design basis is to consider a 5 percent reduction in the flow to the hot assembly. Reference 65. The same design basis of 5% reduction to the hot assembly inlet is used in THINC IV analyses.

The experimental error estimated in the inlet velocity distribution has been considered as outlined in Reference 52 where the sensitivity of changes in inlet velocity distributions to hot channel thermal performance is shown to be small. Studies (Reference 52) made with the improved THINC model (THINC-IV) show that it is adequate to use the 5% reduction in inlet flow to the hot assembly for a loop out of service based on the experimental data in References 56 and 57.

The effect of the total flow rate on the inlet velocity distribution was studied in the experiments of Reference 56. As was expected, on the basis of the theoretical analysis, no significant variation could be found in inlet velocity distribution with reduced flow rate.

No relative effects on the core inlet velocity distribution caused by the change from a 15 x 15 to 17 x 17 fuel assembly array are expected since the lower internals design will remain unchanged. The flow impedance of the lower core plate and fuel assembly nozzles is equal at all locations.

4.4.3.1.3 Empirical Friction Factor Correlations

Two empirical friction factor correlations are used in the THINC-IV computer code (described in Subparagraph 4.4.3.4.1).

The friction factor in the axial direction, parallel to the fuel rod axis, is evaluated using the Novendstern-Sandberg correlation (Reference 66). This correlation consists of the following:

TABLE 4.4-1 (Continued)

REACTOR DESIGN COMPARISON TABLE

Thermal and Hydraulic Design Parameters	Sequoyah Units 1 & 2 17 x 17 With Densification	Reference Plant 17 x 17 With Densification
Average in Core, °F	~~581.3~~ 582.2	585.9
Average in Vessel, °F	578.2	584.7
Heat Transfer		
Active Heat Transfer, Surface Area, Ft^2	59,700	59,700
Average Heat Flux, BTU/hr-ft^2	189,800	189,800
Maximum Heat Flux, for normal operation BTU/hr-ft^2	474,500[a]	474,500[a]
Average Thermal Output, kW/ft	5.44	5.44
Maximum Thermal Output, for normal operation kW/ft	13.6[a]	13.6[a]
Peak Linear Power for Determination of protection setpoints, kW/ft	18.0[c]	18.0[c]
Fuel Central Temperature		
Peak at 100% Power, °F	3400	3400
Peak at Thermal Output Maximum for Maximum Overpower Trip Point, °F	4150	4150
Pressure Drop[b]		
Across Core, psi	~~24.9 ± 5.0~~ 24.3 ± 2.4	~~25.0 ± 5.0~~ 25.7 ± 2.6
Across Vessel, including nozzle psi	~~46.4 ± 7.0~~ 46.65 ± 4.6	~~42.6 ± 6.4~~ 45.1 ± 4.5

[a] This limit is associated with the value of F_Q = 2.50

[b] Based on best estimate reactor flow rate as discussed in Section 5.1.

[c] See Subparagraph 4.3.2.2.6.

Three reactor coolant flow rates are identified for the various plant design considerations. The definitions of these flows are presented in the following paragraphs, and the application of the definitions is illustrated by the system and pump hydraulic characteristics on Figure 5.1-11.

Best Estimate Flow

The best estimate flow is the most likely value for the actual plant operating condition. This flow is based on the best estimate of the reactor vessel, steam generator and piping flow resistance, and on the best estimate of the reactor coolant pump head, with no uncertainties assigned to either the system flow resistance or the pump head. System pressure losses based on best estimate flow are presented in Table 5.1-1. Although the best estimate flow is the most likely value to be expected in operation, more conservative flow rates are applied in the thermal and mechanical designs.

Thermal Design Flow

Thermal design flow is the basis for the reactor core thermal performance, the steam generator thermal performance, and the nominal plant parameters used throughout the design. To provide the required margin, the thermal design flow accounts for the uncertainties in reactor vessel, steam generator and piping flow resistances. The combination of these uncertainties, which includes a conservative estimate of the pump discharge weir flow resistance, is equivalent to increasing the best estimate reactor coolant system flow resistance by approximately 18 percent. The intersection of this conservative flow resistance with the best estimate pump curve, as shown in Figure 5.1-11, establishes the thermal design flow. This procedure provides a flow margin for thermal design of approximately 4.5 percent. The thermal design flow will be confirmed when the plant is placed in operation. Tabulations of important design parameters based on the thermal design flow are provided in Table 5.1-1.

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Mechanical Design Flow

Mechanical design flow is the conservatively high flow used in the mechanical design of the reactor vessel internals and fuel assemblies. To assure that a conservatively high flow is specified, the mechanical design flow is based on a reduced system resistance (90 percent of best estimate) and on the maximum uncertainty on pump head capability (105.5 percent of best estimate for machined pump impellers). The intersection of this flow resistance with the higher pump curve, as shown on Figure 5.1-2, establishes the mechanical design flow. The resulting flow is approximately 4 percent greater than the best estimate flow ~~(96,300 gpm)~~.

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Pump overspeed, due to a turbine-generator overspeed of 20 percent, results in a peak reactor coolant flow of 120 percent of the mechanical design flow. The overspeed condition is applicable only to operating conditions when the reactor and turbine-generator are at power.

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TABLE 5.1-1

SYSTEM DESIGN AND OPERATING PARAMETERS

Plant Design Life, years	40
Nominal Operating Pressure, psig	2235
Total System Volume, including pressurizer and surge line, ft^3	12,612
System Liquid Volume, including pressurizer water at maximum guaranteed power, ft^3	11,892
Reactor Power, MW_t	3411
NSSS Power, ~~Btu/hr~~ MW_t	~~11,680 x 10^6~~ 3423

System Thermal and Hydraulic Data Temperatures
(Based on Thermal Design Flow)

Thermal Design Flow, gpm/loop	~~88,500~~ 91400
Total Reactor Coolant Flow, lb/hr	138.1 ~~133~~ x 10^6
Reactor Vessel Inlet Temperature, °F	~~545.7~~ 546.7
Reactor Vessel Outlet Temperature, °F	~~610.7~~ 609.7
Steam Generator Outlet Temperature, °F	~~545.5~~ 546.4
Steam Pressure at Full Power, psia	857
Steam Generator Steam Temperature, °F	526
Steam Flow at Full Power, lb/hr (total)	14.92 ~~14.12~~ x 10^6
Feedwater Inlet Temperature, °F	~~435~~ 434.6
Pressurizer Spray Rate, max., gpm	800
Pressurizer Heat Capacity, kW	1800
Pressurizer Relief Tank Volume, ft^3	1800

Flows and Pressure Drops
(Based on Best Estimate Flow)

Best Estimate Flow, gpm/loop	~~92,500~~ 97800
Pump Head, ft.	266
Reactor Vessel ΔP, psi	46.2
Steam Generator ΔP, psi	34.6
Piping ΔP, psi	6.4

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5.5.7.3.3 Overpressurization Protection

The inlet line to the Residual Heat Removal System is equipped with a pressure relief valve sized to relieve the combined flow of all the charging pumps at the relief valve set pressure.

Each discharge line to the Reactor Coolant System is equipped with a pressure relief valve to relieve the maximum possible back-leakage through the valves separating the Residual Heat Removal System from the Reactor Coolant System. These relief valves are located in the Emergency Core Cooling System (see Figure 6.3-1).

The design of the Residual Heat Removal System includes two isolation valves in series on the inlet line between the high pressure Reactor Coolant System and the lower pressure Residual Heat Removal System. Each isolation valve is interlocked with one of the two independent Reactor Coolant System pressure signals. The interlocks prevent the valves from being opened when Reactor Coolant System pressure is greater than approximately 425 psig. If the valves are in the open position, the interlocks cause the valves to automatically close when the Reactor Coolant System pressure increases to ~~approximately 600 psi~~ above the high setpoint g. These interlocks are described in more detail in Subsection 7.6.2.

5.5.7.3.4 Shared Function

The safety function performed by the Residual Heat Removal System is not compromised by its normal function which is normal plant cooldown. The valves associated with the Residual Heat Removal System are normally aligned to allow immediate use of this system in its safeguard mode of operation. The system has been designed in such a manner that two redundant flow circuits are available, assuring the availability of at least one train for safety purposes.

The normal plant cooldown function of the Residual Heat Removal System is accomplished through a suction line arrangement which is independent of any safeguards function. The normal cooldown return lines are arranged in parallel redundant circuits and are utilized also as the low head safguards injection lines to the Reactor Coolant System. Utilization of the same return circuits for safeguards as well as for normal cooldown lends assurance to the proper functioning of these lines for safeguards purposes.

5.5.7.3.5 Radiological Considerations

The highest radiation levels experienced by the Residual Heat Removal System are those which would result from a loss of coolant accident. Following a loss of coolant accident, the Residual Heat Removal System is used as part of the Emergency Core Cooling System. During the recirculation phase of emergency core cooling, the Residual Heat Removal System is designed to operate for up to a year pumping water from the containment sump, cooling it, and returning it to the containment to cool the core.

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