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14.0 SAFETY ANALYSES

This section evaluates the safety aspects of the nuclear units and demonstrates that the units can be operated safely and that exposures from credible accidents do not exceed the guidelines of 10 CFR 100. Each unit is designed for licensing at 2300 MWt and has been re-evaluated for conditions associated with an uprate to 2300 MWt core power. The site and engineered safety features are evaluated and presented for both units operating at this rating. This section is divided into three subsections, each dealing with a different behavior category:

Core and Coolant Boundary Protection Analysis, Section 14.1

The abnormalities presented in Section 14.1 have no off-site radiation consequences.

Standby Safety Features Analysis, Section 14.2

The accidents presented in Section 14.2 are more severe and may cause release of radioactive material to the environment.

Rupture of a Reactor Coolant Pipe, Section 14.3

The rupture of a reactor coolant pipe, the accident presented in Section 14.3, is the basis for the design of engineered safety features. Even for this accident, the unit design meets the guidelines of 10 CFR 100.

Parameters and assumptions that are common to various accident analyses are described below to avoid repetition in subsequent sections.

Containment Bulk Ambient Temperature

The specific effects of elevated containment bulk temperature above the normal design value of 120°F, with a limit of 125°F for up to two weeks per year, was evaluated with regard to structural integrity, cable ampacities, environmental qualification of equipment, and effect on the conclusions of the accident analysis of this chapter. These effects are discussed in the appropriate sections, and were found to have slight or negligible impact while being well within the existing design limitations of the plant.

Steady State Errors

For accident evaluation, the initial conditions are obtained by adding maximum steady state errors to rated values. The following steady state errors are considered:

Power (Reactor Core)	± 2% for calorimetric error.
Core Inlet Temperature	+6°F, -7°F for deadband and measurement error.
Primary Pressure	±60 psi for steady state fluctuation and measurement error.

Hot Channel Factors

Unless otherwise stated in the section describing specific accidents, the hot channel factors used are:

F (heat flux hot channel factor) = 2.50

F_H (enthalpy rise hot channel factor) = 1.70

The incore instrumentation system will be available to verify the actual hot channel factors and core power distributions at various times in the core life.

Reactor Trip

A reactor trip signal acts to open the two series trip breakers feeding power to the control rod drive mechanisms. The loss of power to the mechanism coils causes the mechanisms to release the control rods, which then fall into the core. In order to provide additional assurance of tripping the reactor trip breakers, the reliability is enhanced by using the shunt trip attachments to open the reactor trip breakers automatically. There are various instrumentation delays associated with each tripping function, including delays in signal actuation, in opening the trip breakers, and in the release of rods by the control rod drive mechanisms. The total delay to trip is defined as the time delay from the time that trip conditions are reached to the time the rods are free and begin to fall. The maximum time delay assumed for each tripping function is as follows:

Tripping Function	Time Delay (Seconds)	Maximum Trip Point Assumed for Analysis
Overpower (nuclear)	0.5	118%
Power Range Flux (low Setting)	0.5	35%
Overtemperature ΔT	2.0	Variable
Overpower ΔT	2.0	Variable
High Pressurizer Pressure	2.0	2440 psig
Low pressurizer pressure	2.0	1790 psig
High pressurizer level	Note 1	100% of pressurizer level span
Low reactor coolant flow -		
(from loop flow detectors)	1.0	84.5% loop flow
(from undervoltage)	2.0	Not applicable
(from frequency)	0.6	55 Hz
Turbine Trip	2.0	Not applicable
Low-Low Steam Generator Level (Feedwater Flow)	2.0	4% of narrow range level span

NOTE :

1. Although this function is not explicitly modeled in any non-LOCA transient, it is assumed to be operable in the uncontrolled RCCA bank withdrawal at power event to preclude pressurizer filling.

The negative reactivity insertion following a reactor trip is a function of the acceleration of the control rods and the variation in rod worth as a function of rod position. Control rod positions after trip have been determined experimentally as function of time using an actual prototype assembly under simulated flow conditions. The resulting rod positions were combined with rod worths to define the negative reactivity insertion as a function of time, according to Figure 14-1.

The maximum nuclear overpower trip point assumed for all analyses is 118%. The trips will be calibrated at power such that the calibration error is the calorimetric error of ± 2 percent. The design allowance for non-repeatable errors is ±6 percent. Non-repeatable errors include both instrument drift and errors due to process changes such as control rod motion since both are observable as an error between the indicated signal and the known power from calorimetric measurement. In summary, the trip setpoints, established in the Technical Specifications, are less than the trip values assumed in the analyses to ensure that trip occurs within the assumed value when including the design error allowance.

Positive Moderator Temperature Coefficient Power Operation

Analyses contained in Chapter 14 are based on a most positive moderator temperature coefficient of +7 pcm/°F at 0% Rated Thermal Power, ramping to 0 pcm/°F at 100% Rated Thermal Power.

FPL Response to NRC Generic Letter (GL) 93-04

GL 93-04 was issued requesting information pertaining to the single failure of the Rod Control System with respect to the General Design Criteria (GDC) 25 (Draft GDC 31), which requires that acceptable fuel design limits not be exceeded for any single malfunction of the reactivity control system. This GL was in response to the event that occurred at Salem Unit 2 on May 27, 1993 when a withdrawal of a single Rod Cluster Control Assembly (RCCA) occurred when an insert command was given. A Westinghouse Owners Group (WOG) program was initiated to evaluate the event and provide an industry response. The program concluded that the licensing basis continued to be met but recommended revising the CRDM current order timing to enhance the basis for that determination. PC/M's 94-111 (Unit 3) and 95-087 (Unit 4) implemented this change to the CRDM's. The revised CRDM current order timing ensures that an asymmetric rod withdrawal cannot occur due to a single failure in the rod control circuitry. The effects of the Salem type failure (i.e., simultaneous insert and withdrawal signals) have been altered to ensure more predictable and conservative consequences. Specifically, all rods in a selected group/bank will now insert in the presence of a failure that causes simultaneous insert and withdrawal commands. However, these failure modes have been analyzed (Reference 1) and shown to be bounded by the consequences of other Condition II events already analyzed in the FSAR (specifically, RCCA Misalignment and Dropped Rod events, Section 14.1.4)

REFERENCES

1. Westinghouse WCAP-13864,"Rod Control System Evaluation Program," Revision 1-A, approved November 10, 1994.

2. Westinghouse WCAP-14276,"Turkey Point Units 3 and 4 - Uprating Licensing Report," Revision 1, dated December 1995.

3. Westinghouse Letter 96-FP-G-0003,"Turkey Point Unit 4 Cycle 15 Final RSE, Revision 1, Tavg/Power Coastdown," dated January 17, 1996.

NEGATIVE REACTIVITY INSERTION VERSUS TIME FOR REACTOR TRIP HOT ZERO CONDITIONS, 105 PERCENT REACTOR COOLANT FLOW

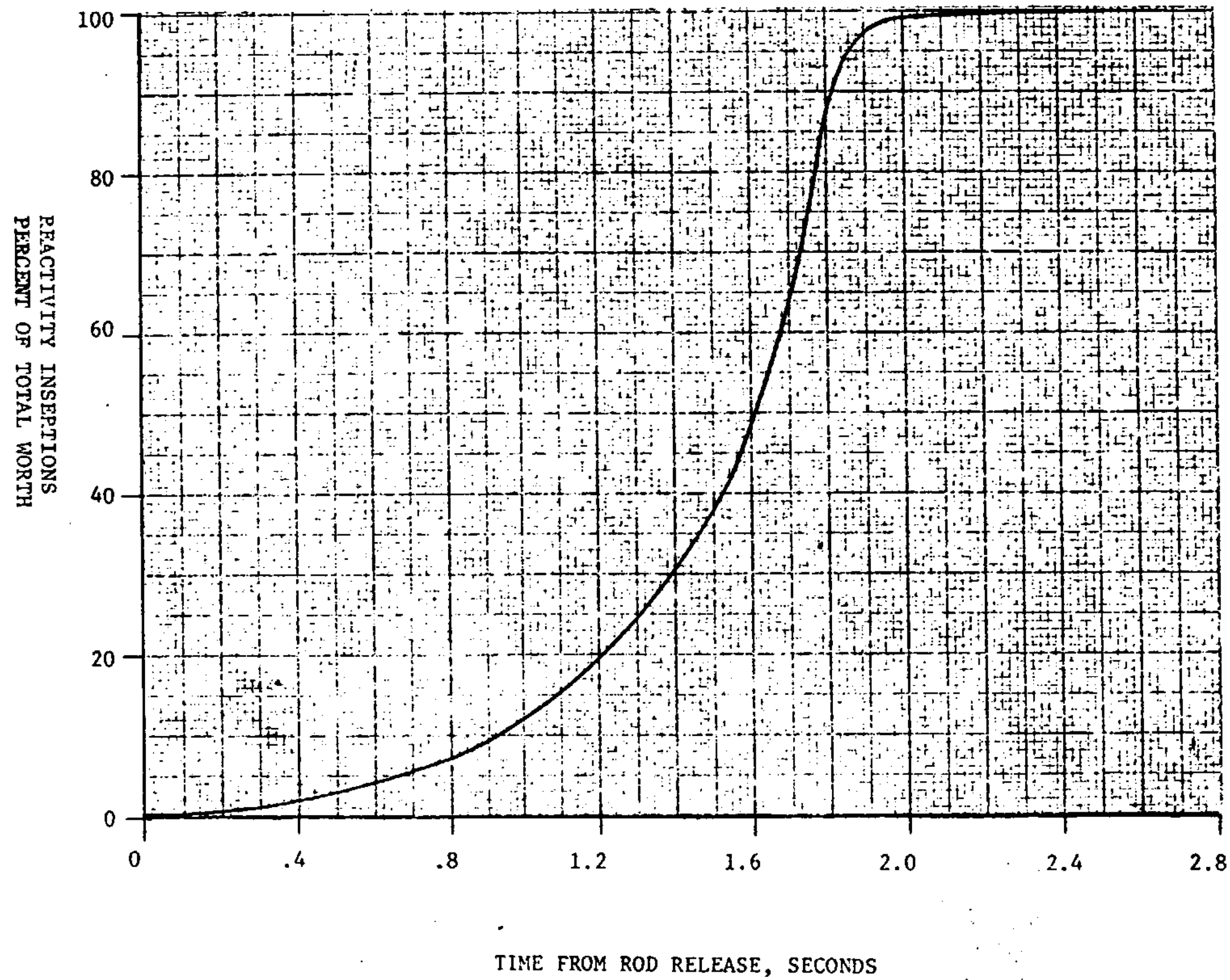


FIGURE 14-1

14.1 CORE AND COOLANT BOUNDARY PROTECTION ANALYSIS

For the following abnormalities and transients, the reactor control and protection system is relied upon to protect the core and reactot~coolant boundary from damage:

a) uncontrolled RCCA withdrawal from a Subcritical Condition
b) uncontrol]ed RCCA Withdrawal, at Power
c) Rod C]uster Control Assembly (RCCA) Drop,
d) Chemical and Volume Control System (CVCS) Malfunction1
e) Startup of an Inactive Reactor Coolant Loop,
f) Excess Feedwater Incident
g) Excessive toad Increase Incident
h) Loss of Reactor Coolant Flow,
i) Loss of External Electrical Load,
j) Loss of Normal Feedwater
k) Loss of All Normal A-C Power to the Station Auxiliaries,
l) Likelihood and Consequences of Turbine Generator Overspeed,

All reactor protection criteria are met presupposing tile most reactive RCC assembly in its fully withdrawn position. Trip is defined for analytical purposes as the insertion of all full length RCC assemblies except the most reactive assembly which is assumed to remain in the fully withdrawn position. This is to provide margin in shutdown capability against the remote possibility of a stuck RCC assembly condition existing at a time when shutdown is required.

Instrumentation is provided for continuously monitoring all individual RCC assemblies together with their respective group position. This is in the form of a deviation alarm system. If the rod should deviate from its intended position the reactor would then be shut down in an orderly manner and the condition corrected. Such occurrences are expected to be extremely rare based on operation and test experience to date.

In summary, reactor protection is designed to prevent cladding damage in all transients and abnormalities listed above. The most probable modes of failure in each protection channel result in a signal calling for the protective trip. Coincidence of two out of three (or two out of four) signals is required where single channel malfunction could cause spurious trips while at power. A single component or channel failure in the protection system itself coincident with one stuck RCCA is always permissible as a contingent failure and does not cause violation of the protection criteria.

14.1.1 UNCONTROLLED RCCA WITHDRAWAL FROM A SUBCRITICAL CONDITION

An RCCA withdrawal incident is defined as an uncontrolled addition of reactivity to the reactor core by withdrawal of rod cluster control assemblies resulting in power excursion. While the probability of a transient of this type is extremely low, such a transient could be caused by a malfunction of the reactor control or control rod drive systems. This could occur with the reactor either subcritical or at power. The "at power" case is discussed in Section 14.1.2.

Reactivity is added at a prescribed and controlled rate in bringing the reactor from a shutdown condition to a low power level during startup by RCCA withdrawal. Although the initial startup procedure uses the method of boron dilution, the normal startup is with RCCA withdrawal. RCCA motion can cause much faster changes in reactivity than can be made by changing boron concentration.

The rod cluster drive mechanisms are wired into preselected banks, and these bank configurations are not altered during core life. The rods are therefore physically prevented from withdrawing in other than their respective banks. Power supplied to the rod banks is controlled such that no more than two banks can be withdrawn at any time. The rod drive mechanism is of the magnetic latch type and the coil actuation is sequenced to provide variable speed rod travel. The maximum reactivity insertion rate is analyzed in the detailed plant analysis assuming the simultaneous withdrawal of the combination of the two rod banks with the maximum combined worth at maximum speed which is well within the capability of the protection system to prevent core damage.

Should a continuous RCCA withdrawal be initiated and assuming the source and intermediate range indication and annunciators are ignored, the transient will be terminated by the following automatic protective functions.

a) Source range flux level trip - actuated when either of two independent source range channels indicates a flux level above a preselected, manually adjustable value. This trip function may be manually bypassed when either intermediate range flux channel indicates a flux level above the source range cutoff power level. It is automatically reinstated when both intermediate range channels indicate a flux level below the source range cutoff power level.

b) Intermediate range rod stop - actuated when either of two independent intermediate range channels indicates a flux level above a preselected, manually adjustable value. This rod stop may be manually bypassed when two out of the four power range channels indicate a power level above approximately ten percent power. It is automatically reinstated when three of the four power range channels are below this value.

c) Intermediate range flux level trip - actuated when either of two independent intermediate range channels indicates a flux level above a preselected, manually adjustable value. This trip function may be manually bypassed, when two of the four power range channels are reading above approximately ten percent power and is automatically reinstated when three of the four channels indicate a power level below this value.

d) Power range flux level trip (low setting) - actuated when two out of the four power range channels indicate a power level above approximately 25 percent. This trip function may be manually bypassed when two of the four power range channels indicate a power level above approximately ten percent and is automatically reinstated when three of the four channels indicate a power level below this value.

e) Power range flux level trip (high setting) - actuated when two out of the four power range channels indicate a power level above a preset setpoint, usually ≤109 percent of full-power. This trip function is always active.

The neutron flux response to a continuous reactivity insertion is characterized by a very fast flux increase terminated by the reactivity feedback effect of the negative Doppler coefficient. This self-limitation of the initial power burst results from a fast negative fuel temperature feedback (Doppler effect) and is of prime importance during a startup accident since it limits the power to a tolerable level prior to external control action. After the initial power burst, the nuclear power is momentarily reduced and then if the accident is not terminated by a reactor trip, the nuclear power increases again, but at a much slower rate.

Termination of the startup accident by the above protection channels prevents core damage. In addition, the reactor trip from high pressurizer pressure serves as backup to terminate the accident before an overpressure condition could occur.

Method of Analysis

The analysis of the uncontrolled RCCA bank withdrawal from subcritical accident is performed in three stages. First, a spatial neutron kinetics computer code, TWINKLE (Reference 1), is used to calculate the core average nuclear power transient, including the various core feedback effects, i.e., Doppler and moderator reactivity. Next, the FACTRAN computer code (Reference 2) uses the average nuclear power calculated by TWINKLE and performs a fuel rod transient heat transfer calculation to determine the average heat flux and temperature transients. Finally, the average heat flux calculated by FACTRAN is used in the THINC computer code (References 3 & 4) for transient DNBR calculations.

In order to give conservative results for the uncontrolled RCCA bank withdrawal from subcritical accident analysis, the following assumptions are made concerning the initial reactor conditions:

a) Since the magnitude of the nuclear power peak reached during the initial part of the transient, for any given rate of reactivity insertion, is strongly dependent on the Doppler Power reactivity coefficient, the least negative design value is used for the uncontrolled RCCA bank withdrawl from subcritical accident analysis.

b) The contribution of the moderator reactivity coefficient is negligible during the initial part of the transient because the heat transfer time constant between the fuel and moderator is much longer than the nuclear flux response time constant. However, after the initial nuclear flux peak, the succeeding rate of power increase is affected by the moderator reactivity coefficient. Accordingly, the conservative value of 7 pcm/°F is used, since this yields the maximum rate of power increase.

c) The analysis assumes the reactor to be at hot zero power conditions with a nominal temperature of 547°F. This assumption is more conservative than that of a lower initial system temperature. The higher initial system temperature yields a larger fuel-to-water heat transfer coefficient, a larger specific heat of the water and fuel, and a less-negative (smaller absolute magnitude) Doppler coefficient. The less-negative Doppler coefficient reduces the Doppler feedback effect, thereby increasing the neutron flux peak. The high neutron flux peak combined with a high fuel specific heat and larger heat transfer coefficient yields a larger peak heat flux. The analysis assumes the initial effective multiplication factor (K_{eff}) to be 1.0 since this results in the maximum neutron flux peak.

d) Reactor trip is assumed to be initiated by power range high neutron flux (low setting). The most adverse combination of instrumentation error, setpoint error, delay for trip signal activation, and delay for trip signal actuation, and delay for control rod assembly release is taken into account. The analysis assumes a 10 percent uncertainty in the power range flux trip setpoint (low setting), raising it from the nominal value of 25 percent to a value of 35 percent; no credit is taken for the source and intermediate range protection. Figure 14.1.1-1 shows that the rise in nuclear power is so rapid that the effect of error in the trip setpoint on the actual time at which the rods release is negligible. In addition, the total reactor trip reactivity is based on the assumption that the highest worth rod cluster control assembly is stuck in its fully withdrawn position.

e) The maximum positive reactivity insertion rate assumed is greater than that for the simultaneous withdrawal of the two sequential control banks having the greatest combined worth at the maximum speed (45 in/min, which corresponds to 72 steps/min).

f) The DNB analysis assumes the most-limiting axial and radial power shapes possible during the fuel cycle associated with having the two highest combined worth banks in their highest worth position.

g) The analysis assumes the initial power level to be below the power level expected for any shutdown condition (10^{-9} fraction of nominal power). The combination of highest reactivity insertion rate and low initial power produces the highest peak heat flux.

h) The analysis assumes two reactor coolant pumps (RCPs) to be in operation. This is conservative with respect to the DNB transient.

i) The accident analysis employs the Standard Thermal Design Procedure (STDP) methodology. The use of STDP stipulates that the Reactor Coolant System (RCS) flow rate will be based on a fraction of the Thermal Design Flow for two RCPs operating and that the RCS pressure is at a conservatively low value which accounts for uncertainty due to instrument error. Since the event is analyzed from hot zero power, the steady-state STDP uncertainties on core power and RCS average temperature are not considered in defining the initial conditions.

<u>Results</u>

Figures 14.1.1-1 through 14.1.1-4 show the transient behavior for a reactivity insertion rate of 75 pcm/sec with the accident terminated by reactor trip at 35% of nominal power. The rate is greater than that calculated for the two highest worth sequential control banks with both assumed to be in their highest incremental worth region.

Figure 14.1.1-1 shows the neutron flux transient. The neutron flux overshoots the full power nominal value for a very short period of time; therefore, the energy release and fuel temperature increase are relatively small. The thermal flux response, of interest for the DNB considerations, is shown in Figure 14.1.1-2. The beneficial effect of the inherent thermal lag in the fuel is evidenced by a peak heat flux of much less than the nominal full power value. Figures 14.1.1-3 and 14.1.1-4 show the transient response of the hot spot average fuel and cladding inner temperatures, respectively. Note the hot spot average fuel temperature increases, but remains below the full power value. The minimum DNBR remains above the safety analysis limit value at all times.

Table 14.1.1-1 presents the calculated sequence of events. After reactor trip, the plant returns to a stable condition. The plant may subsequently be cooled down further by following normal shutdown procedures.

Conclusion

In the event of an RCCA withdrawal accident from the subcritical condition, the core and the RCS are not adversely affected since the combination of thermal power and coolant temperature result in a minimum DNBR greater than the safety analysis limit value. No damage could occur to the fuel due to low temperatures (<2800^{o}F) if compared to the fuel melting temperature limit (4800^{o}F). Thus, no fuel damage is predicted as a result of this transient.

REFERENCES

1. Westinghouse WCAP-8028-A, Risher, D. H., Jr., and Barry, R. F., "TWINKLE - a Multi-Dimensional Neutron Kinetics Computer Code," dated January 1975.

2. Westinghouse WCAP-7908, Hargrove, H. G.,"FACTRAN - A FORTRAN-IV Code for Thermal Transients in a $U0_2$ Fuel Rod," dated December 1989.

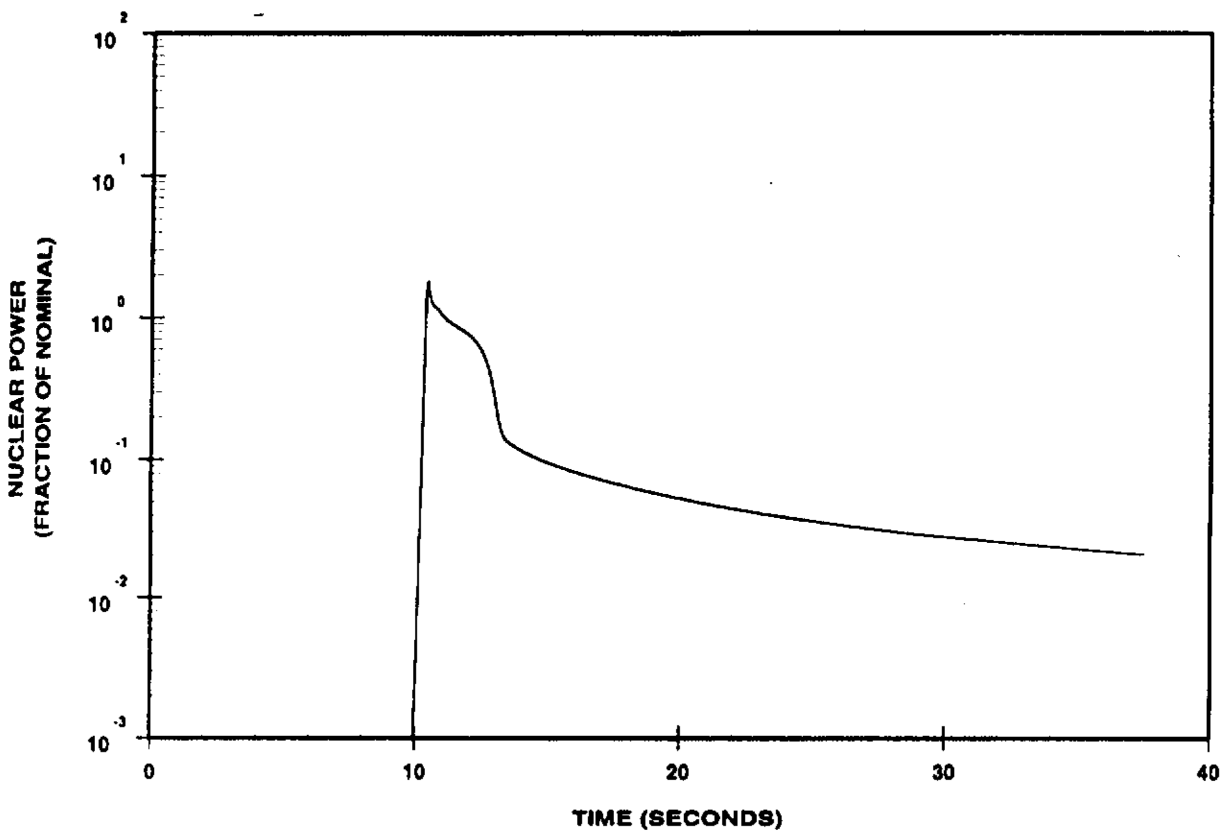
3. Westinghouse WCAP-8195, Chelmer, H., and Hochreiter, L. E.,"Application of the THINC-IV Program to PWR Design," dated February 1989.

4. Westinghouse WCAP-7956, Chelmer, H., Chu, P. T., Hochreiter, L. E., "THINC-IV - An Improved Program for Thermal-Hydraulic Analysis of Rod Bundle Cores," dated February 1989.

TABLE 14.1.1-1

SEQUENCE OF EVENTS
UNCONTROLLED RCCA WITHDRAWAL FROM SUBCRITICAL ACCIDENT

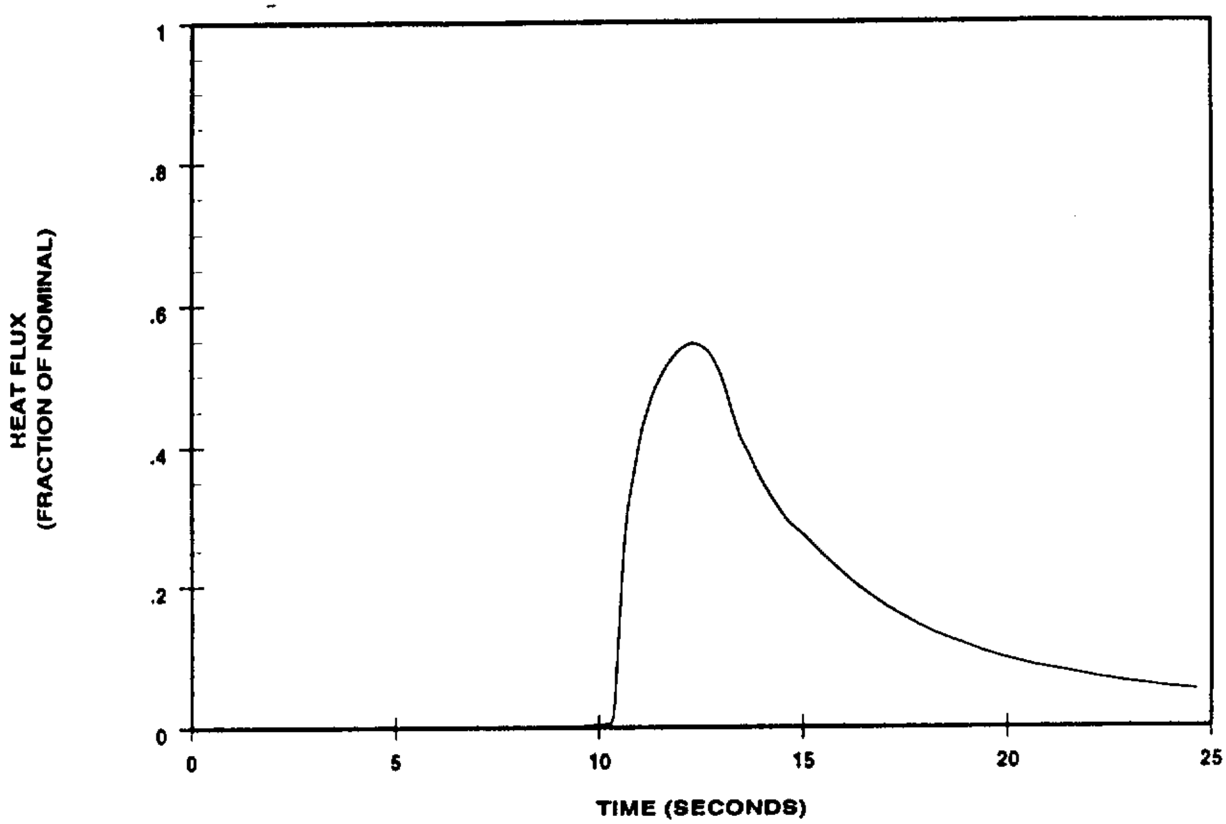
Event	Time (Sec)
Initiation of Uncontrolled RCCA Withdrawal	0.0
Power Range High Neutron Flux, Low Setpoint Reached	10.31
Peak Nuclear Power Occurs	10.45
Rods Begin to Fall	10.81
Minimum DNBR Occurs	12.38
Peak Average Clad Temperature Occurs	12.66
Peak Average Fuel Temperature Occurs	12.96
Peak Fuel Centerline Temperature Occurs	14.41



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

NUCLEAR POWER TRANSIENT
DURING UNCONTROLLED RCCA
WITHDRAWAL FROM SUBCRITICAL
FIGURE 14.1.1-1



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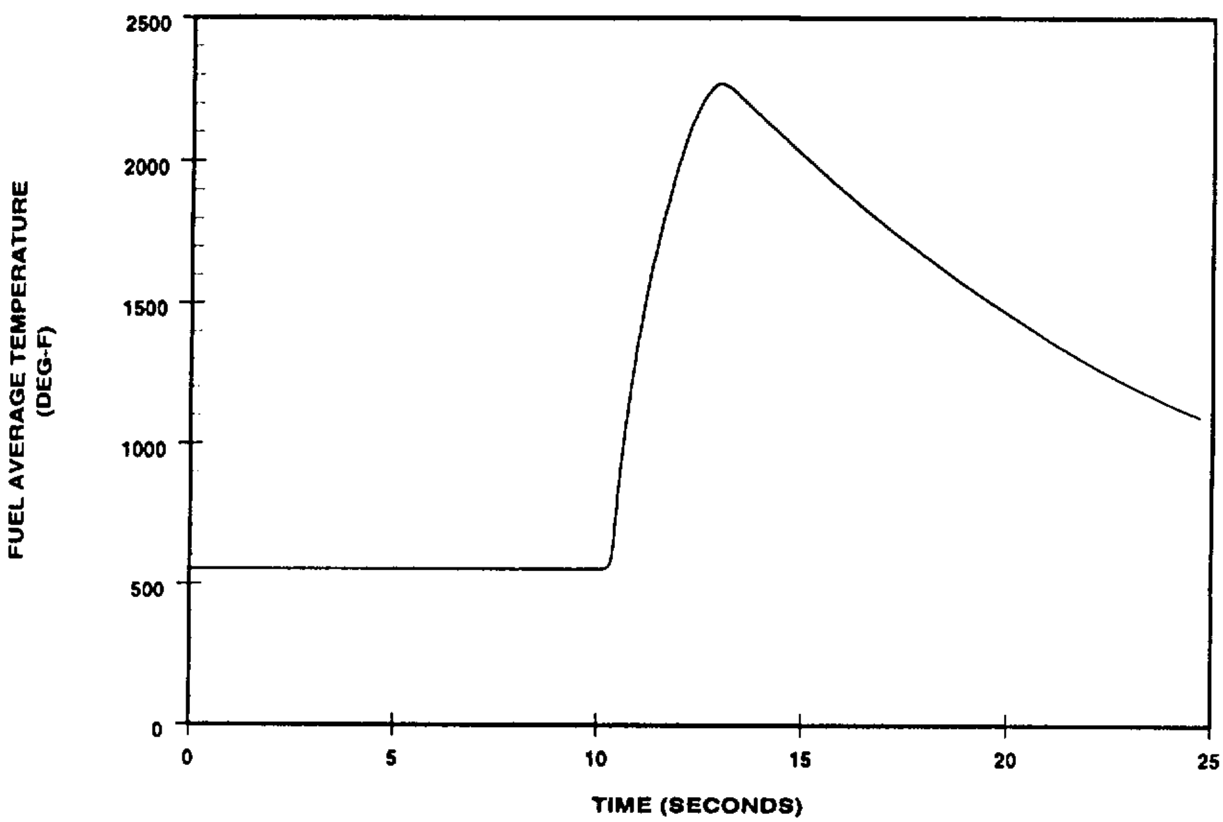
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

HEAT FLUX TRANSIENT DURING
UNCONTROLLED RCCA WITHDRAWAL
FROM SUBCRITICAL
FIGURE 14.1.1-2



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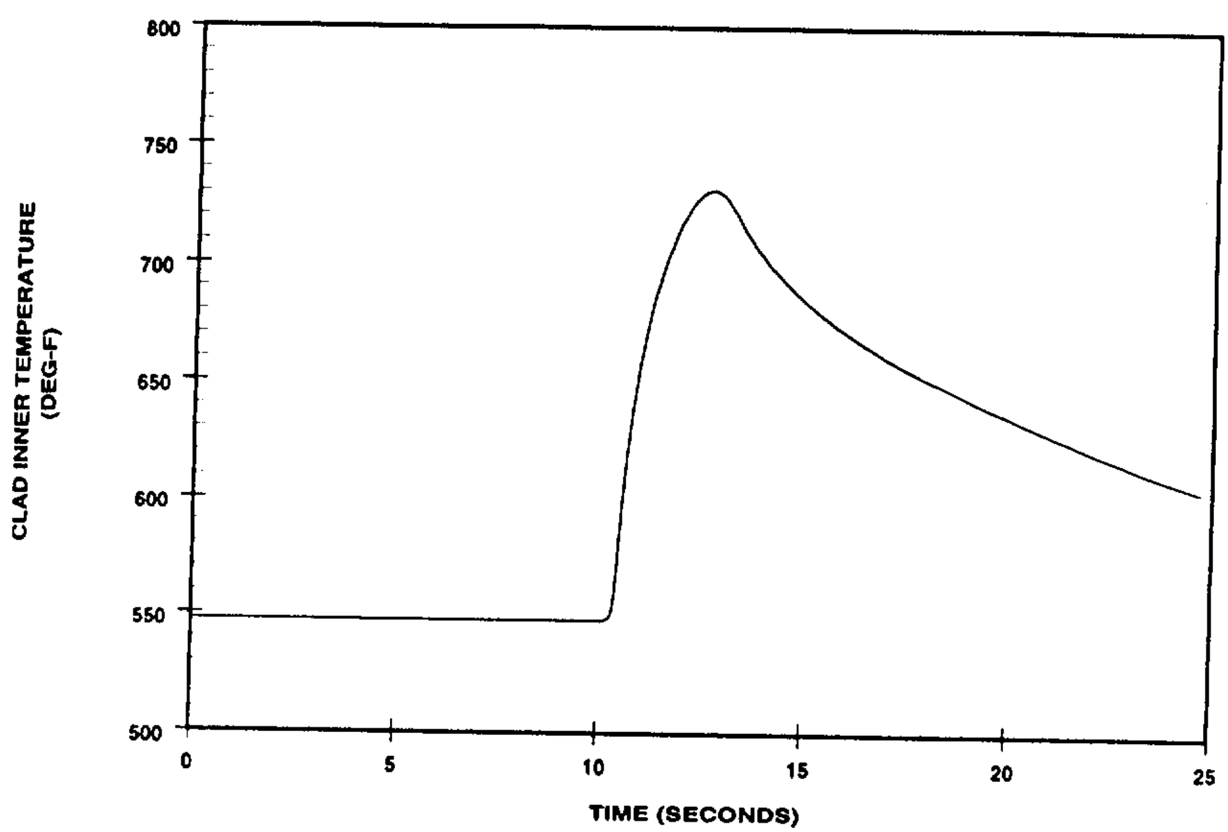
HOT SPOT AVERAGE FUEL TEMPERATURE TRANSIENT DURING UNCONTROLLED RCCA WITHDRAWAL FROM SUBCRITICAL

FIGURE 14.1.1-3



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HOT SPOT CLAD INNER TEMPERATURE TRANSIENT DURING UNCONTROLLED RCCA WITHDRAWAL FROM SUBCRITICAL
FIGURE 14.1.1-4

14.1.2 UNCONTROLLED RCCA WITHDRAWAL AT POWER

A uncontrolled RCCA withdrawal at power results in an increase in core heat flux. Since the heat extraction from the steam generator lags behind the core power generation until the steam generator pressure reaches the relief or safety valve setpoint, there is a net increase in reactor coolant temperature. Unless terminated by manual or automatic action, this power mismatch and resultant coolant temperature rise would eventually result in DNB. Therefore, to avert damage to the fuel cladding, the Reactor Protection System is designed to terminate any such transient before the DNBR falls below the safety analysis limit value or the fuel rod linear heat generation rate (kw/ft) is exceeded.

The automatic features of the Reactor Protection System which prevent core damage in a rod withdrawal accident at power include the following:

a. Nuclear power range instrumentation actuates a reactor trip on neutron flux if two out of the four channels exceed an overpower setpoint.

b. Reactor trip is actuated if any two out of three ΔT channels exceed an overtemperature ΔT setpoint. This setpoint is automatically varied with power distribution, coolant average temperature and pressurizer pressure to protect against DNB.

c. Reactor trip is actuated if any two out of three ΔT channels exceed an overpower ΔT setpoint. This setpoint is automatically varied with coolant average temperature so that the allowable heat generation rate (kW/ft) is not exceeded.

d. A high pressure reactor trip, actuated from any two out of three pressure channels, is set at a fixed point. This set pressure will be less than the set pressure for the pressurizer safety valves.

e. a high pressurizer water level reactor trip actuates if any two-out-of-three level channels exceed a fixed setpoint.

In addition to the above listed reactor trips, there are the following RCCA withdrawal blocks:

- High neutron flux (one out of four power range).
- Overpower ΔT (two out of three).
- Overtemperature ΔT (two out of three).

Method of Analysis

The purpose of this analysis is to demonstrate the manner in which the above protective systems function for various reactivity insertion rates from different initial conditions. Reactivity coefficients, initial conditions and effects of control functions govern which protective function occurs first.

This transient is analyzed by the LOFTRAN code (Reference 1). This code simulates the neutron kinetics, RCS, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, and main steam safety valves. The code computes pertinent plant variables including temperatures, pressures, power level, and departure from nucleate boiling ratio (DNBR).

For an uncontrolled RCCA bank withdrawal at power accident, the following conservative assumptions are made:

a. This accident is analyzed with the Revised Thermal Design Procedure (Reference 2). Therefore, initial reactor power, pressure, and RCS temperatures are assumed to be at their nominal values. Uncertainties in initial conditions are included in the limit DNBR.

b. Reactivity coefficients - two cases are analyzed:

   1. Minimum Reactivity Feedback

      A +7 pcm/°F moderator temperature coefficient and a least-negative Doppler only power coefficient form the basis of the beginning-of-life minimum reactivity feedback assumption.

   2. Maximum Reactivity Feedback

      A conservatively large positive moderator density coefficient of 0.5 Δk/gm/cc (corresponding to a large negative moderator temperature coefficient) and a most-negative Doppler only power coefficient form the basis of the end-of-life maximum reactivity feedback assumption.

c. The reactor trip on high neutron flux is assumed to be actuated at a conservative value of 118 percent of nominal full power. The ΔT trips include all adverse instrumentation and setpoint errors; the delays for trip actuation are assumed to be the maximum values.

d. The RCCA trip insertion characteristic is based on the assumption that the highest worth assembly is stuck in its fully withdrawn position.

e. A range of reactivity insertion rates is examined. The maximum positive reactivity insertion rate is greater than that which would be obtained from the simultaneous withdrawal of the combination of the two control banks having the maximum combined worth at a conservative speed (45 in/min, which corresponds to 72 steps/min).

f. Power levels of 10%, 60%, 80%, and 100% are considered.

In the analysis, the effect of the RCCA movement on core power distribution is considered by its effect of causing a decrease in overtemperature ΔT and overpower ΔT setpoints proportionate to the decrease in margin to DNB. This has the effect of causing a reactor trip sooner in the transient.

Results

Figures 14.1.2-1 and 14.1.2-2 show the transient response for a rapid RCCA bank withdrawal incident (75 pcm/sec) starting from 60% power with minimum feedback. Reactor trip on high neutron flux occurs shortly after the start of the accident. Because of the rapid reactor trip with respect to the thermal time constants of the plant, small changes in T_{avg} and pressure result, and margin in DNB is maintained.

The transient response for a slow RCCA bank withdrawal (1 pcm/sec) from 60% power with minimum feedback is shown in Figure 14.1.2-3 and 14.1.2-4. Reactor trip on overtemperature ΔT occurs after a longer period and the rise in temperature is consequently larger than for rapid RCCA bank withdrawal. Again, the minimum DNBR is greater than the safety analysis limit value.

Figure 14.1.2-5 shows the minimum DNBR as a function of reactivity insertion rate from initial full-power operation for minimum and maximum reactivity feedback. It can be seen that two reactor trip functions (high neutron flux and overtemperature ΔT) provide protection over the whole range of reactivity insertion rates. The minimum DNBR is never less than the safety analysis limit value.

Figure 14.1.2-6, 14.1.2-7, and 14.1.2-8 show the minimum DNBR as a function of reactivity insertion rate for RCCA bank withdrawal incidents starting at 80%, 60%, and 10% power, respectively. The results are similar to the 100% power case; however, as the initial power decreases, the range over which the overtemperature ΔT trip is effective is increased. In none of these cases does the DNBR fall below the safety analysis limit value.

The calculated sequence of events for this accident is shown on Table 14.1.2-1. With the reactor tripped, the plant eventually returns to a stable condition. The plant may subsequently be cooled down further by following normal plant shutdown procedures.

<u>Conclusions</u>

The high neutron flux and overtemperature ΔT reactor trip functions provide adequate protection over the entire range of possible reactivity insertion rates (i.e., the minimum value of DNBR is always larger than the safety analysis limit value).

REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-proprietary), Burnett, T.W.T., et al., "LOFTRAN Code Description," dated April 1984.

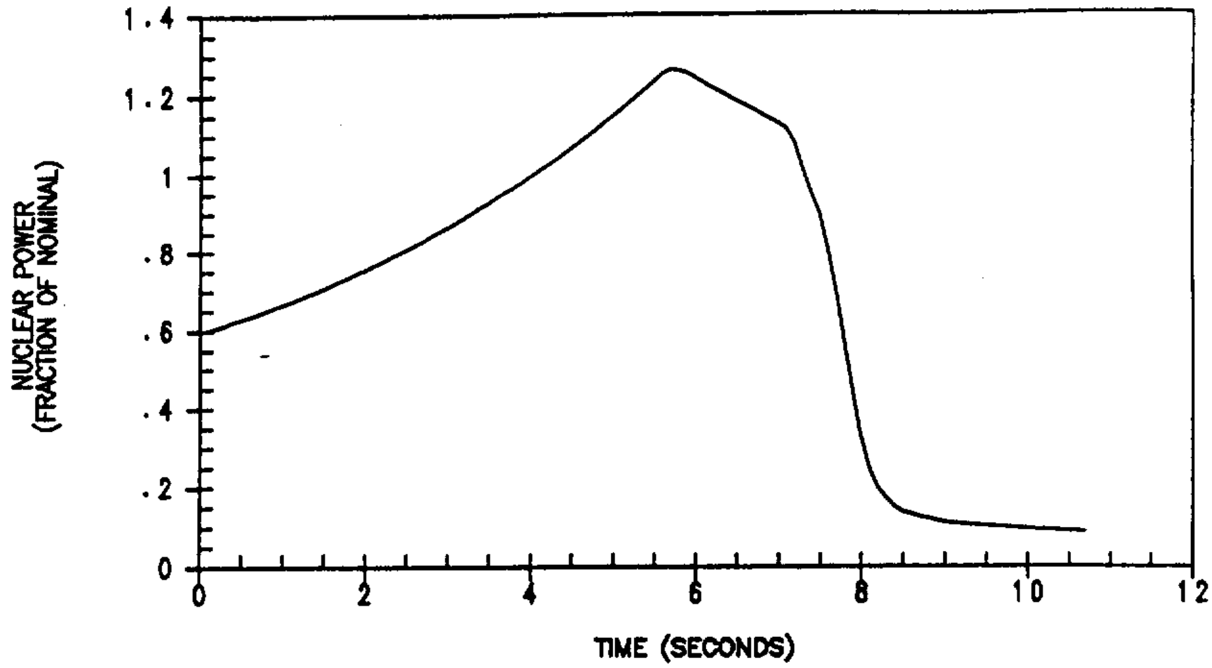
2. Westinghouse WCAP-11397-P-A (Proprietary), Friedland, A. J., and S. Ray, "Revised Thermal Design Procedure," dated April 1989.

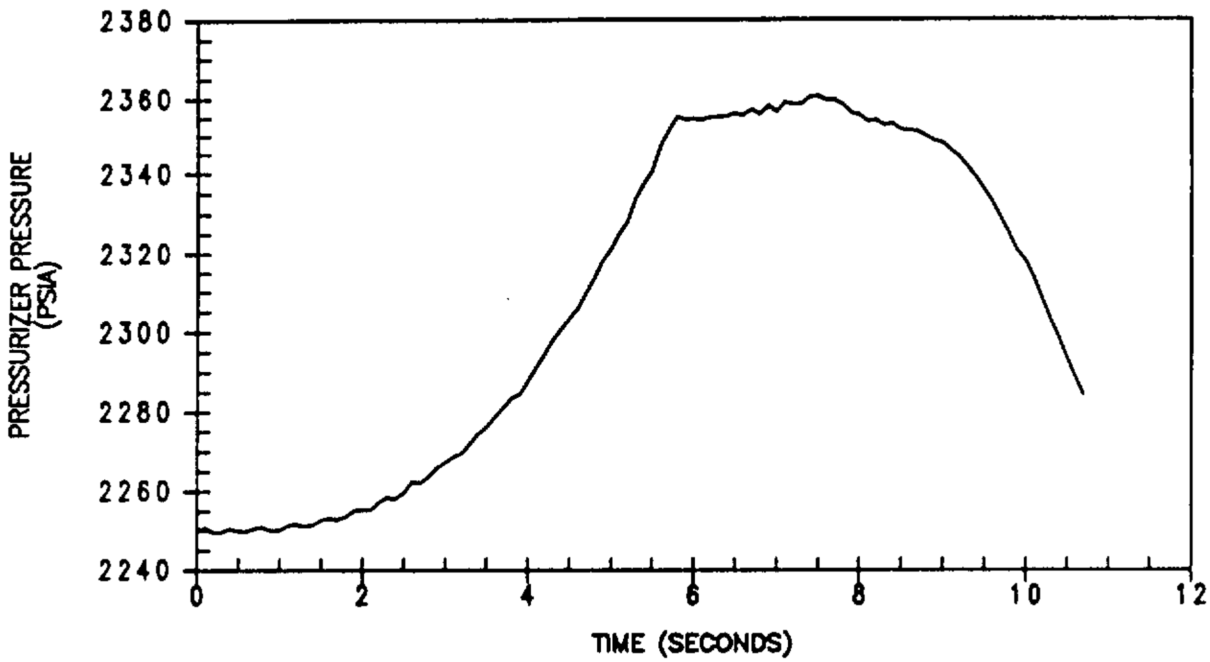
TABLE 14.1.2-1

SEQUENCE OF EVENTS

UNCONTROLLED RCCA WITHDRAWAL AT POWER ACCIDENT

Case	Event	Time (Sec)
60%Power 75 pcm/sec	Initiation of withdrawal	0.00
	High Neutron Flux Trip Setpoint Reached	5.11
	Rods begin to fall	5.61
	Minimum DNBR reached	7.20
60% Power 1 pcm/sec	Initiation of withdrawal	0.00
	Overtemperature ΔT Trip Setpoint Reached	100.10
	Rods begin to fall	102.10
	Minimum DNBR reached	103.20

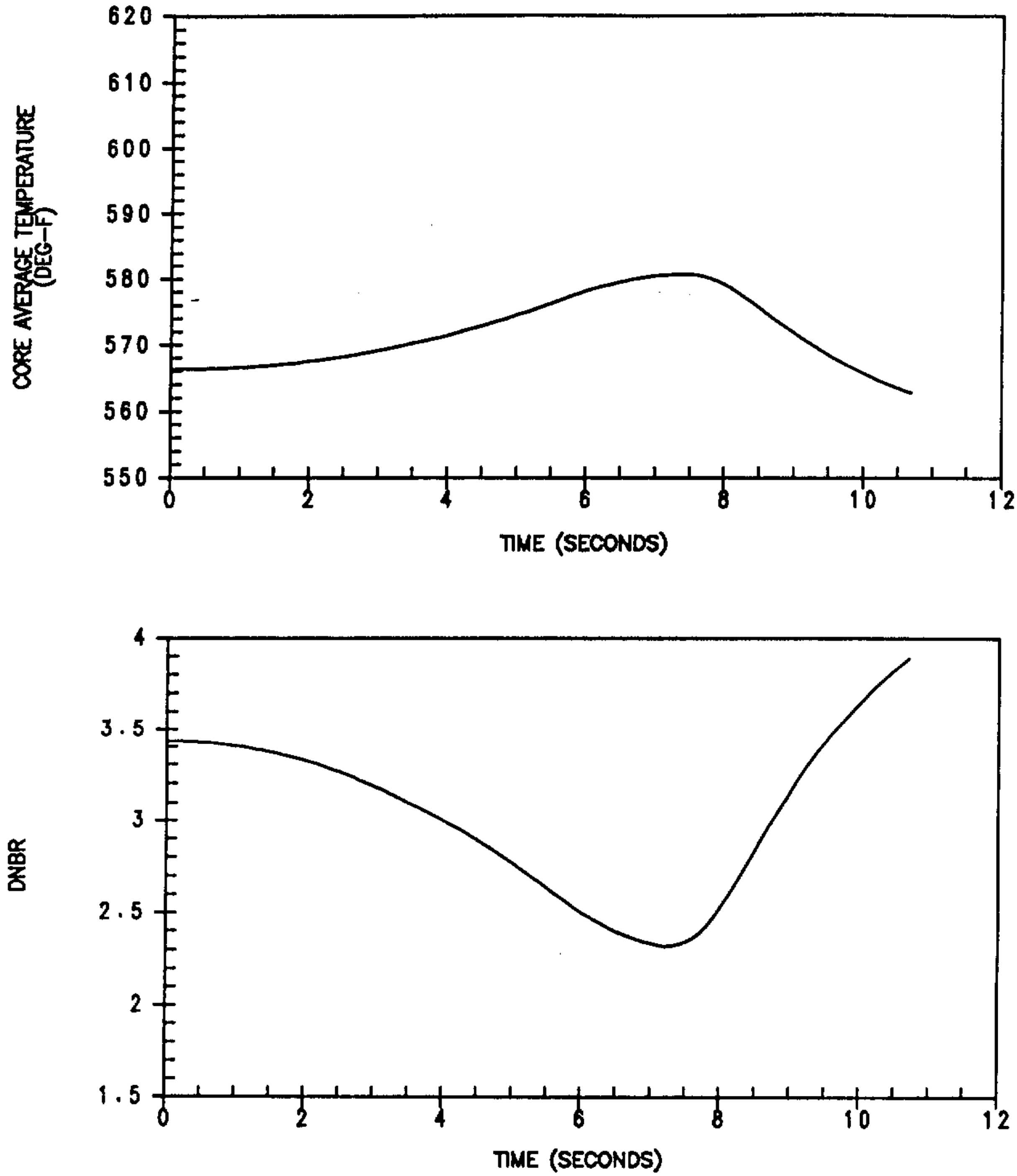




REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

UNCONTROLLED RCCA WITHDRAWAL
60% POWER - MINIMUM FEEDBACK
(75 pcm/sec withdrawal rate)
FIGURE 14.1.2-1

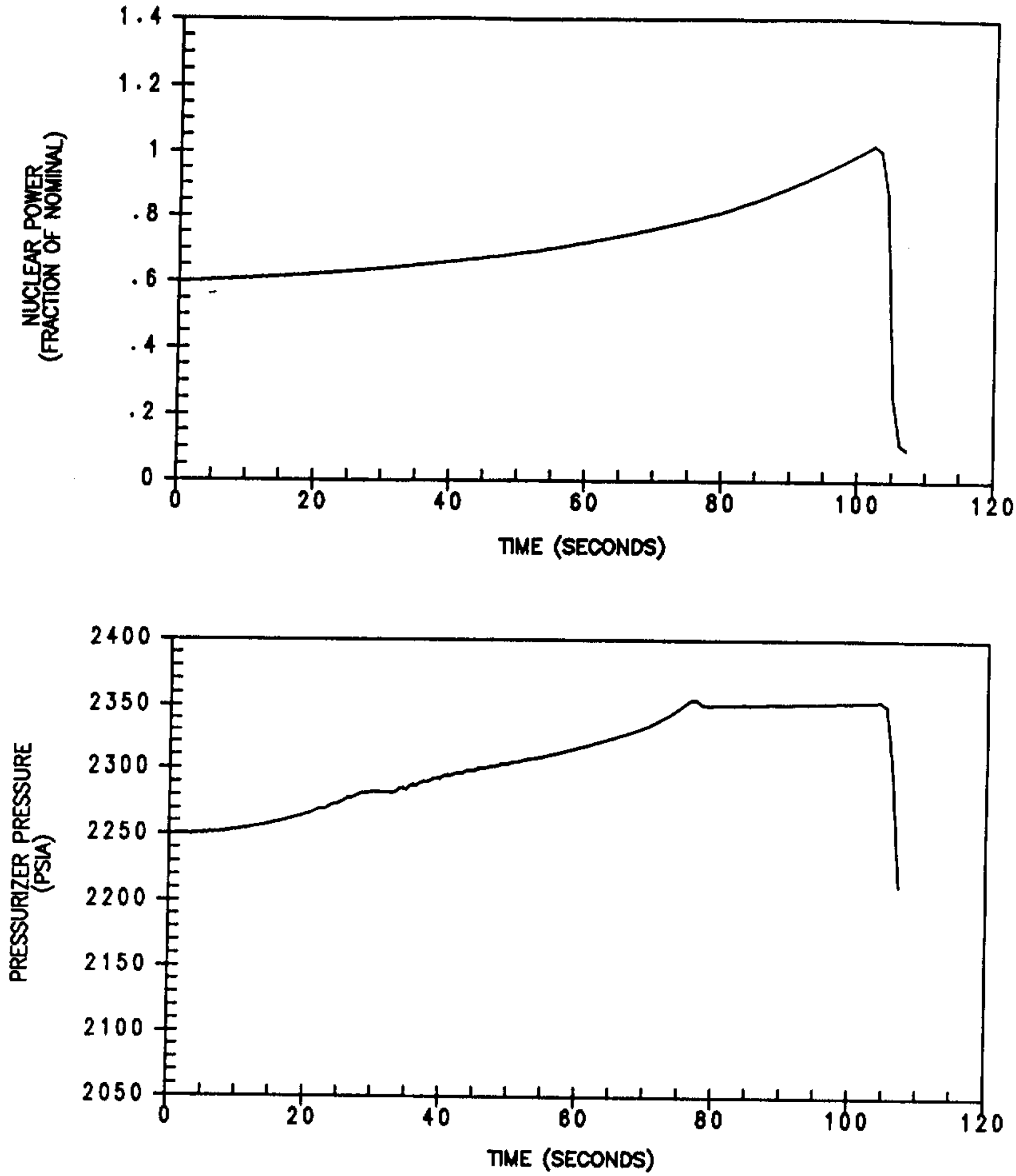


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

UNCONTROLLED RCCA WITHDRAWAL
60% POWER - MINIMUM FEEDBACK
(75 pcm/sec withdrawal rate)

FIGURE 14.1.2-2



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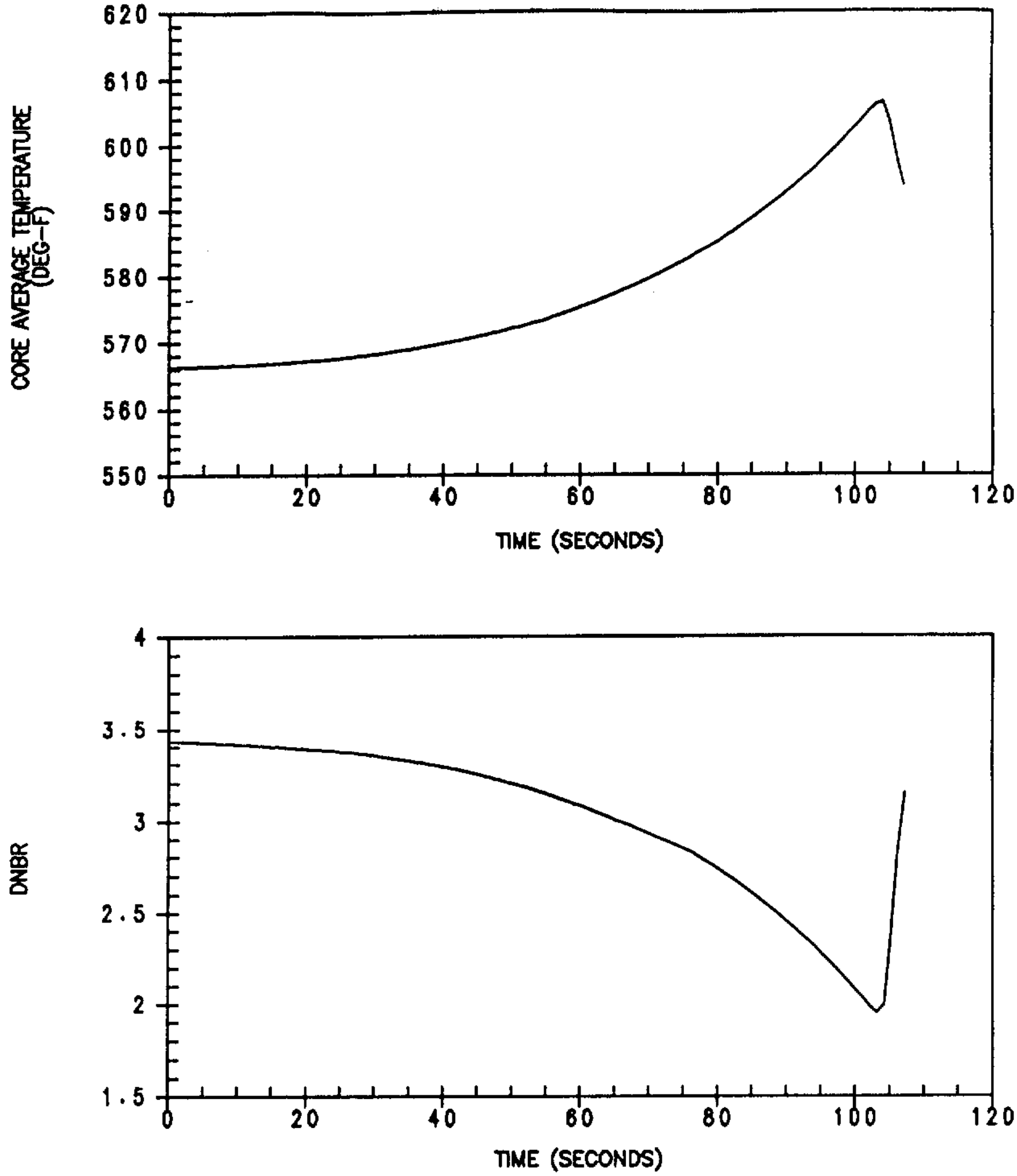
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

UNCONTROLLED RCCA WITHDRAWAL
60% POWER - MINIMUM FEEDBACK
(1 pcm/sec withdrawal rate)
FIGURE 14.1.2-3



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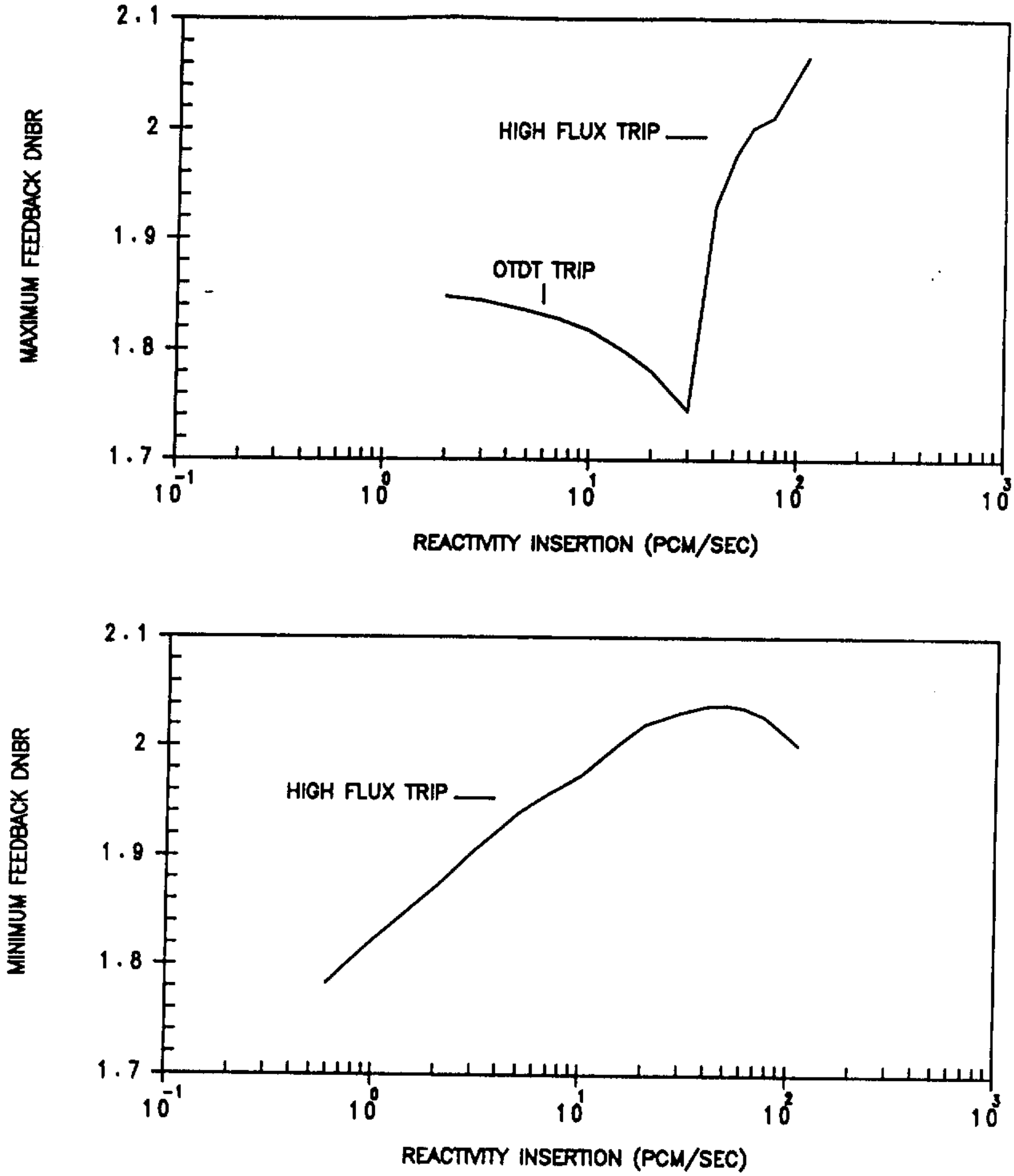
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

UNCONTROLLED RCCA WITHDRAWAL
60% POWER - MINIMUM FEEDBACK
(1 pcm/sec withdrawal rate)
FIGURE 14.1.2-4



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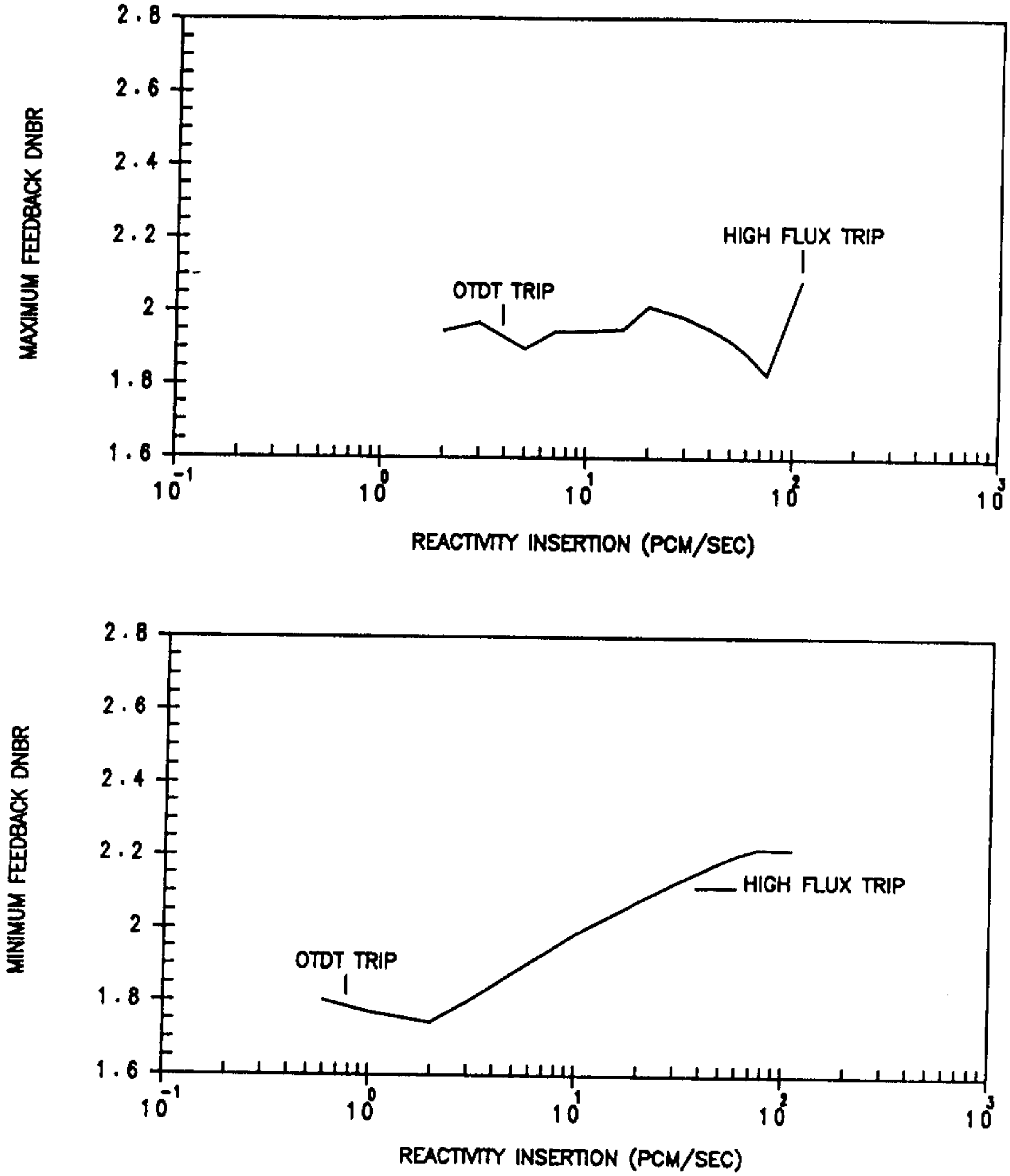
TURKEY POINT PLANT UNITS 3 & 4

MINIMUM DNBR vs REACTIVITY INSERTION RATE FOR ROD WITHDRAWAL AT 100 PERCENT POWER

FIGURE 14.1.2-5



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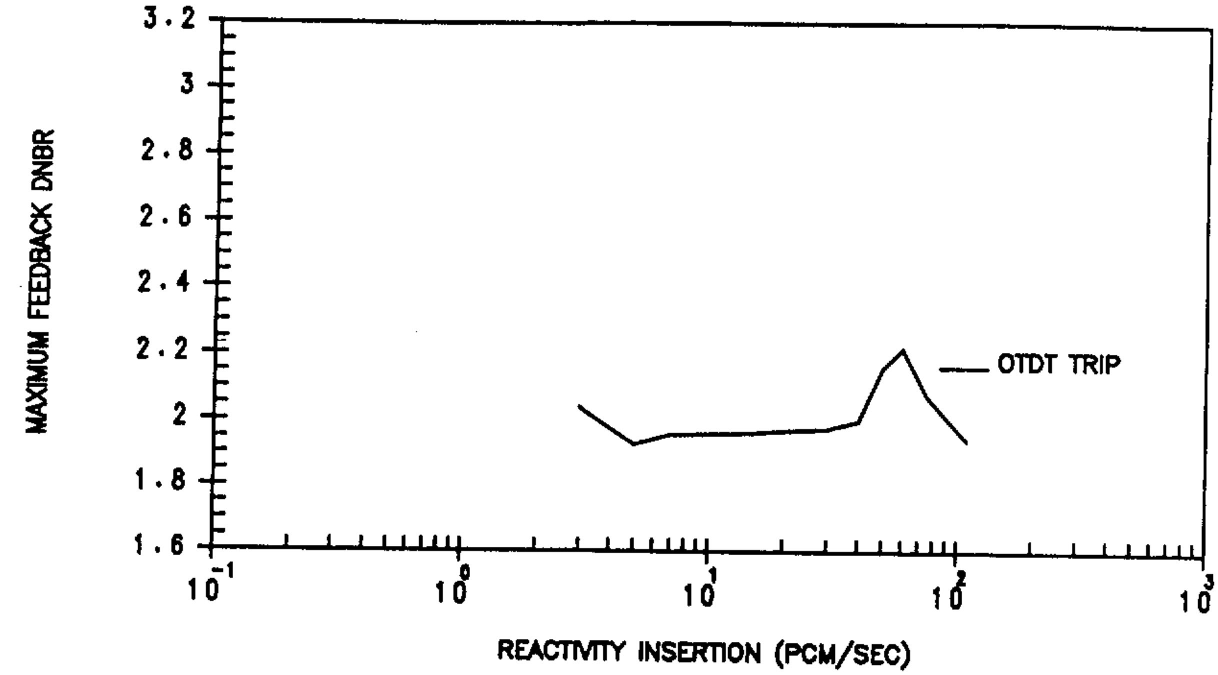
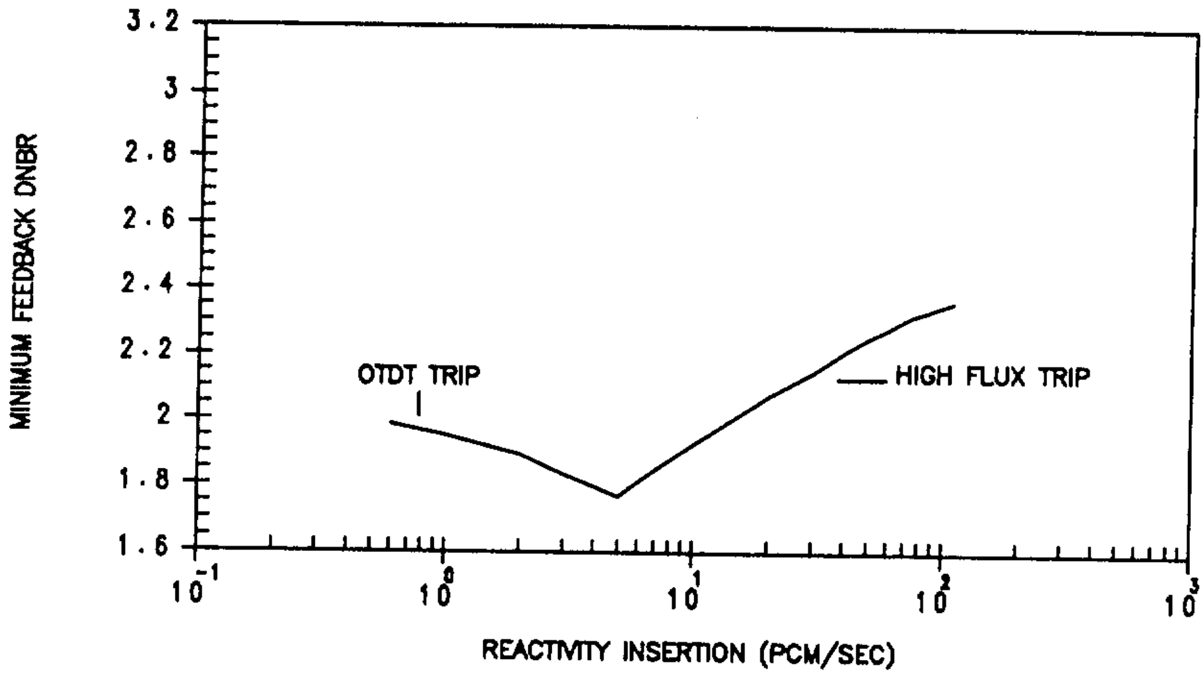
MINIMUM DNBR vs REACTIVITY INSERTION RATE FOR ROD WITHDRAWAL AT 80 PERCENT POWER

FIGURE 14.1.2-6





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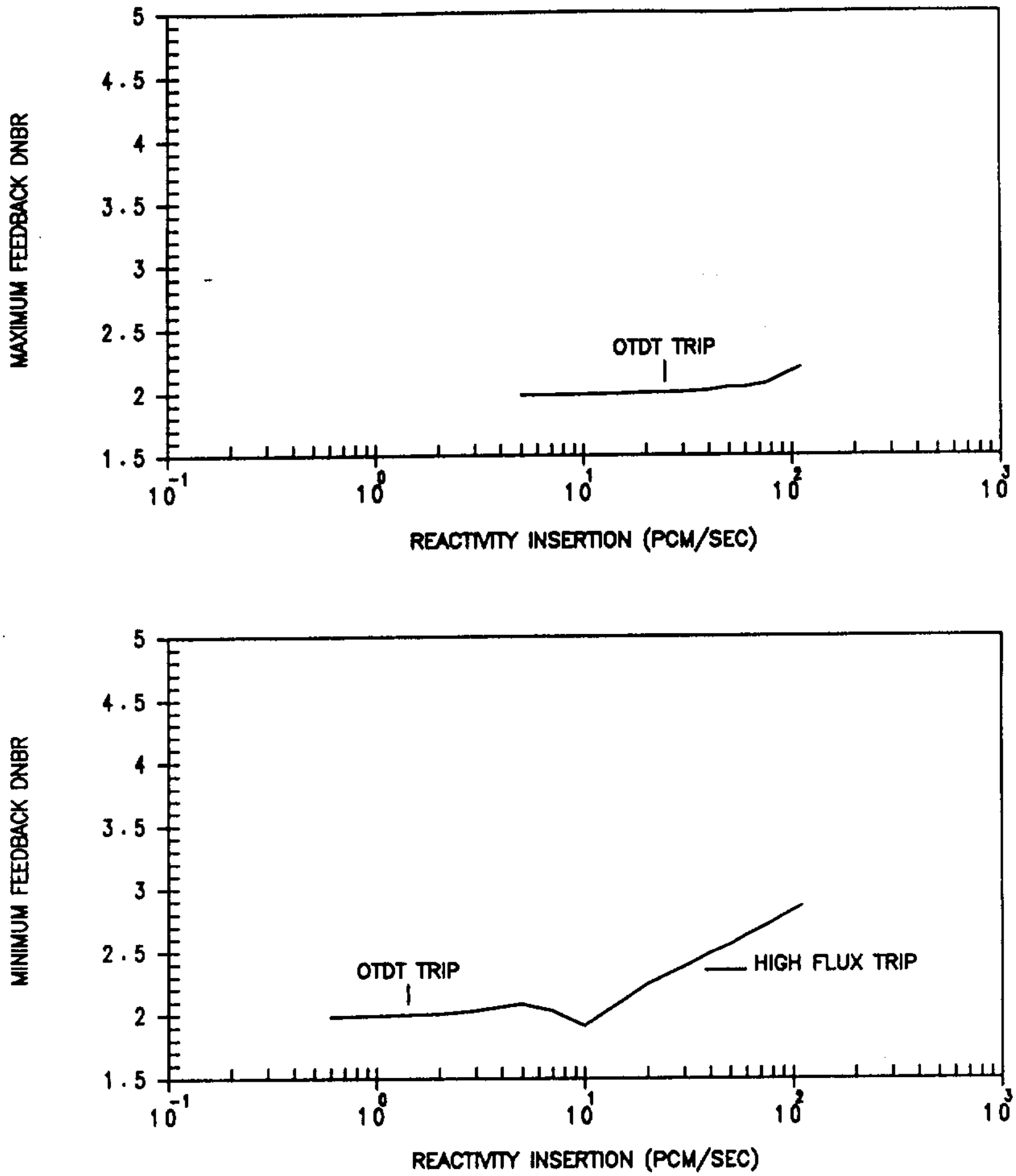
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

MINIMUM DNBR vs REACTIVITY INSERTION RATE FOR ROD WITHDRAWAL AT 60 PERCENT POWER

FIGURE 14.1.2-7



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FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

MINIMUM DNBR vs REACTIVITY INSERTION RATE FOR ROD WITHDRAWAL AT 10 PERCENT POWER

FIGURE 14.1.2-8

14.1.3 MALPOSITIONING OF THE PART LENGTH RODS

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14.1.4 ROD CLUSTER CONTROL ASSEMBLY (RCCA) DROP

14.1.4.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

A dropped RCCA event is a Condition II event that is assumed to be initiated by a single electrical or mechanical failure which causes any number and combination of RCCAs from the same group of a given bank to drop to the bottom of the core. The resulting negative reactivity insertion causes nuclear power to rapidly decrease. An increase in the hot channel factor may occur due to the skewed power distribution representative of a dropped RCCA configuration. Since this is a Condition II event, it must be shown that the DNB design basis is met for the combination of power, hot channel factor, and other system conditions which exist following the dropped RCCA(s).

If an RCCA drops into the core during power operation, it would be detected by either a rod bottom signal, by an excore detector, or both. The rod bottom signal device provides an indication signal for each RCCA. The other independent indication of a dropped RCCA is obtained by using the excore power range channel signals. This RCCA drop detection circuit is actuated upon sensing a rapid decrease in flux and is designed such that normal load variations do not cause it to be actuated.

Following a dropped RCCA event in manual rod control (or with automatic rod withdrawal defeated), the plant will establish a new equilibrium condition. The equilibrium process is monotonic, in that, there is no power overshoot without control bank withdrawal. The Turkey Point units have deleted the automatic rod withdrawal capability.

14.1.4.2 METHOD OF ANALYSIS

The transient following a dropped RCCA event is determined by a detailed digital simulation of the plant using the LOFTRAN code (Reference 1). The code simulates the neutron kinetics, RCS, pressurizer, pressurizer relief and safety valves, pressure spray, steam generator, and steam generator safety valves. The code computes pertinent plant variables including temperatures, pressures and power level. Since LOFTRAN employs a point neutron kinetics model, a dropped RCCA event is modeled as a negative reactivity insertion corresponding to the reactivity worth of the dropped RCCA(s) regardless of the actual configuration of the RCCA(s) that drop. The system transient is calculated by assuming a constant turbine load demand at the initial value (no turbine runback) and no bank withdrawal. A spectrum of dropped RCCA worths from 100 pcm to 1000 pcm was analyzed.

Statepoints are calculated and nuclear models are used to obtain a hot channel factor consistent with the primary system conditions and reactor power. By incorporating the primary conditions from the transient and the hot channel factor from the nuclear analysis, the DNB design basis is shown to be met. The transient response, nuclear peaking factor analysis, and DNB design basis confirmation are performed in accordance with the methodology described in WCAP-11394 (Reference 2).

Results

For a dropped RCCA event, with no automatic rod withdrawal, power may be reestablished by reactivity feedback.

Following a dropped RCCA(s) event, with no automatic rod withdrawal, the plant will establish a new equilibrium condition. Figures 14.1.4-1 and 14.1.4-2 show a typical transient response (specifically for the 100 pcm, 0 pcm/^{o}F case) to a dropped RCCA(s). Uncertainties in the initial conditions are included in the DNB evaluation as described in Reference 2. In all cases, the minimum DNBR remains greater than the limit value.

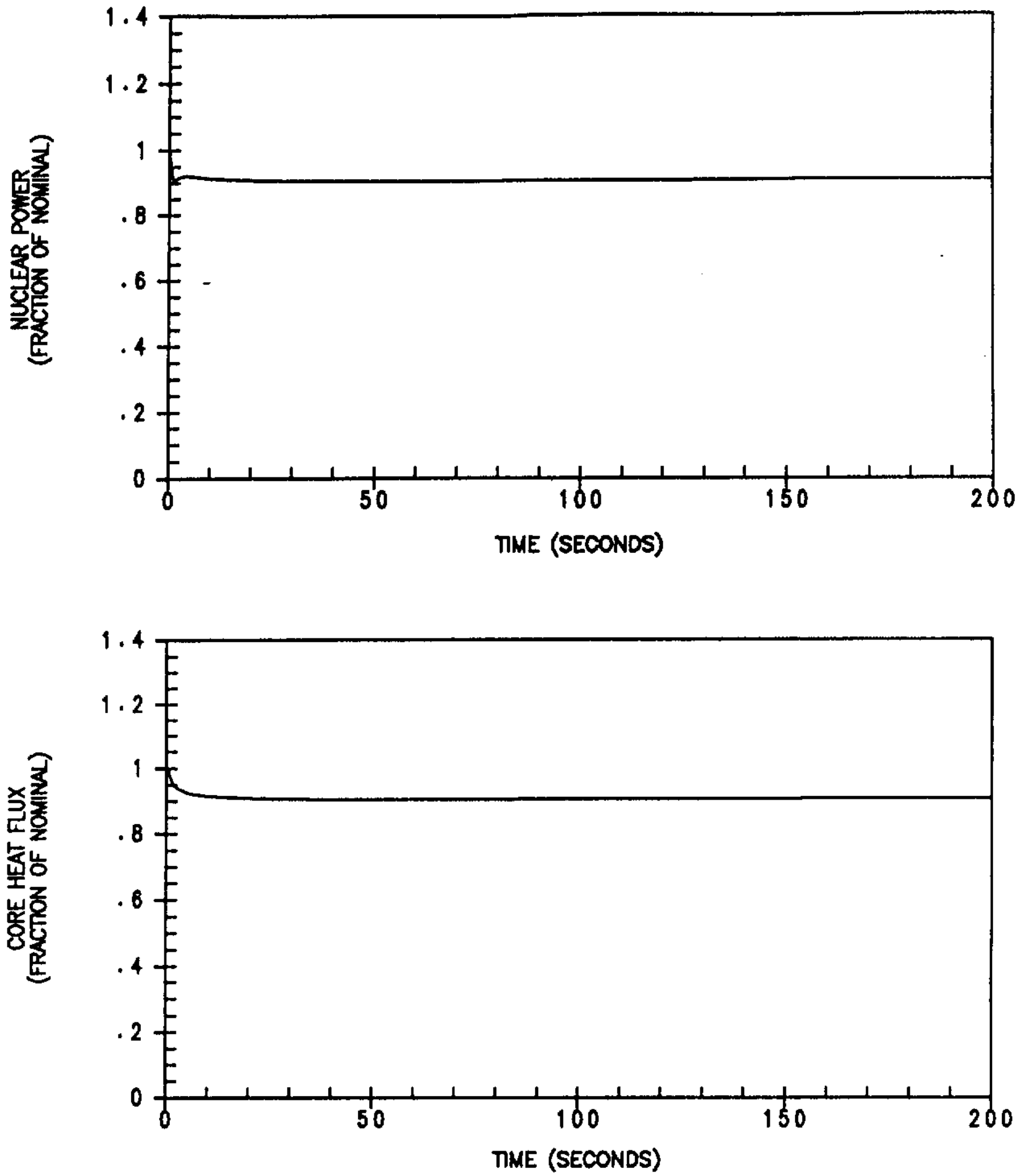
14.1.4.3 CONCLUSIONS

Following a dropped RCCA(s) event, without automatic rod withdrawal, the plant will return to a stabilized condition at less than or equal to the initial power. Results of the analysis show that a dropped RCCA event does not adversely affect the core, since the DNBR remains above the limit value for a range of dropped RCCA worths.

14.1.4.4 REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-Proprietary), Burnett, T.W.T., et al., "LOFTRAN Code Description," dated April 1984.

2. Westinghouse WCAP-11394 (Proprietary) and WCAP-11395 (Non-Proprietary), Hassler, R. L., et al.,"Methodology for the Analysis of the Dropped Rod Event," dated April 1987.



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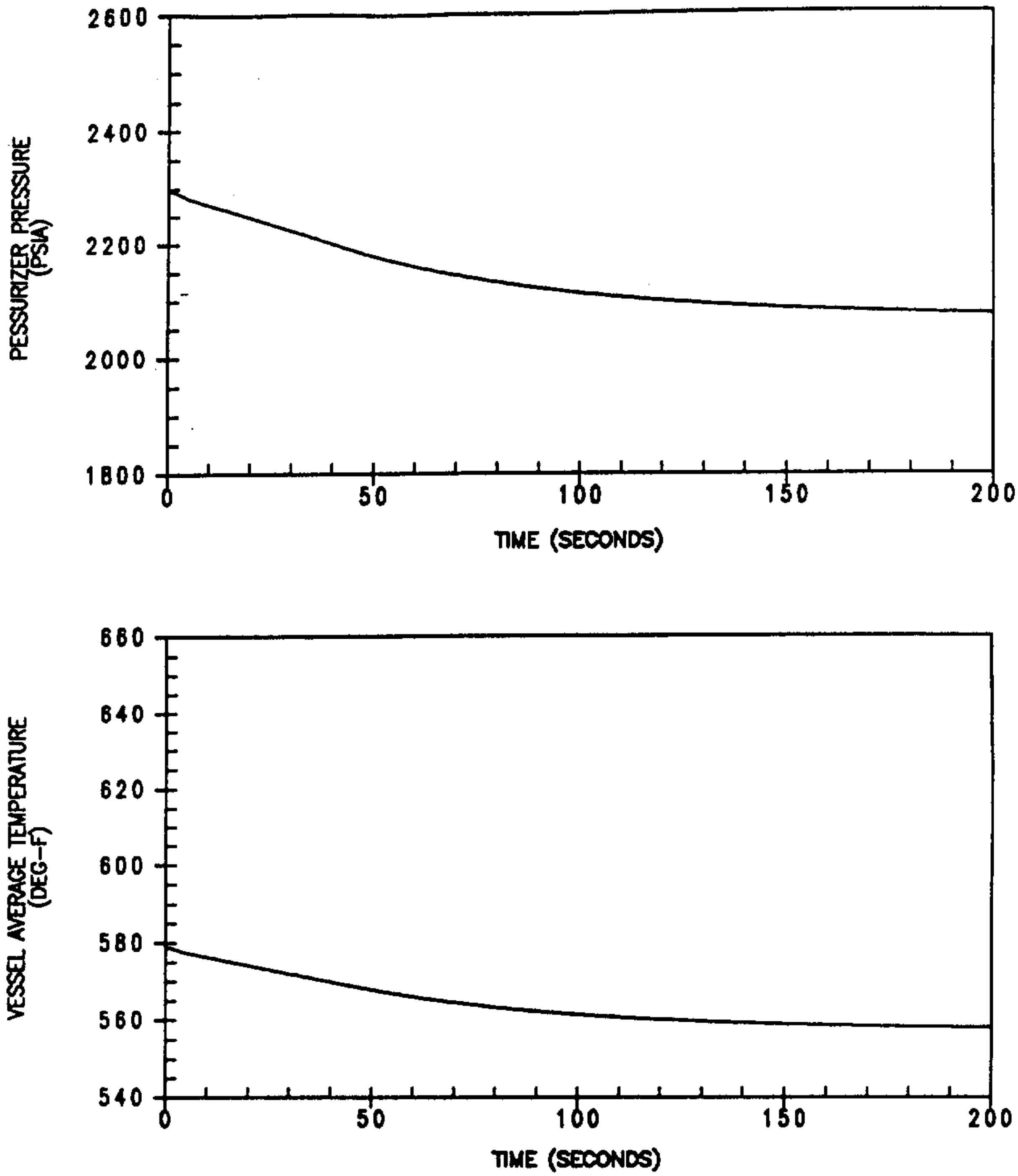
DROPPED RCCA
NUCLEAR POWER AND CORE HEAT FLUX

FIGURE 14.1.4-1



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY **TURKEY POINT PLANT UNITS 3 & 4**
DROPPED RCCA PRESSURIZER PRESSURE AND VESSEL AVERAGE TEMPERATURE **FIGURE 14.1.4-2**

14.1.5 CHEMICAL AND VOLUME CONTROL SYSTEM MALFUNCTION

Reactivity can be added to the core with the Chemical and Volume Control System by feeding primary water makeup into the Reactor Coolant System via the reactor makeup control system. The normal dilution procedures call for a limit on the rate and magnitude for any individual dilution, under administrative controls. Boron dilution is a manual operation. A boric acid blend system is provided to permit the operator to match the concentration of primary water makeup to that existing in the coolant at the time. The Chemical and Volume Control System is designed to limit, even under various postulated failure modes, the potential rate of dilution to a value which, after indication through alarms and instrumentation, provides the operator sufficient time to correct the situation in a safe and orderly manner.

There is only a single, common source of primary water makeup to the Reactor Coolant System from the primary water makeup system, and inadvertent dilution can be readily terminated by isolating this single source. The operation of the primary water makeup pumps which take suction from this tank provides the only supply of makeup water to the Reactor Coolant System. In order for makeup water to be added to the Reactor Coolant System, the charging pumps must be running, in addition to the primary water makeup pumps. One of the primary water makeup pumps is operating continuously.

The rate of addition of unborated water makeup to the Reactor Coolant System is limited to the capacity of the charging pumps, which is conservatively assumed to be 252 gpm for 3 pumps operating. Normally, only one charging pump is in service.

The boric acid from the boric acid tank is blended with the primary water makeup in the blender and the concentration is determined by the preset flow rates of boric acid and primary water makeup on the Reactor Makeup Control. Two separate operations are required. First, the operator must switch from the automatic makeup mode to the dilute mode. Second, the RCS Makeup Control Switch must be turned to the start position. Omitting either step would prevent dilution. This makes the possibility of inadvertent dilution very small.

Information on the status of the primary water makeup is continuously available to the operator. Lights are provided on the control board to indicate the operating status of pumps in the Chemical and Volume Control System. Alarms are actuated to warn the operator if boric acid or demineralized water flow rates deviate from preset values as a result of system malfunction.

Method of Analysis and Results

Plant operation during refueling, startup, and power operation is considered in this analysis. Table 14.1.5.1 contains the time sequence of events of the boron dilution analysis for refueling, startup and power operation. Table 14.1.5-2 presents the results of the boron dilution analysis for refueling, startup and power operation. Also included in this table are pertinent analysis assumptions. Perfect mixing is assumed in this analysis. This assumption results in a conservative rate of RCS boron dilution.

Dilution During Refueling

During refueling the following conditions exist:

a. One residual heat removal pump is running to ensure continuous mixing in the reactor vessel,

b. The dilute mode adds water in the Volume Control Tank where the primary water is mixed with letdown before it is pumped back into the system. The alternate dilute mode adds water in the volume control tank and to the charging pump suction header. Either mode can be assumed for the analysis,

c. The valves on the suction side of the charging pumps are adjusted for addition of concentrated boric acid solution,

d. The minimum boron concentration of the reactor coolant system is 1950 ppm, corresponding to a shutdown of at least 5 percent delta k/k with all control rods in; periodic sampling ensures that this concentration is maintained, and

e. Fuel which has been reloaded from the previous cycle provides a sufficient neutron source to assure the excore BF_3 detectors can monitor subcritical multiplication.

A minimum water volume in the Reactor Coolant System of 3204.6 ft^3 is considered. This corresponds to the volume necessary to fill the reactor vessel above the nozzles to ensure mixing via the residual heat removal loop. The maximum dilution flow of 252 gpm and uniform mixing are also considered.

The operator has prompt and definite indication of any boron dilution from the audible count rate instrumentation. High count rate is alarmed in the containment building and the control room. The count rate increase is proportional to the inverse multiplication factor.

For dilution during refueling, the boron concentration must be reduced from greater than 1950 ppm to approximately 1400 ppm before the reactor will go critical. This would take at least 31 minutes. This is ample time for the operator to recognize the high count rate signal and isolate the primary water makeup source.

Dilution During Startup

In this mode, the plant is being taken from one long-term mode of operation, Hot Standby, to another, Power. Typically, the plant is maintained in the Startup mode only for the purpose of startup testing at the beginning of each cycle. During this mode of operation, rod control is in manual. All normal actions required to change power level, either up or down, require operator initiation. Conditions assumed for the analysis are:

1. Dilution flow of the charging pumps is convervatively assumed to be 252 gpm.

2. A minimum RCS water volume of 7308.2 ft^3. This corresponds to the active RCS volume minus the pressurizer and its surge line.

3. The initial boron concentration is assumed to be 2000 ppm, which is a conservative maximum value for the critical concentration at the condition of hot zero power, rods at Rod Insertion Limits, and no Xenon.

4. The critical boron concentration following reactor trip is assumed to be 1800 ppm, corresponding to the hot zero power, all rods inserted (minus the most reactive RCCA), no Xenon condition. The 200 ppm change from the initial condition noted above is a conservative minimum value.

This mode of operation is a transitory operational mode in which the operator intentionally dilutes and withdraws control rods to take the plant critical. During this mode, the plant is in manual control with the operator required to maintain a high awareness of the plant status. For a normal approach to criticality, the operator must manually initiate a limited dilution and subsequently manually withdraw the control rods, a process that takes several hours. The Technical Specifications require that the operator determine the estimated critical position of the control rods prior to approaching criticality, thus assuring that the reactor does not go critical with the control rods below the insertion limits. Once critical, the power escalation must be sufficiently slow to allow the operator to manually block the source range reactor trip after receiving P-6 from the intermediate range (nominally at 10^5 cps). Too fast a power escalation (due to an unknown dilution) would result in reaching P-6 unexpectedly, leaving insufficient time to manually block the source range reactor trip. Failure to perform this manual action results in a reactor trip and immediate shutdown of the reactor.

However, in the event of an unplanned approach to criticality or dilution during power escalation while in the Startup mode, the plant status is such that minimal impact will result. The plant will slowly escalate in power until the power range high neutron flux low setpoint is reached and a reactor trip occurs. From initiation of the event, there is greater than 15 minutes available for operator action prior to return to criticality.

Dilution at Power

In this mode, the plant may be operated in either automatic or manual rod control. Conditions assumed for the analysis are:

1. Dilution flow of the charging pumps is conservatively assumed to be 252 gpm.

2. A minimum RCS water volume of 7308.2 ft^3. This corresponds to the active RCS volume minus the pressurizer and its surge line.

3. The initial boron concentration is assumed to be 1900 ppm, which is a conservative maximum value for the critical concentration at the condition of hot full power, rods at Rod Insertion Limits, and no Xenon.

4. The critical boron concentration following reactor trip is assumed to be 1550 ppm, corresponding to the hot zero power, all rods inserted (minus the most reactive RCCA), no Xenon condition. The 350 ppm change from the initial condition noted above is a conservative minimum value.

With the reactor in automatic rod control, the power and temperature increase from boron dilution results in insertion of the control rods and a decrease in the available shutdown margin. The rod insertion limit alarms (LOW and LOW-LOW settings) alert the operator at least 15 minutes prior to criticality. This is sufficient time to determine the cause of dilution, isolate the reactor water makeup source, and initiate boration before the available shutdown margin is lost.

With the reactor in manual control and no operator action taken to terminate the transient, the power and temperature rise will cause the reactor to reach the Overtemperature ΔT trip setpoint resulting in a reactor trip. The boron dilution transient in this case is essentially the equivalent to an uncontrolled RCCA bank withdrawal at power. The maximum reactivity insertion rate for a boron dilution is conservatively estimated to be 3.1 pcm/sec, which is within the range of insertion rates analyzed. Thus, the effects of dilution prior to reactor trip are bounded by the uncontrolled RCCA bank withdrawal at power analysis (Section 14.1.2). Following reactor trip, there is greater than 15 minutes (30.3 minutes calculated) prior to criticality. This is sufficient time for the operator to determine the cause of dilution, isolate the reactor water makeup source, and initiate boration before the available shutdown margin is lost.

Conclusions

Because of the procedures involved in the dilution process, an erroneous dilution is considered incredible. Nevertheless, if an unintentional dilution of boron in the reactor coolant system does occur, numerous alarms and indications are available to alert the operator to the condition. The maximum reactivity addition due to the dilution is slow enough to allow the operator to determine the cause of the addition and take corrective action before excessive shutdown margin is lost.

TABLE 14.1.5-1

SEQUENCE OF EVENTS OF THE BORON DILUTION ANALYSIS
FOR
REFUELING, STARTUP, AND POWER OPERATION

Uncontrolled Boron Dilution		Time (seconds)
1. Dilution During Refueling	Dilution begins	0.0
	Shutdown margin lost (if dilution continues)	>1800.0
2. Dilution During Startup	Power range-low setpoint reactor trip due to dilution	0.0 (1)
	Shutdown margin lost (if dilution continues)	>900
3. Dilution During Full-Power Operation		
a. Automatic Reactor Control	Operator receives low-low rod insertion limit alarm due to dilution	0.0
	Shutdown margin lost (if dilution continues)	>900
b. Manual Reactor Control	Reactor trip on OTDT due to dilution	0.0 (1)
	Shutdown margin is lost (if dilution continues)	>900

Notes:

1. Zero time corresponds to time at reactor trip, not start of the dilution event.

TABLE 14.1.5-2

SUMMARY OF BORON DILUTION ANALYSIS RESULTS
AND ANALYSIS ASSUMPTIONS

Mode of Operation	Dilution Flow Rate (gpm)	Active Volume (cubic feet)	Calculated Time to Criticality (minutes)
Power Operation			
Auto Rod Control	252	7308.2	31.5
Manual Rod Control	252	7308.2	30.3
Startup	252	7308.2	17.0
Refueling	252	3204.6	31.0

OTHER IMPORTANT ANALYSIS ASSUMPTIONS

Mode of Operation	Assumed Initial Boron Conc. (ppm)	Assumed Critical Boron Conc. (ppm)	Average Core Coolant Temp. (^oF)
Power Operation			
Auto Rod Control	1900	1550	583.2
Manual Rod Control	1900	1550	583.2
Startup	2000	1800	554.5
Refueling	1950	1400	140.0

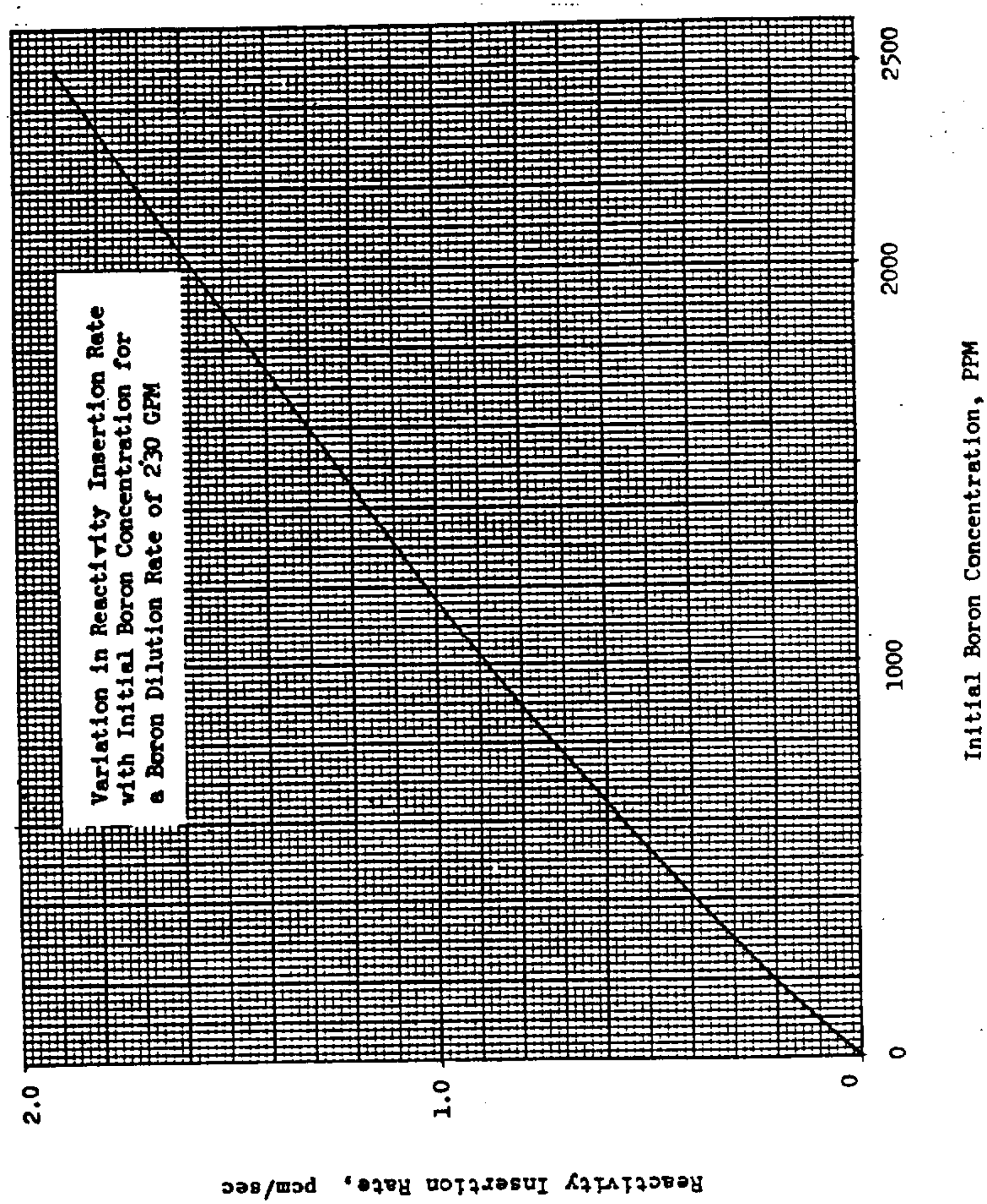


FIGURE 14.1.5-1

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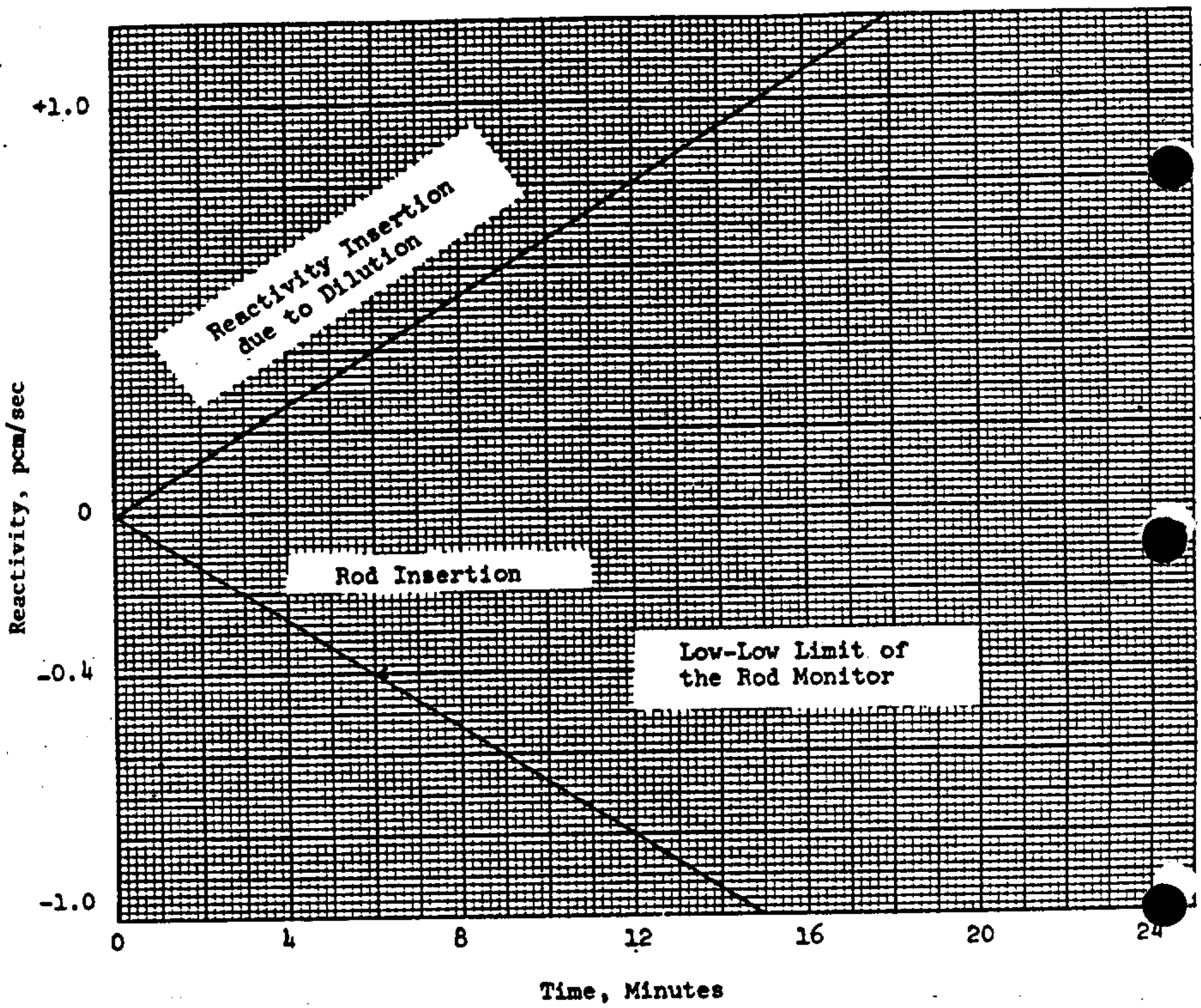


Reactivity Insertion
due to Dilution

FIGURE 14.1.5-2

14.1.6 START-UP OF AN INACTIVE REACTOR COOLANT LOOP

The current Turkey Point plant technical specifications (Reference 1) preclude plant operation with one or more reactor coolant loops inactive. The startup of an inactive reactor loop event was originally included in the Updated FSAR when the potential for operation with a loop out of service was allowed under plant technical specifications. Based on the current plant technical specifications which prohibit plant startup and power operation (Modes 1 and 2) with one or more loops out of service, this event was removed from the Turkey Point licensing basis as part of the plant thermal uprate evaluation (Reference 2).

REFERENCES

1. Turkey Point Technical Specifications, Section 3/4.4.1, "Reactor Coolant Loops and Coolant Circulation," License Amendment No. 137/132, effective August 28, 1991.

2. Westinghouse WCAP-14276 (Non-Propriety), "Turkey Point Units 3 and 4 - Uprating Licensing Report," Revision 1, dated December 1995.

14.1.7 EXCESS FEEDWATER FLOW AND REDUCTION IN FEEDWATER ENTHALPY INCIDENT

The reduction in feedwater enthalpy is another means of increasing core power above full power. Such increases are attenuated by the thermal capacity in the secondary plant and in the Reactor Coolant System. The overpower-overtemperature protection (high neutron flux, overtemperature ΔT and overpower ΔT trips) prevents any power increase which could lead to a DNBR less than the limit value.

An example of excessive feedwater flow would be a full opening of a feedwater control valve due to a feedwater control system malfunction or an operator error. At power, this excess flow causes a greater load demand on the RCS due to increased subcooling in the steam generator. With the plant at no-load conditions, the addition of cold feedwater may cause a decrease in RCS temperature and thus a reactivity insertion due to the effects of the negative moderator temperature coefficient of reactivity. Continuous excessive feedwater addition is prevented by the steam generator high-high level signal.

A second example of excess heat removal by the feedwater system is the transient associated with the accidental opening of the low pressure feedwater heater bypass valve which diverts flow around the low pressure feedwater heaters. The function of this valve is to maintain net positive suction head on the main feedwater pump in the event that the heater drain pump flow is lost, e.g., during a large sudden load decrease. In the event of accidental opening, there is a sudden reduction in inlet feedwater temperature to the steam generators. The increased subcooling will create the greater load demand on the primary system which can lead to a reactor trip.

Method of Analysis

This accident is analyzed using the LOFTRAN Code (Reference 1). The code simulates the neutron kinetics, reactor coolant system, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, steam generator safety valves, and feedwater system. The code computes pertinent plant variables including temperatures, pressures, and power level.

The Reactor Coolant System is analyzed to demonstrate acceptable consequences in the event of a feedwater system malfunction. Feedwater temperature reduction due to low-pressure heater bypass valve actuation in conjunction with an inadvertent trip of the heater drain pump in considered. Additionally, excessive feedwater addition due to a control system malfunction or operator error that allows a feedwater control valve to open fully is considered.

Four excessive feedwater flow cases are analyzed as follows:

a. Accidental opening of one feedwater control valve with the reactor just critical at zero-load conditions with both manual and automatic rod control, assuming a conservatively large moderator density coefficient characteristic of EOL conditions.

b. Accidental opening of one feedwater control valve with the reactor at full power assuming automatic and manual rod control, also assuming a conservatively large moderator density coefficient characteristic of EOL conditions.

The reactivity insertion rate following a feedwater system malfunction is calculated with the following assumptions:

a. This accident is analyzed with the Revised Thermal Design Procedure as described in WCAP-11397-P-A (Reference 2). Therefore, initial reactor power, pressure, and RCS temperatures are assumed to be at their nominal values. Uncertainties in initial conditions are included in the limit DNBR calculated using the methodology described in Reference 2.

b. For the feedwater control valve accident at full power, one feedwater control valve is assumed to malfunction resulting in a step increase to 200% of nominal feedwater flow to one steam generator.

c. For the feedwater control valve accident at zero-load condition, a feedwater valve malfunction occurs that results in an increase of flow to one steam generator from zero to 200% of the nominal full-load value for one steam generator.

d. For the zero-load condition, feedwater temperature is at a conservatively low value of 32°F.

e. The initial water level in all the steam generators is at a conservatively low level.

f. No credit is taken for the heat capacity of the RCS and steam generator thick metal in attenuating the resulting plant cooldown.

g. No credit is taken for the heat capacity of the steam and water in the unaffected steam generators.

h. The feedwater flow resulting from a fully open control valve is terminated by the steam generator high-high water level signal that closes all feedwater main control and feedwater control-bypass valves, and trips the main feedwater pumps and turbine generator.

   Note that the steam generator overfill protection function, utilizing the Steam Generator high-high water level, is not part of the Engineered Safety Features Actuation System (ESFAS), but was added to the ESFAS Technical Specification tables without modification of the existing design. This function was specifically developed to meet commitments to the NRC criteria contained in Generic Letter 89-19, dated September 20, 1989. Although the steam generator overfill protection feature uses much of the same instrumentation as the steam generator low-low trip (reactor trip circuitry), portions of the circuitry for steam generator high-high level overfill protection may not meet all the criteria which apply to ESFAS functions. This is because the steam generator high-high level function was not originally designed to be part of the ESFAS system.

i. The 1.0 second time lag in the control logic of the turbine pressure signal to the automatic rod control system is included (Reference 3).

Normal reactor control systems and engineered safety systems (e.g., Safety Injection) are not required to function. The reactor protection system may actuate to trip the reactor due to an overpower condition or a turbine trip. No single active failure in any system or component required for mitigation will adversely affect the consequences of this event.

Results

Opening of a low-pressure heater bypass valve and trip of the heater drain pumps causes a reduction in the feedwater temperature which increases the thermal load on the primary system. The reduction in the feedwater temperature is less than 60°F, resulting in an increase in the heat load on the primary system of less than 10 percent of full power. The increased thermal load due to the opening of the low-pressure heater bypass valve would result in a transient very similar (but of reduced magnitude) to the Excessive Load Increase incident presented in Section 14.1.8. Thus, the results of this event are bounded by the Excessive Load Increase event and, therefore, not presented here.

In the case of an accidental full opening of one feedwater control valve with the reactor at zero power (assumed to be 0.01% power) and the above-mentioned assumptions, the maximum reactivity insertion rate is conservatively calculated to be less than 100 pcm/sec (1 pcm=10^{-5} $\Delta k/k$). A DNB analysis was performed to demonstrate that the DNB design basis is met. A reactivity insertion rate of 100 pcm/sec was assumed in order to bound the value calculated for the zero power feedwater malfunction analysis. The method of analysis used is the same as described in Section 14.1.1, Uncontrolled RCCA Bank Withdrawal from a Subcritical Condition, except that the analysis assumed that all three reactor coolant pumps are in operation as required by the plant Technical Specifications in operating Mode 2. Although the zero power feedwater malfunction reactivity insertion rate is calculated assuming reactivity parameters representative of EOL core conditions, the DNB analysis was conservatively performed at BOL conditions. The results of the DNB analysis show that the DNBR remains above the safety analysis limit value. It should be noted that for the case with the unit just critical at no-load, the reactor may be tripped by the power range high neutron flux trip (low setting) set at approximately 25%.

The full-power conditions combined with EOL maximum reactivity feedback yield the largest power increase for this event. Both automatic and manual rod control are assumed at HFP. However, the results of these transients are very similar. The rod control system is not required to function for this event. A turbine trip, which results in a reactor trip, is actuated when the steam generator water level in the affected steam generator reaches the high-high level setpoint.

For all cases of excessive feedwater flow, continuous addition of cold feedwater is prevented by automatic closure of all feedwater control valves, closure of all feedwater bypass valves, a trip of the feedwater pumps, and a turbine trip on high-high steam generator water level. In addition, the feedwater pump discharge valves will automatically close upon receipt of the feedwater pump trip signal.

Following turbine trip, the reactor will automatically be tripped, either directly due to the turbine trip or due to one of the reactor trip signals discussed in Section 14.1.10 (Loss of External Electrical Load).

Transient results (see Figures 14.1.7-1 through 14.1.7-3) show the core heat flux, pressurizer pressure, core average temperature, and DNBR, as well as the increase in nuclear power and loop ΔT associated with the increased thermal load on the reactor. Steam generator water level rises until the feedwater addition is terminated as a result of the high-high steam generator water level signal. The DNBR does not drop below the limit value at any time.

Since the power level rises during this event, the fuel temperature will also rise until the reactor trip occurs. The core heat flux lags behind the neutron flux due to the fuel rod thermal time constant and, as a result, the peak core heat flux value does not exceed 118% of nominal. Thus, the peak fuel melting temperature will remain well below the fuel melting point.

The calculated sequence of events is shown in Table 14.1.7-1. The transient results show that the DNBR does not fall below the limit value at any time during the feedwater flow increase transient; thus, the ability of the primary coolant to remove heat from the fuel rods is not reduced. Therefore, the fuel cladding temperature does not rise significantly above its initial value during the transient.

Conclusion

The decrease in feedwater temperature transient due to an opening of the low-pressure heater bypass valve is less severe than the excessive load increase event (see Section 14.1.8). Based on the results presented in Section 14.1.8, the applicable acceptance criteria for the decrease in feedwater temperature event have been met.

For the excessive feedwater addition at power transient, the results show that the DNB ratios encountered are above the limit value; hence, no fuel damage is predicted. The DNB ratios for the rods in manual and automatic cases are almost identical, with the limiting DNBR value obtained for the rods in manual case. Additionally, an analysis at zero power demonstrates that the minimum DNBR remains above the safety analysis limit for a maximum reactivity insertion rate corresponding to an excessive feedwater addition at no-load conditions. The limiting minimum DNBR for all four cases was at zero power.

REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-proprietary), Burnet, T. W. T., et. al., "LOFTRAN Code Description," dated April 1984.

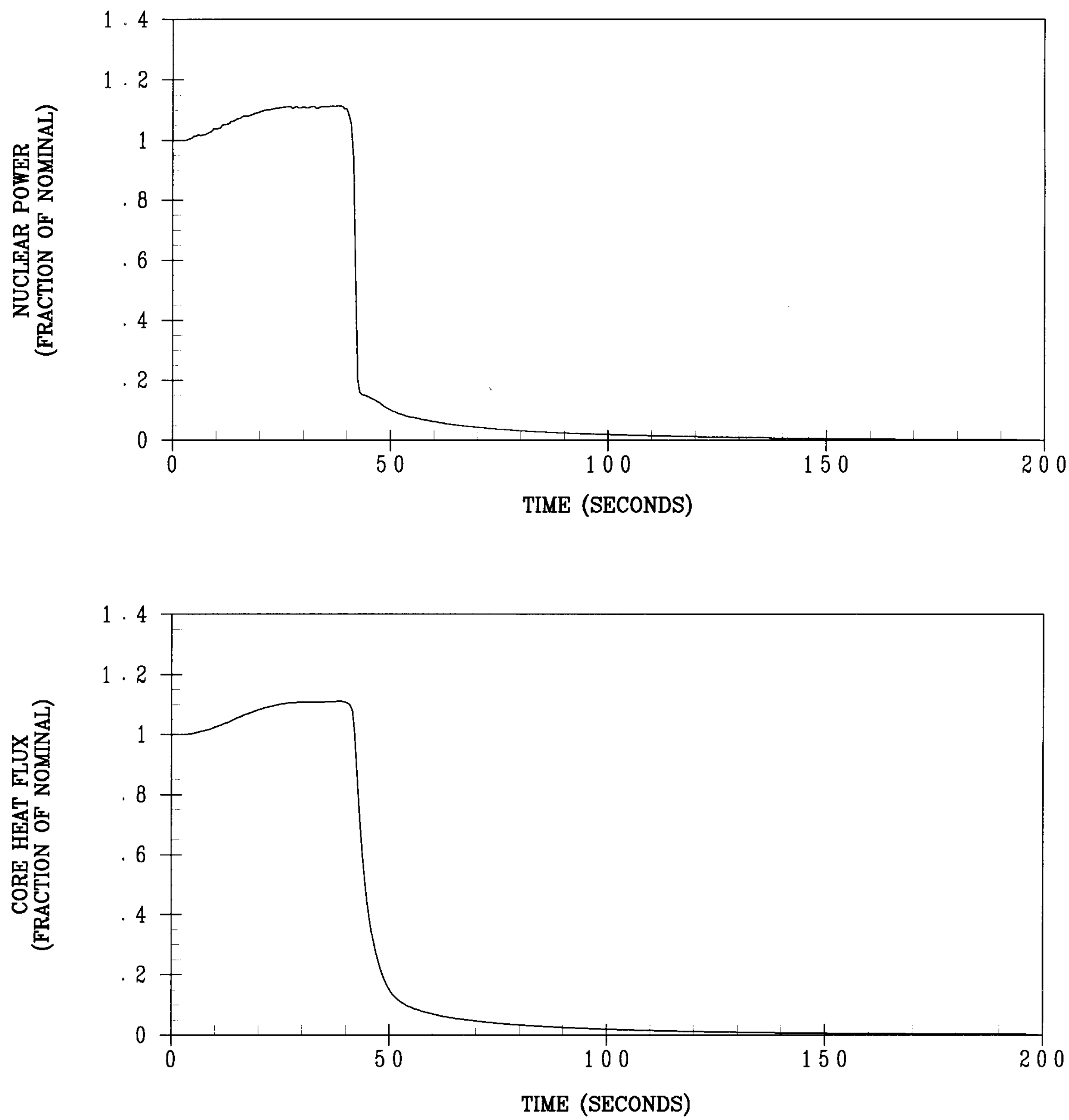
2. Westinghouse WCAP-11397-P-A, Friedland, A. J. and Ray, S., "Revised Thermal Design Procedure," dated April 1989.

3. "Florida Power & Light Co., Turkey Point Units 3 & 4, FSAR and AABD Updates Resulting from Reanalysis of Non-LOCA Transients Impacted by an increased Time Delay on the Turbine Pressure Signal," J. J. Deblasio to J. Perryman, FPLN-97-0107, NSD-SAE-ESI-97-337, May 30, 1997.

TABLE 14.1.7-1

TIME SEQUENCE OF EVENTS
FOR EXCESSIVE FEEDWATER FLOW AT FULL POWER EVENT
WITH AUTOMATIC ROD CONTROL

Event	Time (seconds)
One main feedwater control valve fails fully open	0.0
High-High Steam Generator water level signal generated	35.0
Turbine trip occurs due to High-High Steam Generator water level signal	37.5
Minimum DNBR occurs	38.0
Reactor trip on turbine trip occurs	39.5
Feedwater isolation valves close due to High-High Steam Generator water level signal	44.0

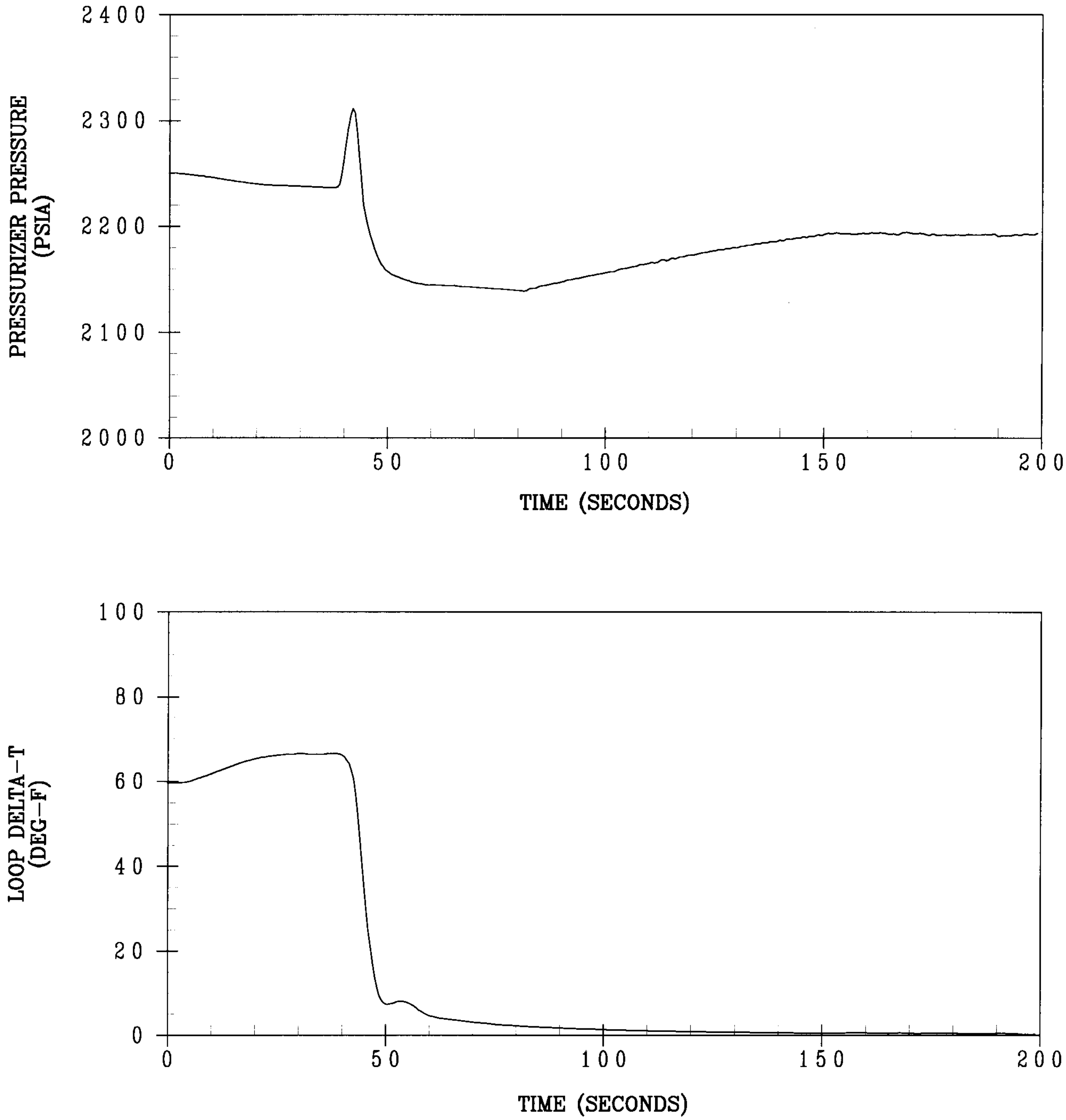


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

FEEDWATER CONTROL VALVE MALFUNCTION - NUCLEAR POWER AND CORE HEAT FLUX VERSUS TIME

FIGURE 14.1.7-1

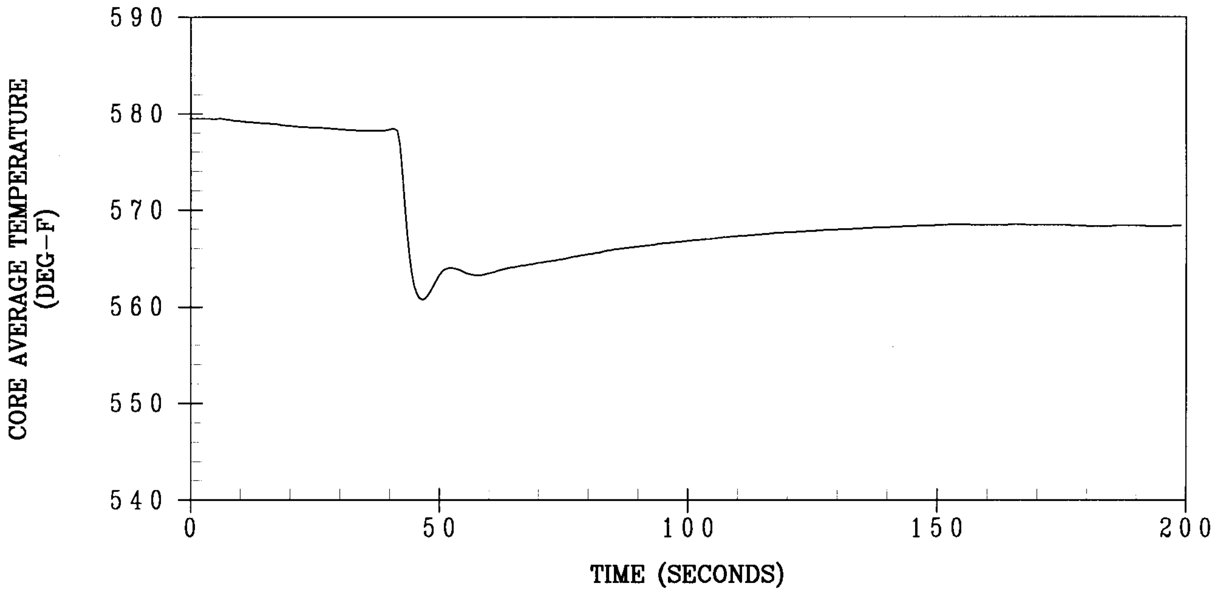


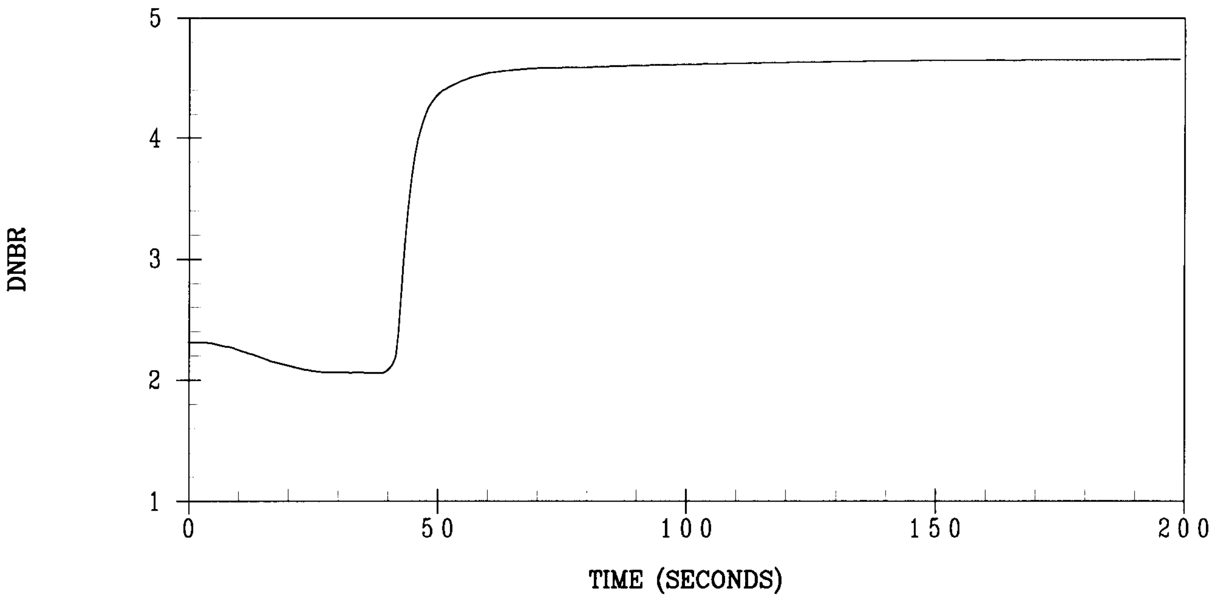
REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

FEEDWATER CONTROL VALVE
MALFUNCTION - PRESSURIZER
PRESSURE
AND LOOP ΔT VERSUS TIME

FIGURE 14.1.7-2





REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

FEEDWATER CONTROL VALVE MALFUNCTION - CORE AVERAGE TEMPERATURE AND DNBR VS TIME

FIGURE 14.1.7-3

14.1.8 EXCESSIVE LOAD INCREASE INCIDENT

An excessive load increase incident is defined as a rapid increase in steam generator steam flow causing a power mismatch between the reactor core power and the steam generator load demand. The reactor control system is designed to accommodate a 10 percent step load increase and a 5 percent per minute ramp load increase without a reactor trip in the range of 15 to 100 percent full power. Any loading rate in excess of these values may cause a reactor trip actuated by the protection system. If the load increase exceeds the capability of the reactor control system, the transient is terminated in sufficient time to prevent DNBR from going below the limit value since the core is protected by a combination of the nuclear overpower trip and the overpower-overtemperature trips, as discussed in Section 7. An excessive load increase incident could result from either an administrative violation such as excessive loading by the operator or an equipment malfunction such as steam bypass control or turbine speed control.

The load demand is limited to 100% load by the turbine load limiter.

During power operation, steam bypass to the condenser is controlled by signals of reactor coolant conditions, i.e., abnormally high reactor coolant temperature indicates a need for steam bypass. A single controller malfunction does not cause steam bypass because an interlock is provided which blocks the control signal to the valves unless a sudden large turbine load decrease has occurred. In addition, the reference temperature and loss of load signals are developed by independent sensors.

Regardless of the rate of load increase, the reactor protection system will trip the reactor in time to prevent DNBR from going below the limit value. Increases in steam load to more than design flow are analyzed as steam line ruptures in Section 14.2.5.

Protection against an excessive load increase accident is provided by the following reactor protection system signals.

a. Overtemperature ΔT
b. Power range high neutron flux
c. Low pressurizer pressure

Method of Analysis

This accident is analyzed using the LOFTRAN Code (Reference 1). The code simulates the neutron kinetics, reactor coolant system including natural circulation, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, main steam safety valves, and auxiliary feedwater system. The code computes pertinent plant variables including DNBR, temperatures, pressures, and power level.

Four cases are analyzed to demonstrate plant behavior following a 10-percent step load increase from rated load. These cases are as follows:

1. Reactor control in manual with minimum moderator reactivity feedback (BOL).

2. Reactor control in manual with maximum moderator reactivity feedback (EOL).

3. Reactor control in automatic with minimum moderator reactivity feedback (BOL).

4. Reactor control in automatic with maximum moderator reactivity feedback (EOL).

For the minimum moderator feedback cases (BOL), the core has the least negative moderator temperature coefficient of reactivity and the least negative Doppler only power coefficient curve; therefore, reductions in coolant temperature will have the least impact on core power. Since a positive moderator temperature coefficient would provide a transient benefit, a zero moderator temperature coefficient was assumed in the minimum feedback cases. For the (EOL) maximum moderator feedback cases, the moderator temperature coefficient of reactivity has its highest absolute value and the most negative Doppler only power coefficient curve. This results in the largest amount of reactivity feedback due to changes in coolant temperature.

A 10-percent step increase in steam demand is assumed, and all cases are studied without credit being taken for pressurizer heaters.

Normal reactor control systems and engineered safety systems are not required to function. The reactor protection system is assumed to be operable; however, reactor trip is not encountered for the cases analyzed. No single active failure will prevent the reactor protection system from performing its intended function.

Results

Figures 14.1.8-1 through 14.1.8-4 illustrate the transient with the reactor in the manual rod control mode. As expected, for the (BOL) minimum moderator feedback case there is a slight power increase, and the average core temperature shows a decrease. This results in a departure from nucleate boiling ratio (DNBR) which increases (after a slight decrease) above its initial value. For the (EOL) maximum moderator feedback, manually controlled case, there is a larger increase in reactor power due to the moderator feedback. A reduction in DNBR is experienced, but DNBR remains above the limit value.

Figures 14.1.8-5 through 14.1.8-8 illustrate the transient assuming the reactor is in the automatic rod control mode and no reactor trip signals occur. Both the BOL and EOL cases show that core power increases. The BOL case shows the core average temperature stabilizes, due to the action of the control rod system, at a slightly higher value from the initial temperature. The EOL case shows that after a slight increase the core average temperature stabilizes, again due to the action of the rod control system, at a value approximately equal to the initial temperature. For both of these cases, the minimum DNBR remains above the limit value.

The calculated sequence of events for the excessive load increase incident is shown in Table 14.1.8-1. Note that a reactor trip signal was not generated for any of the four cases.

Conclusions

The analysis presented above shows that for a 10-percent step load increase, the DNBR remains above the limit value. The plant rapidly reaches a stabilized condition following the load increase.

REFERENCES

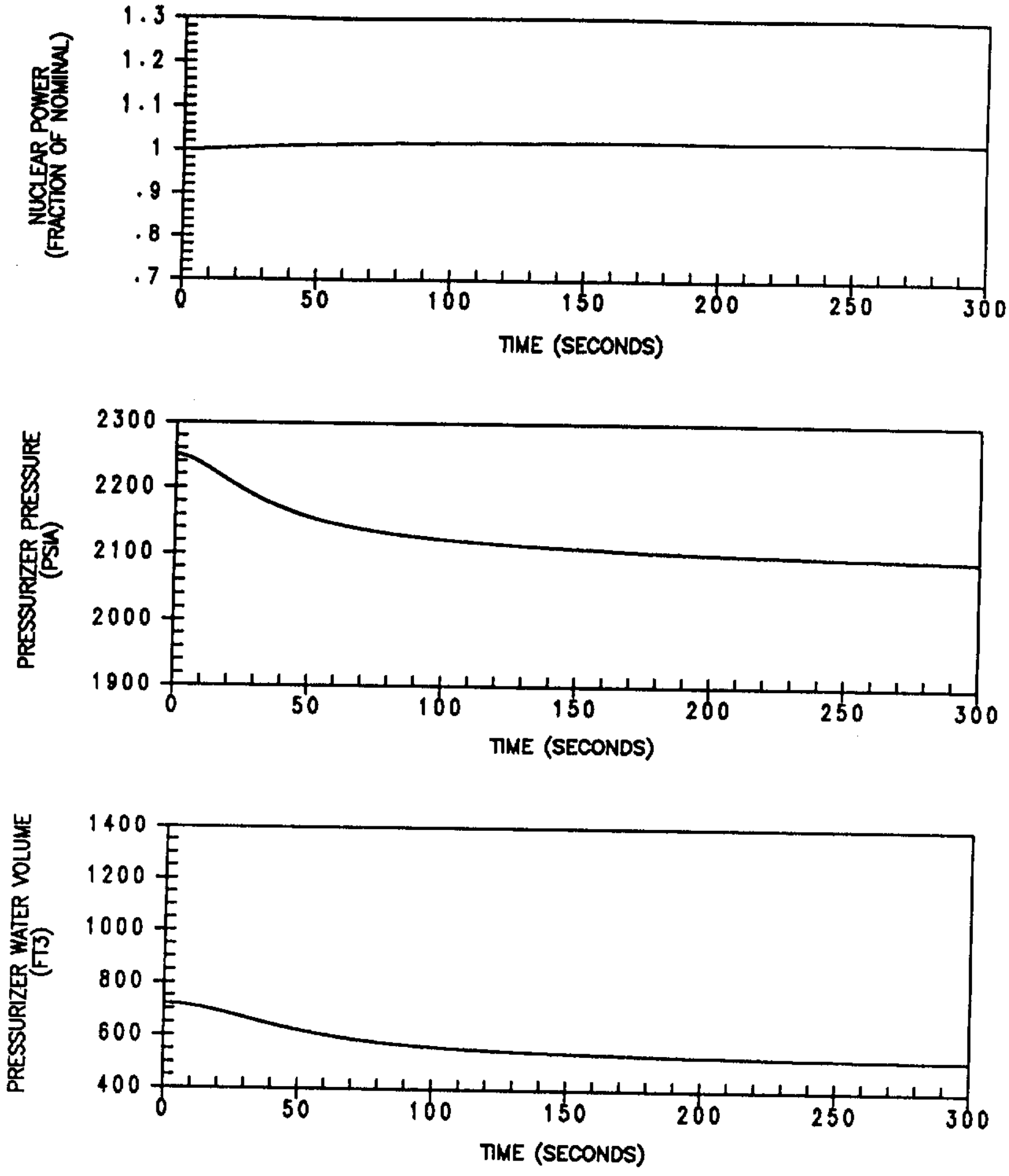
1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-proprietary), Burnett, T. W. T., et al.,"LOFTRAN Code Description," dated April 1984.

2. Westinghouse WCAP-11397-P-A, Friedland, A. J., and Ray, S.,"Revised Thermal Design Procedure," dated April 1989.

TABLE 14.1.8-1

TIME SEQUENCE OF EVENTS
FOR
EXCESSIVE LOAD INCREASE INCIDENT

Case	Event	Time (seconds)
1. Manual Reactor control (minimum moderator feedback)	10-percent step load increase	0.0
	Equilibrium conditions reached (approximate time only)	170.0
2. Manual reactor control (maximum moderator feedback)	10-percent step load increase	0.0
	Equilibrium conditions reached (approximate time only)	90.0
3. Automatic reactor control (minimum moderator feedback)	10-percent step load increase	0.0
	Equilibrium conditions reached (approximate time only)	140.0
4. Automatic reactor control (maximum moderator feedback)	10-percent step load increase	0.0
	Equilibrium conditions reached (approximate time only)	40.0



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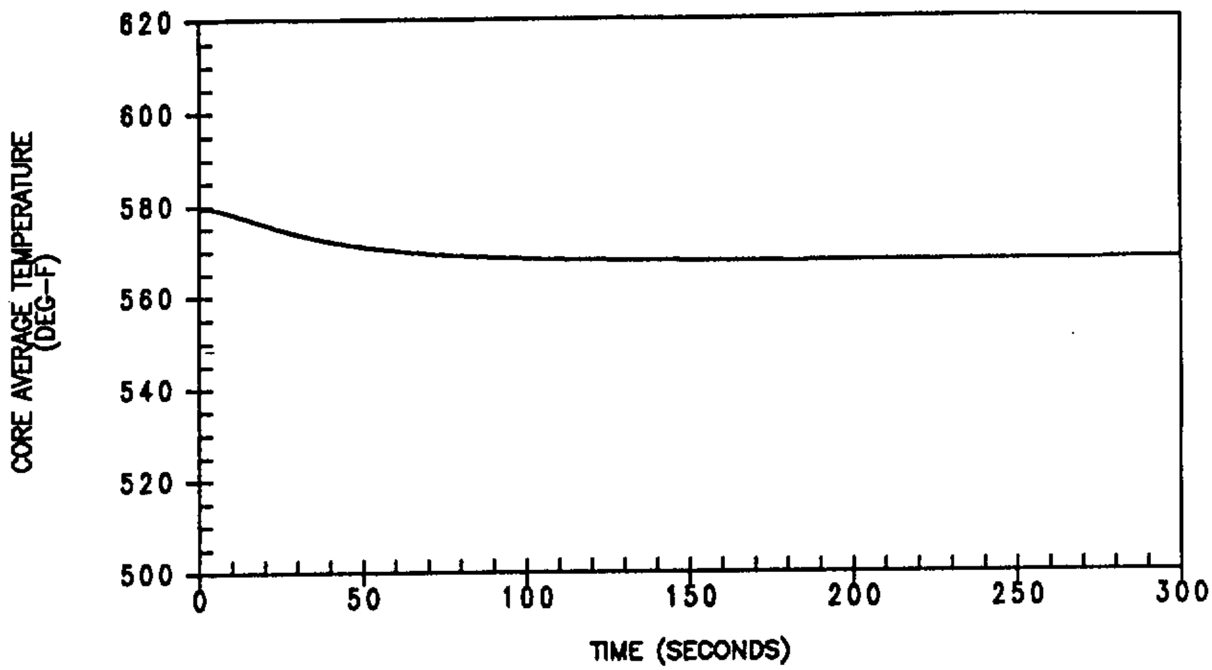
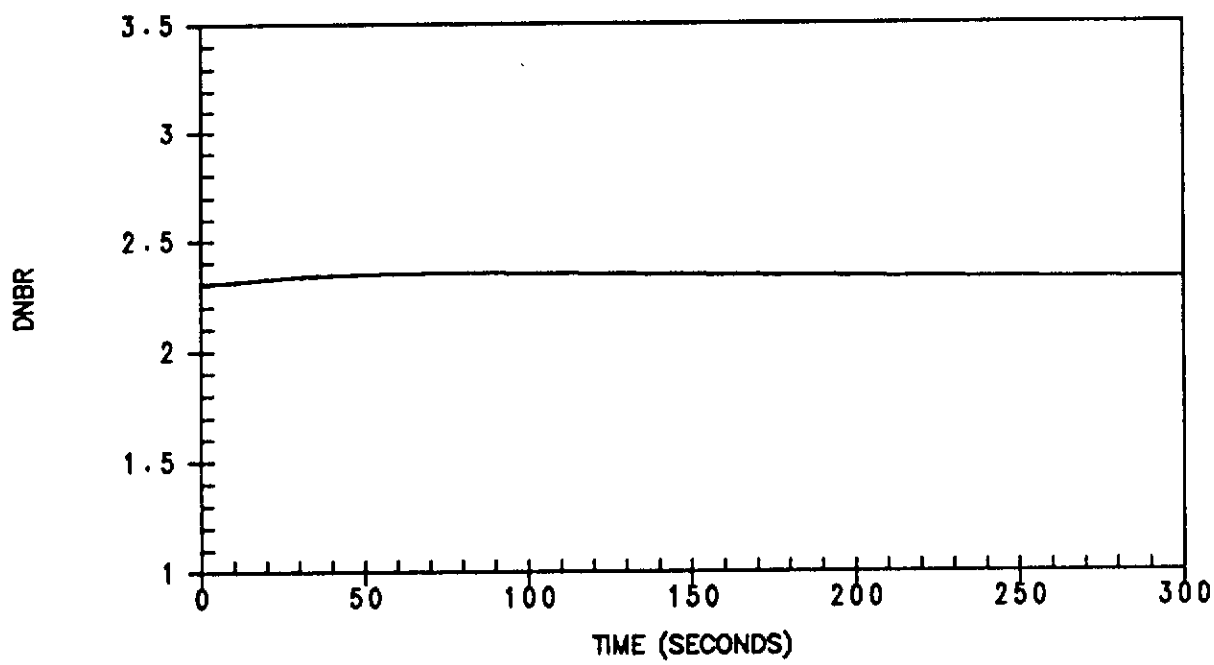
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

10% STEP LOAD INCREASE
MINIMUM MODERATOR FEEDBACK
MANUAL REACTOR CONTROL
FIGURE 14.1.8-1





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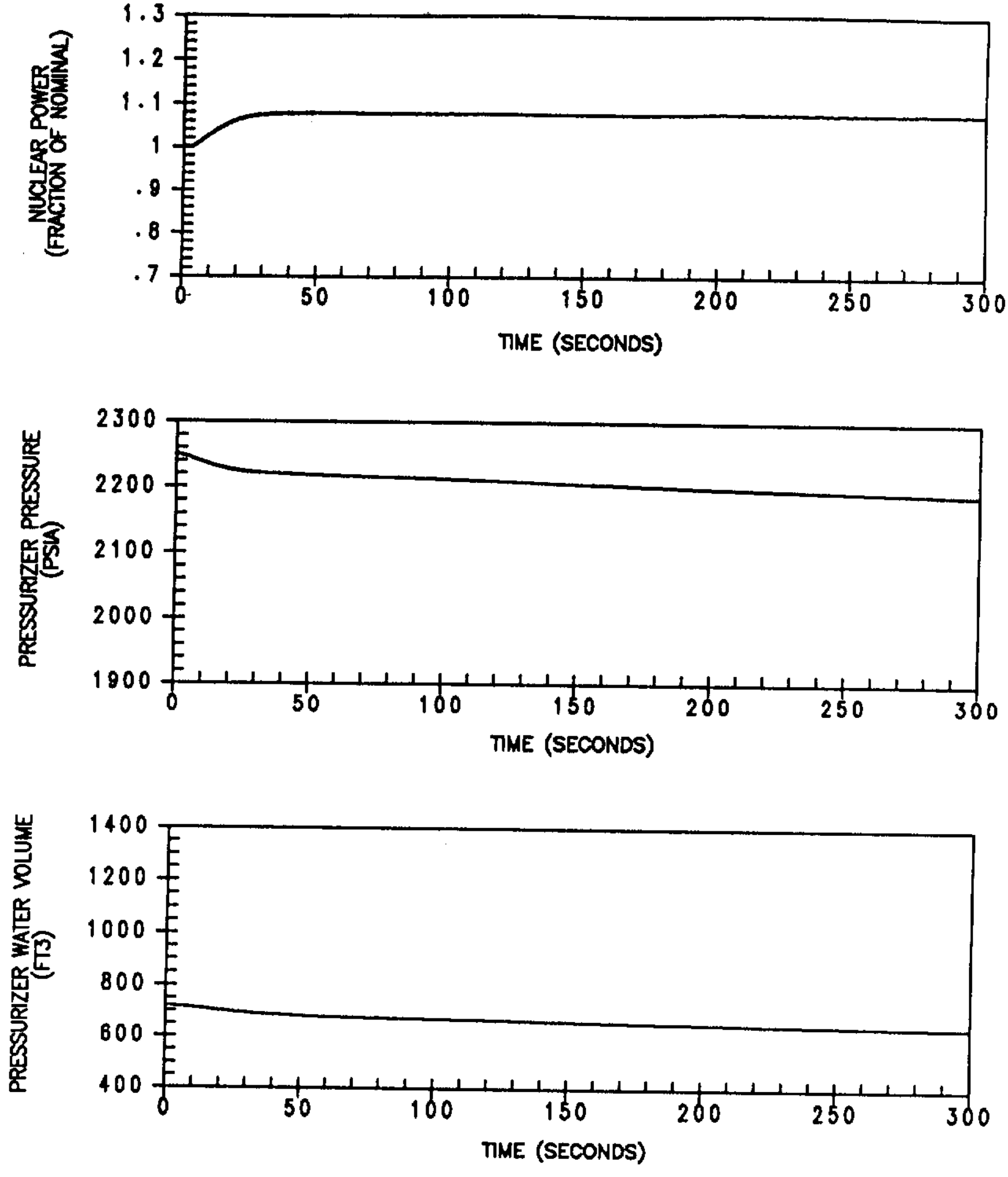
TURKEY POINT PLANT UNITS 3 & 4

10% STEP LOAD INCREASE
MINIMUM MODERATOR FEEDBACK
MANUAL REACTOR CONTROL
FIGURE 14.1.8-2



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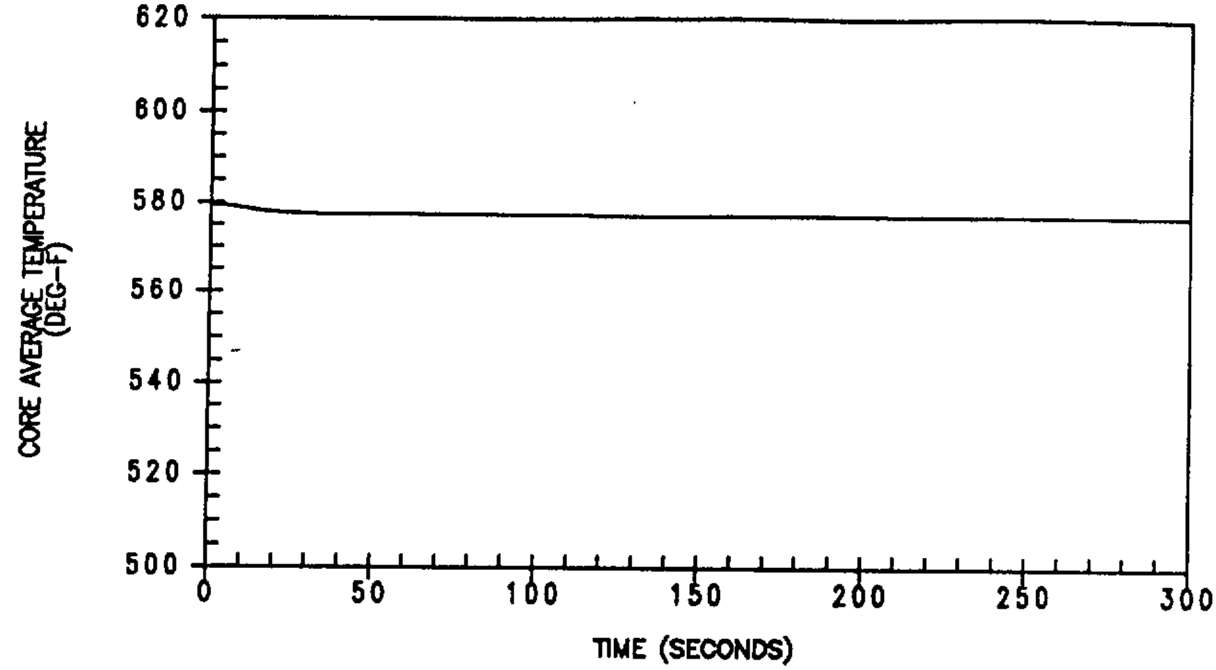
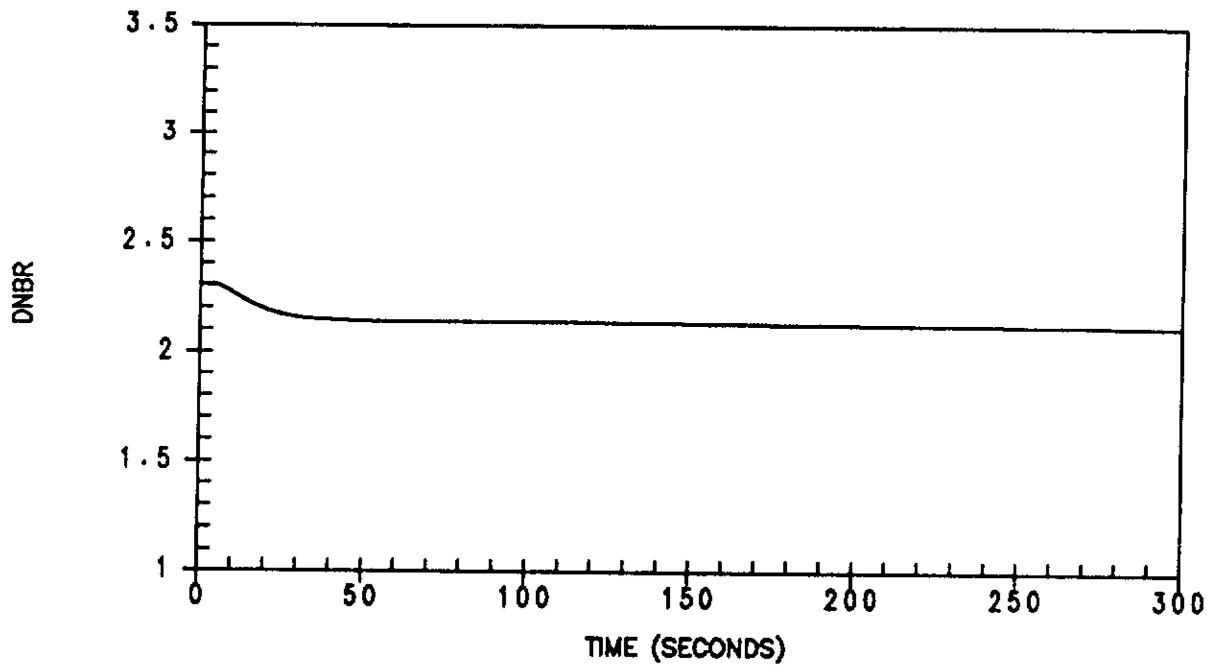
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

10% STEP LOAD INCREASE
MAXIMUM MODERATOR FEEDBACK
MANUAL REACTOR CONTROL
FIGURE 14.1.8-3





REV 14 (2/97)

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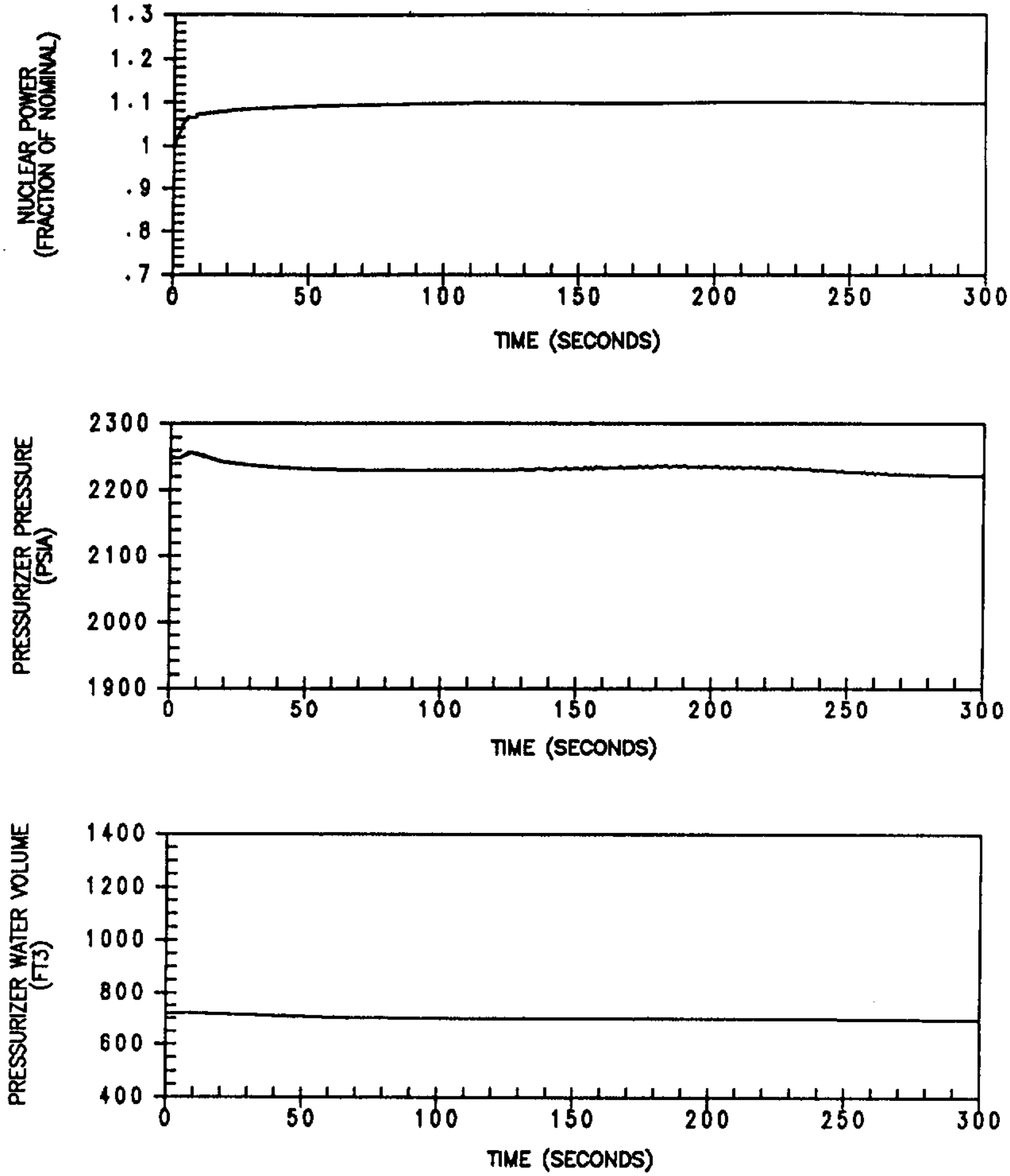
TURKEY POINT PLANT UNITS 3 & 4

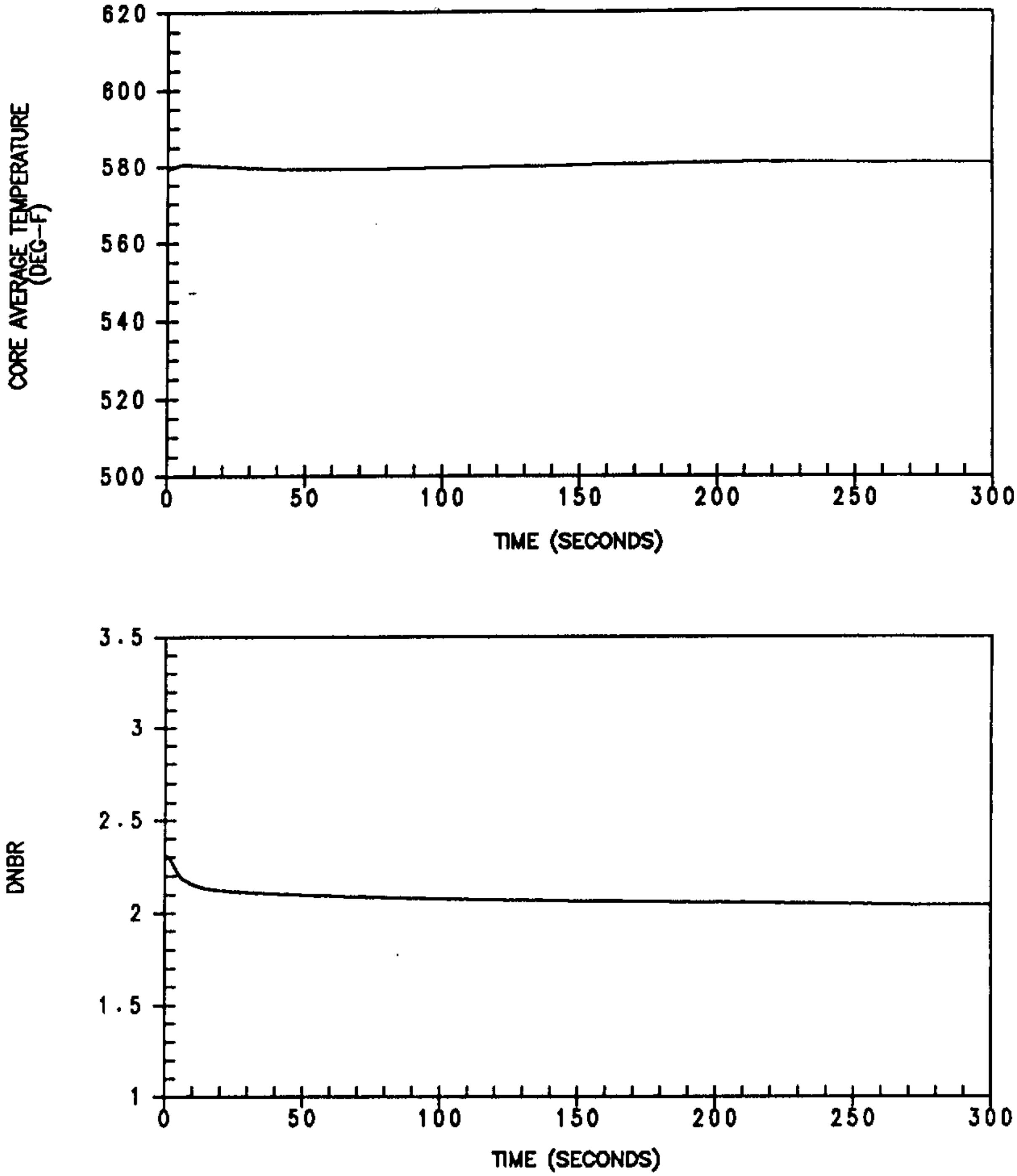
10% STEP LOAD INCREASE
MAXIMUM MODERATOR FEEDBACK
MANUAL REACTOR CONTROL
FIGURE 14.1.8-4



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

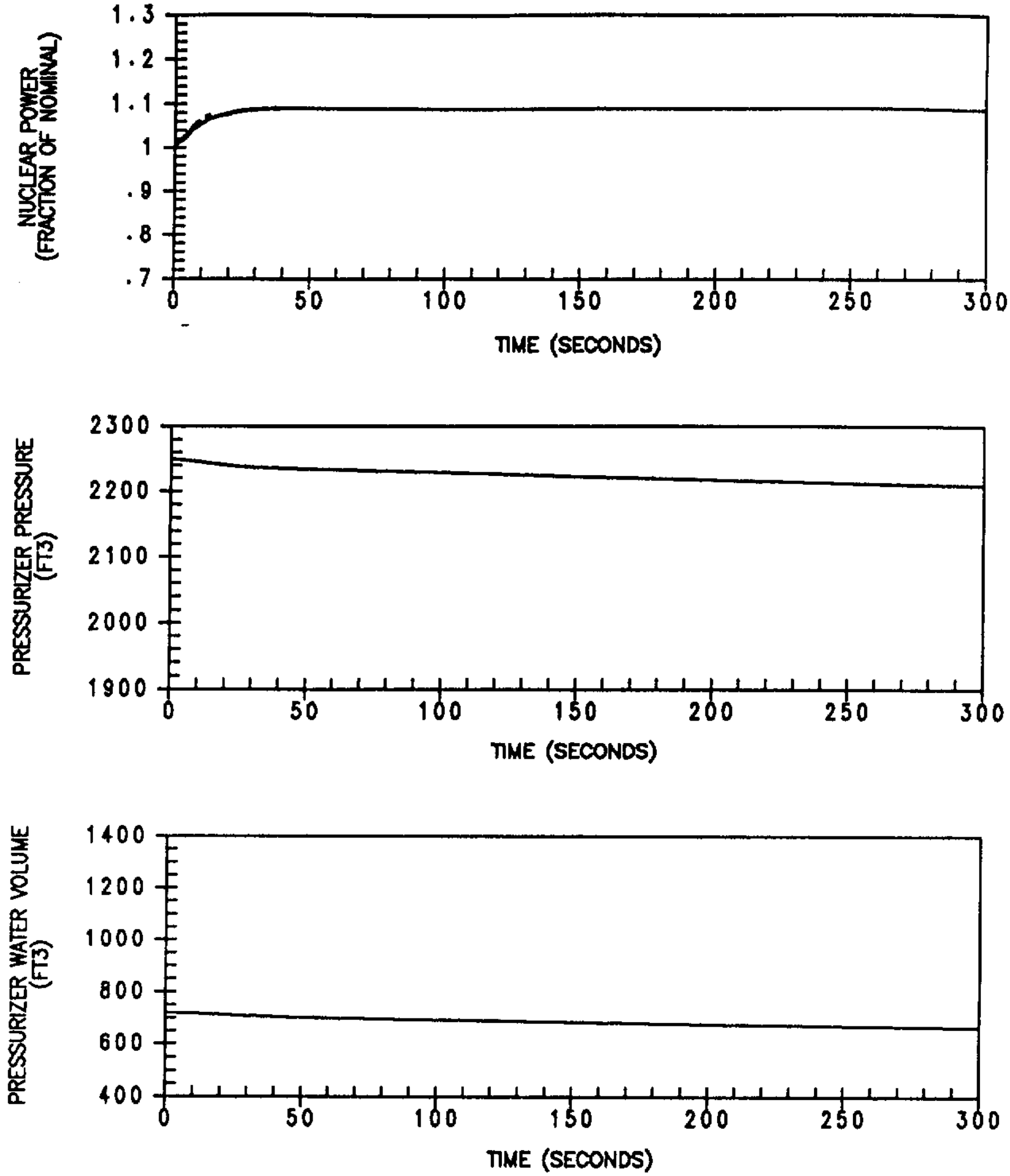
10% STEP LOAD INCREASE
MINIMUM MODERATOR FEEDBACK
AUTOMATIC REACTOR CONTROL
FIGURE 14.1.8-5



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

10% STEP LOAD INCREASE
MINIMUM MODERATOR FEEDBACK
AUTOMATIC REACTOR CONTROL
FIGURE 14.1.8-6



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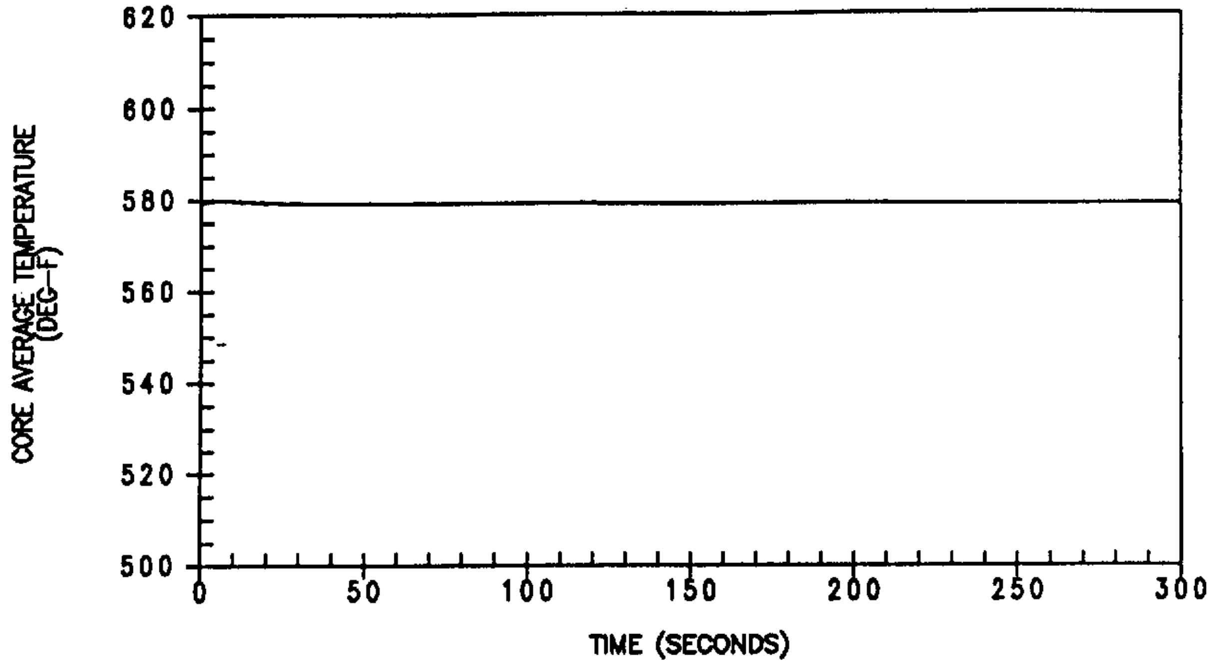
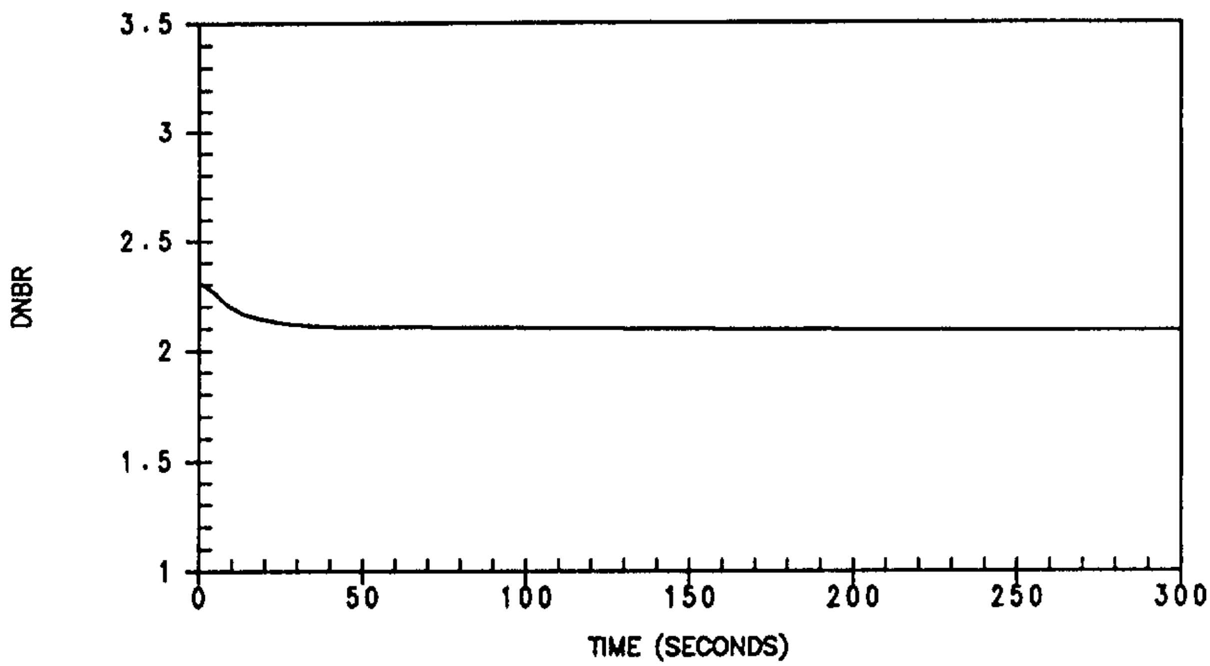
10% STEP LOAD INCREASE
MAXIMUM MODERATOR FEEDBACK
AUTOMATIC REACTOR CONTROL
FIGURE 14.1.8-7





REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

10% STEP LOAD INCREASE
MAXIMUM MODERATOR FEEDBACK
AUTOMATIC REACTOR CONTROL
FIGURE 14.1.8-8

14.1.9 LOSS OF REACTOR COOLANT FLOW

Flow Coast-Down Accidents

A loss of coolant flow incident can result from a mechanical or electrical failure in one or more reactor coolant pumps, or from a fault in the power supply to these pumps. If the reactor is at power at the time of the incident, the immediate effect of loss of coolant flow is a rapid increase in coolant temperature. This increase could result in departure from nucleate boiling (DNB) with subsequent fuel damage if the reactor is not tripped promptly.

Normal power supplies for the pumps are the two buses connected to the generator, one of which supplies power to one of the three pumps and the other of which supplies power to two of the three pumps. When a generator trip occurs, the buses are automatically transferred to a transformer supplied from external power lines so that the pumps will continue to provide forced coolant flow to the core.

The following signals provide the necessary protection against a loss of coolant flow accident:

A. Undervoltage or underfrequency on reactor coolant pump power supply buses.
B. Low reactor coolant loop flow.
C. Pump circuit breaker opening.

These trip circuits and their redundancy are further described in Table 7.2-1 Reactor Control and Protection System.

The reactor trip on reactor coolant pump undervoltage is provided to protect against conditions which can cause a loss of voltage to all reactor coolant pumps i.e., loss of offsite power. This function is blocked below approximately 10 percent power (Permissive P-7). See Table 7.2-2 for a definition of permissive setpoints.

The reactor coolant pump underfrequency function is provided to trip the reactor for an underfrequency condition resulting from frequency disturbances on the power grid. The underfrequency function will open all reactor coolant pump breakers whenever an underfrequency condition occurs to ensure adequate RCP pump coastdown and to provide breaker open input signals to the pump breaker position reactor trip logic.

The reactor trip on low primary coolant loop flow is provided to protect against loss of flow conditions which affect only one reactor coolant loop. It also serves as a backup to the undervoltage and underfrequency trips for the loss of all three reactor coolant pumps case. This function is generated by two-out-of-three low flow signals per reactor coolant loop. Above Permissive P-8, low flow in any loop will actuate a reactor trip. Between approximately 10 percent power (Permissive P-7) and the power level corresponding to Permissive P-8 (approximately 45% power), low flow in any two loops will actuate a reactor trip. Reactor trip on low flow is blocked below Permissive P-7.

A reactor trip from pump breaker position is to implement the underfrequency function and to provide protection against other conditions for which the RCP breakers are designed to trip open. Similar to the low flow trip, above P-8, a breaker open signal from any pump will actuate a reactor trip, and between P-7 and P-8, a breaker open signal from any two pumps will actuate a reactor trip. Reactor trip on reactor coolant pump breakers open is blocked below Permissive P-7.

<u>Method of Analysis</u>

The following loss of flow cases have been analyzed:

1. Loss of all three reactor coolant pumps with three loops in operation.

2. Loss of two reactor coolant pumps with three loops in operation.

These transients are analyzed by three digital computer codes. First, the LOFTRAN code (Reference 1) is used to calculate the loop and core flow transients, the nuclear power transient, and the primary system pressure and temperature transients. The FACTRAN code (Reference 2) is then used to calculate the heat flux transient based on the nuclear power and flow from LOFTRAN. Finally, the THINC code is used to calculate the DNBR during the transient based on the heat flux from FACTRAN and the flow from LOFTRAN. The DNBR transient presented represents the minimum of the typical and thimble cells.

The accidents are analyzed using the Revised Thermal Design Procedure. Initial core power, reactor coolant temperature, and pressure are assumed to be at their nominal values consistent with steady-state full-power operation. Uncertainties in initial conditions are included in the limit departure from nucleate boiling ratio (DNBR) as described in WCAP-11397-P-A (Reference 3).

A conservatively large absolute value of the Doppler only power coefficient is used. The total integrated Doppler reactivity from 0 to 100% power is assumed to be -0.016 Δk.

The most-positive moderator temperature coefficient (+7 pcm/°F) is assumed since this results in the maximum core power and hot spot heat flux during the initial part of the transient when the minimum DNBR is reached.

The flow coastdown analysis is based on a momentum balance around each reactor coolant loop and across the reactor core. This momentum balance is combined with the continuity equation, a pump momentum balance, and the as-built pump characteristics and is based on high estimates of system pressure losses.

Results (Flow Coast-Down)

Figures 14.1.9-1 through 14.1.9-4 show the transient response for the loss of power to all reactor coolant pumps. The reactor is assumed to be tripped on an undervoltage signal. Figures 14.1.9-5 through 14.1.9-8 show the transient response for the loss of two reactor coolant pumps with three loops initially in operation. The reactor is tripped on a low flow signal. The DNBR-versus-time plots (Figure 14.1.9-4 and 14.1.9-8), representing the limiting cells, show that the DNBR is always greater than the safety analysis limit value.

For the cases analyzed, since DNB does not occur, the ability of the primary coolant to remove heat from the fuel rod is not significantly reduced. Thus, the average fuel and clad temperatures do not increase far above their respective initial values.

The calculated sequence of events for the cases analyzed is shown in Table 14.1.9-1.

Conclusions

The analyses performed have demonstrated that for the above loss of flow incidents, the DNBR does not decrease below the limit value at any time during the transient. Thus, no fuel damage is predicted, and all applicable acceptance criteria are met.

Locked Rotor Accident

A hypothetical transient analysis is performed for the postulated instantaneous seizure of a reactor coolant pump rotor. Flow through the reactor coolant system is rapidly reduced, leading to a reactor trip on a low-flow signal.

Following the trip, heat stored in the fuel rods continues to pass into the core coolant, causing the coolant to expand. At the same time, heat transfer to the shell side of the steam generator is reduced, first because the reduced flow results in a decreased tube side film coefficient and then because the reactor coolant in the tubes cools down while the shell side temperature increases (turbine steam flow is reduced to zero upon trip). The rapid expansion of the coolant in the reactor core, combined with the reduced heat transfer in the steam generator, causes an insurge into the pressurizer and a pressure increase throughout the Reactor Coolant System. The insurge into the pressurizer compresses the steam volume, actuates the automatic spray system, opens the power-operated relief valves, and opens the pressurizer safety valves, in that sequence. The two power-operated relief valves are designed for reliable operation and would be expected to function properly during the accident. However, for conservatism, their pressure-reducing effect is not included in the analysis.

The consequences of a locked rotor (i.e., an instantaneous seizure of a pump shaft) are very similar to those of a pump shaft break. The initial rate of the reduction in coolant flow is slightly greater for the locked rotor event. However, with a broken shaft, the impeller could conceivably be free to spin in the reverse direction. The effect of reverse spinning is to decrease the steady-state core flow when compared to the locked rotor scenario. Only one analysis has been performed, and it represents the most limiting condition for the locked rotor and pump shaft break accidents.

Method of Analysis

Two digital computer codes are used to analyze this transient. The LOFTRAN code (Reference 1) is used to calculate the resulting loop and core flow transients following the pump seizure, the time of reactor trip based on the loop flow transients, the nuclear power following reactor trip, and the peak RCS pressure. The thermal behavior of the fuel located at the core hot spot is investigated using the FACTRAN code (Reference 2) which uses the core flow and the nuclear power values calculated by LOFTRAN. The FACTRAN code includes a film boiling heat transfer coefficient.

One case is analyzed:

1. One locked rotor/shaft break with three loops in operation.

The accident is evaluated with no loss of offsite power. The two unaffected RCPs continue to operate through the duration of the event.

Initial Conditions

At the beginning of the postulated locked rotor accident, the plant is assumed to be operating under the most adverse steady-state operating conditions. These include the maximum steady-state power level, pressure, and coolant average temperature. The reactivity coefficients assumed in the analysis include a positive moderator temperature coefficient and a conservatively large (absolute value) of the Doppler-only power coefficient. For this analysis, the negative reactivity insertion upon trip is based on a 4% trip reactivity from full power.

For the peak pressure evaluation, the initial pressure is conservatively estimated as 60 psi above the nominal pressure of 2250 psia to allow for errors in the pressurizer pressure measurement and control channels. This is done to obtain the highest possible rise in the coolant pressure during the transient. To obtain the maximum pressure in the primary side, conservatively high loop pressure drops are added to the calculated pressurizer pressure. The pressure response shown in Figure 14.1.9-10 is at the point in the Reactor Coolant System having the maximum pressure (i.e., the outlet of the faulted loop's RCP).

For a conservative analysis of fuel rod behavior, the hot spot evaluation assumes that DNB occurs at the initiation of the transient and continues throughout the event. This assumption reduces heat transfer to the coolant and results in conservatively high hot spot temperatures.

The reactor coolant flow coastdown analysis is based on a momentum balance around each reactor coolant loop and across the reactor core. This momentum balance is combined with the continuity equation, a pump momentum balance, and the as-built pump characteristics and is based on high estimates of system pressure losses.

Evaluation of the Pressure Transient

After pump seizure, the neutron flux is rapidly reduced by control rod insertion. Rod motion is assumed to begin one second after the flow in the affected loop reaches 84.5 percent of nominal flow. No credit is taken for the pressure-reducing effect of the pressurizer power-operated relief valves, pressurizer spray, steam dump or controlled feedwater flow after plant trip. Although these systems are expected to function and would result in a lower peak pressure, an additional degree of conservatism is provided by ignoring their effect.

The pressurizer safety valves are modelled including the effects of the pressurizer safety valve loop seals using WOG methodology (Reference 4). The pressurizer safety valve includes a 4% uncertainty (1% set pressure shift and a 3% set pressure tolerance) over the nominal setpoint of 2500 psia. Additionally, no steam flow is assumed until the valve loop seals are purged.

Evaluation of DNB in the Core During the Accident

For this accident, DNB is assumed to occur in the core and therefore, an evaluation of the consequences with respect to fuel rod thermal transients is performed. Results obtained from analysis of this "hot spot" condition represent the upper limit with respect to clad temperature and zirconium-water reaction. In the evaluation, the rod power at the hot spot is assumed to be 2.5 times the value at the initial core power level.

Film Boiling Coefficient

The film boiling coefficient is calculated in the FACTRAN code using the Bishop-Sandberg-Tong film boiling correlation (Reference 2). The fluid properties are evaluated at the film temperature (average between the wall and bulk temperatures). The program calculates the film coefficient at every time step based upon the actual heat transfer conditions at the time. The neutron flux, system pressure, bulk density, and mass flow rate as a function of time are used as program input.

For this analysis, the initial values of the pressure and the bulk density are used throughout the transient, since they are the most conservative with respect to the clad temperature response. As indicated earlier, DNB was assumed to start at the beginning of the accident.

Film Clad Gap Coefficient

The magnitude and time dependence of the heat transfer coefficient between fuel and clad (gap coefficient) has a pronounced influence on the thermal results. The larger the value of the gap coefficient, the more heat is transferred between pellet and clad.

For the initial portion of the transient, a high gap coefficient produces higher clad temperatures, since the heat stored and generated in the fuel redistributes itself in the cooler cladding. This effect is reversed when the clad temperature exceeds the pellet temperature in cases where a zirconium-steam reaction is present. Based on investigations on the effect of the gap coefficient upon the maximum clad temperature during the transient, the gap coefficient was assumed to increase from a steady-state value consistent with initial fuel temperatures to 10,000 Btu/hr-ft^2-^{o}F at the initiation of the transient. Thus, the large amount of energy stored in the fuel is released to the clad at the initiation of the transient.

Zirconium Steam Reaction

The zirconium-steam reaction can become significant above 1800^{o}F (clad temperature). The Baker-Just parabolic rate equation (Reference 2) shown below is used to define the rate of the zirconium-steam reaction.

$$\frac{dw^2}{dt} = 33.3 \times 10^6 \exp^{(-45,500/1.986T)}$$

where,

w = amount reacted, mg/cm^2
t = time, sec
T = temperature, ^{o}K
The reaction heat is about 1510 cal/gm

The effect of zirconium-steam reaction is included in the calculation of the "hot spot" clad temperature transient.

AIRLO Fuel Clad Evaluation

The effects on the overall transient behavior due to a change of the clad material from Zircaloy-4 to Zirlo waere determined in Reference 5 to be hegligible. The calculated PCT is estimated to increase by 2^{o}F and there is no impact on the metal-to water reaction rate.

Results

The calculated sequence of events is shown in Table 14.1.9-1. The transient results are shown in Figures 14.1.9-9 through 14.1.9-12. The peak Reactor Coolant System pressure reached during the transient is less than that which would cause stresses to exceed the faulted condition stress limits. Also, the peak clad surface temperature is considerably less than 2700^{o}F. It should be noted that the clad temperature was conservatively calculated assuming that DNB occurs at the initiation of the transient. The results of these calculations (peak pressure, peak clad temperature (including ZIRLO), and zirconium-steam reaction) are also summarized in Table 14.1.9-2. Fewer than 10% of the fuel rods exhibited a DNBR less than the limit value.

Dose Evaluation

For the analysis of offsite doses following a locked rotor, the doses are determined assuming a pre-accident spike that has raised the RCS iodine concentration to 60 μCi/gm of dose equivalent (DE) I-131.

The noble gas activity concentration in the RCS at the time the accident occurs is based on a fuel defect of 1.0%. The iodine activity of the secondary coolant at the time the locked rotor occurs is assumed to be the equivalent to the Technical Specification limit of 0.10 μCi/gm of DE I-131.

As a result of the locked rotor event, fewer than 10% of the fuel rods undergo DNB. However, it is conservatively assumed that 10% of the fuel rods fail and that all of the fuel rod gap activity is released to the RCS. The gap activity is assumed to be 10% of the total core activity for both iodine and noble gases.

The total primary to secondary SG tube leak rate used in the analysis is the Technical Specification limit of 1.0 gpm. No credit for iodine removal is taken for any steam released to the condenser prior to the reactor trip and concurrent loss of offsite power. An iodine partition factor in the SGs of 0.01 (Ci I/gm steam)/(Ci I/gm water) is used. All noble gas activity carried over to the secondary side through SG tube leakage is assumed to be immediately released to the outside atmosphere.

At 24 hours after the accident the RHR System is assumed to be placed in service for heat removal and there are no further steam releases to the atmosphere from the secondary system.

The major assumptions and parameters used in the analysis are itemized in Table 14.1.9-3. The thyroid dose conversion factors, breathing rates, and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4.

The dose limits for a locked rotor are a small fraction of the 10 CFR 100 guideline values of 30 rem thyroid and 2.5 rem whole body. The offsite thyroid and whole body doses due to the locked rotor are given in Table 14.1.9-4. The offsite doses due to the locked rotor are within the acceptance criteria.

Conclusions

The analysis has shown the following:

a. Since the peak RCS pressure reached during the transient is less than that which would cause stresses to exceed the faulted condition stress limits, the integrity of the primary coolant system is not endangered.

b. Since the peak clad surface temperature calculated for the hot spot during the worst transient remains considerably less than 2700°F, the core will remain in place and intact with no loss of core cooling capability.

REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-proprietary), Burnett, T. W. T., et al.,"LOFTRAN Code Description," dated April 1984.

2. Westinghouse WCAP-7908-A, Hargrove, H. G., "FACTRAN - A FORTRAN-IV Code for Thermal Transients in a UO_2 Fuel Rod," dated December 1989.

3. Westinghouse WCAP-11397-P-A, Friedland, A. J., and Ray, S., "Revised Thermal Design Procedure," dated April 1989.

4. Westinghouse WCAP-12910, Barret, G. O., et al.,"Pressurizer Safety Valve Set Pressure Shift," dated March 1991.

5. S.S. Witter to J.L. Perryman "Turkey Point Units 3 & 4 - ZIRLO Safety Assessment Revision 1," 98FP-G-0054, June 9,1998.

TABLE 14.1.9-1

SEQUENCE OF EVENTS
LOSS OF FLOW ACCIDENTS

Case	Event	Time (sec)
Loss of 3 RCP's	Coastdown Begins	0.0
	Low Voltage Trip setpoint Reached	0.0
	Rods Begin to Drop	2.0
	Low Flow Setpoint Reached	2.2
	Minimum DNBR Occurs	3.8
	Maximum RCS Pressure Occurs	5.1
Loss of 2 RCP's	Coastdown Begins	0.0
	Low Flow Setpoint Reached	2.3
	Rods Begin to Drop	3.3
	Minimum DNBR Occurs	5.1
	Maximum RCS Pressure Occurs	6.1
Locked Rotor	Rotor Locks on one RCP	0.0
	Low Flow Setpoint Reached	0.05
	Rods Begin to Drop	1.05
	Maximum Clad Temperature Occurs	3.5
	Maximum RCS Pressure Occurs	3.8

TABLE 14.1.9-2

SUMMARY OF RESULTS
FOR
THE LOCKED ROTOR TRANSIENT

Criteria	3 Loops Initially Operating One Locked Rotor
Maximum RCS Pressure	2690 psia
Maximum Clad Temperature at Core Hot Spot	1908 ^{o}F
$Zr-H_20$ Reaction at Core Hot Spot	0.4 wt. %

TABLE 14.1.9-3

ASSUMPTIONS USED
FOR LOCKED ROTOR DOSE ANALYSIS

Power	2346 MWt
Reactor Coolant Noble Gas Activity Prior to Accident	1.0% Fuel Defect Level
Reactor Coolant Activity Prior to Accident	60 μCi/gm of DE I-131
Activity Released to Reactor Coolant from Failed Fuel (Noble & Iodine)	10% of Fuel Rod Gap Activity
Fraction of Core Activity in Gap (Noble & Iodine)	0.10
Secondary Coolant Activity Prior to Accident	0.10 μCi/gm of DE I-131
Total SG Tube Leak Rate During Accident	1.0 gpm
SG Iodine Partition Factor	0.01
Duration of Activity Release from Secondary System	24 hr
Offsite Power	Lost(1)
Steam Release from SGs to Environment	521,000 lb (0-2 hr) 448,400 lb (2-8 hr) 1,196,000 lb (8-24 hr)

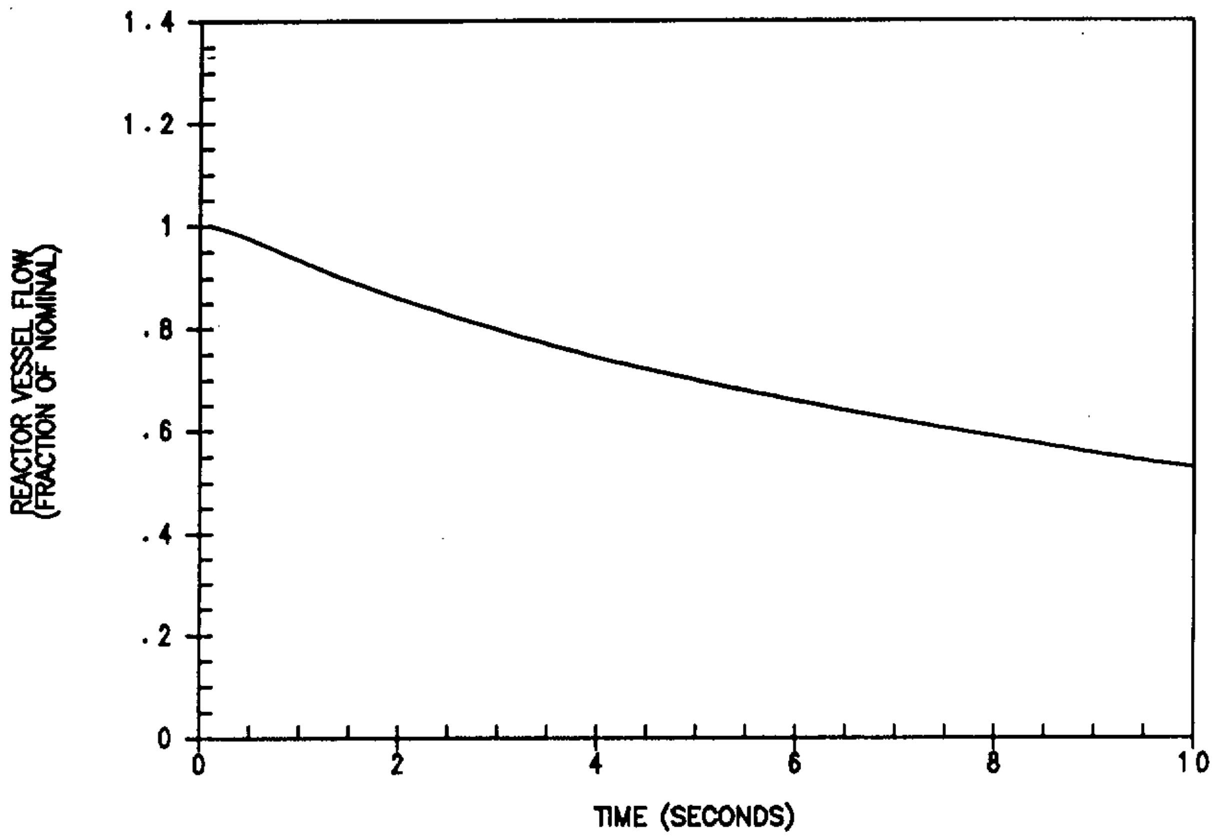
NOTE:

1. Assumption of a Loss of Offsite power is conservative for the locked rotor dose analysis.

TABLE 14.1.9-4

LOCKED ROTOR OFFSITE DOSES (REM)

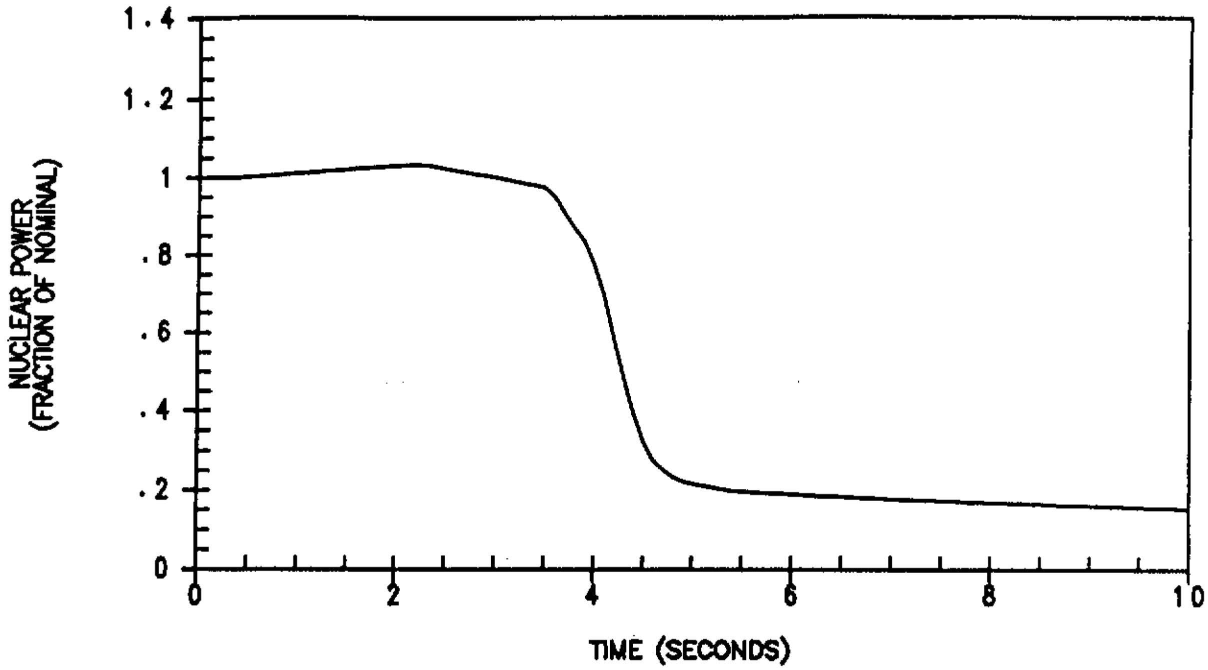
	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-24 Hours)
Thyroid Dose (rem)	1.0 E+0	4.0 E-1
Whole Body Dose (rem)	9.9 E-2	1.5 E-2

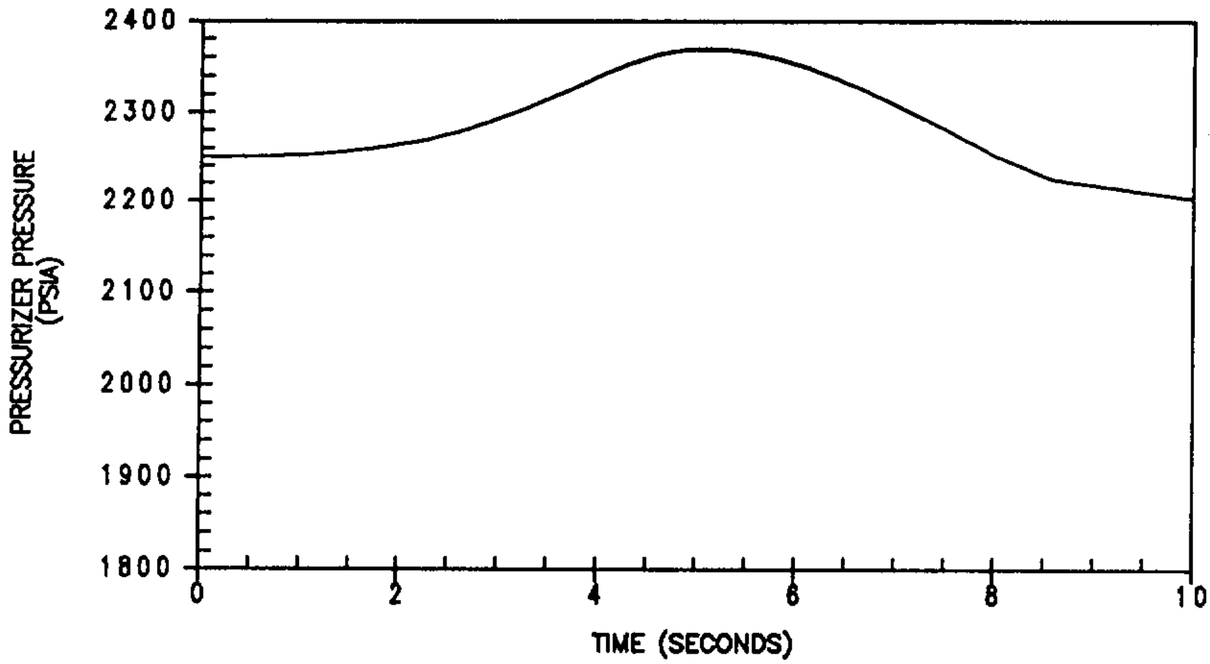


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS OPERATING,
ALL LOOPS COASTING DOWN -
CORE FLOW VERSUS TIME
FIGURE 14.1.9-1

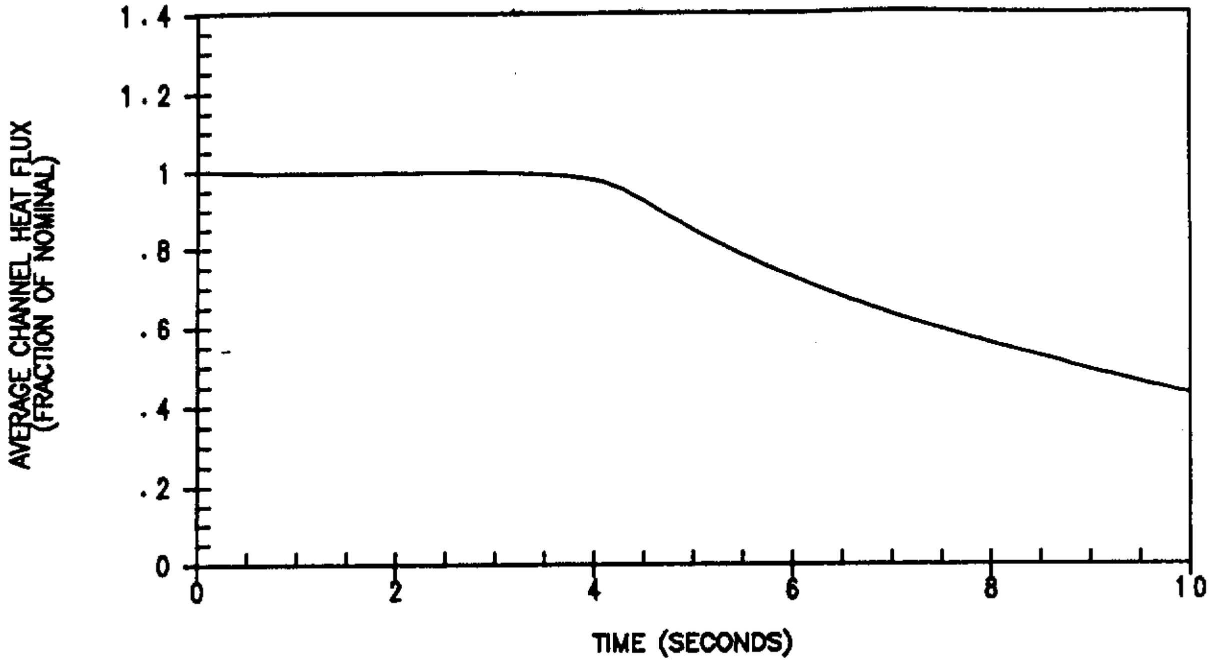


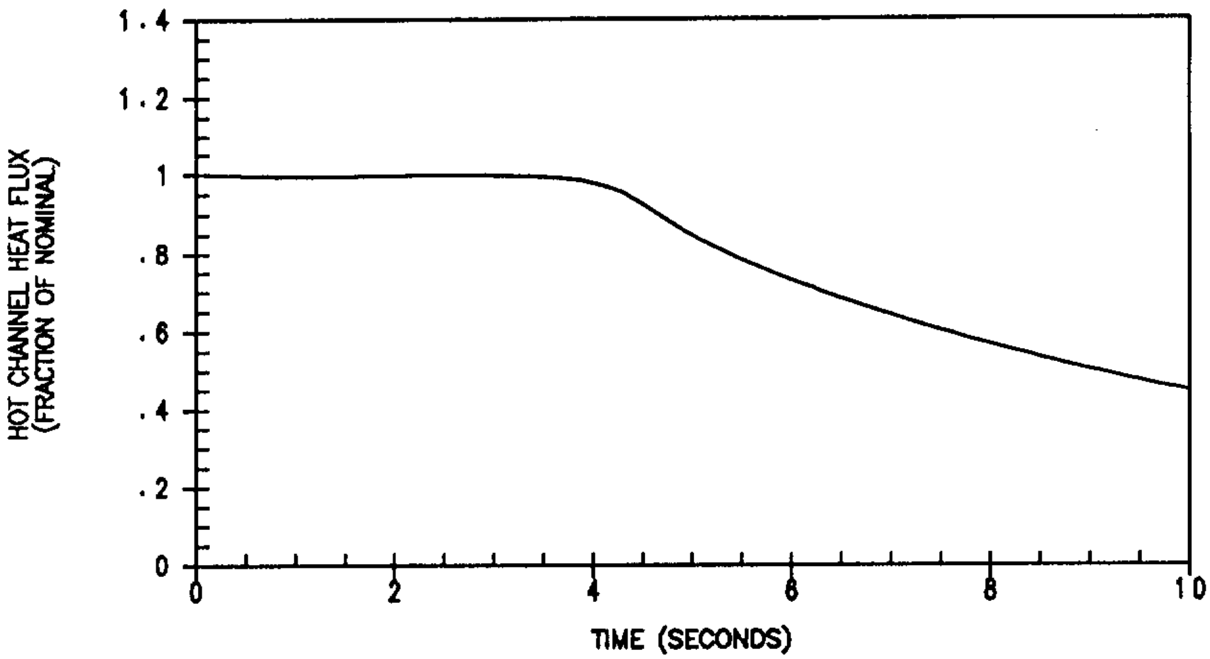


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS OPERATING,
ALL LOOPS COASTING DOWN -
NUCLEAR POWER AND PRESSURIZER
PRESSURE TRANSIENTS
FIGURE 14.1.9-2

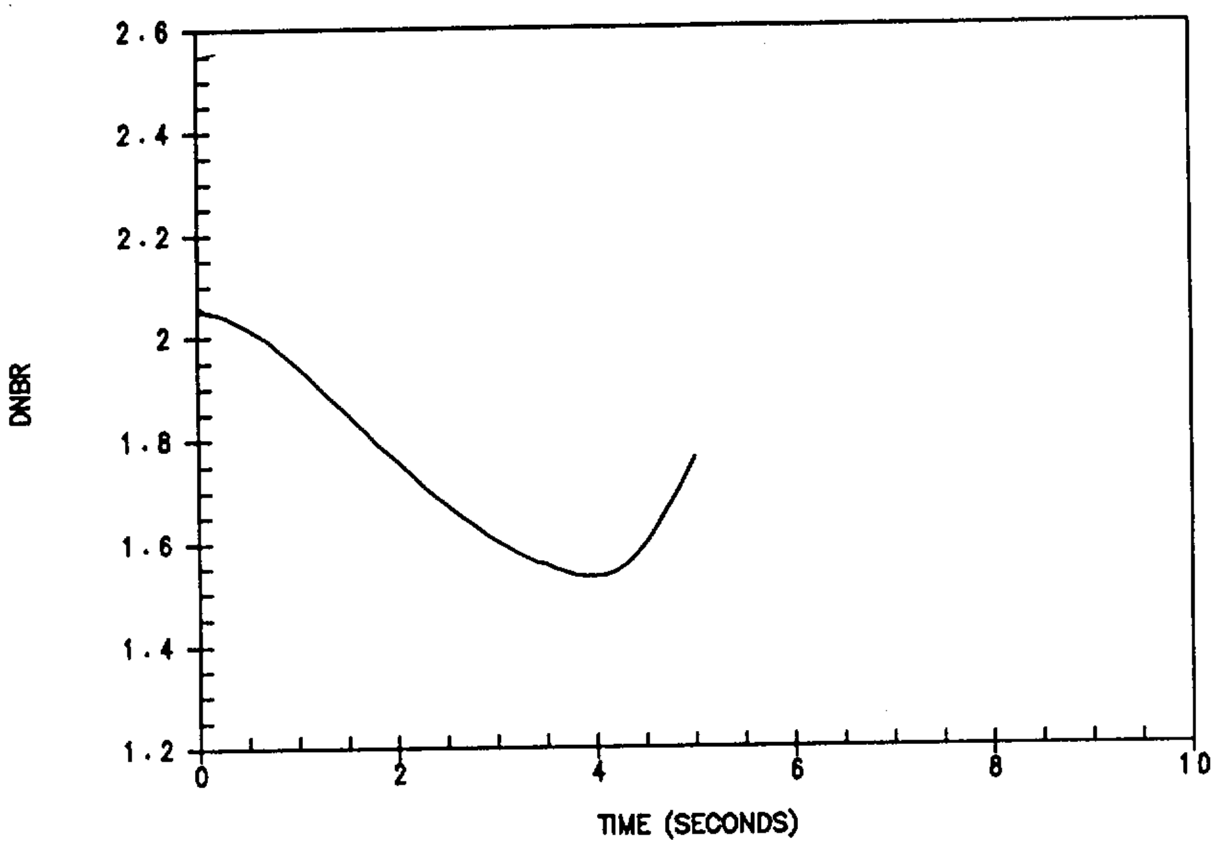




REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS OPERATING,
ALL LOOPS COASTING DOWN -
AVERAGE AND HOT CHANNEL
HEAT FLUX TRANSIENTS
FIGURE 14.1.9-3

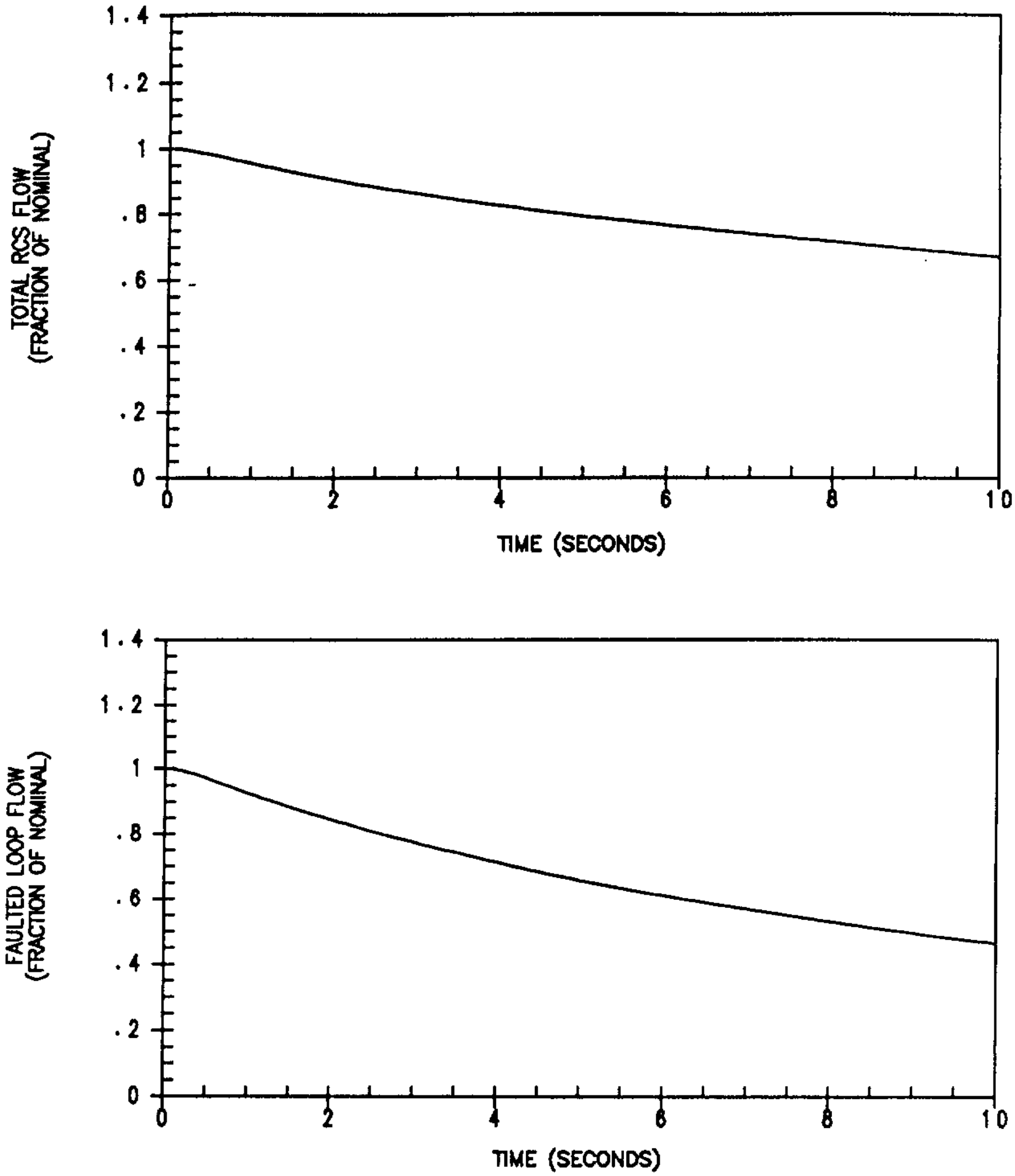


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS OPERATING,
ALL LOOPS COASTING DOWN -
DNBR VERSUS TIME

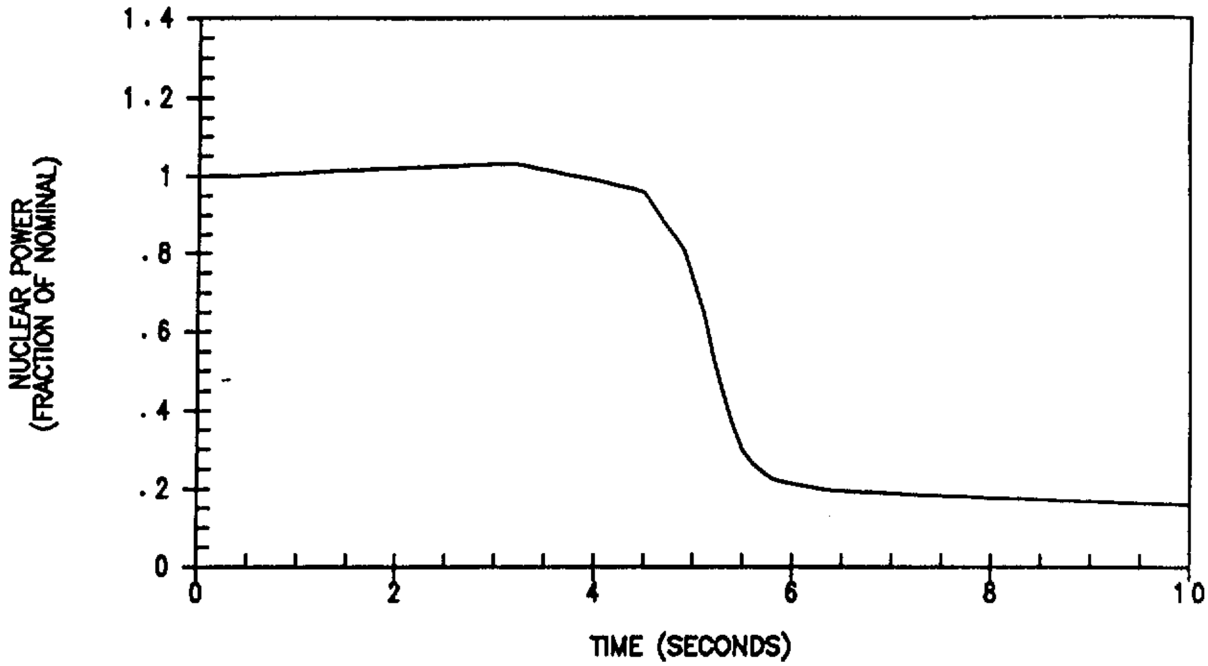
FIGURE 14.1.9-4

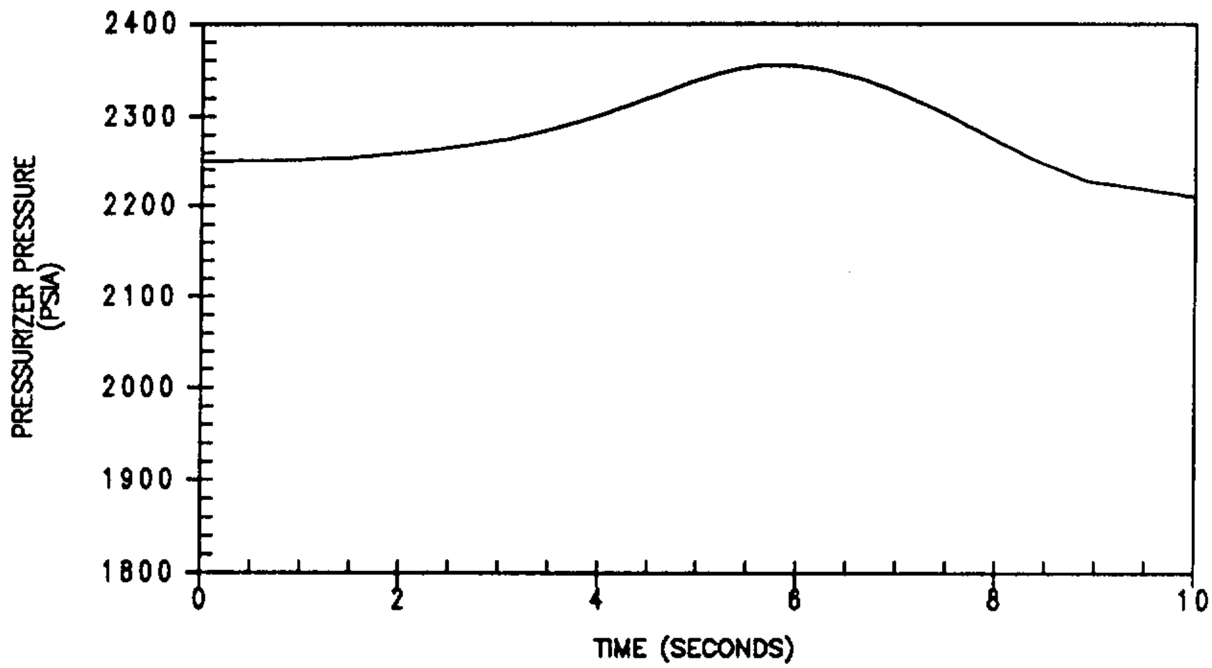


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING,
TWO LOOPS COASTING DOWN -
FLOW COASTDOWNS VERSUS TIME
FIGURE 14.1.9-5



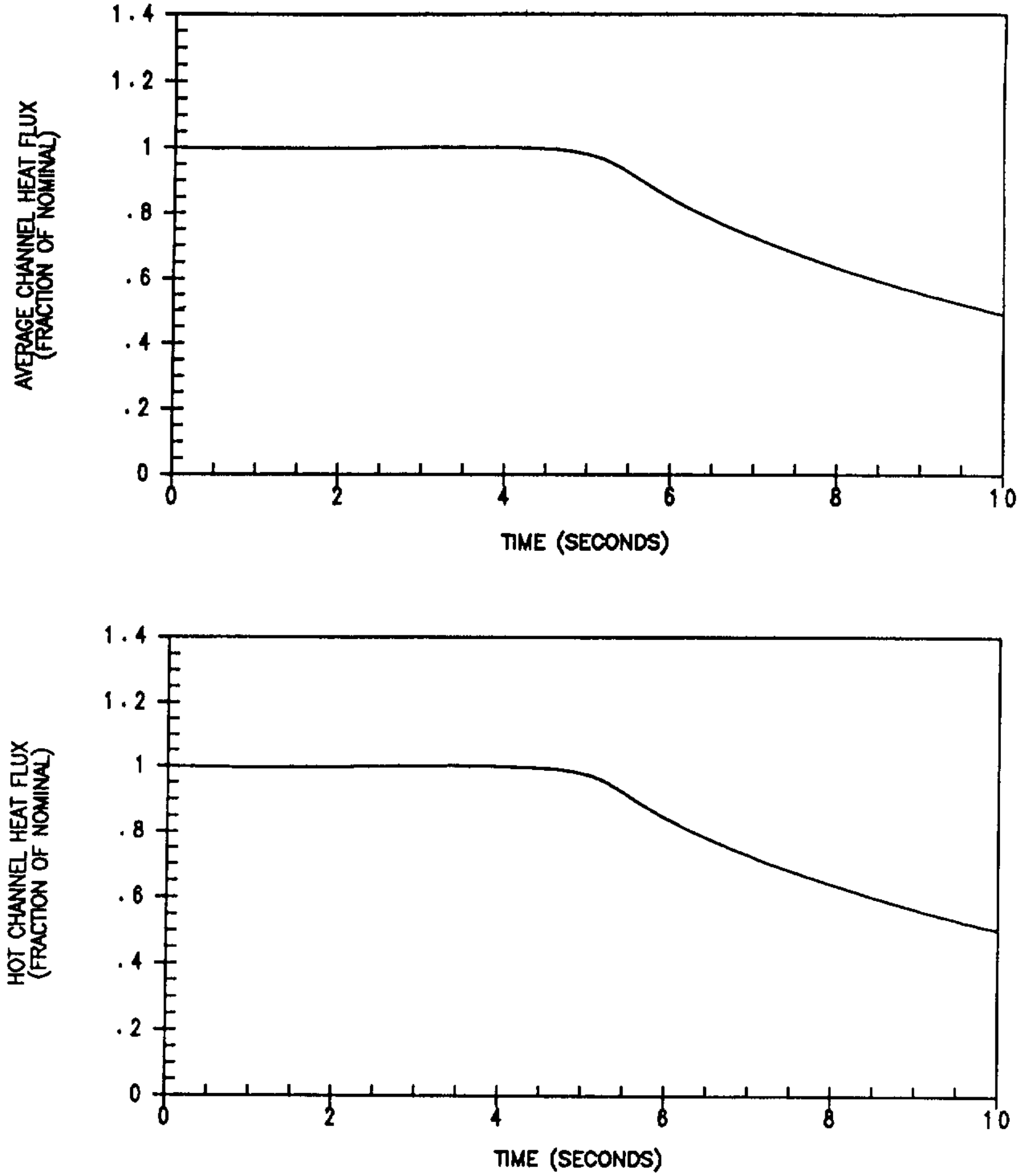


REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING, TWO LOOPS COASTING DOWN - NUCLEAR POWER AND PRESSURIZER PRESSURE TRANSIENTS

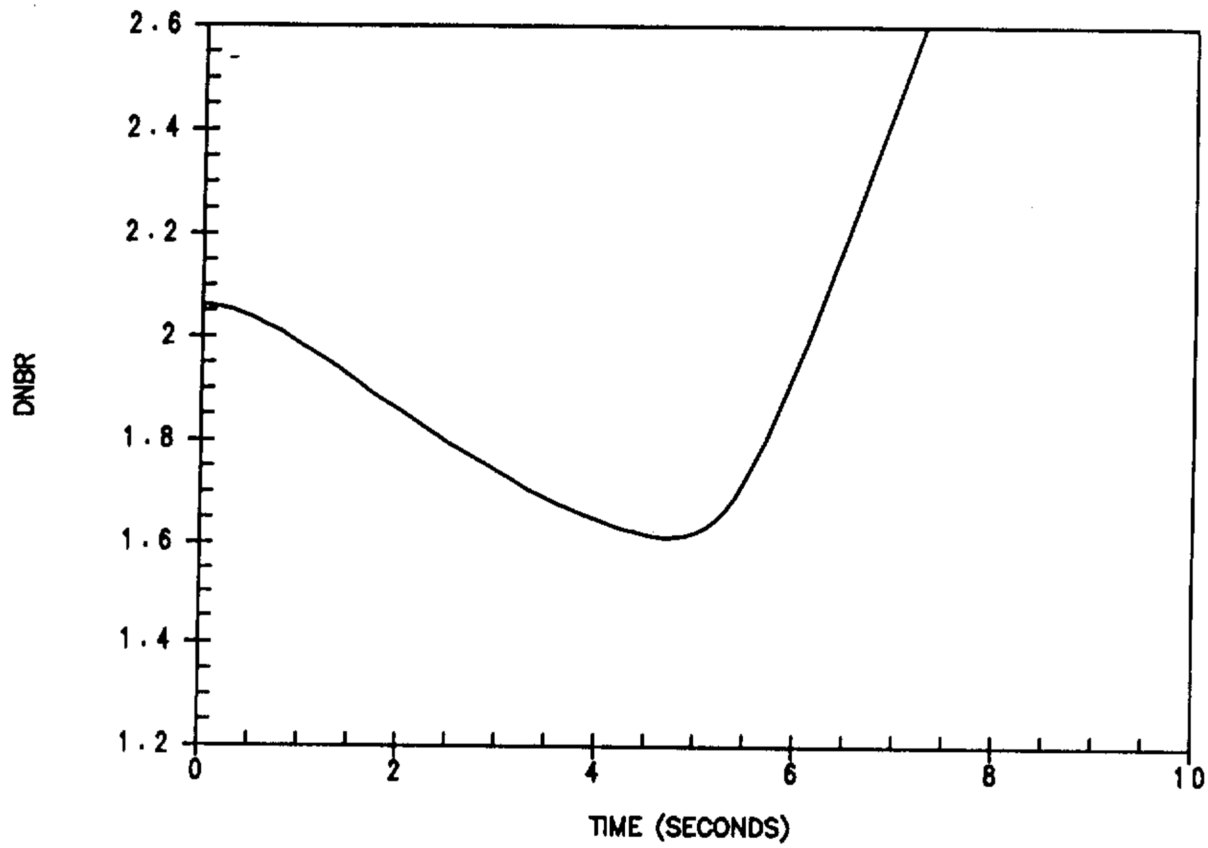
FIGURE 14.1.9-6



REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

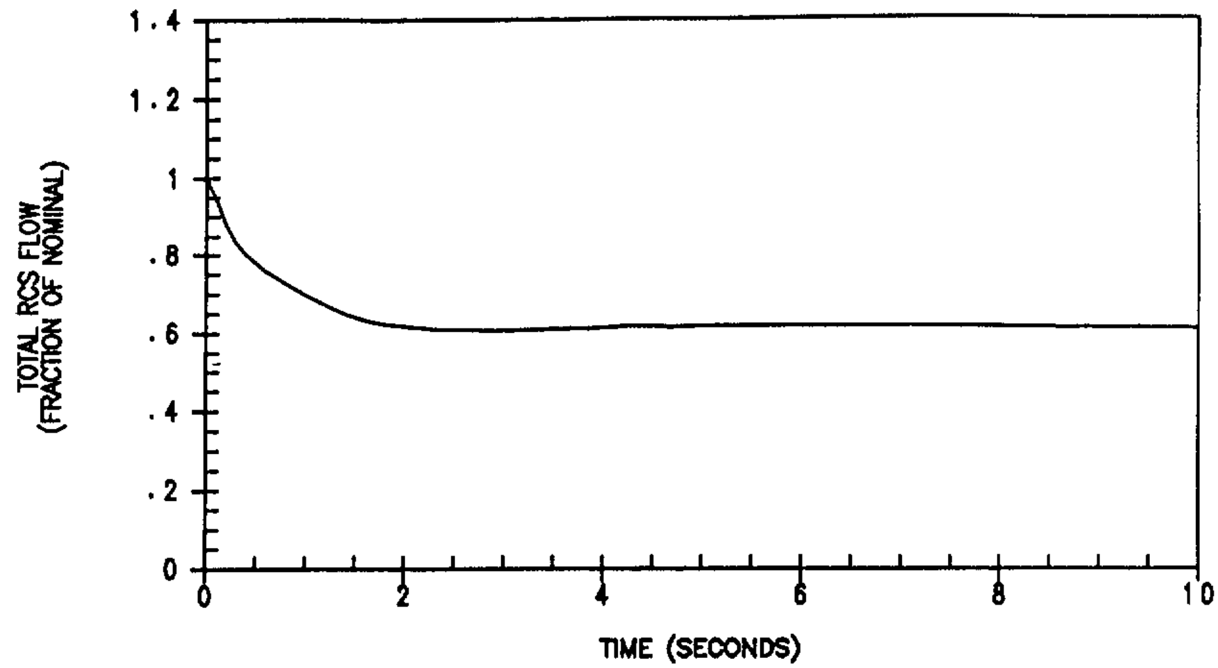
ALL LOOPS INITIALLY OPERATING,
TWO LOOPS COASTING DOWN -
AVERAGE AND HOT CHANNEL
HEAT FLUX TRANSIENTS
FIGURE 14.1.9-7

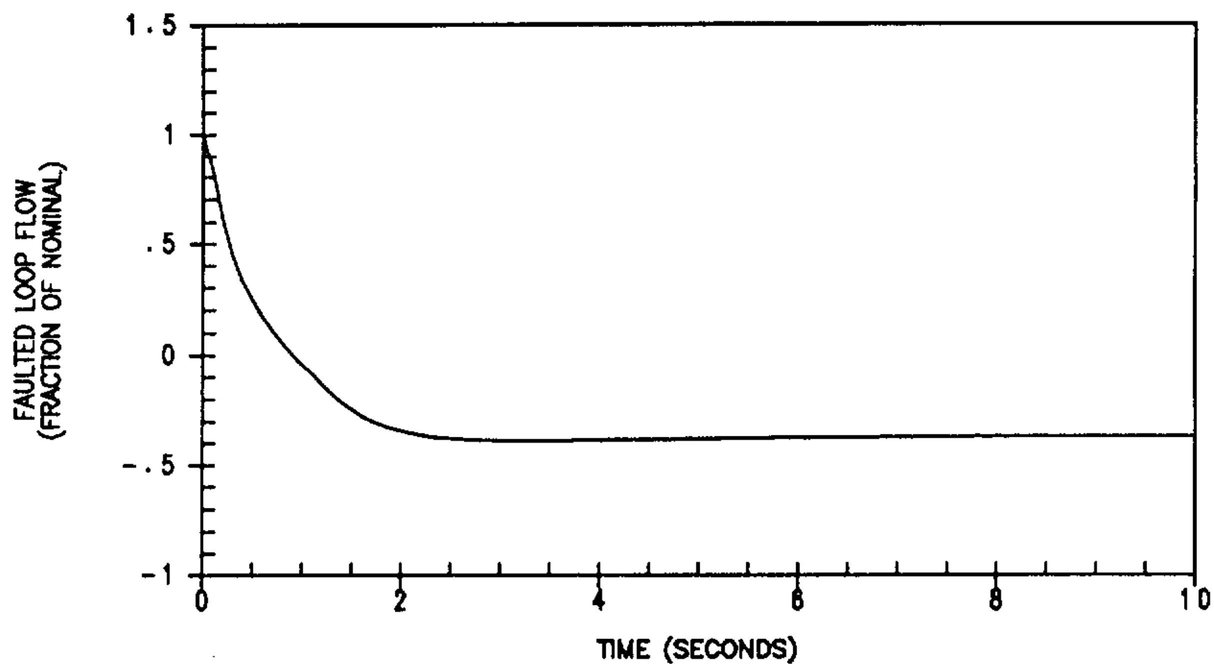


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING,
TWO LOOPS COASTING DOWN -
DNBR VERSUS TIME
FIGURE 14.1.9-8



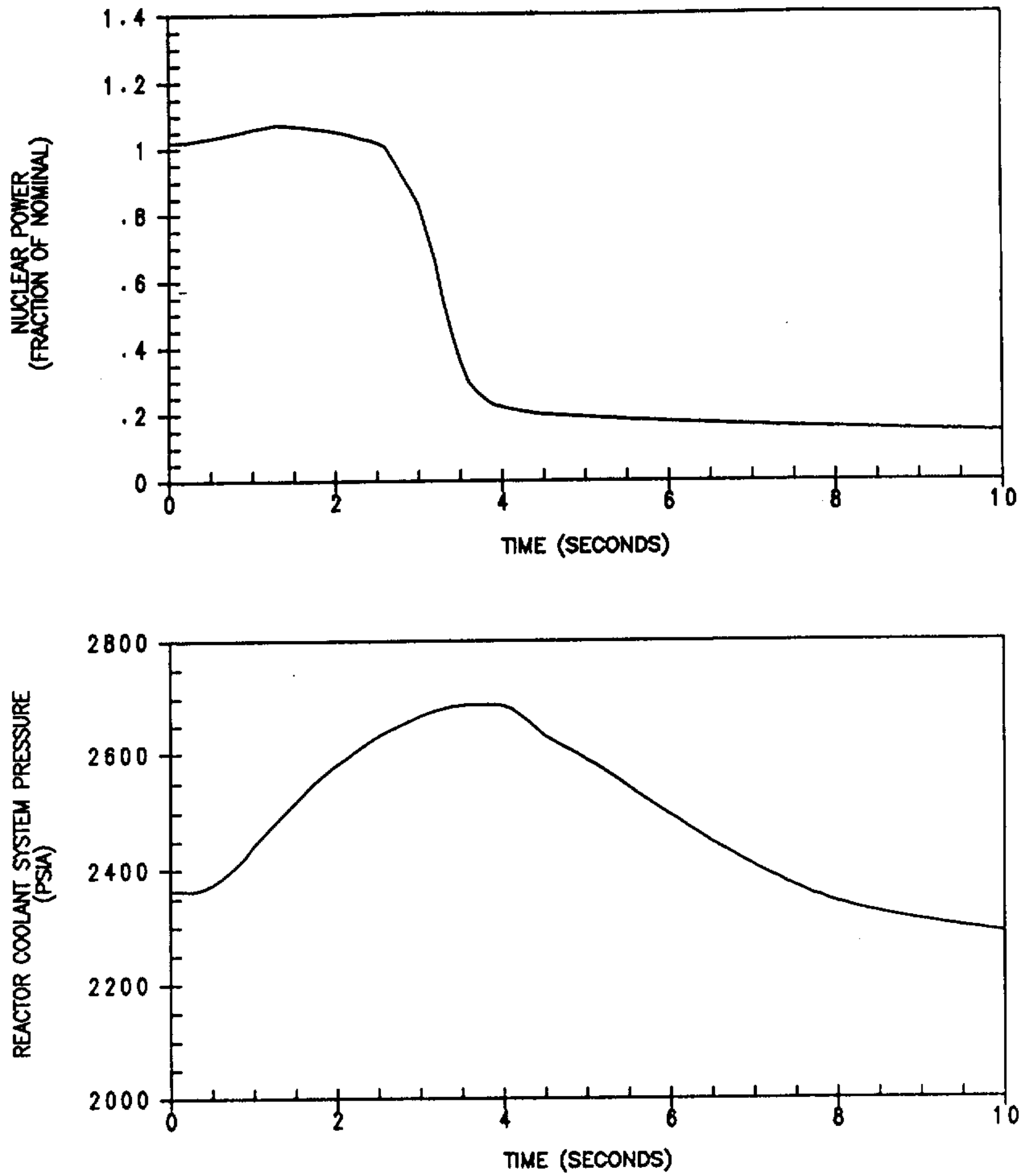


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING,
ONE LOCKED ROTOR -
FLOW COASTDOWNS VERSUS TIME

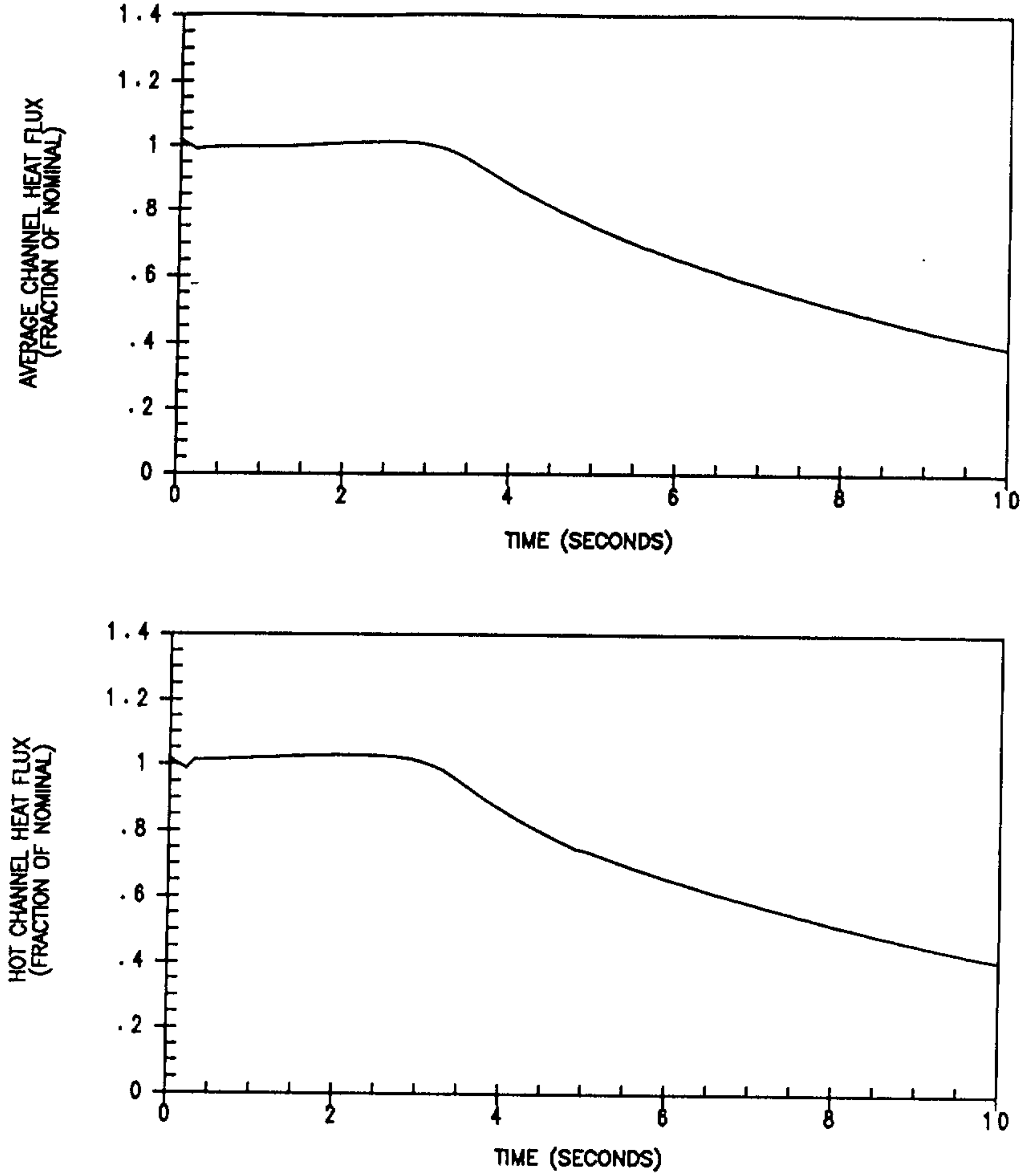
FIGURE 14.1.9-9



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING,
ONE LOCKED ROTOR - NUCLEAR
POWER AND RCS PRESSURE TRANSIENTS
FIGURE 14.1.9-10

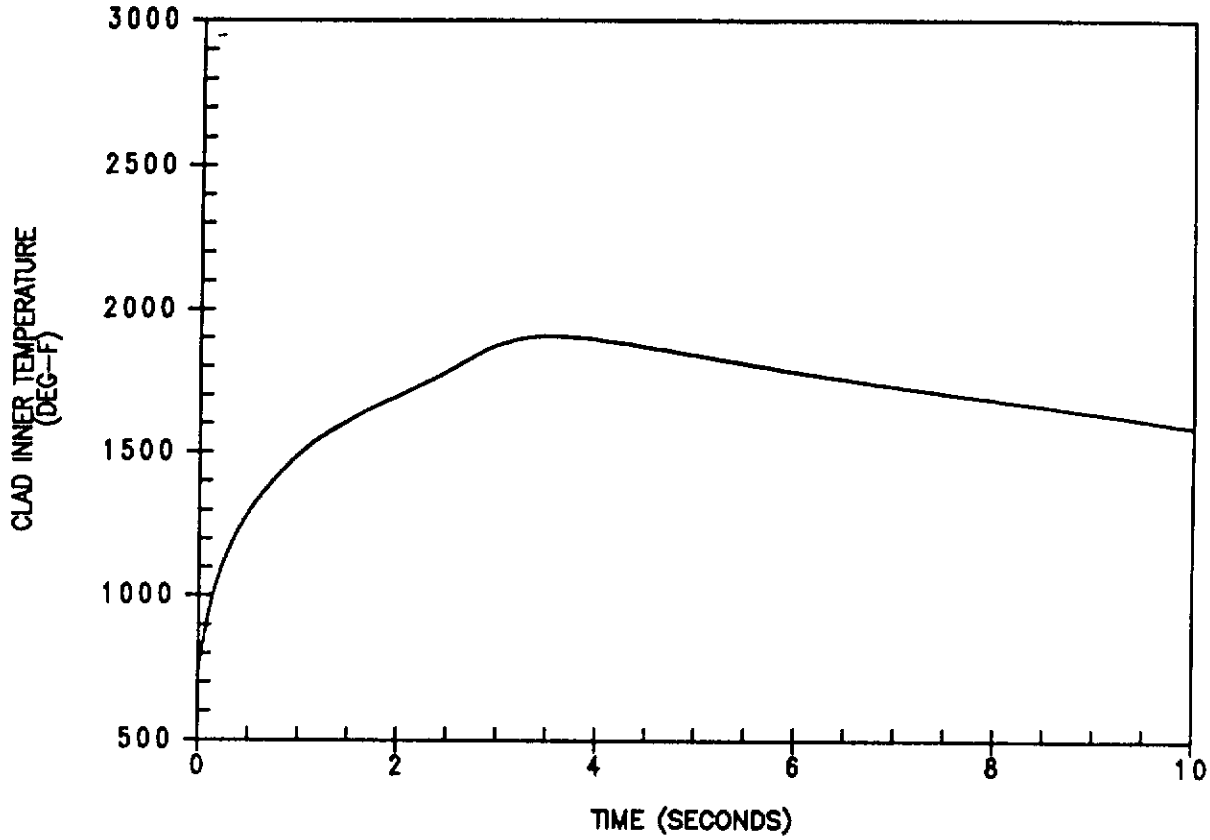


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING, ONE LOCKED ROTOR - AVERAGE AND HOT CHANNEL HEAT FLUX TRANSIENTS

FIGURE 14.1.9-11



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ALL LOOPS INITIALLY OPERATING, ONE LOCKED ROTOR - CLAD INNER TEMPERATURE VERSUS TIME

FIGURE 14.1.9-12

14.1.10 LOSS OF EXTERNAL ELECTRICAL LOAD

The loss of external electrical load may result from an abnormal increase in network frequency, or an accidental opening of the main breaker from the generator which fails to cause a turbine trip but causes a rapid large load reduction by the action of the turbine control. For either case, offsite power is available for the continued operation of plant components such as the reactor coolant pumps. The case of loss of all non-emergency AC power is presented in Section 14.1.12.

The unit is designed to accept a 50 percent step loss of load without actuating a reactor trip with all NSSS control systems in automatic (reactor control system, pressurizer pressure and level, steam generator water level control, and steam dumps). The automatic turbine bypass system with 27 percent design flow to the condenser is able to accommodate this abnormal load rejection by reducing the transient imposed upon the reactor coolant system. The reactor power is reduced to the new equilibrium power level at a rate consistent with the capability of the rod control system. The pressurizer power-operated relief valves may be actuated, but the pressurizer safety valves and the steam generator safety valves do not lift in this case.

A loss of external load would normally trip the reactor directly from a signal derived from the turbine autostop oil pressure (a two out of three signal). Reactor coolant temperatures and pressure do not significantly increase if the steam dump system and pressurizer pressure control system are functioning properly.

In the event the turbine bypass valves fail to open following a large load loss, the main steam safety valves lift and the reactor may be tripped by the high pressurizer pressure signal or high pressurizer level signal or the overtemperature ΔT signal. In the event of feedwater flow also being lost, the reactor may also be tripped by a steam generator low-low water level signal. The steam generator shell side pressure and reactor coolant temperatures increase rapidly. The pressurizer safety valves are sized to protect the reactor coolant system against overpressure without taking credit for the turbine bypass system, pressurizer spray, pressurizer power-operated relief valves, automatic RCCA control, or the direct reactor trip on turbine trip.

The pressurizer safety valve capacity is sized based on a complete loss of heat sink with the plant initially operating at the maximum calculated turbine load along with operation of the main steam safety valves. The pressurizer and main steam safety valves are then able to maintain the RCS and Main Steam System pressures within 110% of the corresponding design pressure without a direct reactor trip on turbine trip action.

The Turkey Point Units 3 and 4 Reactor Protection System and primary and secondary system designs preclude overpressurization without requiring the automatic rod control, pressurizer pressure control, and/or turbine bypass control system.

Method of Analysis

In this analysis, the behavior of the unit is evaluated for a complete loss of steam load from full power, without direct reactor trip, primarily to show the adequacy of the pressure-relieving devices, and also to demonstrate core protection margins; i.e., the turbine is assumed to trip without actuating all the sensors for reactor trip on the turbine stop valves. This assumption delays reactor trip until conditions in the reactor coolant system (RCS) result in a trip due to other signals. Thus, the analysis assumes a worst transient. In addition, no credit is taken for the turbine bypass system. Main feedwater flow is terminated at the time of turbine trip, with no credit taken for auxiliary feedwater (except for long-term recovery) to mitigate the consequences of the transient.

The turbine trip transients are analyzed by employing the detailed digital computer program LOFTRAN (Reference 1). The program simulates the neutron kinetics, RCS, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, and main steam safety valves. The program computes pertinent plant variables, including temperatures, pressures, DNBR, and power level.

Four cases are analyzed for a total loss of load from full power conditions.

1. Minimum reactivity feedback with pressure control;

2. Maximum reactivity feedback with pressure control;

3. Minimum reactivity feedback without pressure control; and

4. Maximum reactivity feedback without pressure control.

The primary concern for the cases analyzed with pressure control is minimum DNBR, whereas the primary concern for those cases analyzed without pressure control is maintaining reactor coolant and main steam system pressure below 110% of design pressure.

The major assumptions used in these analyses are summarized below:

A. Initial Operating Conditions

The automatic pressure control cases are analyzed using the Revised Thermal Design Procedure (Reference 2). The initial reactor power and RCS temperatures are assumed at their nominal values consistent with steady-state, full-power operation. Uncertainties in initial conditions are included in the departure from nucleate boiling ratio (DNBR) limit as described in WCAP-11397 (Reference 2). The RCS total flow rate assumed is the value of the minimum measured flow consistent with 20% steam generator tube plugging.

The cases without pressure control are analyzed using the Standard Thermal Design Procedure. Initial uncertainties on core power, reactor coolant temperature, and pressure are applied in the most conservative direction to obtain the initial plant conditions for the beginning of the transient. The RCS total flow rate assumed is the value of the thermal design flow consistent with 20% steam generator tube plugging.

B. Reactivity Coefficients

The total loss of load transient is analyzed with both maximum and minimum reactivity feedback. The maximum feedback (EOL) cases assume a large (absolute value) negative moderator temperature coefficient and the most negative Doppler only power coefficient. The minimum feedback (BOL) cases assume a zero moderator temperature coefficient and the least negative Doppler only coefficient.

C. Reactor Control

From the standpoint of the maximum pressures attained, it is conservative to assume that the reactor is in manual rod control. If the reactor were in automatic rod control, the control rod banks would move prior to trip and reduce the severity of the transient.

D. Pressurizer Spray and Power-Operated Relief Valves

The loss of load event is analyzed both with and without pressurizer pressure control (for both minimum and maximum reactivity feedback). The pressurizer PORVs and sprays are assumed operable for the cases with pressure control. The cases with pressure control conservatively minimize the increase in primary pressure which is more limiting for the DNBR transient. The cases without pressure control conservatively maximize the pressure increase which is more limiting for the RCS overpressurization criterion. In all cases the main steam and pressurizer safety valves are operable.

The pressurizer safety valve modeling includes the effects of the pressurizer safety valve loop seals. For those cases which are analyzed primarily for DNBR (pressurizer control cases), a -3% uncertainty was applied to reduce the setpoint. For those cases which are analyzed primarily for peak RCS pressure, a +2% uncertainty and a +1% set pressure shift were applied to increase the set point pressure by a total of 3%, such that the pressurizer safety valves begins to open at 2575 psia. Additionally, no steam flow is assumed until the valve loop seals are purged.

E. Feedwater Flow

Main feedwater flow to the steam generators is assumed to be lost at the time of turbine trip. No credit is taken for auxiliary feedwater flow, however, the auxiliary feedwater pumps would be expected to start on a trip of the main feedwater pumps. The auxiliary feedwater flow would remove core decay heat following plant stabilization.

F. Reactor Trip

Only the overtemperature ΔT, high pressurizer pressure, and low-low steam generator water level reactor trips are assumed operable for the purposes of this analysis. No credit is taken for a reactor trip on high pressurizer level or the direct reactor trip on turbine trip.

G. Steam Release

No credit is taken for the operation of the steam dump to condenser or atmosphere. This assumption maximizes both primary and secondary pressure. The main steam safety valves are assumed to be fully open at the valve set-pressure plus 6%. This includes 3% setpoint tolerance plus 3% valve accumulation.

H. Pressure Drop in the Main Steam Safety Valves Piping

The pressure drop in the piping between the steam generators and the Main Steam Safety Valves is included (Reference 3).

<u>Results</u>

The transient responses for a total loss of load from 100 percent of full-power operation are shown for four cases. The calculated sequence of events for the accident is shown in Table 14.1.10-1.

Case 1:

Figures 14.1.10-1 through 14.1.10-3 show the transient response for the total loss of steam load event under BOL conditions, including a zero moderator temperature coefficient, with pressure control. The reactor is tripped on overtemperature ΔT. The neutron flux increases until the reactor is tripped, and although the DNBR value decreases below the initial value, it remains well above the safety analysis limit throughout the entire transient. The pressurizer relief valves and sprays maintain primary pressure below 110% of the design value. The main steam safety valves are also actuated and maintain secondary pressure below 110% of the design value.

Case 2:

Figures 14.1.10-4 through 14.1.10-6 show the transient response for the total loss of steam load event under EOL conditions, assuming a conservatively large positive moderator density coefficient of 0.5 Δk/gm/cc (corresponding to a large negative moderator temperature coefficient) and a most-negative Doppler only power coefficient, with pressure control. The reactor does not trip under these conditions. The plant stabilizes at a power level established by the relief valve capacity of the main steam safety valves. Without operator intervention, the reactor would eventually reach a low-low steam generator water level reactor trip condition as the secondary system inventory decreases. The DNBR increases throughout the transient and never drops below the initial value. The pressurizer relief valves and sprays maintain primary pressure below 110% of the design value. The pressurizer pressure remains below the safety valve setpoint during the transient. The main steam safety valves are also actuated and maintain secondary pressure below 110% of the design value.

Case 3:

Figures 14.1.10-7 through 14.1.10-9 show the transient response for the total loss of steam load event under BOL conditions, including a zero moderator temperature coefficient, without pressure control. The reactor is tripped on high pressurizer pressure. The neutron flux remains essentially constant at full power until the reactor is tripped, and the DNBR remains above the initial value for the duration of the transient. The pressurizer safety valves are actuated and maintain primary pressure below 110% of the design value. The main steam safety valves are also actuated and maintain secondary pressure below 110% of the design value.

Case 4:

Figures 14.1.10-10 through 14.1.10-12 show the transient response for the total loss of steam load event under EOL conditions, assuming a conservatively large positive moderator density coefficient of 0.5 Δk/gm/cc (corresponding to a large negative moderator temperature coefficient) and a most-negative Doppler only power coefficient, without pressure control. The reactor is tripped on high pressurizer pressure. The DNBR increases throughout the transient and never drops below the initial value. The pressurizer safety valves are actuated and maintain primary pressure below 110% of the design value. The main steam safety valves are also actuated and maintain secondary pressure below 110% of the design value.

<u>Conclusions</u>

The analysis indicates that a total loss of load without a direct or immediate reactor trip presents no hazard to the integrity of the Reactor Coolant System and the Steam System. All of the applicable acceptance criteria are met. The minimum DNBR for each case is greater than the safety analysis limit value. The peak primary and secondary pressures remain below 110% of design at all times.

REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-proprietary), Burnett, T. W. T., et al.,"LOFTRAN Code Description," dated April 1984.

2. Westinghouse WCAP-11397 (Proprietary), Friedland, A. J., and Ray, S., "Revised Thermal Design Procedure," dated April 1989.

3. "Florida Power & Light Co., Turkey Point Units 3 & 4, UFSAR and AABD Updates Resulting from Reanalysis of Non-LOCA Transients Impacted by)P Losses Between the Steam Generator and MSSVs and LOCA Assessment," J. J. Deblasio to J. Perryman, FPLN-97-0108, NSD-SAE-ESI-97-342, June 2, 1997.

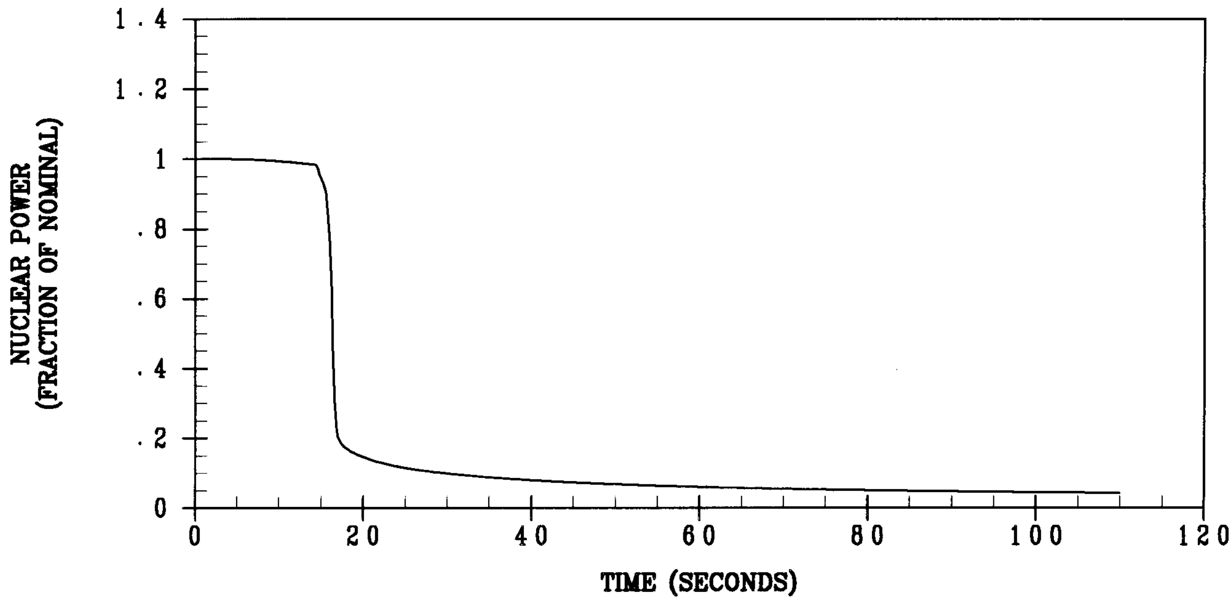
TABLE 14.1.10-1

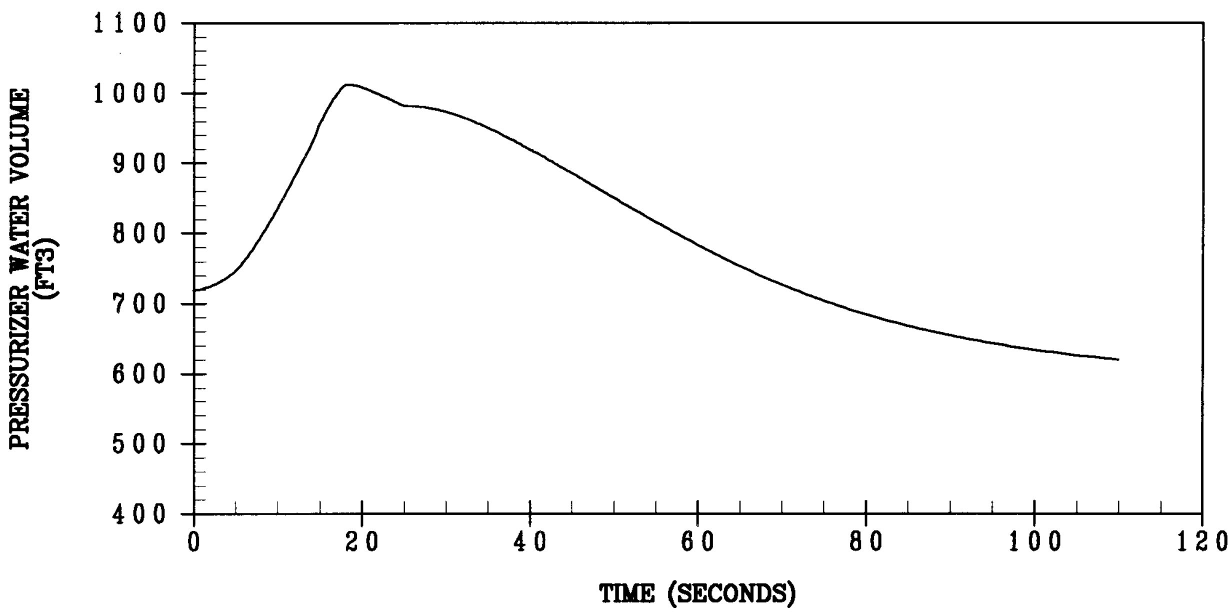
SEQUENCE OF EVENTS - LOSS OF LOAD/TURBINE TRIP ACCIDENTS

Case	Event	Time (sec)
With pressurizer pressure control (minimum reactivity feedback)	Turbine Trip	0.0
	Overtemperature ΔT setpoint reached	12.1
	Peak RCS pressure occurs	14.0
	Rods begin to drop	14.1
	Minimum DNBR occurs	15.2
	Peak Main Steam System pressure occurs	20.6
With pressurizer pressure control (maximum reactivity feedback) (See Note 2)	Turbine Trip	0.0
	Peak RCS pressure occurs	7.6
	Peak Main Steam System pressure occurs	21.2
	Minimum DNBR occurs	(Note 1)
Without pressurizer pressure control (minimum reactivity feedback)	Turbine Trip	0.0
	High Pressurizer pressure setpoint reached	7.0
	Rods begin to drop	9.0
	Peak RCS pressure occurs	9.9
	Peak Main Steam System pressure occurs	15.8
	Minimum DNBR occurs	(Note 1)
Without pressurizer pressure control (maximum reactivity feedback)	Turbine Trip	0.0
	High Pressurizer pressure setpoint reached	7.0
	Rods begin to drop	9.0
	Peak RCS pressure occurs	10.0
	Peak Main Steam System pressure occurs	14.8
	Minimum DNBR occurs	(Note 1)

Notes:

1. Never falls below the initial value.
2. A reactor trip condition is never reached in the analysis. The reactor stabilizes at a power level established by the relief capacity of the MSSVs. Eventually, a low-low steam generator water level reactor trip would occur.





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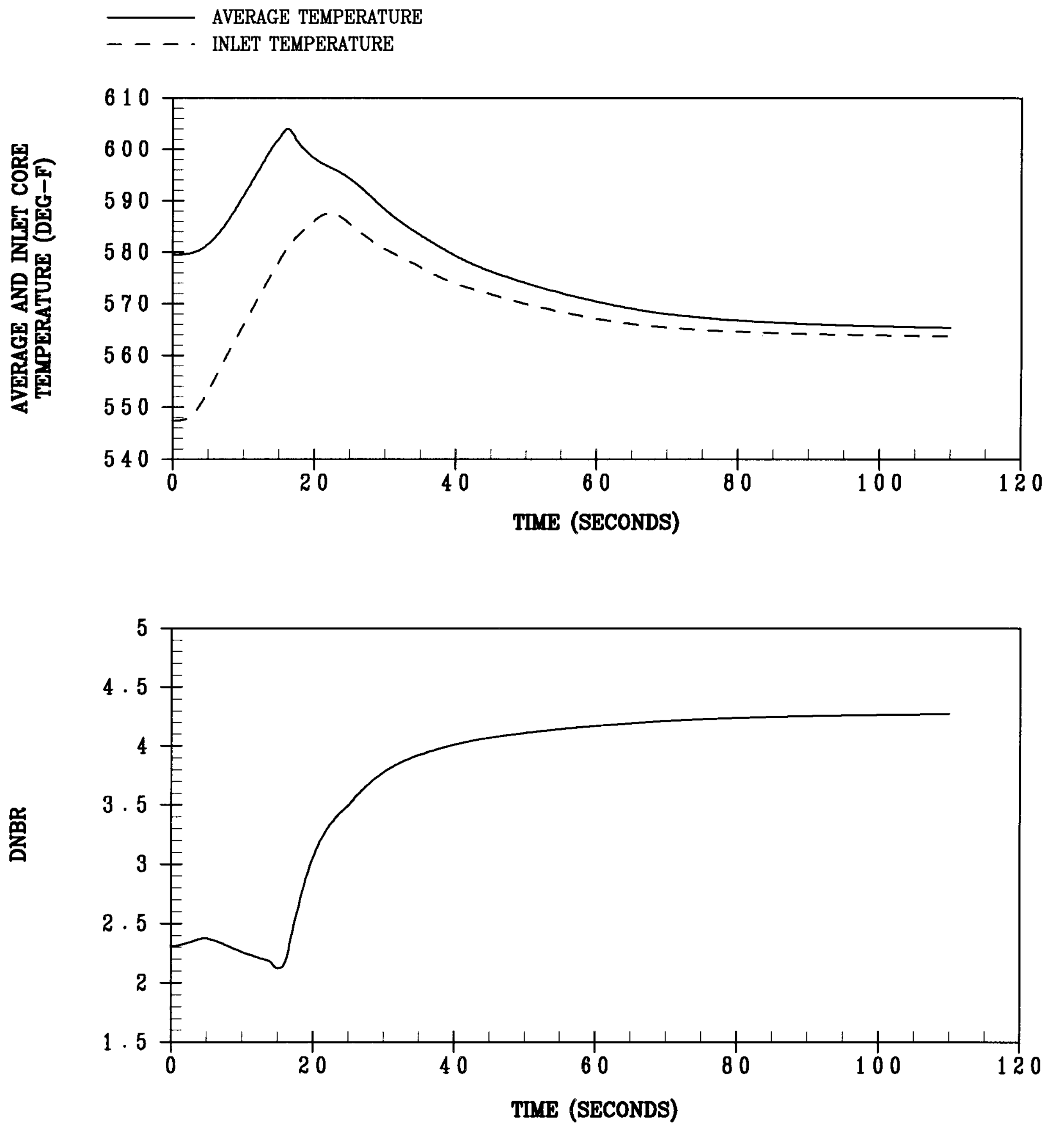
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-1



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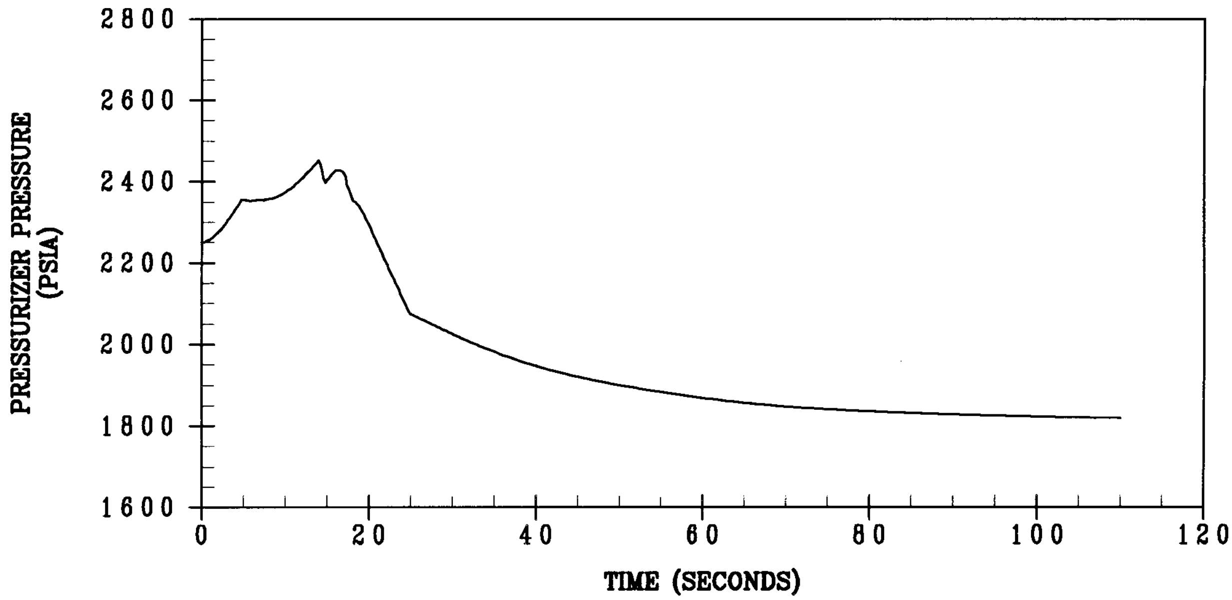
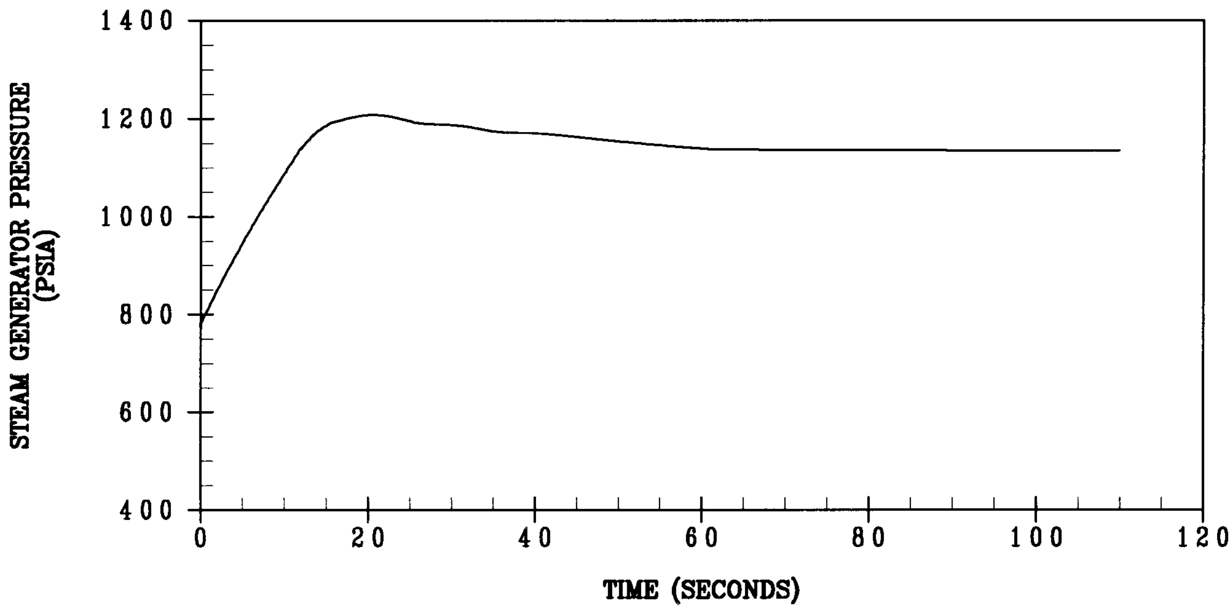
TURKEY POINT PLANT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-2





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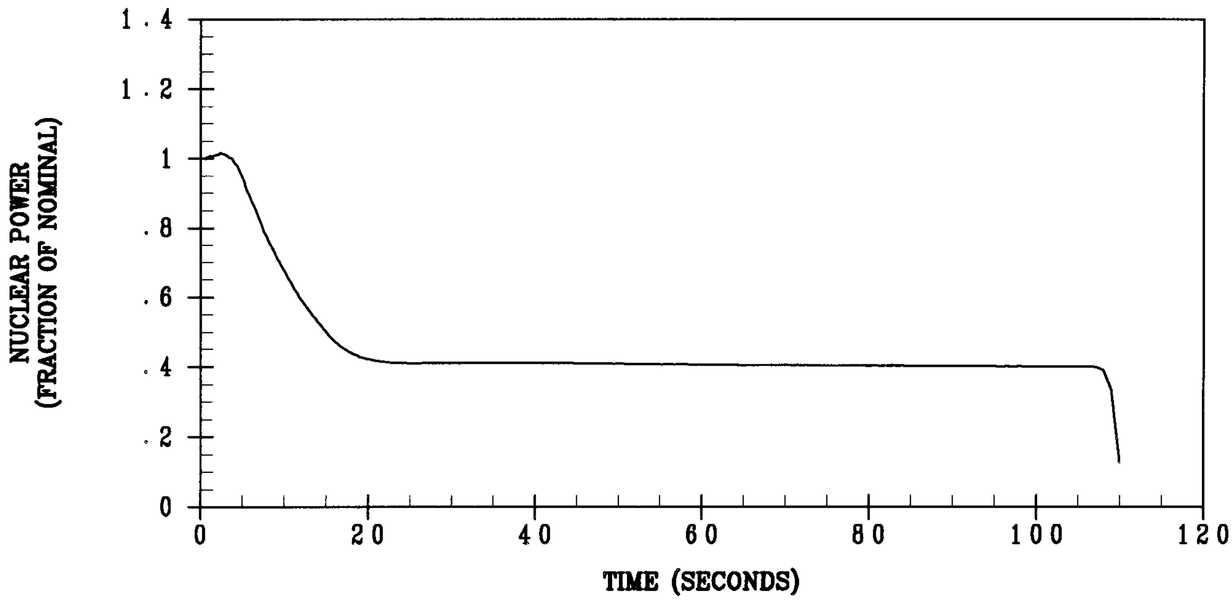
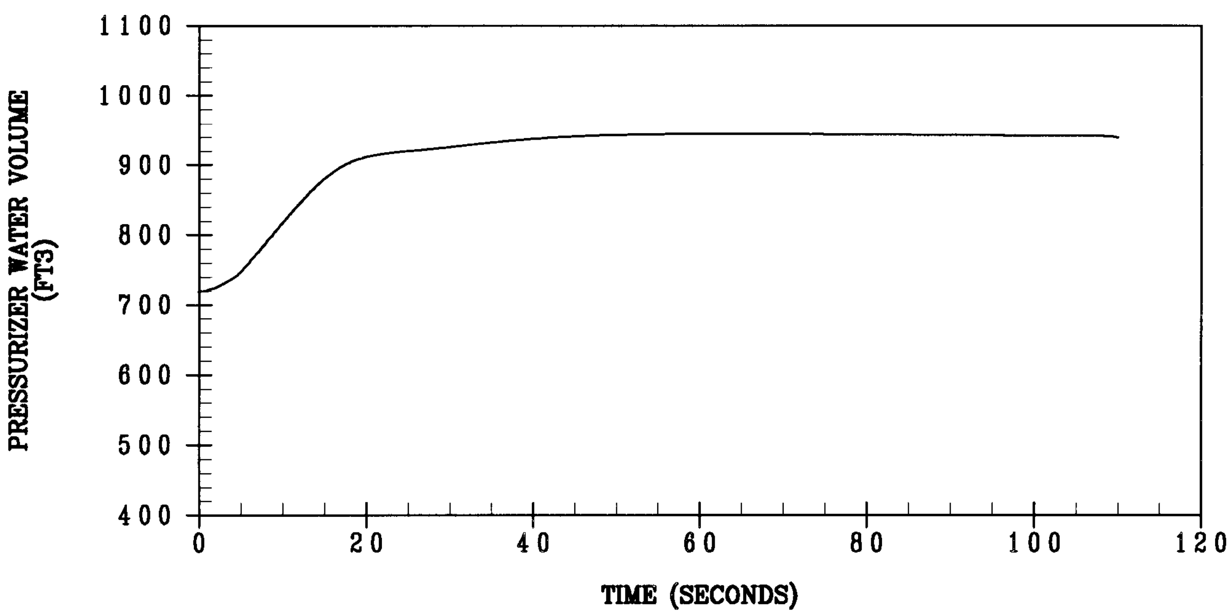
TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-3





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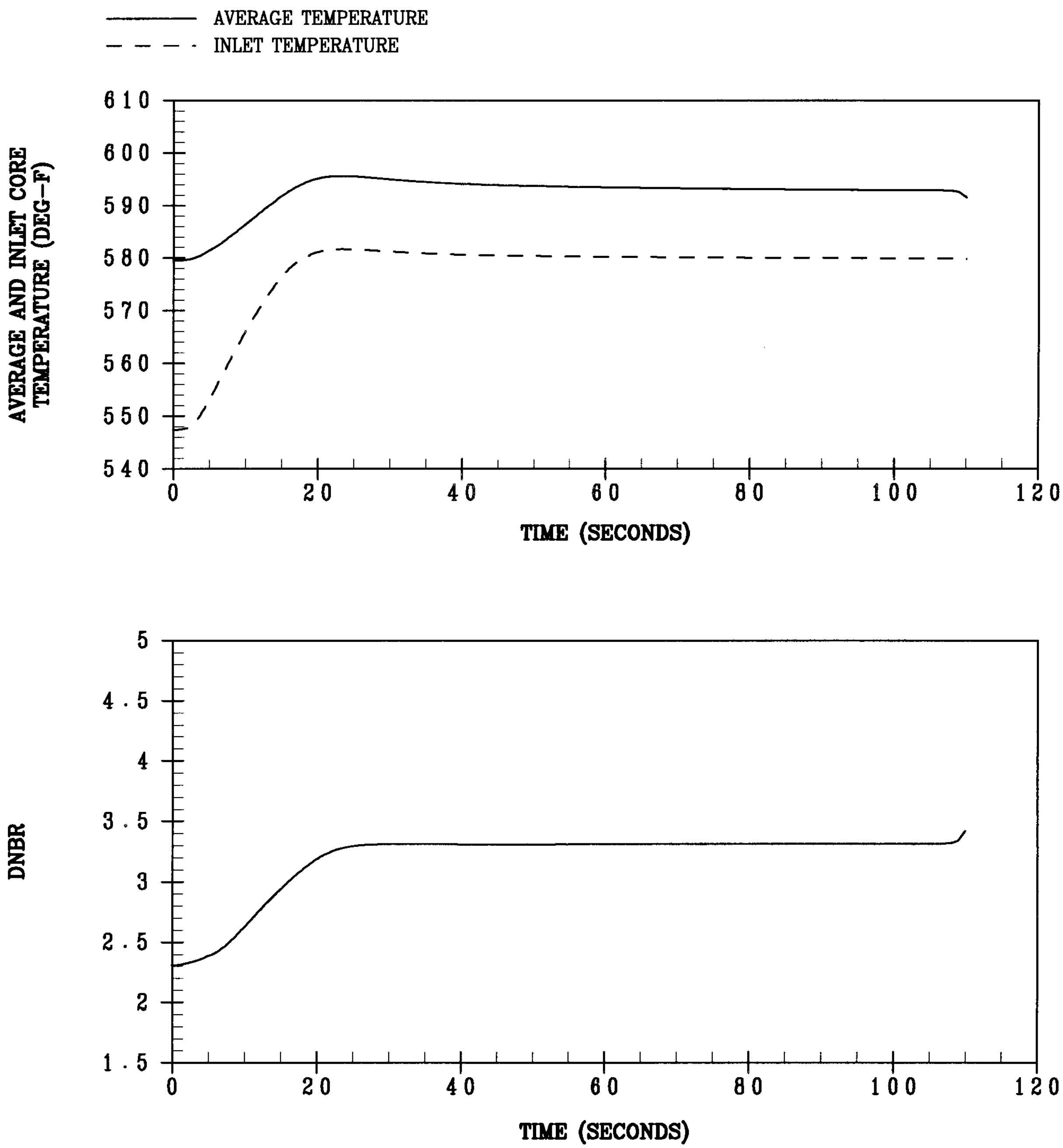
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-4



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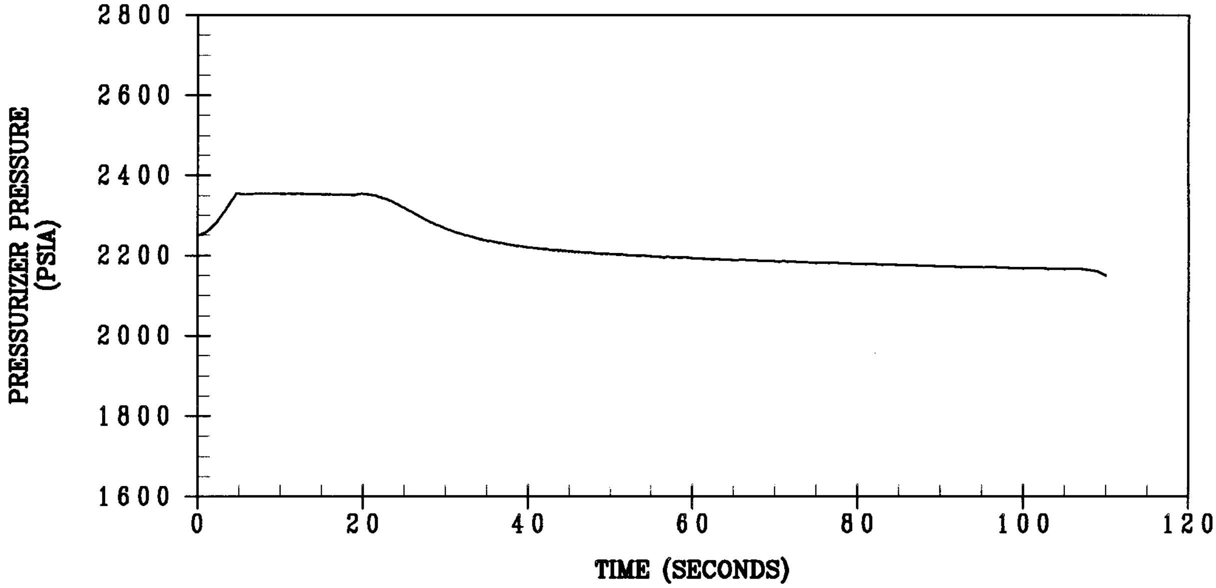
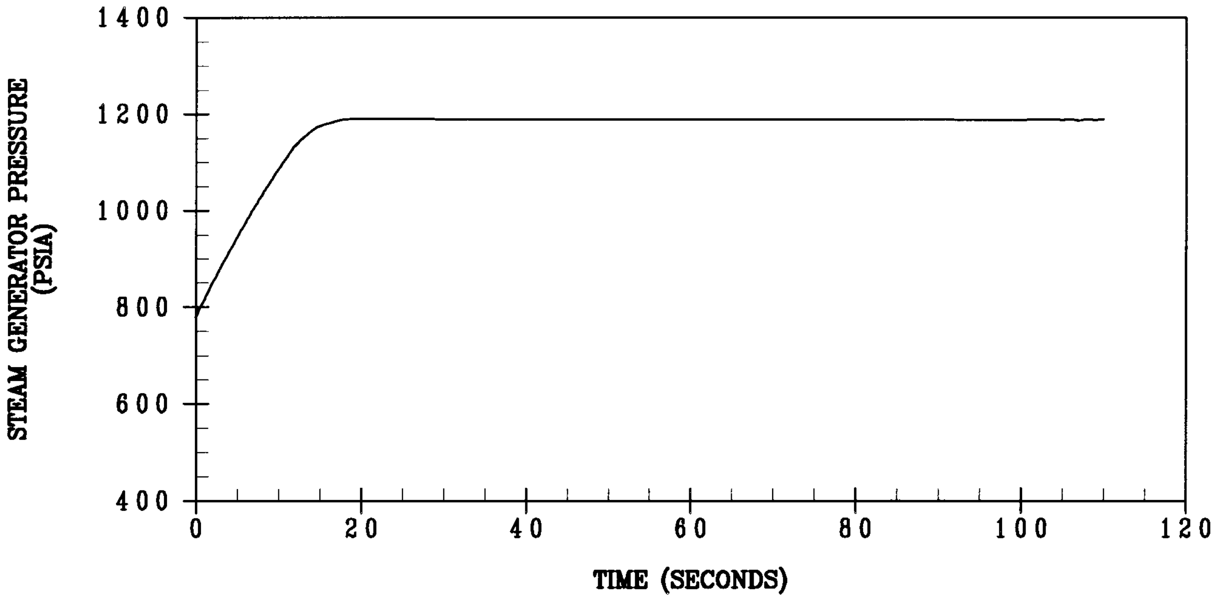
TURKEY POINT PLANT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-5





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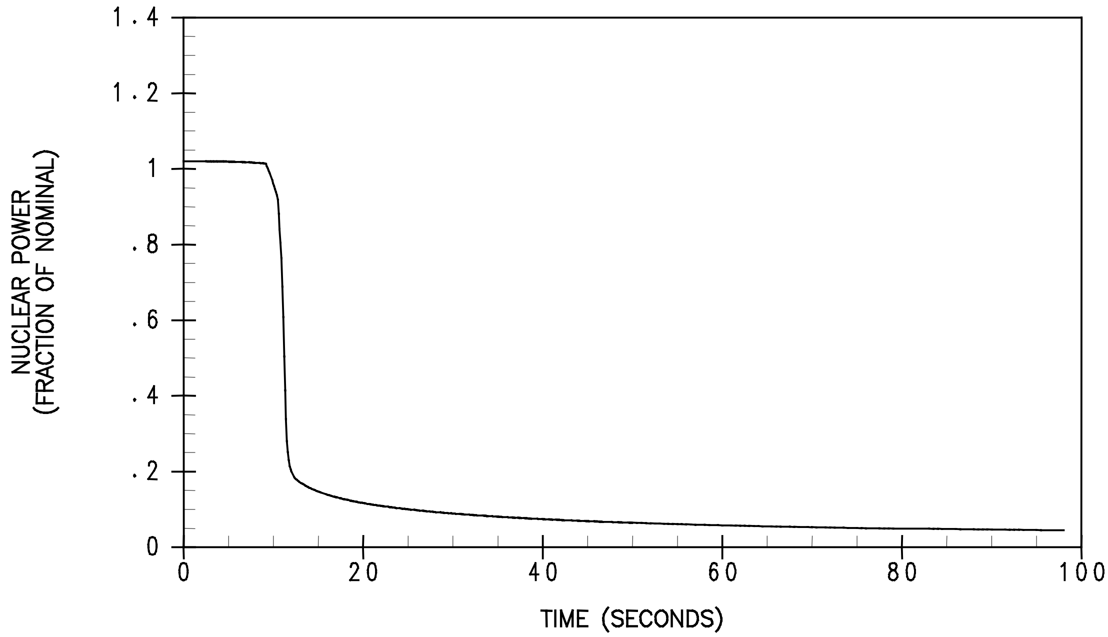
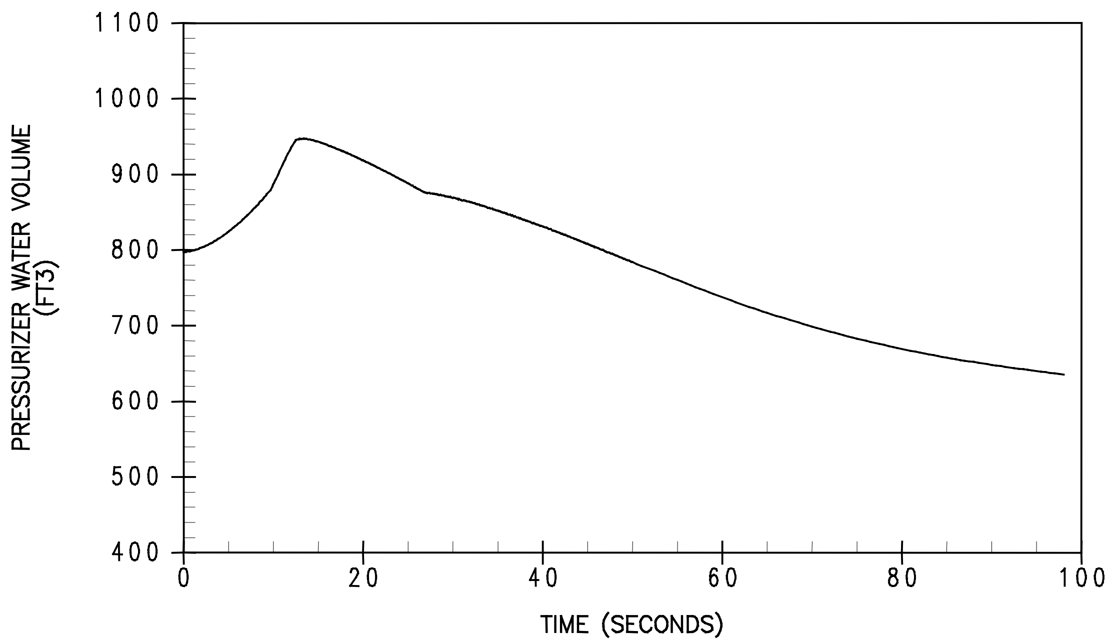
TURKEY POINT PLANT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITH PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-6





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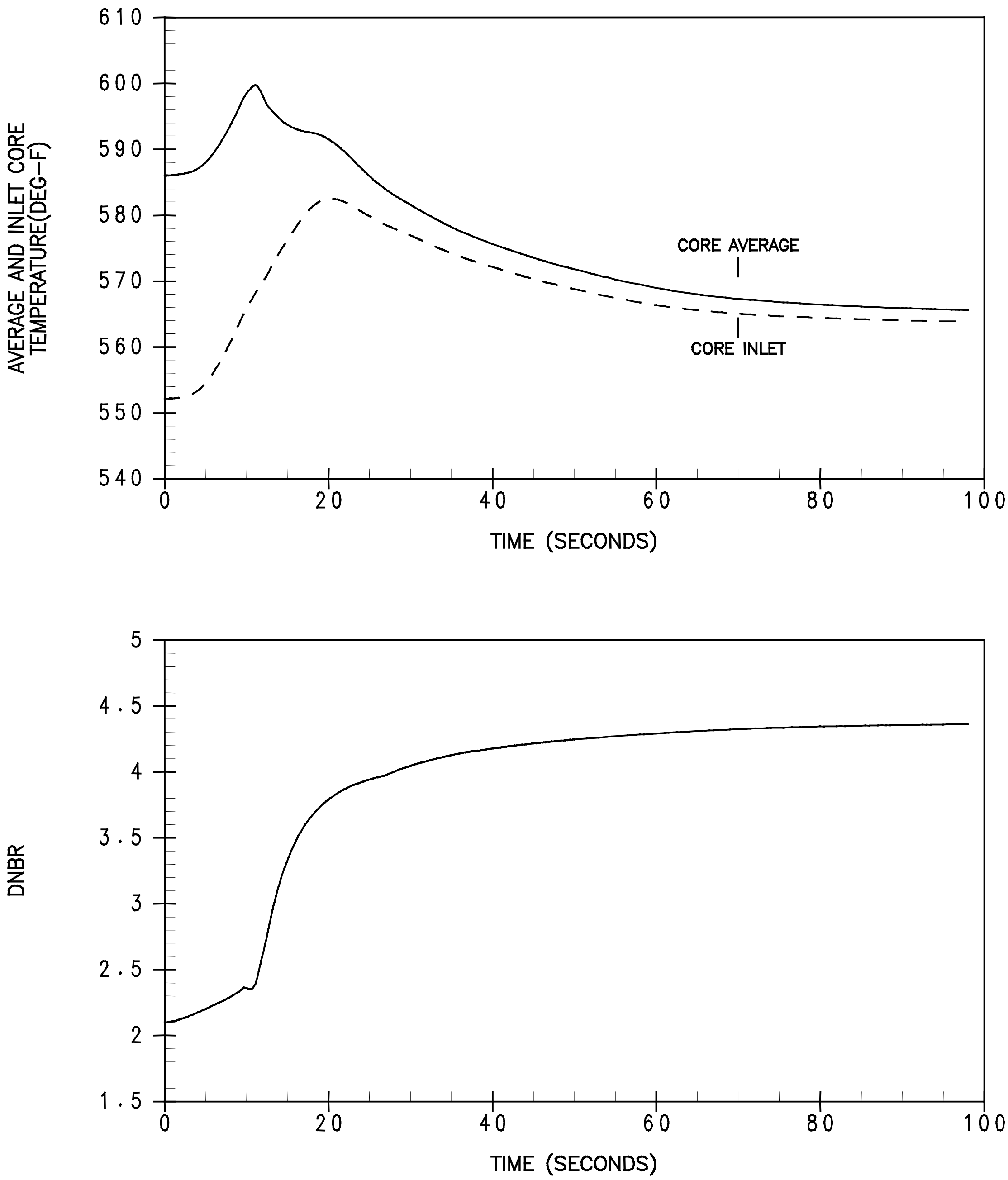
TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL
LOAD WITHOUT PRESSURE CONTROL
MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-7



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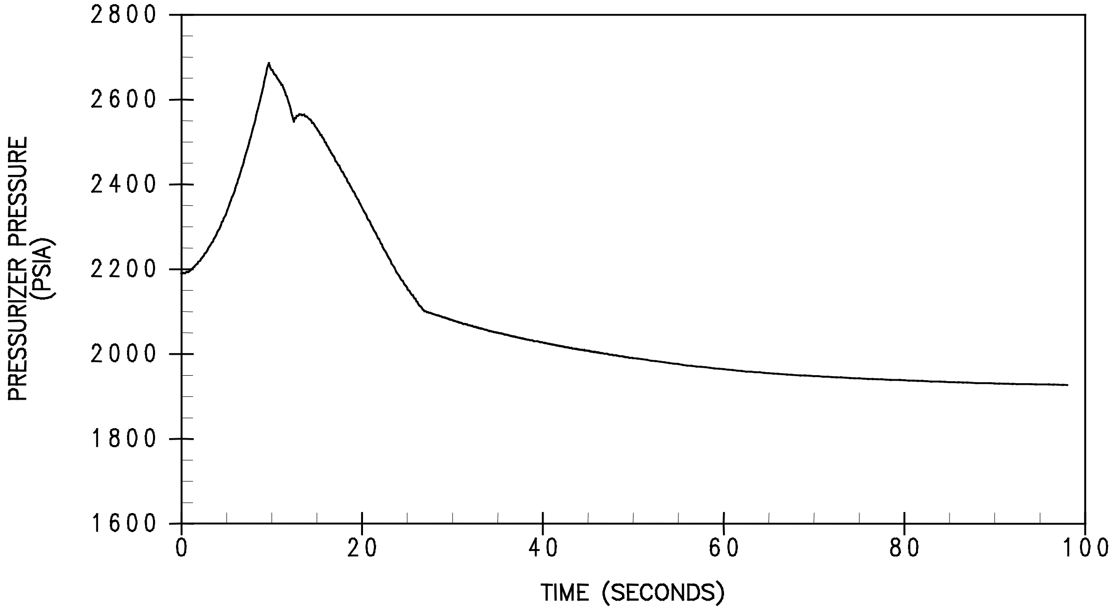
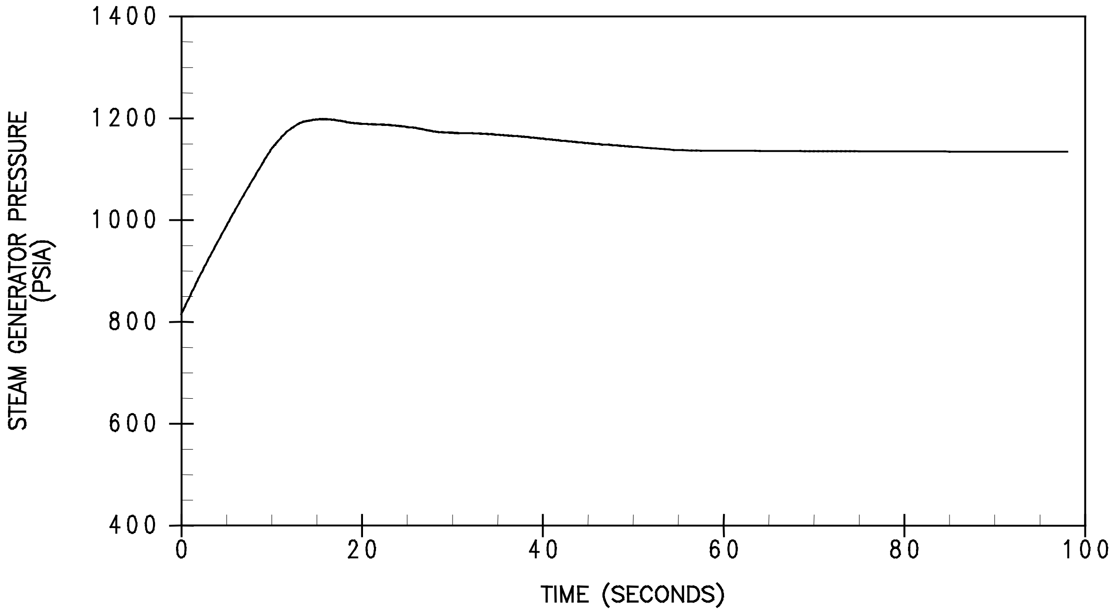
TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITHOUT PRESSURE CONTROL MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-8





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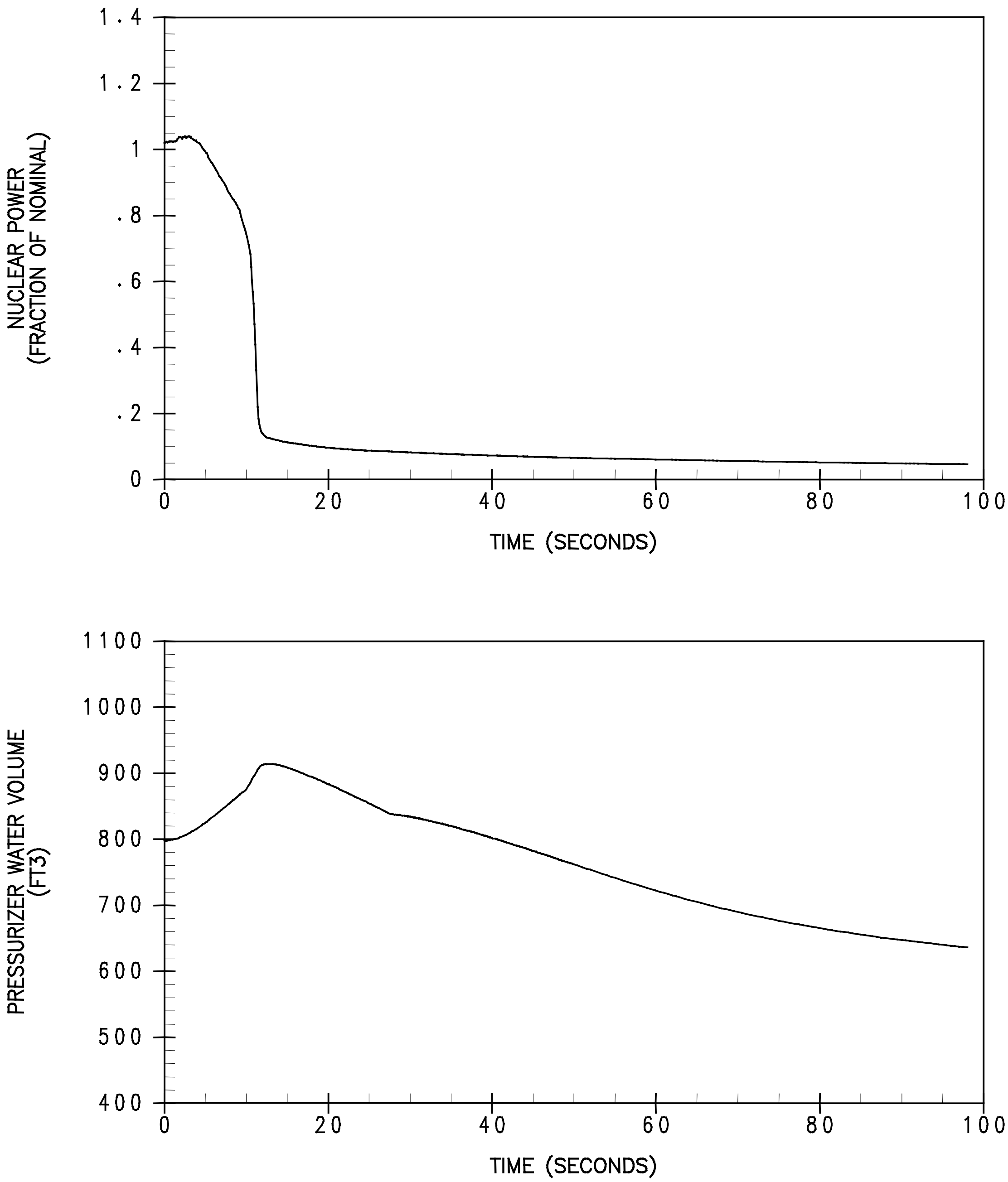
TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITHOUT PRESSURE CONTROL MINIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-9



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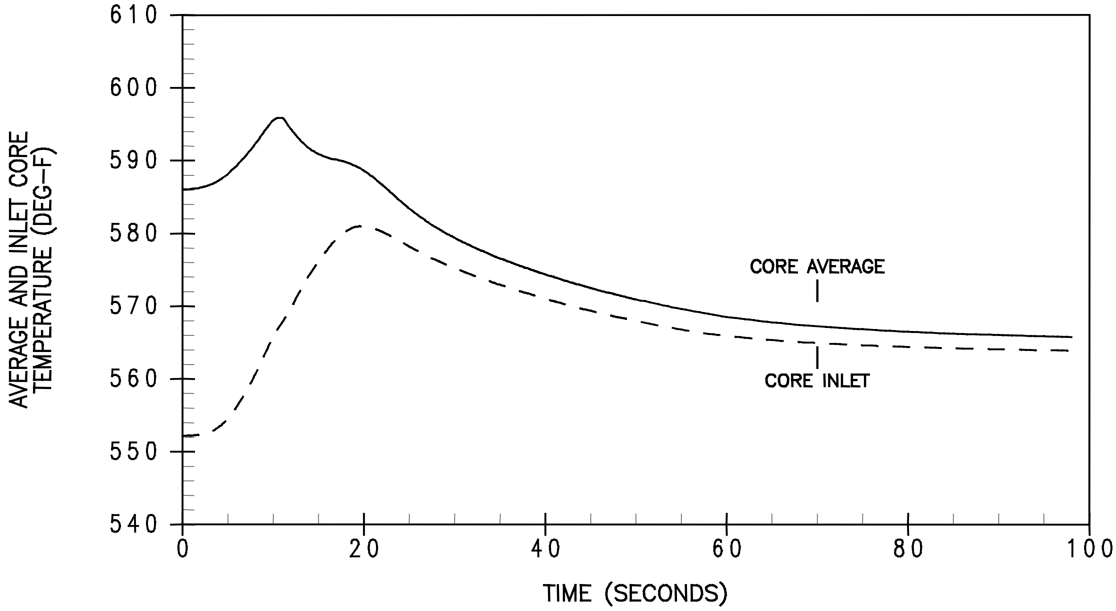
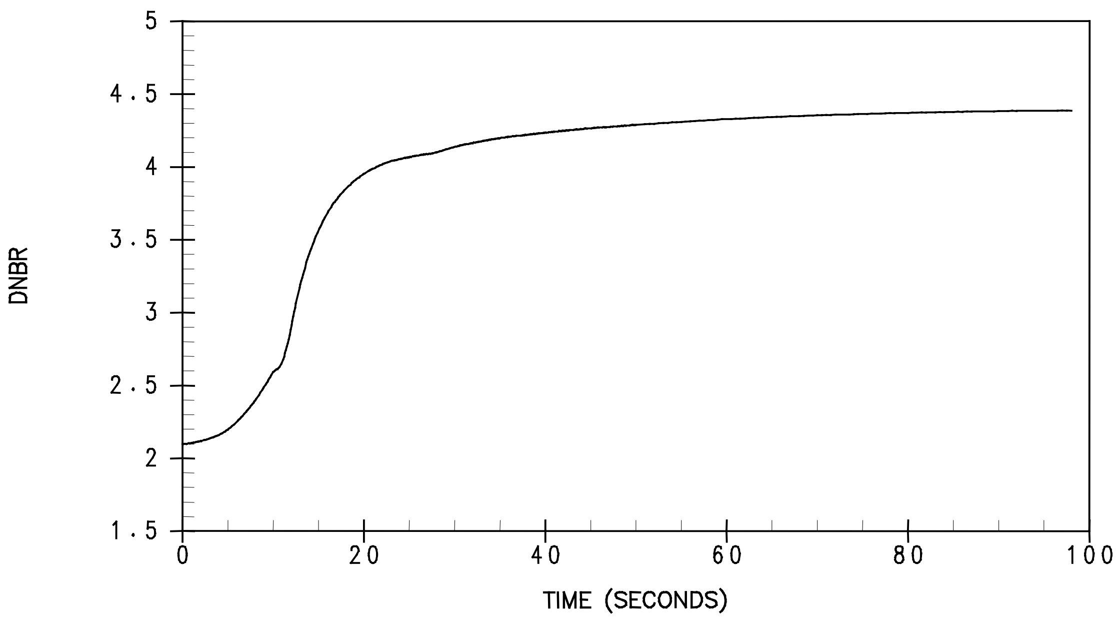
TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITHOUT PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-10





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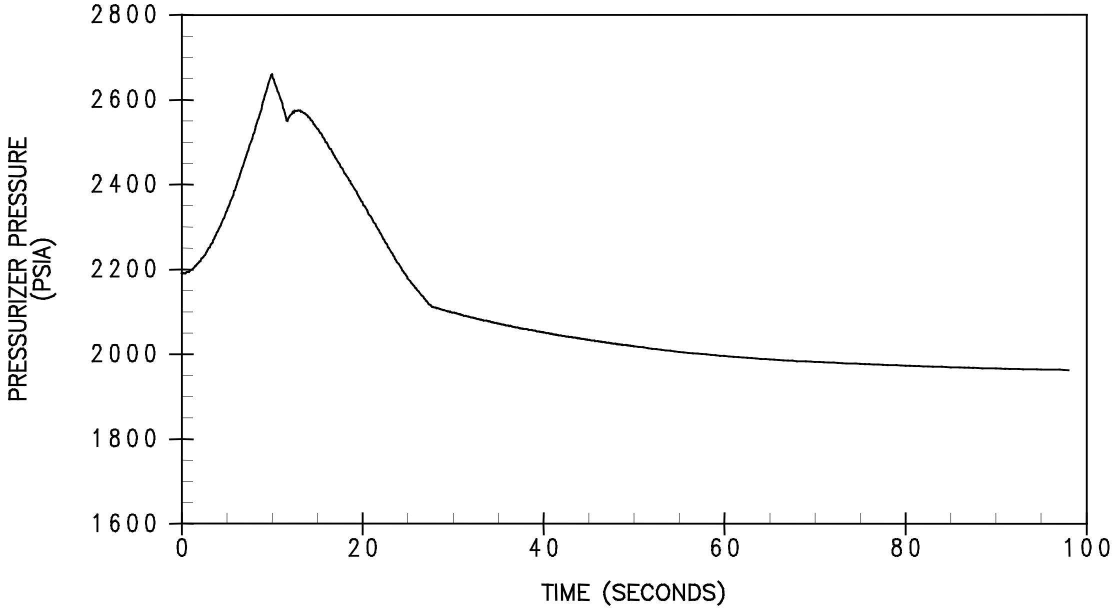
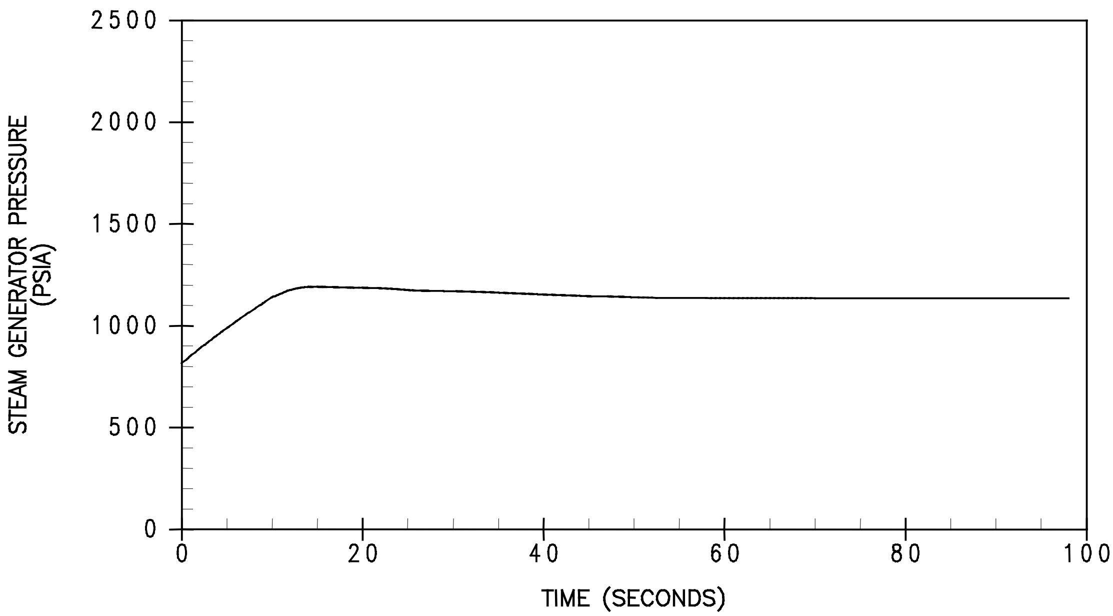
TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITHOUT PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-11





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TURKEY POINT UNITS 3 & 4

TOTAL LOSS OF EXTERNAL ELECTRICAL LOAD WITHOUT PRESSURE CONTROL MAXIMUM REACTIVITY FEEDBACK

FIGURE 14.1.10-12

14.1.11 LOSS OF NORMAL FEEDWATER FLOW

14.1.11.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

A loss of normal feedwater (from pump failures, valve malfunctions, or loss of offsite AC power) results in a reduction in capability of the secondary system to remove the heat generated in the reactor core. If an alternate supply of feedwater were not supplied to the plant, core residual heat following reactor trip would heat the primary system water to the point where water relief from the pressurizer would occur, resulting in a substantial loss of water from the Reactor Coolant System (RCS). Since the plant is tripped well before the steam generator heat transfer capability is reduced, the primary system variables never approach a DNB condition.

The following occur upon loss of normal feedwater (assuming main feedwater pump failures or valve malfunctions):

1. As the steam system pressure rises following the trip, the atmospheric dump valves are automatically opened to the atmosphere. Steam dump to the condenser is assumed not to be available. If steam flow through the atmospheric dump valves is not available, the main steam safety valves may lift to dissipate the sensible heat of the fuel and coolant plus the residual decay heat produced in the reactor.

2. As the no load temperature is approached, the atmospheric dump valves (or safety valves, if the atmospheric dump valves are not available) are used to dissipate the residual decay heat and to maintain the plant at the hot shutdown condition.

The following provides the necessary protection against a loss of normal feedwater:

1. Reactor trip on:

    a. Low-low water level in any steam generator.

    b. Steam flow-feedwater flow mismatch coincident with low water level in any steam generator.

2. Three turbine-driven auxiliary feedwater pumps (shared by Units 3 & 4) are started on any of the following:

   a. Low-low water level in any steam generator.
   b. Any safety injection signal.
   c. Loss of offsite power (automatic transfer to diesel generators).
   d. Loss of either A or B 4.16 kV bus on either unit.
   e. Trip of all main feedwater pumps in either unit.
   f. Manual actuation.

An analysis of the system transient is presented below to show that following a loss of normal feedwater, the Auxiliary Feedwater System is capable of removing the stored and residual heat, thus preventing either overpressurization of the RCS, overpressurization of the secondary side, or water relief from the pressurizer and uncovering of the reactor core.

14.1.11.2 ANALYSIS OF EFFECTS AND CONSEQUENCES

Method of Analysis

A detailed analysis using the LOFTRAN code (Reference 1) is performed in order to obtain the plant transient conditions following a loss of normal feedwater. The analysis addresses the core neutron kinetics, RCS including natural circulation, pressurizer, pressurizer PORVs and sprays, steam generators, main steam safety valves, and auxiliary feedwater system. The digital program computes pertinent variables including the pressurizer pressure, pressurizer water level, steam generator mass, and reactor coolant average temperature.

Assumptions made in the analysis are:

1. The plant is initially operating at 102 percent of the nominal NSSS power of 2311.4 MWt which includes a maximum reactor coolant pump heat of 11.4 MWt. The reactor coolant volumetric flow is assumed to remain constant at its Thermal Design value. Although not assumed in the analysis, the reactor coolant pumps may be manually tripped at some later time to reduce the heat addition to the RCS.

2. The initial reactor coolant average temperature is 6.0°F higher than the nominal value which is comprised of the uncertainty on nominal temperature. The initial pressurizer pressure uncertainty is 60 psi.

3. Reactor trip occurs on steam generator low-low water level at 4.0% of narrow range span, including an incremental reduction of 10% in the steam generator water mass to account for modeling uncertainties.

4. The worst single failure is assumed to occur in the auxiliary feedwater system. This results in the availability of only one auxiliary feedwater pump supplying a minimum of 310 gpm to three steam generators, 120 seconds following a low-low steam generator water level signal.

5. The pressurizer sprays, heaters (Reference 4), and PORVs are assumed operable. This maximizes the peak transient pressurizer water volume. If these control systems did not operate, the pressurizer safety valves would maintain peak RCS pressure at or below the actuation setpoint (2500 psia) throughout the transient.

6. Secondary system steam relief is achieved through the self-actuated main steam safety valves. Note that steam relief will, in fact, be through the atmospheric dump valves or condenser dump valves for most cases of loss of normal feedwater. However, for the sake of analysis, these have been assumed unavailable.

7. The main steam safety valves are assumed to be fully open at the valve set-pressure plus 6%. This includes 3% setpoint tolerance plus 3% valve accumulation.

8. The AFW line purge volume is conservatively assumed to be the average value for Unit 3 which is 129.7 ft^3. The average purge volume for Unit 4 is 127 ft^3. An inital maximum AFW enthalpy of 73.0 Btu/lbm is assumed.

9. Core residual heat generation is based on the 1979 version of ANS 5.1 (Reference 2). ANSI/ANS-5.1-1979 is a conservative representation of the decay energy release rates. Long-term operation at the initial power level preceding the trip is assumed.

10. The pressure drop in the piping between the steam generators and the main steam safety valves is included (Reference 3).

Results

Figures 14.1.11-1 and 14.1.11-2 show the significant plant parameters following a loss of normal feedwater with the assumptions listed in the previous subsection.

The calculated sequence of events for this accident is listed in Table 14.1.11-1. Following the reactor and turbine trip from full load, the water level in the steam generators will fall due to the reduction of steam generator void fraction, and because steam flow through the safety valves continues to dissipate the stored and generated heat. Two minutes following the initiation of the low-low water level trip, the auxiliary feedwater pump starts, consequently reducing the rate of water level decrease in the steam generators.

The capacity of one auxiliary feedwater pump is such that the water level in the steam generators does not recede below the level at which sufficient heat transfer area is available to dissipate core residual heat and reactor coolant pump heat without water relief from the RCS pressurizer relief or safety valves. Figure 14.1.11-1 shows that at no time is there water relief from the pressurizer. If the auxiliary feedwater delivered is greater than that of one AFW pump, or the initial reactor power is less than 102% of the NSSS power, or the steam generator water level in one or more steam generators is above the conservatively low 4% narrow range span level assumed for the low-low steam generator setpoint, the results for this transient will be bounded by the analysis presented.

14.1.11.3 CONCLUSIONS

Results of the analysis show that a loss of normal feedwater does not adversely affect the core, the RCS, or the main steam system, since the auxiliary feedwater capacity is such that all applicable acceptance criteria are met.

14.1.11.4 REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-Proprietary), Burnett, T. W. T., et al, "LOFTRAN Code Description," dated April 1984.

2. ANSI/ANS-5.1-1979, "American National Standard for Decay Heat Power in Light Water Reactors," dated August 1979.

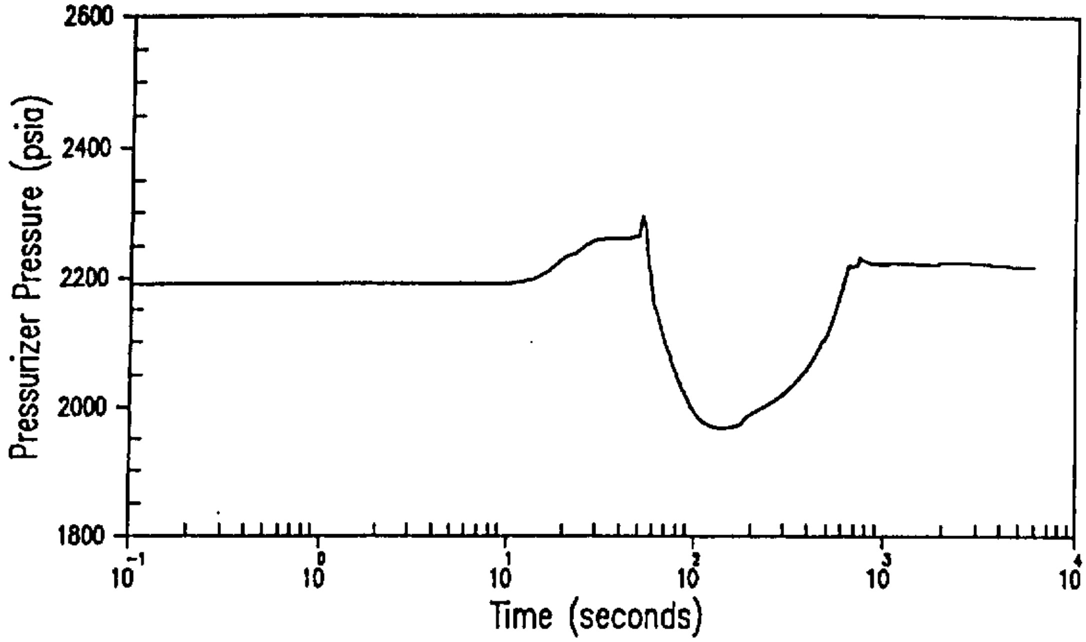
3. "Florida Power & Light Co., Turkey Point Units 3 & 4, UFSAR and AABD Updates Resulting from Reanalysis of Non-LOCA Transients Impacted by Δ P Losses Between the Steam Generator and MSSVs and LOCA Assessment," J.J. Deblasio to J. Perryman, FPLN-97-0108, NSD-SAE-ESI-97-342, June 2, 1997.

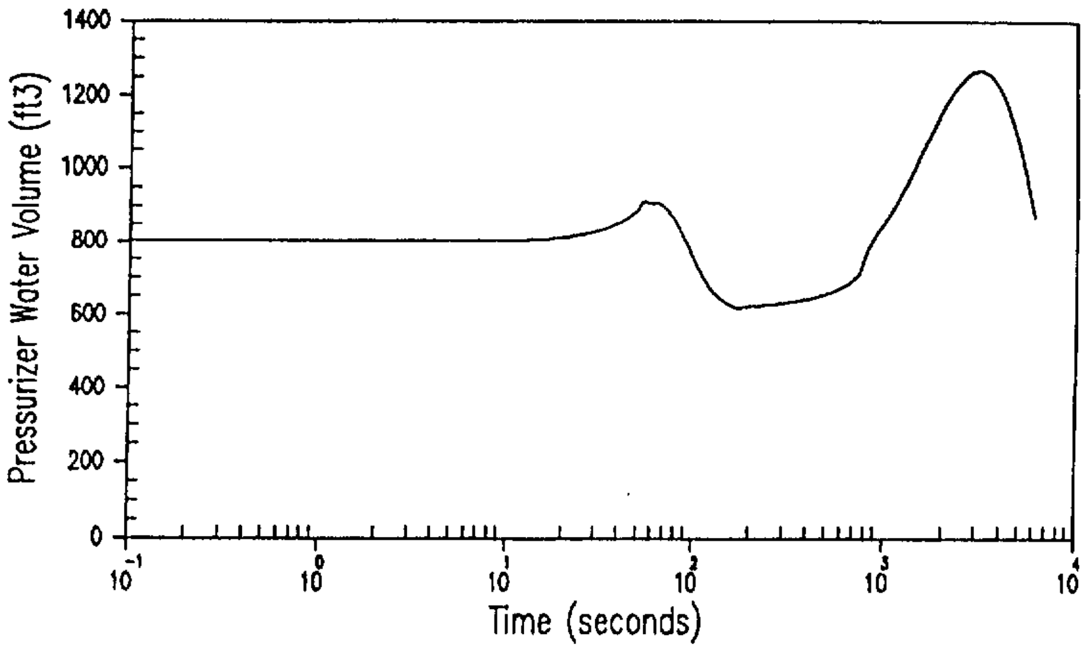
4. “Florida Power & Light Co., Turkey Point Units 3 & 4, Analysis Modeling of Pressurizer Heaters,” N.R. Metcalf to J.L. Perryman, 98FP-G-0104, September 23, 1998.

TABLE 14.1.11-1

SEQUENCE OF EVENTS
FOR
LOSS OF NORMAL FEEDWATER FLOW

Event	Time (sec)
Main feedwater flow stops	10
Low-low steam generator water level trip	47.5
Rods begin to drop	49.5
Flow from one turbine driven auxiliary feedwater pump is started	167.5
Feedwater lines are purged and cold auxiliary feedwater is delivered to three steam generators	732.0
Peak water level in pressurizer occurs (post trip)	3084.0
Core decay and reactor coolant pump heat decreases to auxiliary feedwater heat removal capacity	~3100





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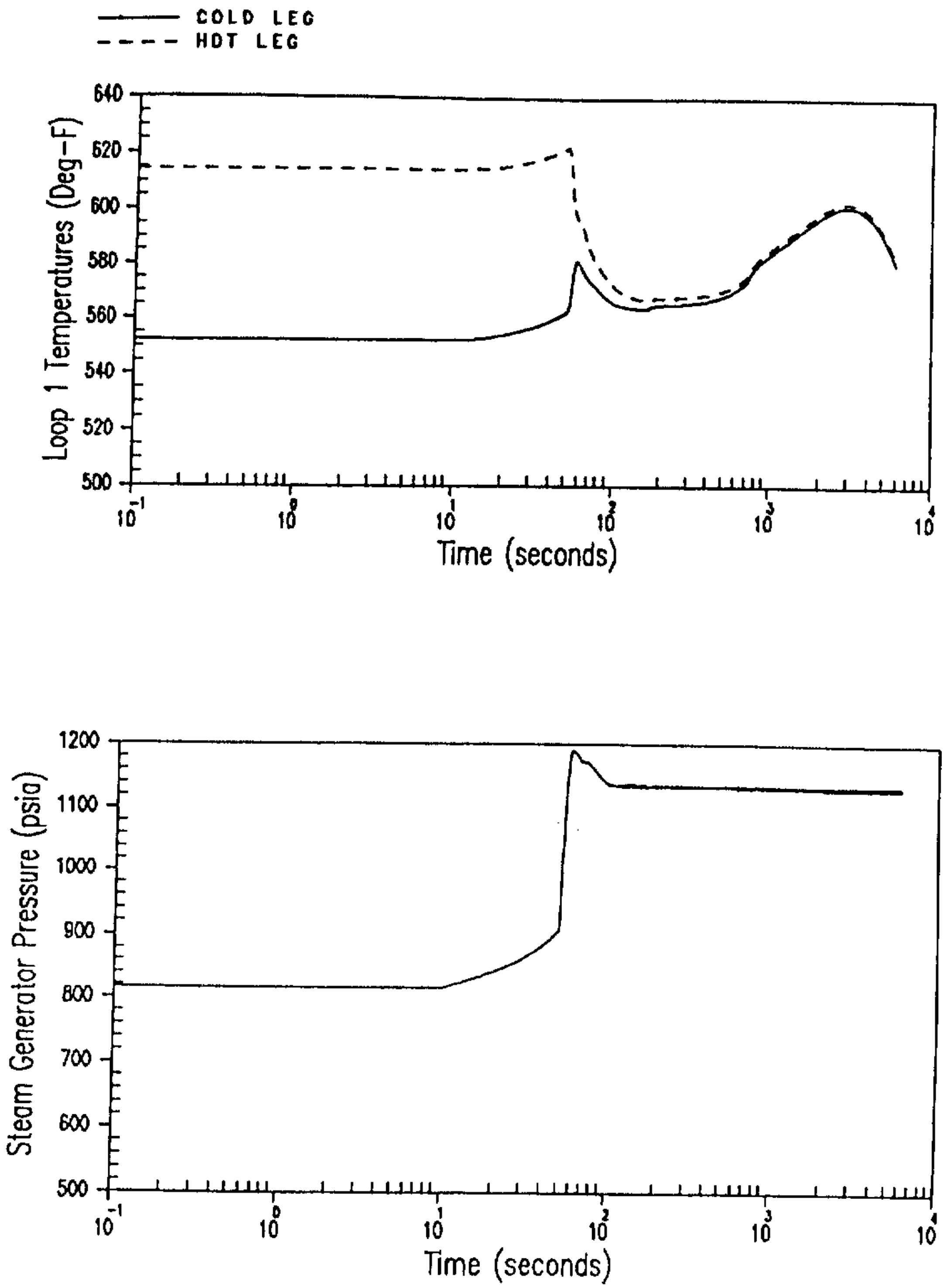
TURKEY POINT PLANT UNITS 3 & 4

PRESSURIZER PRESSURE AND WATER VOLUME TRANSIENTS FOR LOSS OF NORMAL FEEDWATER

FIGURE 14.1.11-1



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LOOP TEMPERATURES AND STEAM GENERATOR PRESSURE FOR LOSS OF NORMAL FEEDWATER

FIGURE 14.1.11-2

14.1.12 LOSS OF NON-EMERGENCY A-C POWER TO THE PLANT AUXILIARIES

14.1.12.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

A complete loss of non-emergency AC power may result in the loss of all power to the plant auxiliaries, i.e., the reactor coolant pumps, condensate pumps, etc. The loss of power may be caused by a complete loss of the offsite grid accompanied by a turbine generator trip at the station, or by a loss of the onsite AC distribution system.

Following a loss of AC power with turbine and reactor trips, the sequence described below will occur:

1. Plant vital instruments are supplied from emergency DC power sources.

2. As the steam system pressure rises following the trip, the atmospheric dump valves are automatically opened to the atmosphere. The condenser is assumed not to be available for steam dump. If the steam flow rate through the dump valves is not available, the main steam safety valves may lift to dissipate the sensible heat of the fuel and coolant plus the residual decay heat produced in the reactor.

3. As the no load temperature is approached, the atmospheric dump (or safety valves, if the dump valves are not available) are used to dissipate the residual decay heat and to maintain the plant at the hot shutdown condition.

4. Both emergency diesel generators associated with the unit will automatically start following the loss of voltage to the A and B 4160 volt buses of that unit. At the same time, these buses will be isolated from their normal supply and their motor supply and feed breakers will be opened. The breaker from the emergency diesel generator to its associated 4160 volt bus will close energizing the buses. Equipment will be sequentially loaded on to the 4160 volt buses, load centers and motor control centers will be energized as controlled by the load

sequencers. All required additional manual loads will be powered by the emergency diesel generators as required by procedures.

The following provides the necessary protection against a loss of AC power:

1. Reactor trip on:
   a. Low-low water level in any steam generator.
   b. Steam flow-feedwater flow mismatch coincident with low water level in any steam generator.

2. Three turbine-driven auxiliary feedwater pumps (shared by units 3 & 4) are started on any of the following:
   a. Low-low water level in any steam generator.
   b. Any safety injection signal.
   c. Loss of offsite power (automatic transfer to diesel generators).
   d. Loss of A or B 4160 VAC bus on either Unit.
   e. Trip of all main feedwater pumps on either Unit.
   f. Manual actuation.

The steam driven auxiliary feedwater pumps are started upon the loss of normal feedwater supply. The auxiliary feedwater turbine utilizes steam from the main steam line to drive the auxiliary feedwater pump to deliver water to the steam generators. The pumps take suction directly from the condensate storage tanks for delivery to the steam generators.

Following the reactor coolant pump coastdown caused by the loss of AC Power, the natural circulation capability of the RCS will remove residual and decay heat from the core, aided by auxiliary feedwater in the secondary system. An analysis is presented here to show that the natural circulation flow in the RCS following a loss of AC power event is sufficient to remove residual heat from the core.

The analysis shows that following a loss of all AC power to the station auxiliaries, RCS natural circulation and the AFW system are capable of removing the stored and residual heat; consequently, preventing overpressurization of the RCS, overpressurization of the secondary side, or water relief from the pressurizer and uncovering of the reactor core. The plant is, therefore, able to return to a safe condition.

Turkey Point Units 3 and 4 share common auxiliary feedwater systems. Thus, a loss of non-emergency AC power to the plant auxiliaries could simultaneously affect both units. The auxiliary feedwater system would then be required to provide flow to both units.

The worst single failure in the auxiliary feedwater system could result in availability of only one of the three auxiliary feedwater pumps. Flow from this pump could be as low as 233.4 gpm to one of the units until the operator takes action from the control board to realign the flow split to the units.

The analysis is performed for one unit, representing the worst case of the two units.

14.1.12.2 ANALYSIS OF EFFECTS AND CONSEQUENCES

Method of Analysis

A detailed analysis using the LOFTRAN Code (Reference 1) is performed to obtain the plant transient following a loss of all AC power. The analysis addresses the plant thermal kinetics, RCS including the natural circulation, pressurizer, pressurizer PORVs and sprays, steam generators, main steam safety valves, and the auxiliary feedwater system. The digital program computes pertinent variables including the pressurizer pressure, pressurizer water level, steam generator mass, and reactor coolant average temperature.

The major assumptions used in this analysis are identical to those used in the loss of normal feedwater analysis (Subsection 14.1.11) with the following exceptions.

1. Loss of AC power occurs at reactor trip on low-low SG water level. No credit is taken for the immediate insertion of the control rods as a result of the loss of AC power to the station auxiliaries.

2. Power is assumed to be lost to the RCPs following the start of rod motion. This assumption results in the maximum amount of stored energy in the RCS.

3. A heat transfer coefficient in the steam generators associated with RCS natural circulation is assumed following the RCP coastdown.

4. The RCS flow coastdown is based on a momentum balance around each reactor coolant loop and across the reactor core. This momentum balance is combined with the continuity equation, a pump momentum balance, the as-built pump characteristics and conservative estimates of system pressure losses.

5. The worst single failure assumed to occur is in the AFW system. This results in the availability of only one AFW pump supplying 233.4 gpm to three steam generators 95 seconds following a start signal on low-low steam generator water level. This AFW flow is less than that assumed for a loss of normal feedwater, because Turkey Point Units 3 and 4 have a shared AFW system and a loss of AC power may occur simultaneously at both Units.

6. The pressure drop in the piping between the steam generators and the main steam safety valves is included (Reference 3).

7. The pressurizer backup heaters are not modeled after the loss of AC Power (Reference 4).

Results

The transient response of the RCS following a loss of AC power is shown in Figures 14.1.12-1 and 14.1.12-2. The calculated sequence of events for this accident is listed in Table 14.1.12-1.

The first few seconds after the loss of power to the reactor coolant pumps will closely resemble a simulation of the complete loss of flow incident, i.e., core damage due to rapidly increasing core temperatures is prevented by the reactor trip on the low-low steam generator water level signal.

After the reactor trip, stored and residual heat must be removed to prevent damage to the core and the reactor coolant and main steam systems. The LOFTRAN code results show that the natural circulation and AFW flow available is sufficient to provide adequate core decay heat removal following reactor trip and RCP coastdown.

The capacity of the turbine-driven AFW pump is such that the water level in the steam generators does not recede below the lowest level at which sufficient heat transfer area is available to establish enough natural circulation flow in order to dissipate core residual heat without water release through the RCS relief or safety valves. From Figure 14.1.12-1, it can be seen that at no time is there water relief from the pressurizer.

The calculated sequence of events for this accident is listed in Table 14.1.12-1. As shown in Figures 14.1.12-1 and 14.1.12-2, the plant approaches a stabilized condition following reactor trip, pump coastdown, and auxiliary feedwater initiation.

14.1.12.3 CONCLUSIONS

Results of the analysis show that, for the loss of offsite power to the station auxiliaries event, all safety criteria are met. The DNBR transient is bounded by the complete loss of flow event (Section 14.1.9) and remains above the safety analysis limit value. AFW capacity is sufficient to prevent water relief through the pressurizer relief and safety valves; this assures that the RCS is not overpressurized.

Analysis of the natural circulation capability of the Reactor Coolant System has demonstrated that sufficient heat removal capability exists following RCP coastdown to prevent fuel or clad damage.

14.1.12.4 REFERENCES

1. Westinghouse WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-Proprietary), Burnett, T. W. T. et al, "LOFTRAN Code Description," dated April 1984.

2. ANSI/ANS-5.1-1979, "American National Standard for Decay Heat Power in Light Water Reactors," dated August 1979.

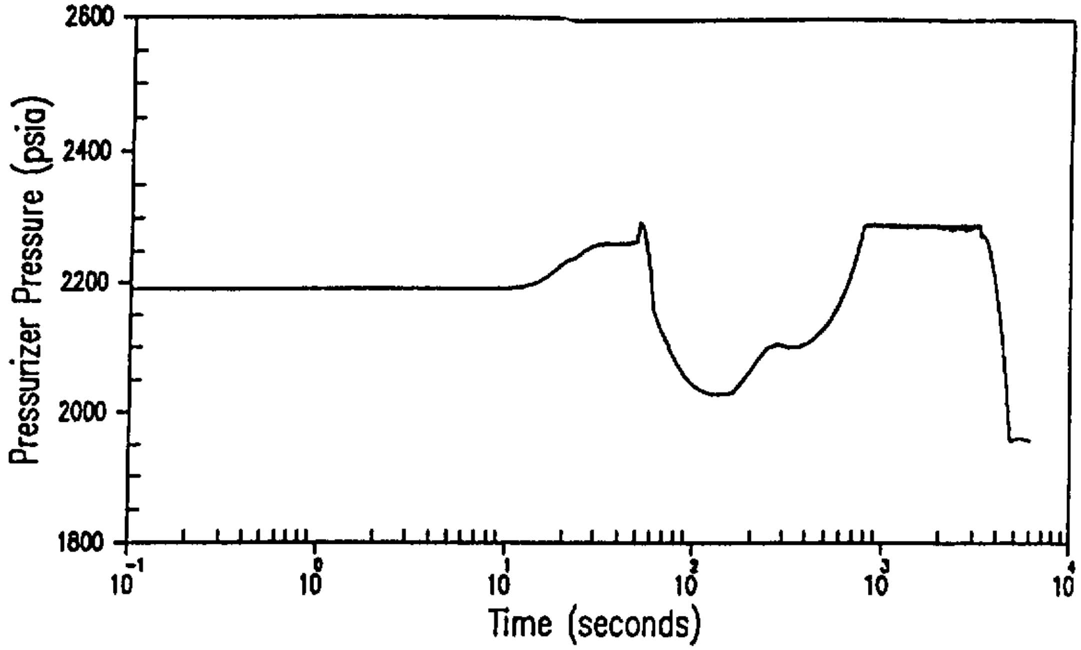
3. "Florida Power & Light Co., Turkey Point Units 3 & 4, UFSAR and AABD Updates Resulting from Reanalysis of Non-LOCA Transients Impacted by ΔP Losses Between the Steam Generator and MSSVs and LOCA Assessment," J. J. Deblasio to J. Perryman, FPLN-97-0108, NSD-SAE-ESI-97-342, June 2, 1997.

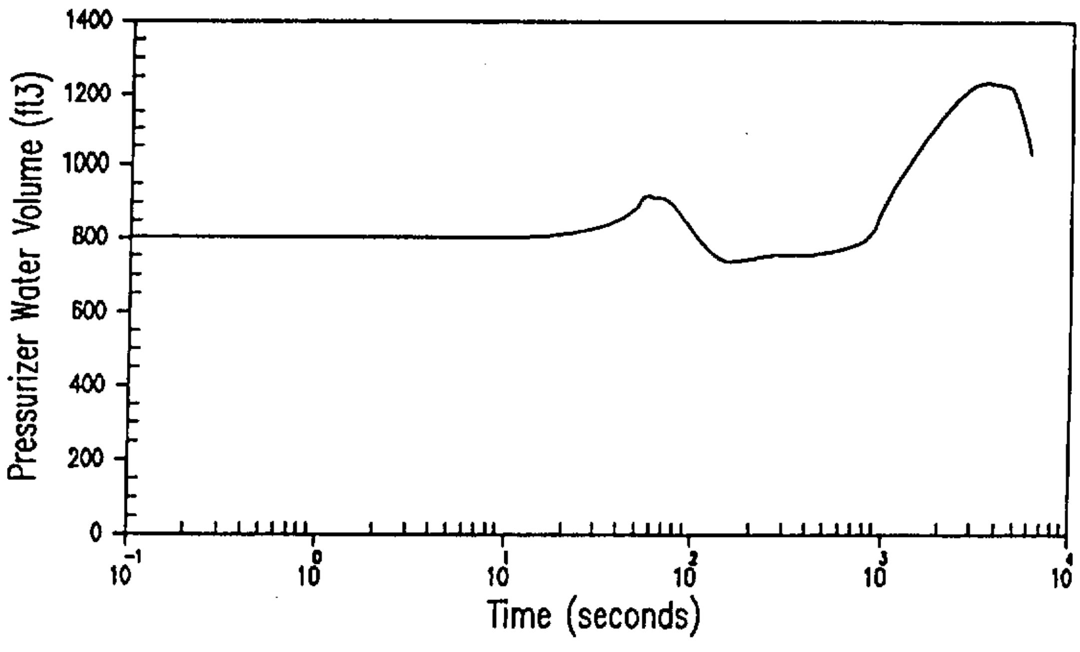
4. “Florida Power & Light Co., Turkey Point Units 3 & 4, Analysis Modeling of Pressurizer Heaters,” N. R. Metcalf to J. L. Perryman, 98FP-G-0104, September 23, 1998.

TABLE 14.1.12-1

SEQUENCE OF EVENTS
FOR
LOSS OF NON-EMERGENCY AC POWER

Event	Time (sec)
Main feedwater flow stops	10
Low-low steam generator water level trip	47.5
Rods begin to drop	49.5
Reactor coolant pumps begin to coastdown	51.5
Flow from one turbine driven auxiliary feedwater pump is started	142.5
Feedwater lines are purged and cold auxiliary feedwater is delivered to three steam generators	892.0
Core decay heat decreases to auxiliary feedwater heat removal capacity	~3300
Peak water level in pressurizer occurs	3532.0





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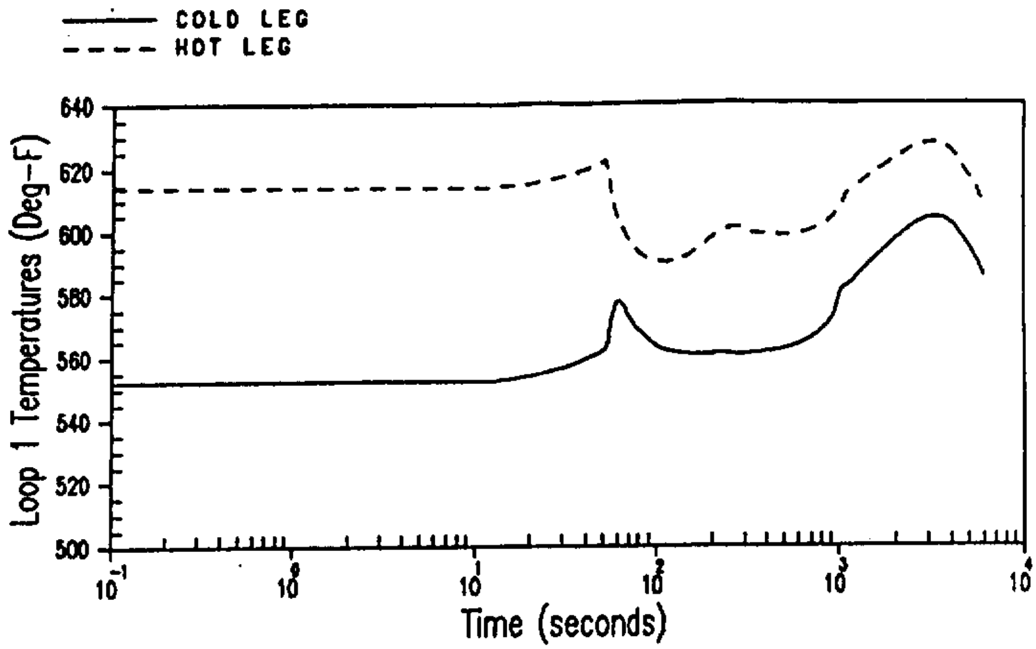
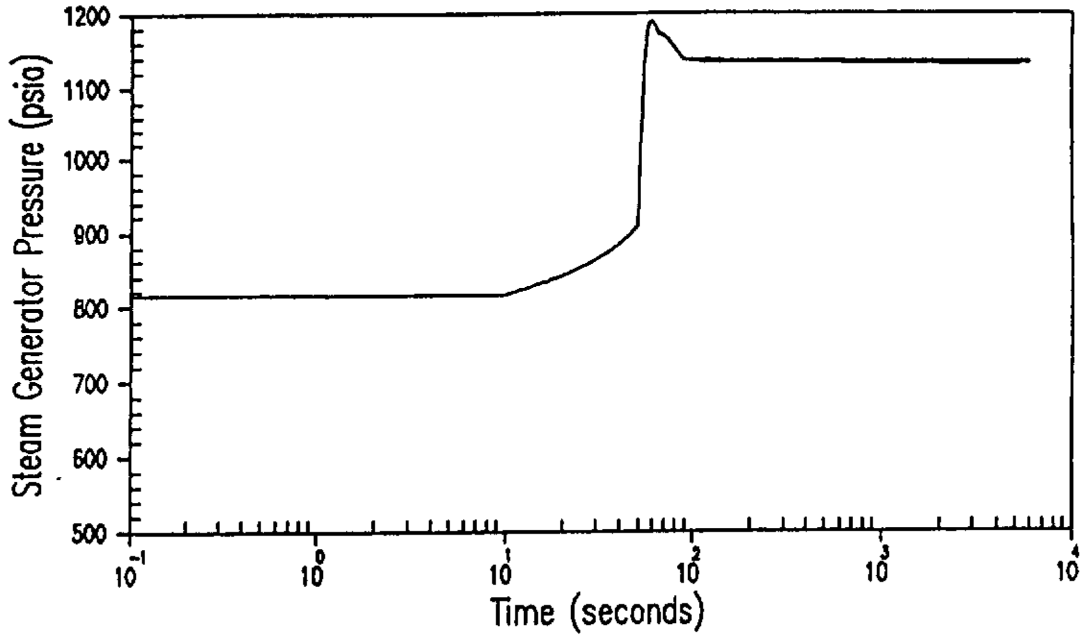
PRESSURIZER PRESSURE AND WATER VOLUME TRANSIENTS FOR LOSS OF OFFSITE POWER
FIGURE 14.1.12-1





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FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

LOOP TEMPERATURES AND STEAM GENERATOR PRESSURE FOR LOSS OF OFFSITE POWER

FIGURE 14.1.12-2

14.1.13 TURBINE GENERATOR DESIGN ANALYSIS

Turbine Generator Description

Each turbine generator is a tandem compound four flow machine, with forty five inch last stage blades, which has an operating speed of 1800 rpm.

There is one double flow high pressure cylinder. A sectional view and internal design features are shown in Figures 14.1.13-1 and 14.1.13-2.

There are two double flow low pressure elements. Views of these are shown in Figures 14.1.13-3 and 14.1.13-4.

The turbine mechanical properties are listed in Table 14.1.13-1.

Steam flow of the high pressure turbine is through two main stop control valve assemblies. Each assembly consists of one stop valve with two single seat-type control valves downstream of it, thus providing redundancy in valving.

Exhaust from the high pressure element flows to four moisture separator - reheaters and then to the low pressure elements. In each cross over from a moisture separator - reheater there is a reheat stop valve and an interceptor valve. The stop valves serve as redundant devices to prevent overspeed if the intercepts fail to close when the overspeed trip mechanism operates.

The steam paths described are shown schematically in Figure 10.2-1.

Turbine Generator Speed Control

The turbine generator is a constant speed machine which has its speed controlled by the electrical tie of the generator to the distribution system connected to all generating plants. Output is controlled by the turbine governor, which is essentially a torque changing device, that varies the position of the turbine control valves.

In addition to the governor, there is an auxiliary governor, which is speed and acceleration responsive. It can sense an acceleration of 3%/second and at 102% of rated speed, it reduces control oil pressure.

Also, an overspeed protection controller is installed. This senses a sudden loss of load and closes control and intercept valves.

Further, there is an overspeed trip mechanism which stops the flow of all steam into or through the turbine should the speed increase to 1998 RPM (11% above normal). This overspeed protection is provided by a mechanical trip weight.

A diverse backup turbine overspeed trip function is installed on Unit 3. The trip function senses main oil pump discharge high pressure and actuates both primary and backup autostop solenoid valves. This backup trip function is redundant to the speed control and overspeed protection functions and is not required to be in service at any time. A keylock switch allows this trip function to be disabled.

In essence there are three levels of speed control:

1. The main electrical tie.

2. The main and auxiliary governors, for redundant governing.

3. The overspeed protection controller and overspeed trip mechanisms, for redundant overspeed protection.

Energy of Turbine Parts

Modern design, manufacturing and testing practices made the possibility of a major turbine structural failure extremely remote. Dissasembled inspection of the turbine ensures that flaws arising during turbine operation are detected and repaired long before they become a potential challenge to turbine structural integrity.

The original low pressure turbine rotors have been replaced with fully integral rotors. The fully integral (FI) rotors have neither discs, nor key-ways to provide areas of stress concentration and stress corrosion cracking previously exhibited by other turbine designs. To ensure that the turbine will not catastrophically fail from stress corrosion cracking, the rotors are inspected at least at the interval recommended by the vendors.

TABLE 14.1.13-1

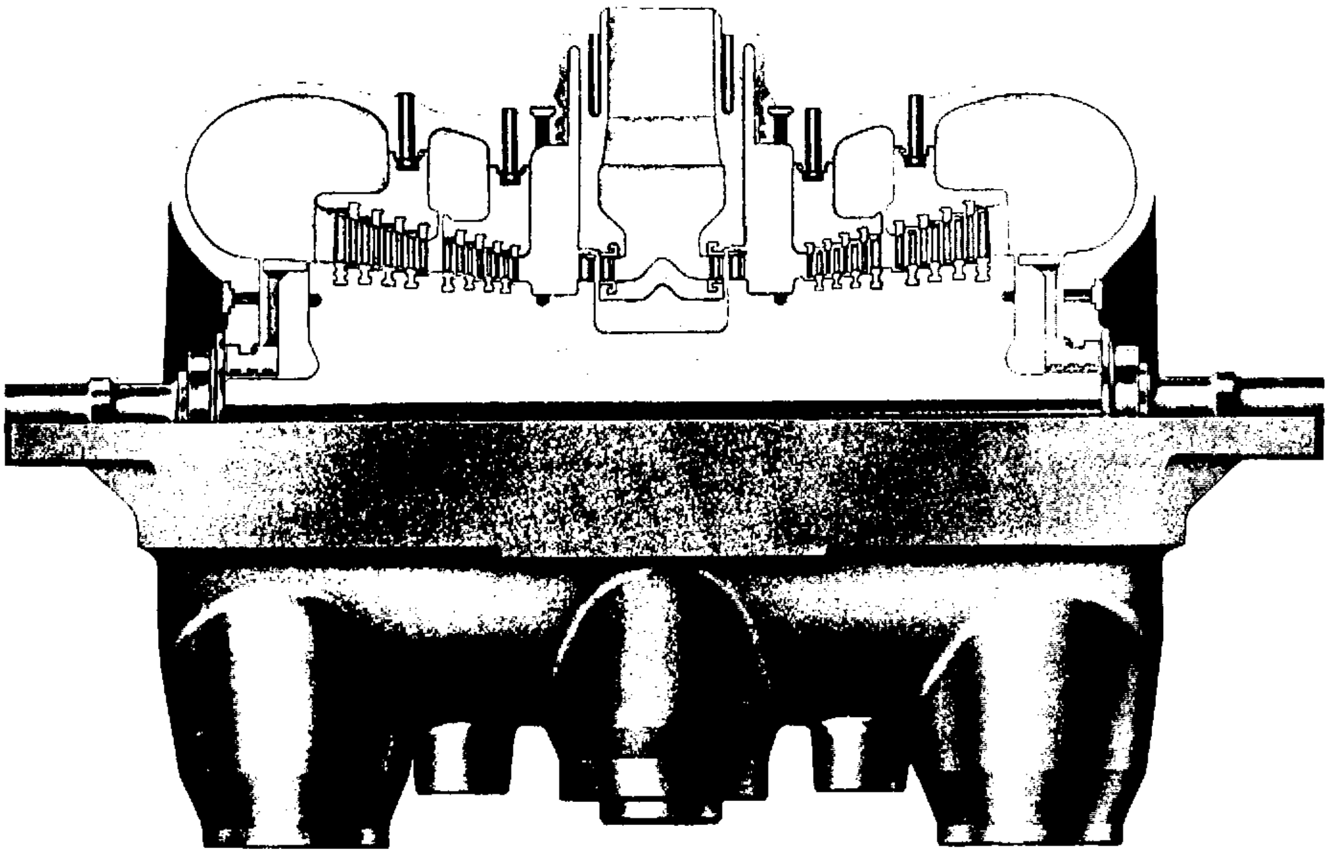
TURBINE MECHANICAL PROPERTIES(TYPICAL)

P A R T	HP ROTOR	LP ROTOR	HP CASING	LP CASING
Material	NiCrMoV	3.5 NiCrMoV	CS	ASTM 515-GR65
Tensile Strength, psi, min.	100,000	115,000	70,000	65,000
Yield Strength psi, min.	80,000	100,000	36,000	35,000
Yield Strength, psi, max.	---	---	---	---
Elongation in 2", % min.	18	16	22	23
Reduction of area, % min.	45	40	35	---
Impact Strength, Charpy V-Notch ft-lb, min. at room temperature	60	40	---	---
50% fracture appearance transition temp, max., °F	50	80	---	---

STUD MATERIAL

	2 1/2 & Less	Over 2 1/2 to 4	Over 4 to 7
Tensile strength, psi, min.	125,000	115,000	110,000
Yield strength, psi, min.	105,000	95,000	85,000
Elongation in 2", % min.	16	16	16
Reduction in area, % min.	50	50	45

TYPICAL
HIGH PRESSURE CYLINDER
1800 RPM DOUBLE-FLOW DESIGN

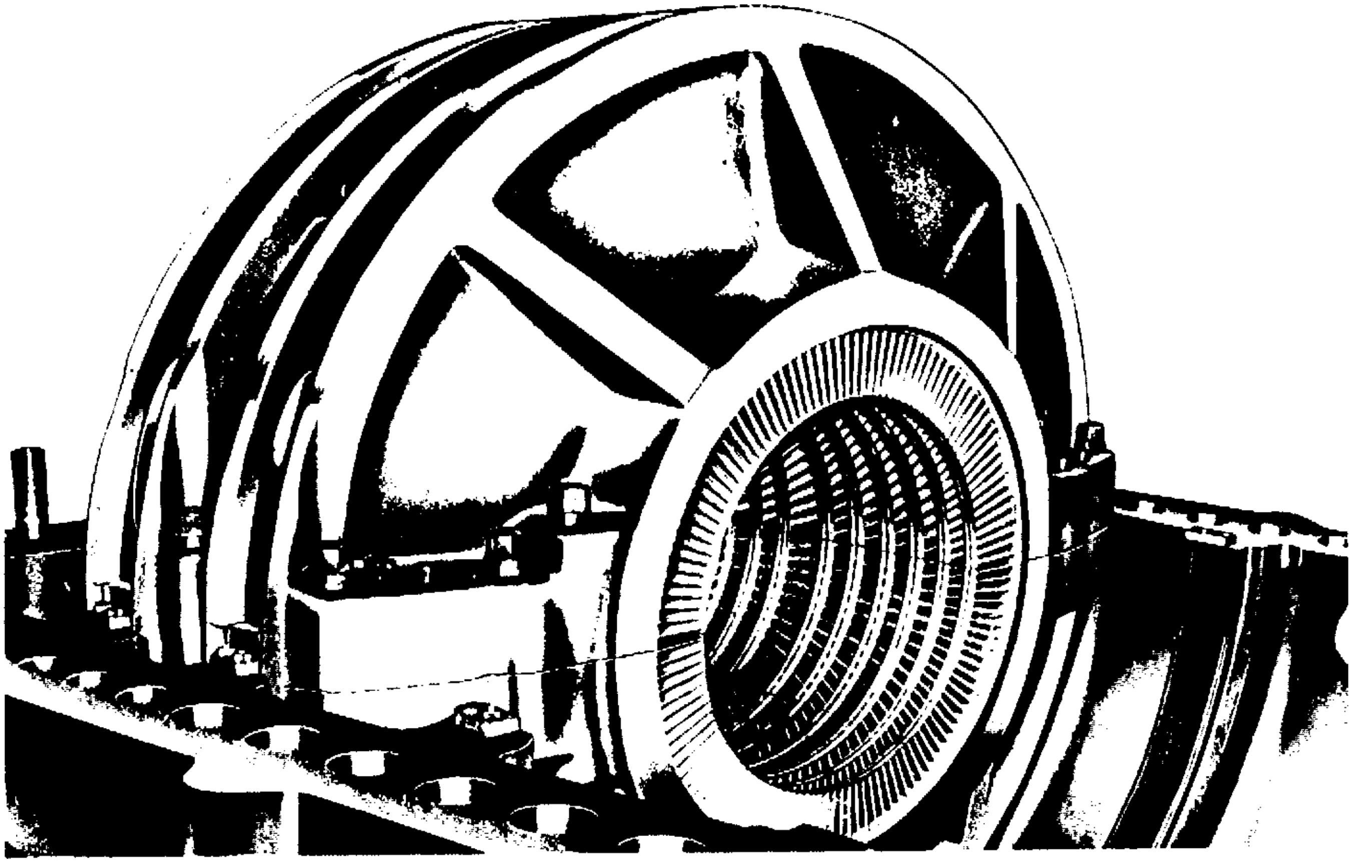


FEATURES

1. Four separate nozzle chambers permit freedom of expansion and contraction during starting and load changes.
2. Double flow design insures thrust balance.
3. Rotor checked in heater box for dynamic balance prior to shipment.
4. Ultrasonic test of rotor performed at steel mill and at the Westinghouse factory.

FIGURE 14.1.13-1

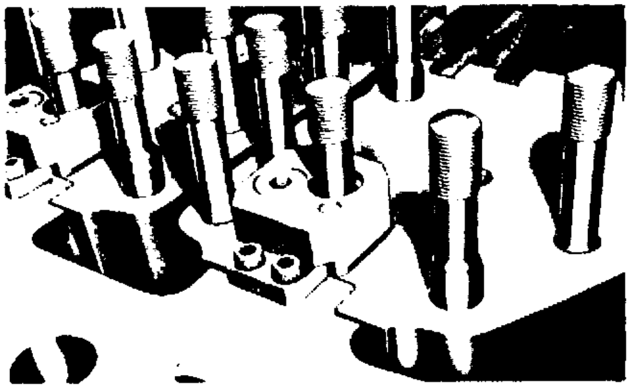
TYPICAL
BLADE RINGS



Blade rings of large high-pressure, high temperature turbine, with stationary blades in place.

FEATURES

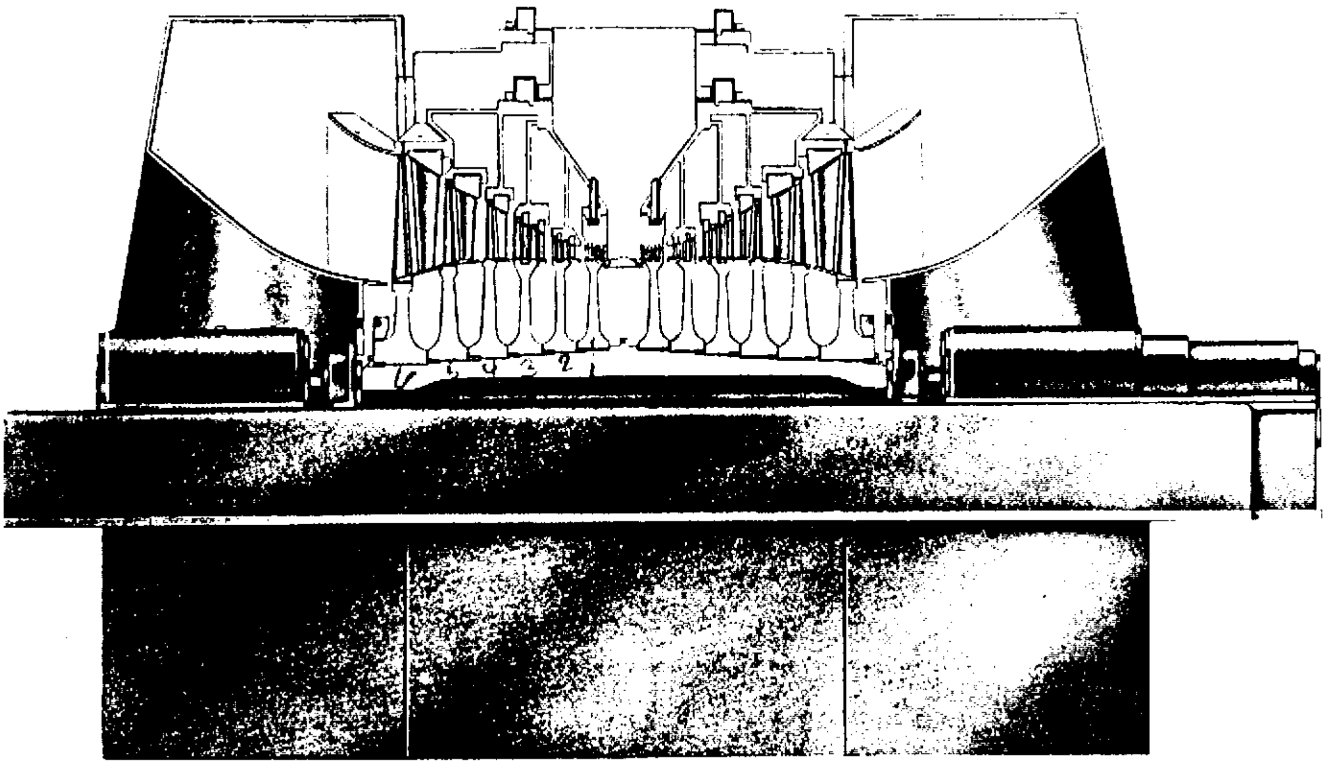
1. Centerline supporting block insures center alignment while allowing differential expansion between blade ring and cylinder.
2. Blades are inserted in blade ring halves.
3. Tongue and groove holds blade ring in position.
4. Metallic seals between blade rings and cylinder prevent leakage of steam in support grooves.
5. Upper plate, in cylinder cover, prevents any "riding-up" of the blade ring.



View of turbine cylinder and blade ring, showing method of supporting and locking lower blade ring in position.

FIGURE 14.1.13-2

TYPICAL
LOW-PRESSURE ELEMENT
1800-RPM DOUBLE-FLOW DESIGN



FEATURES

1. Blade ring, supported at the horizontal centerline and fixed transversely at the top and bottom by dowel pins, allows freedom of expansion independent of the casing.
2. Entire exhaust casing is at exhaust steam temperature.
3. Exhaust hood of laboratory-proved design minimizes hood loss.
4. Provision for extraction zones with moisture removal.
5. Casing and blade ring of fabricated steel construction.

FIGURE 14.1.13-3

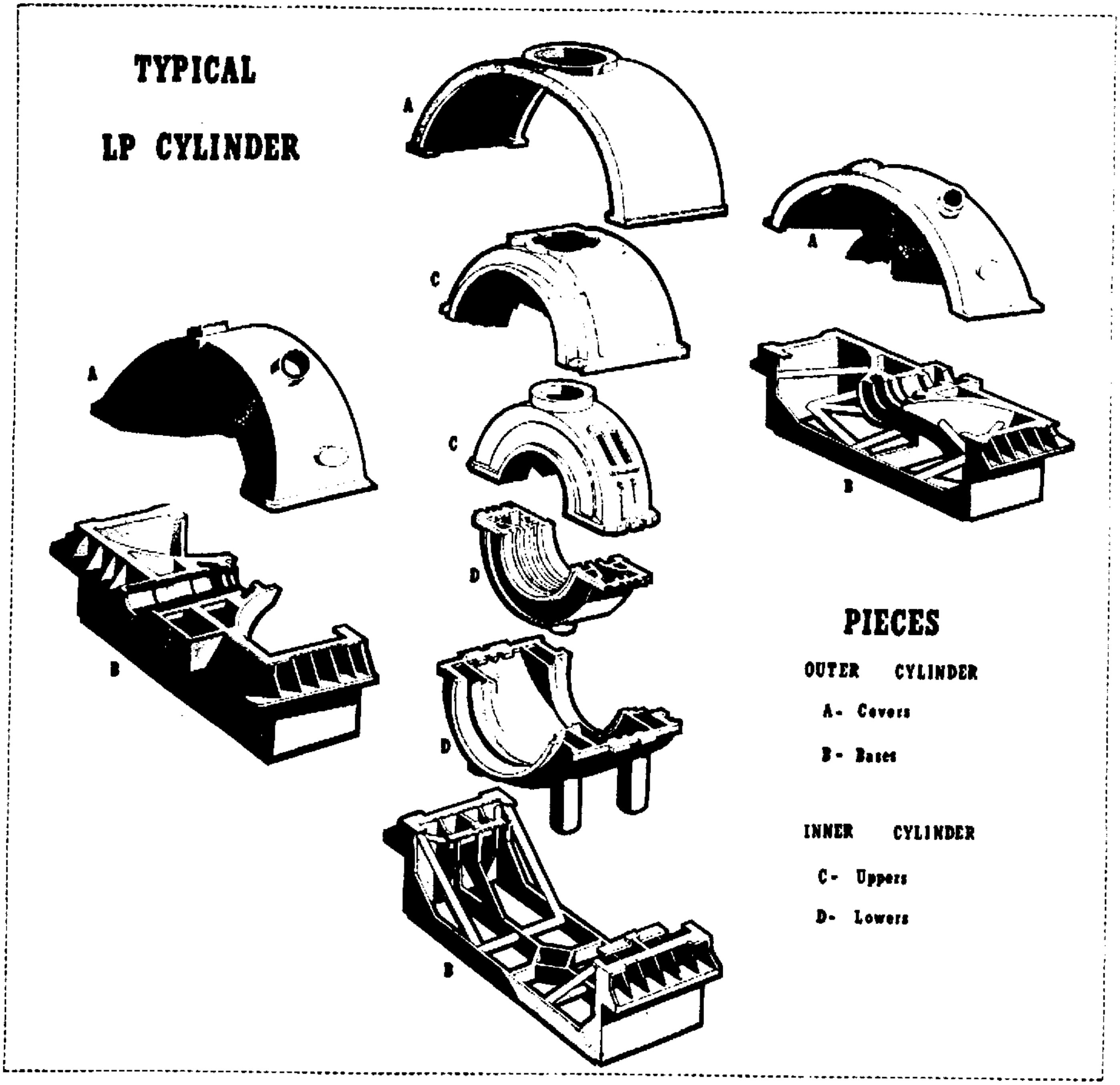


FIGURE 14.1.13-4

14.2 STANDBY SAFETY FEATURES ANALYSIS

Adequate provisions have been included in the design of the unit and its standby engineered safety features to limit potential exposure of the public to well below the limits of 10 CFR 100 for situations which have a very low probability of occurrence, but which could conceivably involve uncontrolled releases of radioactive materials to the environment. The postulated scenarios which have been considered are:

a) Fuel Handling Accidents

b) Accidental Release of Waste Liquid

c) Accidental Release of Waste Gases

d) Rupture of a Steam Generator Tube

e) Rupture of a Steam Pipe

f) Rupture of a Control Rod Drive Mechanism Housing - Rod Cluster Control Assembly (RCCA) Ejection

14.2.1 FUEL HANDLING ACCIDENTS

The following fuel handling accidents are evaluated to ensure that no hazards are created:

a) A fuel assembly becomes stuck inside the reactor vessel.

b) A fuel assembly or control rod cluster is dropped onto the floor of the refueling canal or spent fuel pit.

c) A fuel assembly becomes stuck in the penetration valve.

d) A fuel assembly becomes stuck in the transfer carriage or the carriage becomes stuck.

e) A spent fuel cask is dropped in the passage between the spent fuel pits of Units 3 & 4 while transferring a fuel element between the spent fuel pits.

Causes and Assumptions

The possibility of a fuel handling incident is remote because of the administrative controls and physical limitations imposed on fuel handling operations. All refueling operations are conducted in accordance with prescribed procedures under direct surveillance of a supervisor technically trained in nuclear safety. Also, before any refueling operations begin, verification of complete rod cluster control assembly insertion is obtained by tripping each rod individually to obtain indication of rod drop and disengagement from the control rod drive mechanisms. The boron concentration in the coolant is raised to the refueling concentration and verified by sampling. The refueling boron concentration is sufficient to maintain the clean, cold, fully loaded core subcritical with all rod cluster assemblies withdrawn. The refueling cavity is filled with water meeting the same boric acid specifications. As the vessel head is raised, a visual check is made to verify that the drive shafts are free in the mechanism housing.

After the vessel head is removed, the rod cluster control drive shafts are removed from their respective assemblies using the auxiliary hoist on the manipulator crane and the drive shaft unlatching tool. A spring scale is used to indicate that the drive shaft is free of the control cluster as the lifting force is applied.

The fuel handling manipulators and hoists are designed so that fuel can only be raised up to positions which provide adequate shield water depth for the safety of operating personnel. This safety feature applies to handling facilities in both the containment and in the spent fuel pit area. In the spent fuel pit, the design of storage racks and manipulation facilities is such that:

Fuel at rest is positioned by positive restraints in a safe, subcritical, geometrical array, with no credit for boric acid in the water.

Only one fuel assembly can be handled at a time.

Violation of procedures by placing one fuel assembly in juxtaposition with any group of assemblies in racks will not result in criticality.

Administrative control will be used to prevent the handling of heavy objects such as a spent fuel shipping container, above the fuel racks, until the fuel in the spent fuel pit has decayed for a minimum of 1525 hours.

Adequate cooling of fuel during underwater handling is provided by convective heat transfer to the surrounding water. The fuel assembly is immersed continuously while in the refueling cavity or spent fuel pit.

Should a spent fuel assembly become stuck in the transfer tube, natural convection will maintain adequate cooling. The fuel handling equipment is described in detail in Section 9.5.

Two Nuclear Instrumentation System source range channels are continuously in operation and provide warning of any approach to criticality during refueling operations. This instrumentation provides a continuous audible signal in the containment, and would annunciate a local horn and a bell and light in the control room if the count rate increased above a preset low level.

Refueling boron concentration is sufficient to maintain the clean, cold, fully loaded core subcritical by at least 5 per cent with all rod cluster control assemblies inserted. At this boron concentration the core would also be subcritical with all control rods withdrawn. The refueling cavity is filled with water meeting the same boric acid specification.

All these safety features make the probability of a fuel handling incident very low. Nevertheless, it is possible that a fuel assembly could be dropped during the handling operations. Therefore, this incident is analyzed both from the standpoint of radiation exposure and accidental criticality.

Special precautions are taken in all fuel handling operations to minimize the possibility of damage to fuel assemblies during transport to and from the spent fuel pit and during installation in the reactor. All irradiated fuel handling operations are conducted under water. The handling tools used in the fuel handling operations are conservatively designed and the associated devices are of a fail-safe design.

In the fuel storage area, the fuel assemblies are spaced in a pattern which prevents any possibility of a criticality accident. Also, the design of the facility is such that it is not possible to carry heavy objects, such as a spent fuel transfer cask, over the fuel assemblies in the storage racks. In addition, the design is such that only one fuel assembly can be handled at a given time.

The motions of the cranes which move the fuel assemblies are limited to a low maximum speed. Caution is exercised during fuel handling to prevent the fuel assembly from striking another fuel assembly or structures in the containment or fuel storage building.

The fuel handling equipment suspends the fuel assembly in the vertical position during fuel movements, except when the fuel is moved through the transport tube.

The design of the fuel assembly is such that the fuel rods are restrained by grid clips which provide a total restraining force of approximately 80 pounds on each fuel rod (Reference 2). If the fuel rods are in contact with the bottom plate of the fuel assembly, any force transmitted to the fuel rods is limited due to the restraining force of the grid clips. The force transmitted to the fuel rods during fuel handling is not of a magnitude great enough to breach the fuel rod cladding. If the fuel rods are not in contact with the bottom plate of the assembly, the rods would have to slide against the 80 pound friction force. This would have the effect of absorbing a shock and thus limit the force on the individual fuel rods.

After the reactor is shut down, the fuel rods contract during the subsequent cooldown and would not be in contact with the bottom plate of the assembly.

Considerable deformation would have to occur before the rod would make contact with the top plate and apply any appreciable load on the fuel rod. Based on the above, it is felt that it is unlikely that any damage would occur to the individual fuel rods during handling. If one assembly is lowered on top of another, no damage to the fuel rods would occur that would breach the integrity of the cladding.

If during handling the fuel assembly strikes against a flat surface, the loads would be distributed across the fuel assemblies and grid clips and essentially no damage would be expected in any fuel rods.

If the fuel assembly were to strike a sharp object, it is possible that the sharp object might damage the fuel rods with which it comes in contact but breaching of the cladding is not expected. It is on this basis that the assumption of the failure of an entire row of fuel rods (15) is a conservative upper limit.

Analyses have been made assuming the extremely remote situations where a fuel assembly is dropped and strikes a flat surface, where one assembly is dropped on another, and where one assembly strikes a sharp object. The analysis of a fuel assembly assumed to be dropped and strikes a flat surface considered the stresses the fuel cladding was subjected to and any possible buckling of the fuel rods between the grip clip supports. The results show that the buckling load at the bottom section of the fuel rod, which would receive the highest loading, is below the critical buckling load and the stresses were relatively low and below the yield stress. For the case where one assembly is dropped on top of another fuel assembly, the loads will be transmitted through the end plates and the RCCA guide tubes of the stuck assembly before any of the loads reach the fuel rods.

The end plates and guide thimbles absorb a large portion of the kinetic energy as a result of bending in the lower plate of the falling assembly. Also, energy is absorbed in the struck assembly top end plate before any load can be transmitted to the fuel rods. The results of this analysis indicated that the buckling load on the fuel rods was below the critical buckling loads and the stresses in the cladding were relatively low and below yield.

The refueling operation experience that has been obtained with Westinghouse reactors has verified the fact that no fuel cladding integrity failures are expected to occur during any fuel handling operations.

Although rupture of one complete outer row of fuel rods is considered to be a conservative assumption, a reanalysis of the offsite radiological consequences of a dropped fuel assembly (Section 14.2.1.1) assumed both a case where a single outer row is damaged and an additional case in which all the fuel rods in a single assembly are damaged. The original FSAR assessment of the postulated accidents which evaluated the containment and spent fuel pool area radiological doses (Section 14.2.1.2) only assumed the case where a single outer row of fuel rods was damaged.

14.2.1.1 DOSE EVALUATION

A fuel assembly is assumed to be dropped and damaged during refueling. Analysis of the accident is performed for the accident occurring both inside containment and in the spent fuel pit. Activity released from the damaged assembly is released to the outside atmosphere through either the containment purge system or the spent fuel pit area ventilation systems.

The offsite doses following a fuel handling accident (FHA) reflect the uprated power level of 2346 MWt. Also addressed is a 20% increase in the I-131 gap fraction for high burnup fuel. The gap fractions applied to the remaining iodine and noble gas isotopes are 0.10 for these iodine and noble gas isotopes with the exception of 0.30 for Kr-85.

Two cases are analyzed with respect to the amount of damage suffered by the dropped assembly. For the first case, it is assumed that all of the fuel rods in the equivalent of one assembly are damaged to the extent that all their gap activity is released. In the second case, only the fuel rods in one row of the assembly (i.e., 15 fuel rods) are damaged sufficiently to cause their gap activity to be released.

Since, per Technical Specifications, the reactor has to be subcritical for 100 hours before fuel is moved, 100 hours of radioactive decay is assumed in the analysis. Also in accordance with Technical Specifications, it is assumed that there is a minimum of 23 feet of water above the vessel flange. With this water depth, decontamination factors (DF) of 133 for elemental iodine and 1 for methyl iodide are used for pool scrubbing. The iodine activity in the fuel rod gap is assumed to be 99.75% elemental and 0.25% methyl. The resulting overall pool scrubbing DF for iodine is 100.

All of the noble gas released from the damaged assembly is assumed to be released from the pool water (i.e., the pool scrubbing DF is 1).

A conservatively high radial peaking factor of 1.7 is assumed for the damaged assembly.

No credit is taken for filtration of iodine for either the fuel handling accident inside containment or the fuel handling accident in the spent fuel pool. Although the containment purge will be automatically isolated on a purge line high radiation alarm, isolation is not modeled in the analysis. The activity released from the damaged assembly is assumed to be immediately released to the outside atmosphere.

The major assumptions and parameters used in the analysis are itemized in Table 14.2.1-1. The thyroid dose conversion factors, breathing rates, and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4. Since the assumptions and parameters for a fuel handling accident inside containment are identical to those for a fuel handling accident in the spent fuel pit, the offsite doses are the same regardless of the location of the accident.

The dose limits for a fuel handling accident are "well within" the guideline values of 10 CFR 100, or 75 rem thyroid and 6 rem whole body.

The offsite thyroid and whole body doses due to the fuel handling accident are given in Table 14.2.1-2. The offsite doses due to the fuel handling accident are within the acceptance criteria. (Reference 1)

14.2.1.2 CONTAINMENT AND SPENT FUEL POOL AREA RADIOLOGICAL DOSES

In the original Updated FSAR assessment of the postulated accidents that evaluated the containment and spent fuel pool (SFP) radiological doses to refueling personnel, the inventory of halogens and noble gases available for release from the spent fuel pit pool surface is based on the following:

1. Breakage of 15 rods in the fuel assembly at 50 hours following reactor shutdown after operation at 2300 MWt for a full core cycle.

2. The assembly power is 1.5 times the core average.

3. Fission products released from the assembly consisted of 5.7 percent halogens (as I-131) and 20 percent noble gases (as Kr-85). These values result from the use of a calculated axial power distribution of 1.8 peak-to-average, corresponding to a peak linear assembly power of 17.8 Kw/ft.

4. Of the halogens released, only 1/500 escape from the pool surface to the environment.

5. Of the noble gases released, 100 percent escape the pool surface to the environment.

Based on these conservative assumptions, the I-131 and I-133 release from the water surface is 5.9 curies. Table 14.2.1-3 contains both the iodine and noble gases released from the surface of the water for the original FSAR analysis of the fuel handling accident.

The activity could be released either in the containment or in the spent fuel pit. The ventilation systems in both areas are in operation under administrative control during refueling hence in calculating doses inside the structures uniform dilution is assumed within the structure. Radioactivity monitors would immediately indicate and alarm the increased activity level. Activity in the containment would automatically close the purge ducts. In evaluating dose to refueling personnel inside the containment 15 minutes is assumed a reasonable time for evacuation. In the spent fuel pit the integrated dose is evaluated based on the 20,000 cfm ventilation rate and the 60,000 ft.^{3} free volume. In the containment, the dose is based on the 35,000 cfm purge rate and the 1.55 x 10^6 ft.^{3} free volume.

The calculated doses for refueling personnel inside containment and in the spent fuel pit area are summarized in Table 14.2.1-2. The permissible containment re-entry time after reduction to Xe-133 occupational MPC is six hours, and the fuel storage area re-entry time is one half hour.

14.2.1.3 CASK DROP ACCIDENT

The spent fuel transfer cask will not be moved into the spent fuel pit containing two region density racks until all spent fuel in the pit has decayed for a minimum of 1525 hours. Only a single element cask will be used to transfer one fuel assembly at a time. The radiological effects due to fuel damage resulting from a dropped spent fuel transfer cask during transfer of fuel assemblies between the spent fuel pits have been determined to be lower than those from a design basis fuel handling accident. Also, the spent fuel transfer cask drop while on transit between the Units 3 & 4 spent fuel pits will not damage equipment or structures required for the safe shutdown of Units 3 & 4. An evaluation of the cask drop accident is attached herein as Appendix 14D. Evaluation of the cask drop accident for the uprated power level of 2346 MWt was performed. The assumptions used and the resulting offsite doses for this analysis are presented in Tables 14.2.1-4 and 14.2.1-5, respectively (Reference 1).

14.2.1.4 REFERENCES

1. Westinghouse WCAP-14276,"Florida Power and Light Company Turkey Point Units 3 and 4 Uprating Licensing Report," Revision 1, dated December 1995.

2. S.S. Witter to J.L. Perryman, “Fuel Rod/Grids Spring Loads as Related to Handling and Shipping,” 98FP-G-0063, July 7, 1998.

TABLE 14.2.1-1

ASSUMPTIONS USED
FOR
FUEL HANDLING ACCIDENT DOSE ANALYSIS

Power	2346 MWt
Radial Peaking Factor	1.7
Damaged Fuel:	
Case 1	1 Fuel Assembly
Case 2	15 Rods
Fuel Rod Gap Fractions	0.10 for iodines and noble gases, except 0.12 for I-131 and 0.30 for Kr-85
Percent of Gap Activity Released	100%
Pool Decontamination Factors:	
Elemental Iodine	133
Methyl Iodide	1
Noble Gas	1
Iodine Species in Fuel Rod Gap:	
Elemental Iodine	99.75%
Methyl Iodide	0.25%
Minimum Water Depth Above Vessel Flange	23 feet
Filter Efficiency	No filtration assumed
Containment Isolation	No containment isolation assumed

TABLE 14.2.1-2

FUEL HANDLING ACCIDENT OFFSITE DOSES

Original Fuel Handling Accident Analysis

	Whole Body Dose (Rem)	Thyroid Dose(Rem)
Dose for 15 min. exposure inside Containment (No purging)	0.8	5
Dose in Spent Fuel Pit Area for Duration of Accident with ventilation	5	24

Revised Fuel Handling Accident Analysis [(1)]
(Including Thermal Power Uprate)

	Damaged Fuel Rods	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-2 Hours)
Thyroid Dose (rem)	One Assembly	3.3 E+1	3.2 E+0
	One Row	2.4 E+0	2.4 E-1
Whole Body Dose (rem)	One Assembly	9.3 E-2	9.0 E-3
	One Row	6.8 E-3	6.6 E-4

NOTE:

1. Revised fuel handling accident analysis is consistent with Regulatory Guide 1.25 assumptions.

TABLE 14.2.1-3

NOBLE GAS AND IODINE ACTIVITY RELEASE FROM FUEL
IN THE FUEL HANDLING INCIDENT

(Used for Original Containment and SFP Area Radiological Doses)

Isotope	Activity (Curies)
Kr-85	58
Xe-133(m)	326
Xe-133	14,500
Xe-135	101
I-131	3.9
I-133	2.0

TABLE 14.2.1-4

ASSUMPTIONS USED
FOR
DROPPED CASK DOSE ANALYSIS

Power	2346 MWt
Radial Peaking Factor	1.0
Damaged Fuel (Base Case)	157 Fuel Assemblies
Fuel Rod Gap Fractions	0.10 for iodines and noble gases, except 0.12 for I-131 and 0.30 for Kr-85
Percent of Gap Activity Released	100%
Pool Decontamination Factors:	
Elemental Iodine	133
Methyl Iodide	1
Noble Gas	1
Iodine Species in Fuel Rod Gap:	
Elemental Iodine	99.75%
Methyl Iodide	0.25%
Minimum Water Depth Above Damaged Assembly	23 feet
Filter Efficiency	No filtration assumed

TABLE 14.2.1-5

DROPPED CASK OFFSITE DOSES
(157 Fuel Assemblies Damaged)

	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-2 Hours)
Thyroid Dose (rem)	1.77 E+1	1.73 E+0
Whole-Body Dose (rem)	2.42 E-2	2.36 E-3

14.2.2 ACCIDENTAL RELEASE-RECYCLE OR WASTE LIQUID

Accidents in the auxiliary building and in the radwaste handling facility building which would result in the release of radioactive liquids are those which may involve the rupture or leaking of system pipe lines or storage tanks. The largest vessels are the three liquid holdup tanks, each sized to hold one and one third of a single unit reactor coolant liquid volume, which are used to process the normal recycle or waste fluids produced. The contents of one tank will be passed through the liquid processing train while another tank is being filled.

All liquid waste components except the reactor coolant drain and the pressurizer relief tanks are located in the auxiliary building and in the radwaste handling building, and any leakage from these components or piping will be collected in the respective building sumps to be pumped back into the liquid waste system.

The gross rupture of these tanks is not considered credible in view of the service conditions. However, the plant design has accommodated tank ruptures as described below.

In the unlikely event of a rupture of a full CVCS holdup tank, all spilled liquid will be contained within the CVCS tank cubicles, and no uncontrolled liquid release will occur. The walls of these cubicles have been coated to the calculated flood height and the floor drains are normally closed. The flooded cubicle can be drained to the auxiliary building waste holdup tank room sump and then pumped to the waste holdup tank. Any liquid remaining in the ruptured holdup tank could be transferred to another holdup tank by means of the recirculation pump.

In the unlikely event of a rupture of a full waste holdup tank all spilled liquid will be contained by the walls surrounding the tank and no uncontrolled liquid release will occur. The enclosure is coated to the calculated flood height. The spilled liquid around the auxiliary building waste holdup tank will drain to the waste holdup tank room sump pumps and can then be discharged to the holdup tanks or to the waste evaporator, through the waste evaporator feed pump.

The holdup tanks are also equipped with safety pressure relief and designed to accept without loss of function the maximum potential seismic forces at the site. Liquids in the Chemical and Volume Control System flowing into and out of these tanks are controlled by manual valve operation and governed by prescribed administrative procedures.

The volume control tank design philosophy is similar in many respects to that applied for the holdup tanks. Level alarms, pressure relief valves and automatic tank isolation and valve control assure that a safe condition is maintained during system operation. Excess letdown flow may be directed to the holdup tanks via the reactor coolant drain tank or via the volume control tank.

Piping external to the containment running between the containment and the auxiliary building area will be in concrete pipe chases.

The effect from these process or waste liquid releases is derived only from the volatilized components. The releases are described and their effects summarized in Section 14.2.3.

The evaluation of the credibility of the accidental release of radioactive fluids above normal concentration (~4 x 10^{-5} uc/cc) from the Waste Disposal System discharge is based upon the following review of waste discharge operating procedure, monitoring function description, monitor failure mode and the consequences of a monitor failure.

The normal procedure for discharging liquid wastes is as follows:

a) A batch of waste is collected in a waste monitor tank or monitor tank for discharge (typical).

b) The particular tank is isolated

c) The tank contents are recirculated to mix the liquid

d) A sample is taken for radiochemical analysis

e) If analysis indicates that release can be made within permissible limits, the quantity of activity to be released is recorded on the basis of the liquid volume in the tank and its activity concentration. If release can not be made within permissible limits, the waste is returned to the waste holdup tank.

f) To release the liquid, the tank to be discharged is lined up; the pump used for discharge is started; valve RCV-018, which trips shut automatically on high radiation signal from the monitor, must be opened manually; the last stop valve in the discharge line (which is normally locked shut) must be unlocked and opened; and finally release flow is throttled via a third valve to the prescribed flow rate.

As the operating procedure indicates, the release of liquid waste is under administrative control. The process radiation monitor RD-18 is provided to maintain surveillance over the release.

The monitor is provided with the following features:

a) A calibration source is provided to permit the operator to check the monitor before discharge by turning a switch in the control room to activate the circuitry.

b) If the monitor falls off scale at any time, an indicator visible to the operator in the control room lights.

c) If the power supply to the monitor fails, a high radiation alarm is annunciated. The trip valve also closes.

d) The trip valve is failed closed, normally closed.

It is concluded that the administrative controls imposed on the operator combined with the safety features built into the equipment provide a high degree of assurance against accidental release of waste liquids.

No credible mechanism exists for accidental release of waste liquids to Biscayne Bay.

14.2.3 ACCIDENTAL RELEASE - WASTE GAS

The leakage of fission products through cladding defects can result in a buildup of radioactive gases in the reactor coolant. Based on experience with other operational, closed cycle, pressurized water reactors, the number of defective fuel elements and the gaseous coolant activity is expected to be low. The shielding and sizing of components such as demineralizers and the waste handling system are based on activity corresponding to 1% defective fuel which is at least an order of magnitude greater than expected. Tanks accumulating significant quantities of radioactive gases during operation are the gas decay tanks, the volume control tank, and the liquid holdup tanks.

The volume control tank accumulates gases over a core cycle by stripping action of the entering spray. Equilibrium gaseous activity for the tank based on operation with 1% defective fuel is tabulated in Table 14.2.3-1. During a refueling shutdown this activity is vented to the waste gas system and stored for decay. Rupture of this tank is assumed to release all of the contained noble gases. The released activity would be 32,330 curies equivalent Xe-133. The offsite whole body doses to the volume control tank rupture are 0.038 rem at the exclusion boundary and 0.0036 rem at the low population zone. The offsite whole body doses do not exceed the acceptance criteria of 0.5 rem whole body dose for a waste gas system failure.

The liquid holdup tanks receive reactor coolant, after passing through demineralizers, during the process of coolant deboration. The liquid is stored and then processed. Each of the three liquid holdup tanks is sized to hold one and one-third of a single unit reactor coolant liquid volume. The contents of one tank are passed through the liquid processing train while another tank is being filled. In analyzing the consequence of rupture of a holdup tank it is assumed that 100% of the contained noble gas activity is released. This activity is much less than that available for possible release from a waste gas decay tank due to approximately six hours holdup tank filling time during which activity decay occurs and due to the reactor coolant dilution during the letdown operation.

The waste gas decay tanks receive the radioactive gases from the liquids processed by the waste disposal system. The maximum storage of waste gases occurs after a refueling shutdown at which time the gas decay tanks store the radioactive gases stripped from the reactor coolant. The maximum activity that can be stored in one tank is approximately 55,000 equivalent curies of Xe-133.

Dose Evaluation

Offsite exposure is evaluated for noble gases release based on the model described in Section 14.3 including the effect of dilution in the wake of the containment building, a two m/sec wind velocity and the short term dispersion factor at the site boundary, i.e., $X/Q = 1.54 \times 10^{-4}$ sec/m^3. Assuming that the incident occurred immediately after a refueling shutdown following operation with 1% defective fuel, the offsite whole body dose would be 0.064 rem at the exclusion boundary and 0.0062 rem at the low population zone. The offsite whole body doses do not exceed the acceptance criteria of 0.5 rem whole body dose for a waste gas system failure.

The iodine present in the vapors of the above tanks would be minimal. Based on an iodine removal factor of 10 in the mixed bed demineralizers the maximum iodine concentration in the liquid of the volume control tank or the liquid holdup tank would be less than 0.2 uc/cc I-131 when operating with 1% defective fuel. An iodine partition factor on the order of 10^{-4} is expected between the liquid and vapor. The corresponding gaseous iodine release from a holdup tank, which is the largest of the above tanks, would be 2.5 millicuries I-131 which would result in a negligible thyroid dose. It is therefore concluded that an accidental waste gas release would present no effect on safety of the public.

TABLE 14.2.3-1

VOLUME CONTROL TANK NOBLE GAS ACTIVITY

Isotope	Activity (curies)[1]
Kr-85	1120
Kr-85m	76.5
Kr-87	21.9
Kr-88	110
Xe-131m	204
Xe-133m	306
Xe-133	18,000
Xe-135m	33.2
Xe-135	528
Xe-138	2.35

NOTE:

1. The above activities are computed for a vapor space of 180 ft^3 when operating with 1% fuel defects.

14.2.4 STEAM GENERATOR TUBE RUPTURE

The event examined is a complete tube break adjacent to the tube sheet, since a minor leak may not necessitate immediate action depending on the particular circumstances. If a tube breaks, reactor coolant would discharge into the secondary system. Since the reactor coolant is radioactive, methods of operation to limit uncontrolled condensate release have to be considered.

Once the Reactor Coolant System (RCS) pressure is below the steam generator design pressure the faulted steam generator will be isolated and the possibility of uncontrolled leakage removed.

The following sequence of events is initiated by a tube rupture:

1. Rapidly falling pressure in the pressurizer will initiate a safety injection signal, tripping the unit. The safety injection signal automatically terminates normal feedwater and initiates auxiliary feedwater.

2. The steam generator blowdown monitor and the steam jet air ejector radiation monitor will alarm, indicating the passage of primary fluid into the secondary system.

3. The unit trip will automatically shut off steam flow through the turbine and will open steam bypass valves and bypass steam to the condenser.

4. In the unlikely event of concurrent loss of power, the loss of circulating water through the condenser would eventually result in loss of condenser vacuum and the valves in the turbine bypass lines would automatically close to protect the condenser, thereby causing steam relief to atmosphere.

5. Cooldown procedures are followed which entail condenser relief (if available) or atmospheric relief from the intact steam generators to reduce the reactor coolant temperature.

6. Maximum charging flow may be established prior to SI flow reduction to provide a readily controllable means of maintaining inventory and subcooling.

7. Isolation of the faulted steam generator is achieved by:

   a. reducing safety injection flow to depressurize the RCS below 1100 psia (steam generator design pressure);

   b. closing the steam line stop valve connected to the affected steam generator (determined by steam generator liquid sample activity monitor); and

   c. turning off the auxiliary feedwater flow to that steam generator.

8. Safety injection flow would be terminated to prevent repressurization of the RCS and reinitiation of break flow while the cooldown is continuing from the intact steam generators.

9. After the residual heat removal system is in operation, the condensate accumulated in the secondary system can be examined. If the radioactivity level is in excess of that allowed, the condensate can be processed through the waste disposal system.

The faulty unit will be isolated by a steam line isolation valve once the reactor coolant pressure is reduced below 1100 psia. The mass flow into the secondary system and steam relief from the faulty steam generator will be terminated in approximately 30 minutes.

With power available to the circulating water pumps the steam is bypassed to the condenser.

With concurrent loss of power a portion of the reactor coolant system, activity is released to atmosphere in steam relief during the 30 minutes to isolate the faulty steam generator.

14.2.4.1 STEAM GENERATOR TUBE RUPTURE RADIOLOGICAL CONSEQUENCES

For the analysis of the offsite doses following a steam generator tube rupture, the doses are determined for both a pre-accident iodine spike and an accident initiated iodine spike (Reference 1). For the pre-accident iodine spike, it is assumed that a reactor transient has occurred prior to the steam generator tube rupture and has raised the reactor coolant system (RCS) iodine concentration to 60 μCi/gm of dose equivalent (DE) I-131. For the accident initiated iodine spike, the reactor trip associated with the steam generator tube rupture creates an iodine spike in the reactor coolant system, which increases the iodine spike release rate from the fuel to the reactor coolant system to a value 500 times greater than the release rate corresponding to the maximum equilibrium RCS Technical Specification concentration of 1.0 μCi/gm of DE I-131. The duration of the accident initiated iodine spike is 1.6 hours.

The noble gas activity concentration in the reactor coolant system at the time the accident occurs is based on a fuel defect of 1.0%. The iodine activity concentration of the secondary coolant at the time the steam generator tube rupture occurs is assumed to be equivalent to the Technical Specification limit of 0.10 μCi/gm of DE I-131.

The amount of primary to secondary steam generator tube leakage in each of the two intact steam generators is assumed to be equal to the Technical Specification limit for a single steam generator of 500 gallons/day.

No credit for iodine removal is taken for any steam released to the condenser prior to reactor trip and concurrent loss of offsite power.

An iodine partition factor in the steam generators of 0.01 (Ci/gm steam)/(Ci/gm water) is used.

All noble gas activity carried over to the secondary side through steam generator tube leakage is assumed to be immediately released to the outside atmosphere.

In addition, for conservatism the iodine activity release due to the passive failure of the hydraulic line connecting radiation monitor RAD-6426 to the main steamline (assumed to be the steamline with the ruptured steam generator) is included in the offsite dose.

Since this line taps off of the main steamline and not the liquid phase of the ruptured steam generator, the steam released from the broken line is assumed to have the same iodine concentration as the steam released from the Atmospheric Dump Valves or Safety Relief Valves. This passive failure is not a part of the design basis.

Flow through the ruptured steam generator tube is assumed to be terminated at 30 minutes following accident initiation due to operator action.

Twenty four hours after the accident, the Residual Heat Removal system is assumed to be placed into service for heat removal, and there are no further steam releases to atmosphere from the secondary system.

The major assumptions and parameters used in the analysis are itemized in Table 14.2.4-1. The thyroid dose conversion factors, breathing rates, and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4.

The offsite dose limits for a steam generator tube rupture with a pre-accident iodine spike are the guideline values of 10 CFR 100. These guideline values are 300 rem thyroid and 25 rem whole body. For a steam generator tube rupture with an accident initiated iodine spike, the acceptance criteria "small fraction of" the 10 CFR 100 guideline values, or 30 rem thyroid and 2.5 rem whole body.

The offsite thyroid and whole body doses due to the steam generator tube rupture are given in Table 14.2.4-2. The offsite doses due to the steam generator tube rupture are within the acceptance criteria.

The resultant site boundary dose is less than 0.1 rem whole body and less than 1 rem to the thyroid using the two hour meteorological dispersion factor discussed in Section 14.3.5.

The steam generator tube rupture event is monitored by the main steam line radiation monitor (RAD-6426), which can take an on-line sample from any one of the six steam generators (see Subsection 11.2.3). This monitor is kept in service throughout the event. An evaluation of a sample tube failure concurrent with a steam generator tube rupture was performed. Many hours are available to isolate the monitor without exceeding the offsite dose limits for this event.

For reasons to be discussed later in this section, the multiple spontaneous occurrence of gross tube failures in a single incident is not considered credible. In order to perform a rigorous analysis of the flow dynamics of blowdown through multiple tube ruptures one must understand and define mathematically the physical configuration of the ruptures. Because no reasonable mechanism exists for the multiple ruptures, it is instead just as meaningful to analyze the consequences of a pipe rupture, equivalent in terms of discharge rate to various multiples of the single tube rupture discharge rate.

Such an analysis reveals that the core cooling system will prevent clad damage for break discharge rates equal to or smaller than that resulting from a broken pipe between 4 inches and 6 inches in diameter. The discharge rates which bracket the onset of clad damage correspond to 18 and 40 times the discharge from a single severed steam generator tube. Actually the ratio would be much larger owing to the fact that the discharge from a tube failure will be limited by the back pressure in the steam generator. Ultimately the tube discharge would terminate when the reactor coolant system and the steam generator reached pressure equilibrium. The operator can initiate cooldown through the unaffected steam generators.

The discharge rate required to lift a secondary safety valve is about 15 times the rate from a single severed tube.

These conclusions are based on single-failure mode performance of the core cooling system. Clad damage is prevented in those cases where the top of the core does not become uncovered.

The discharge rate required to cause the top of the core to become uncovered is 18 to 40 times the rate from a single severed tube.

The incredibility of multiple simultaneous tube failures is supported by the following reasoning:

1. At the maximum operating internal pressure the tube wall sees only about 1530 psi, compared with a calculated bursting pressure in excess of 11,100 psi based on ultimate strength at design temperature (factor of 7.3); and compared with a prefabrication test pressure of 7,000 psi (factor of 4.5).

2. The above margin applies to the longitudinal failure mode, induced by hoop stress. This failure mode is the least likely to cause propagation of failure tube-to-tube. An additional factor of two applies to ultimate pressure strength in the axial direction tending to resist double-ended failure (total factor of 14.6).

3. Failures induced by fretting, corrosion, erosion or fatigue, in addition to being rendered extremely improbable by design, are of such a nature as to produce tell-tale leakage in substantial quantity while ample metal remains to prevent severance of the tube (a small fraction of the original tube wall section, as indicated by the margin derived in 2). Thus it is virtually certain that any incipient failures that would develop to the point of severe leakage requiring a shutdown for repair would happen long before the large safety margin in pressure strength is lost.

14.2.4.2 REFERENCES

1. Westinghouse WCAP-14276,"Florida Power and Light Company Turkey Point Units 3 and 4 Uprating Licensing Report," Revision 1, dated December 1995.

TABLE 14.2.4-1

ASSUMPTIONS USED FOR
STEAM GENERATOR TUBE RUPTURE DOSE ANALYSIS

Power	2346 MWt
Reactor Coolant Noble Gas Activity Prior to Accident	1.0% Fuel defect level
Reactor Coolant Iodine Activity Prior to Accident:	
Pre-Accident Spike	60 µCi/gm of DE I-131
Accident Initiated Spike	1.0 µCi/gm of DE I-131
Reactor Coolant Iodine Activity Increase Due to Accident Initiated Spike	500 times equilibrium release rate from fuel for initial 1.6 hours after SGTR
Secondary Coolant Activity Prior to Accident	0.10 µCi/gm of DE I-131
SG Tube Leak Rate for Intact SGs During Accident	500 gallons/day per steam generator
Break Flow to Ruptured SG	102,700 lb (0-30 min)
SG Iodine Partition Factor	0.01
Duration of Activity Release From Secondary System	24 hours
Offsite Power	Lost
Steam Release from SGs to Environment:	
Ruptured SG	55,000 lb (0-30 min) 2,160 lb (0.5-8.5 hr)
Intact SGs	308,500 lb (0-2 hr) 1,731,200 lb (2-24 hr)

TABLE 14.2.4-2

STEAM GENERATOR TUBE RUPTURE OFFSITE DOSES

	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-24 Hours)
Thyroid Dose (rem) (Accident Initiated Spike)	6.8 E-2	1.0 E-2
Thyroid Dose (rem) (Pre-Accident Spike)	4.2 E-1	4.5 E-2
Whole Body Dose (rem)	2.0 E-2	2.0 E-3

14.2.5 RUPTURE OF A STEAM PIPE

A rupture of a steam pipe is assumed to include any accident which results in an uncontrolled steam release from a steam generator. The release can occur due to a break in a pipe line or due to a valve malfunction. The steam release results in an initial increase in steam flow which decreases during the accident as the steam pressure falls. The energy removal from the Reactor Coolant System causes a reduction of coolant temperature and pressure. With a negative moderator temperature coefficient, the cooldown results in a reduction of core shutdown margin. If the most reactive rod control cluster assembly (RCCA) is assumed stuck in its fully withdrawn position, there is a possibility that the core will become critical and return to power even with the remaining RCCAs inserted. A return to power following a steam pipe rupture is a potential problem only because of the high hot channel factors which may exist when the most reactive RCCA is assumed stuck in its fully withdrawn position. Assuming the most pessimistic combination of circumstances which could lead to power generation following a steam line break, the core is ultimately shut down by the boric acid in the refueling water storage tank.

14.2.5.1 INADVERTENT OPENING OF A STEAM GENERATOR RELIEF OR SAFETY VALVE

14.2.5.1.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

The most severe core conditions for an accidental depressurization of the main steam system result from an inadvertent opening of a single steam dump, relief, or safety valve. The analyses performed assuming a rupture of a main steam line are given in Subsection 14.2.5.2.

The steam release as a consequence of this accident results in an initial increase in steam flow which decreases during the accident as the steam pressure falls. The energy removal from the RCS causes a reduction of coolant temperature and pressure. In the presence of a negative moderator temperature coefficient, the cooldown results in an insertion of positive reactivity.

The analysis is performed to demonstrate that the following criterion is satisfied: Assuming a stuck (RCCA), with offsite power available, and assuming a single failure in the Engineered Safety Features (ESF) there will be no consequential damage to the core or reactor coolant system after reactor trip for a steam release equivalent to the spurious opening, with failure to close, of the largest of any single steam dump, relief or safety valve.

The following systems provide protection against an accidental depressurization of the main steam system:

A) Safety Injection System actuation from any of the following:

   1) Two out of three Low pressurizer pressure signals

   2) Two out of three High containment pressure signals

   3) Two out of three High differential pressure signals between any steam line and the main steam header

   4) High steam flow in two out of three lines (one out of two per line) coincident with either:

      a) Two out of three Low reactor coolant system average temperature signals, or

      b) Two out of three Low steam line pressure signals

B) The overpower reactor trips (neutron flux and ΔT) the reactor trip occurring in conjunction with receipt of the Safety Injection Signal.

C) Redundant isolation of the main feedwater lines. Sustained high feedwater flow would cause additional cooldown. Therefore, in addition to the normal control action which will close the main feedwater valves following reactor trip, any SI signal will rapidly close all feedwater control valves, trip the main feedwater pumps, and indirectly close the feedwater pumps discharge valves.

D) Trip of the Main Steam Isolation Valves (MSIVs), designed to close in less than 5 seconds with no flow (analysis assumes 15 second closure time from receipt of signal), on:

   1) Two out of three High-High containment pressure signals coincident with two out of three High containment pressure signals

   2) High steam flow in two out of three lines (one out of two per line) coincident with either:

      a) Two out of three Low reactor coolant system average temperature signals, or

      b) Two out of three Low steam line pressure signals

14.2.5.1.2 ANALYSIS OF EFFECTS AND CONSEQUENCES

<u>Method of Analysis</u>

The following analyses of a secondary system steam release are performed for this section:

A) A full plant digital computer simulation using the LOFTRAN code (Reference 1) to determine RCS temperature and pressure during cooldown, and the effect of safety injection.

B) Analyses to determine that there is no damage to the core or reactor coolant system.

The following conditions are assumed to exist at the time of a secondary steam system release:

A) End-of-life shutdown margin at no load, equilibrium xenon conditions, and with the most reactive rod cluster control assembly (RCCA) stuck in its fully withdrawn position. Operation of RCCA banks during core burnup is restricted in such a way that addition of positive reactivity in a secondary system steam release accident will not lead to a more adverse condition than the case analyzed.

B) A negative moderator coefficient corresponding to the end-of-life rodded core with the most reactive RCCA in the fully withdrawn position. The variation of the coefficient with temperature and pressure is included in the LOFTRAN calculations. The k_{eff} versus temperature at 1050 psia corresponding to the negative moderator temperature coefficient used is shown in Figure 14.2.5-1.

C) Minimum capability for injection of high concentration boric acid solution corresponding to one safety injection pump delivering full flow to the cold leg header. No credit is taken for the low concentration boric acid which must be swept from the safety injection lines downstream of the Refueling Water Storage Tank (RWST) prior to the delivery of boric acid (1950 ppm) to the reactor coolant loops.

D) The case studied is a steam flow of 280 lbs/sec at 1100 psia from one steam generator with offsite power available. This is the maximum capacity of any single steam dump, relief or safety valve. Hot standby conditions with minimum required shutdown margin at the no-load T-avg at time zero are assumed since this represents the most conservative initial condition.

Should the reactor be just critical or operating at power at the time of a steam release, the reactor will be tripped by the normal overpower protection when the power level reaches a trip point. Following a trip at power, the RCS contains more stored energy than at no-load, the average coolant temperature is higher than at no-load and there is appreciable energy stored in the fuel. Thus, the additional stored energy is removed via the cooldown caused by the steam release before the no-load conditions of RCS temperature and shutdown margin assumed in the analyses are reached. After the additional stored energy has been removed, the cooldown and reactivity insertions proceed in the same manner as in the analysis which assumes no-load condition at time zero. However, since the initial steam generator water inventory is greatest at no-load, the magnitude and duration of the RCS cooldown are less for steam line releases occurring at power.

E) In computing the steam flow, the Moody curve (Reference 2) for $fL/D = 0$ is used.

F) Perfect moisture separation in the steam generator is assumed.

<u>Results</u>

The calculated time sequence of events for this accident is listed in Table 14.2.5-1.

The results presented are a conservative indication of the events which would occur assuming a secondary system steam release since it is postulated that all of the conditions described above occur simultaneously.

Figures 14.2.5-2 and 14.2.5-3 show the transient results for a steam flow of 280 lbs/sec at 1100 psia. The assumed steam release is typical of the capacity of any single steam dump, relief or safety valve.

Safety injection is initiated automatically by low pressurizer pressure. Minimum safety injection capability corresponding to one out of four safety injection pumps in operation is assumed. Safety injection flow used in the analysis is shown in Figure 14.2.5-13.

Boron solution at 1950 ppm enters the RCS providing sufficient negative reactivity to prevent core damage. The calculated transient is quite conservative with respect to cooldown, since no credit is taken for this energy stored in the system metal other than that of the fuel elements or the energy stored in the other steam generators. Since the transient occurs over a period of about ten minutes, the neglected stored energy is likely to have a significant effect in slowing the cooldown.

Conclusions

The analysis shows that the criteria stated earlier in this section are satisfied. For an accidental depressurization of the main steam system, the minimum DNBR stays above the limiting value and no system design limits are exceeded.

14.2.5.2 STEAM SYSTEM PIPING FAILURE

14.2.5.2.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

The steam release arising from a rupture of a main steam line would result in an initial increase in steam flow which decreases during the accident as the steam pressure falls. The energy removal from the RCS causes a reduction of coolant temperature and pressure. In the presence of a negative moderator temperature coefficient, the cooldown results in an insertion of positive reactivity. If the most reactive RCCA is assumed stuck in its fully withdrawn position after reactor trip, there is an increased possibility that the core will become critical and return to power. The core is ultimately shut down by the boric acid delivered by the safety injection system.

The analysis of a main steam line rupture is performed to demonstrate that the following criteria are satisfied:

A) Assuming a stuck RCCA with or without offsite power, and assuming a single failure in the Engineered Safety Features, the core remains in place and intact. Radiation doses do not exceed the guidelines of 10 CFR 100.

B) Although DNB and possible clad perforation following a steam pipe rupture are not necessarily unacceptable, the analysis, in fact, shows that no DNB occurs for any rupture assuming the most reactive RCCA stuck in its fully withdrawn position.

The major rupture of a steam line is the most limiting cooldown transient and thus is analyzed at zero power with no decay heat. Decay heat would retard the cooldown, thereby reducing the return to power. A detailed analysis of this transient with the most limiting break size, a double-ended rupture of the main steam piping, is presented here.

The Main Steam Isolation Valve Assembly (MSIVA) consists of the MSIV, the Main Steam Check Valve (MSCV), and the Main Steam Bypass Valve (MSBV). For breaks downstream of the MSIVA, the MSIVs will fully close rapidly following a large break in the steam line, completely terminating the blowdown. For breaks between the steam generator exit and the MSIVA, the passive MSCV will prevent blowdown from the intact steam lines. For any break, in any location, no more than one steam generator would experience an uncontrolled blowdown even if one of the MSIVs fails to close.

Steam flow is measured by monitoring dynamic head in nozzles located in the throat of the steam generator flow restrictor. The effective throat area of the nozzles is 1.4 square feet, which is considerably less than the main steam pipe and thus the nozzles also serve to limit the maximum steam flow for a break at any location.

14.2.5.2.2 ANALYSIS OF EFFECTS AND CONSEQUENCES

Method of Analysis

The analysis of the steam pipe rupture has been performed to determine:

A) The core heat flux and RCS temperature and pressure transients resulting from the cooldown following the steam line break. The LOFTRAN code (Reference 1) has been used.

B) The thermal and hydraulic behavior of the core following a steam line break. A detailed thermal and hydraulic digital-computer code, THINC, has been used to determine if DNB occurs for the core conditions computed in item A above.

The following conditions are assumed to exist at the time of a main steam line break accident: (see Table 14.2.5-4)

A) End-of-life shutdown margin at no load, equilibrium xenon conditions, and the most reactive RCCA stuck in its fully withdrawn position. Operation of the control rod banks during core burnup is restricted in such a way that addition of positive reactivity in a steam line break accident will not lead to a more adverse condition than the case analyzed.

B) A negative moderator coefficient corresponding to the end-of-life rodded core with the most reactive RCCA in the fully withdrawn position. The variation of the coefficient with temperature and pressure has been included. The K_{eff} versus temperature at 1050 psi corresponding to the negative moderator temperature coefficient used is shown in Figure 14.2.5-1.

The core properties associated with the sector nearest the affected steam generator and those associated with the remaining sector were conservatively combined to obtain average core properties for the reactivity feedback calculation. Further, it was conservatively assumed that the core power distribution was uniform. These two conditions cause underprediction of the reactivity feedback in the high power region near the stuck RCCA. The reactivity, as well as the power distribution, was checked for the limiting conditions for the cases analyzed. This core analysis considered the Doppler reactivity from the high fuel temperature near the stuck RCCA, moderator feedback from the high water enthalpy near the stuck RCCA, power redistribution and non-uniform core inlet temperature effects. For cases in which steam generation occurs in the high flux regions of the core, the effect of void formation was also included.

C) Minimum capability for injection of boric acid (1950 ppm) solution corresponding to the most restrictive single failure in the safety injection portion of the Emergency Core Cooling System (ECCS). The ECCS consists of three systems: (1) the passive accumulators; (2) the low head safety injection (residual heat removal) system; and (3) the high head safety injection system. Only the high head safety injection system and the passive accumulators are modeled for the steam line break accident analysis.

The modeling of the safety injection system in LOFTRAN is described in Reference 1. The flow corresponds to that delivered by one SI pump delivering full flow to the cold leg header. No credit has been taken for the low concentration borated water which must be swept from the lines downstream of the RWST prior to the delivery of boric acid to the reactor coolant loops.

The calculation assumes the boric acid is mixed with and diluted by the water flowing in the RCS prior to entering the reactor core. The concentration after mixing depends upon the relative flow rates in the RCS and in the safety injection system. The variation of mass flow rate in the RCS due to water density changes is included in the calculation as is the variation of flow rate in the SI system due to changes in the

RCS pressure. The SI system flow calculation includes the line losses in the system as well as the SI pump head curve. Figure 14.2.5-13 provides the relationship between SI flow and RCS pressure.

For the cases where offsite power is assumed, the sequence of events in the safety injection system is the following: After the generation of the safety injection signal (appropriate delays for instrumentation, logic, and signal transport included), the appropriate valves begin to operate and one SI pump starts. In 23 seconds, the valves are assumed to be in their final position and the pump is assumed to be at full speed. The volume containing the low concentration borated water is swept into core before the 1950 ppm borated water reaches the core. This delay, described above, is inherently included in the modeling.

In cases where offsite power is not available, an additional 22 second delay is assumed to start the diesel generators and to commence loading the necessary safety injection equipment onto them.

D) Design value of the steam generator heat transfer coefficient.

E) Since the steam generators are provided with integral flow restrictors with a 1.4 ft^2 throat area, any rupture with a break area greater than 1.4 ft^2, regardless of location, would have the same effect on the NSSS as the 1.4 ft^2 break. The following cases have been considered in determining the core power and RCS transients:

1) Complete severance of a pipe, with the plant initially at no-load conditions, full reactor coolant flow with offsite power available.

2) Case (1) with loss of offsite power simultaneous with the steam line break and initiation of the SI signal. Loss of offsite power results in reactor coolant pump coastdown.

F) Power peaking factors corresponding to one stuck RCCA and nonuniform core inlet coolant temperatures are assumed to occur in the sector with the stuck RCCA. The power peaking factors account for the effect of the local void in the region of the stuck RCCA during the return to power phase following the steam line break. This void in conjunction with the large negative moderator coefficient partially offsets the effect of the stuck RCCA. The power peaking factors depend upon the core power, temperature, pressure, and flow, and thus are different for each case studied.

The core parameters used for each of the two cases correspond to values determined from the respective transient analysis.

Both the cases above assume initial hot shutdown conditions at time zero since this represents the most pessimistic initial condition. Should the reactor be just critical or operating at power at the time of a steam line break, the reactor will be tripped by the normal overpower protection system when power level reaches a trip point. Following a trip at power, the reactor coolant system contains more stored energy than at no-load, the average coolant temperature is higher than at no-load, and there is appreciable energy stored in the fuel. Thus, the additional stored energy is removed via the cooldown caused by the steam release before the no-load conditions of RCS temperature and shutdown margin assumed in the analyses are reached. After the additional stored energy has been removed, the cooldown and reactivity insertions proceed in the same manner as in the analysis which assumes no-load condition at time zero.

In addition, since the initial steam generator water inventory is greatest at no-load, the magnitude and duration of RCS cooldown are more severe than for the steam line breaks occurring at power.

G) In computing the steam flow during a steam line break, the Moody curve (Reference 2) for fL/D = 0 is used.

H) Feedwater addition aggravates cooldown accidents like the steam line rupture. Therefore, the maximum feedwater flow is assumed. All the main and auxiliary feedwater pumps are assumed to be operating at full capacity when the rupture occurs, even though the plant is assumed to be in a hot standby condition. The maximum auxiliary feedwater flow to the faulted loop is assumed to be 800 gpm.

The sensitivity of the core analysis to plant operation at zero power using the standby steam generator feedwater pumps was considered. The maximum main feedwater flow assumed bounds the case for a zero power MSLB with a bypass feedwater control valve failing open and continued standby feedwater at 1350 gpm to the faulted generator to ten minutes.

When a Safety Injection signal actuation occurs, the main feedwater pumps trip, the feedwater control valves (FCVs) close, and the main feedwater pump discharge valves start to close (90 second closure). In the analysis, the FCV in the faulted loop is assumed to fail open, such that the faulted steam generator continues to be fed by the condensate pumps (which do not trip on SI signal actuation) until the main feedwater pump discharge valves close. A conservatively high flow rate to the depressurizing steam generator is assumed prior to isolation.

I) The effect of the heat transferred from thick metal in the pressurizer and reactor vessel upper head is not included in the cases analyzed. Studies previously performed have shown that the heat transferred to the coolant from these latent sources is a net benefit in DNB and RCS energy when the effect of the extra heat on reactivity and peak power is considered.

Results

The calculated sequence of events for the cases analyzed is shown in Tables 14.2.5-2 and 14.2.5-3.

The results presented are a conservative indication of the events which would occur assuming a steam line rupture since it is postulated that all of the conditions described above occur simultaneously.

Core Power and Reactor Coolant System Transient

Figures 14.2.5-5 through 14.2.5-8 show the RCS transient and core heat flux following a main steam line rupture (complete severance of a pipe) downstream of the MSIVA at initial no-load condition (Case A). Offsite power is assumed available so that full reactor coolant flow exists. The transient shown assumes an uncontrolled steam release from only one steam generator. Should the core be critical at near zero power when the rupture occurs, the initiation of safety injection by high steam flow coincident with low steam line pressure or low T-avg will trip the reactor. Steam release from more than one steam generator will be prevented by automatic closure of the MSIVs in the steam lines, by high containment pressure signals, or by high steam flow coincident with low steam line pressure. (For a break upstream of the MSIVA, MSIV closure is not required due to the presence of the MSCVs, which prevent blowdown of the unfaulted steam generators. In this case, SI actuation would occur immediately from high differential steam pressure between the faulted steam line and the main steam header. The results would be less severe than those for the cases presented.)

As shown in Figure 14.2.5-8, the core attains criticality with the RCCAs inserted (with the design shutdown assuming one stuck RCCA) before boron solution at 1950 ppm enters the RCS. A peak core power well below the nominal full power value is attained.

Figures 14.2.5-9 through 14.2.5-12 show the response of the salient parameters for Case B, which corresponds to the case discussed above with additional loss of offsite power at the time the SI signal is generated. The SI system delay time includes 22 seconds to start the diesel generator and load the necessary equipment and 23 seconds to start the SI pump and open the valves. Criticality is achieved later and the core power increase is slower than in Case A. The ability of the emptying steam generator to extract heat from the RCS is reduced by the decreased flow in the RCS.

It should be noted that following a steam line break, only one steam generator blows down completely. Thus, the remaining steam generators are still available for dissipation of decay heat after the initial transients is over. In the case of loss of offsite power, this heat is removed to the atmosphere via the steam line safety valves.

Margin to Critical Heat Flux

A DNB analysis was performed for the limiting case. It was found that the minimum DNBR is greater than the limit value.

Conclusions

The analysis has shown that the criteria stated earlier are satisfied. Although DNB and possible clad perforation following a steam pipe rupture are not necessarily unacceptable and not precluded by the criteria, the above analysis, in fact, shows that the minimum DNBR remains above the limit value for any rupture assuming the most reactive RCCA stuck in its fully withdrawn position.

14.2.5.3 CONTAINMENT PRESSURE RESPONSE TO STEAMLINE BREAK

Analyses have been performed for the Main Steam Line Break (MSLB) containment response considering a spectrum of break sizes, power levels, and different single failures. These analyses are described in Section 14.3.4 in detail.

14.2.5.4 DOSE EVALUATION

An evaluation of the offsite doses following a steamline break was completed to determine both a pre-accident iodine spike and an accident initiated iodine spike.

For the pre-accident iodine spike it is assumed that a reactor transient has occurred prior to the accident and has raised the RCS iodine concentration to 60 μCi/gm of dose equivalent (DE) I-131. For the accident initiated iodine spike the reactor trip associated with the steamline break (SLB) creates an iodine spike in the RCS which increases the iodine release rate from the fuel to the RCS to a value 500 times greater than the release rate corresponding to the maximum equilibrium RCS Technical Specification concentration of 1.0 μCi/gm of DE I-131. The duration of the accident initiated iodine spike is 1.6 hours.

The noble gas activity concentration in the RCS at the time the accident occurs is based on a fuel defect of 1.0%. The iodine activity concentration of the secondary coolant at the time the steamline break occurs is assumed to be equivalent to the Technical Specification limit of 0.10 μCi/gm of DE I-131.

The amount of primary to secondary steam generator tube leakage in each of the three steam generators is assumed to be equal to the Technical Specification limit for a single steam generator of 500 gallons/day. No credit for iodine removal is taken for any steam released to the condenser prior to reactor trip and concurrent loss of offsite power.

The steam generator connected to the broken steamline is assumed to boil dry within the initial two hours following the steamline break. The entire liquid inventory of this steam generator is assumed to be steamed off and all of the iodine initially in this steam generator is released to the environment. Also, iodine carried over to the faulted steam generator by steam generator tube leaks is assumed to be released directly to the environment.

An iodine partition factor in the intact steam generator of 0.01 (curies I/gm steam) (curies I/gm water) is used. All noble gas activity carried over to the secondary through steam generator tube leakage is assumed to be immediately released to the outside atmosphere from the secondary system.

At 24 hours after the accident the RHR System is assumed to be placed into service for heat removal, and there are no further steam releases to atmosphere from the secondary system.

The major assumptions and parameters used in the analysis are itemized in Table 14.2.5-5. The thyroid dose conversion factors, breathing rates, and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4.

The offsite dose limits for a steamline break with a pre-accident iodine spike are the guideline values of 10 CFR 100. These guideline values are 300 rem thyroid and 25 rem whole body. For a SLB with an accident initiated iodine spike the acceptance criteria are a "small fraction of" the 10 CFR 100 guideline values, or 30 rem thyroid and 2.5 rem whole body. The offsite thyroid and whole body doses due to the steamline break are given in Table 14.2.5-6. The offsite doses due to the steamline break are within the acceptance criteria.

14.2.5.5 REFERENCES

1. Burnett T. W. T. et al., "LOFTRAN Code Description," WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Non-Proprietary), dated April 1984.

2. Moody, F. J., "Transaction of the ASME, Journal of Heat Transfer," page 134, February 1965.

3. Letter, L-81-211, R. E. Uhrig (FPL) to D. G. Eisenhut (NRC), "NRC IE Bulletin 80-04," dated May 19, 1981.

4. Westinghouse letter to FPL, FPL-91-651, dated October 21, 1991, Safety Evaluation for Diesel Loading Scheme - Revision 3. Refer to FPL Safety Evaluation JPN-PTN-SEMJ-91-035, Revision 0; transmitted by letter JPN-PTN-91-0784, dated November 5, 1991.

5. Westinghouse WCAP-14276,"Florida Power and Light Company Turkey Point Units 3 and 4 Uprating Licensing Report," Revision 1, dated December 1995.

TABLE 14.2.5-1

TIME SEQUENCE OF EVENTS
CORE RESPONSE ANALYSIS

(Inadvertent Opening of a Steam Generator Relief or Safety Valve)

Time (sec)	Event
t = 0.	A. Reactor at hot zero power. All control rods inserted except most reactive RCCA. Shutdown Margin = 1.77% delta-k/k. Inadvertent opening of largest steam relief, safety or dump valve.
t = 169.	B. SIS actuation signal - Low Pressurizer Pressure.
t = 192.	C. One SI pump starts at rated speed 23 seconds after SI actuation signal.
t = 405.	D. Reactor becomes critical.

TABLE 14.2.5-2

TIME SEQUENCE OF EVENTS
CORE RESPONSE ANALYSIS

Case A - Steam System Piping Failure, With Offsite Power Available

Time (sec)	Event
t = 0.	A. Reactor at hot zero power. All control rods inserted except most reactive RCCA. Shutdown Margin = 1.77% delta-k/k. Double ended guillotine break located downstream of the Main Steam Isolation Valve Assembly.
t = 11.5	B. First SIS actuation signal - High Steam Flow coincident with Low Steam Pressure.
t = 11.9	C. Second SIS actuation signal - High Steam Flow coincident with Low RCS T-avg.
t = 20.5	D. Main feedwater flow to unfaulted steam generators terminated by FCV closure 9 seconds after SI actuation signal.
t = 25.2	E. Reactor becomes critical.
t = 28.5	F. MSIVs are closed 17 seconds after first SI actuation signal.
t = 34.5	G. One SI pump at rated speed 23 seconds after first SI actuation signal.
t = 71.4	H. Power reaches maximum level.
t = 104.	I. Main feedwater flow to faulted steam generator terminated by closure of main feed pumps discharge valve.
t = 130.	J. Reactor goes subcritical.

TABLE 14.2.5-3

TIME SEQUENCE OF EVENTS
CORE RESPONSE ANALYSIS

Case B - Steam System Piping Failure, Without Offsite Power Available

Time (sec)	Event
t = 0.	A. Reactor at hot zero power. All control rods inserted except most reactive RCCA. Shutdown Margin = 1.77% Δ-k/k. Double ended guillotine break located downstream of the Main Steam Isolation Valve Assembly. Offsite power lost.
t = 3.0	B. Reactor Coolant Pumps lose power, begin to coast down.
t = 11.1	C. First SIS actuation signal - High Steam Flow coincident with Low Steam Pressure.
t = 12.9	D. Second SIS actuation signal - High Steam Flow coincident with Low RCS T-avg.
t = 20.1	E. Main feedwater flow to all steam generators terminated by FCV closure 9 seconds after SI actuation signal.
t = 28.1	F. MSIVs are closed 17 seconds after first SI actuation signal.
t = 32.2	G. Reactor becomes critical.
t = 56.1	H. One SI pump at rated speed 45 seconds after first SI actuation signal.
t = 266.	I. Power reaches maximum level.
t = 298.	J. Reactor goes subcritical.

TABLE 14.2.5-4

Table deleted in Revision 15

TABLE 14.2.5-5

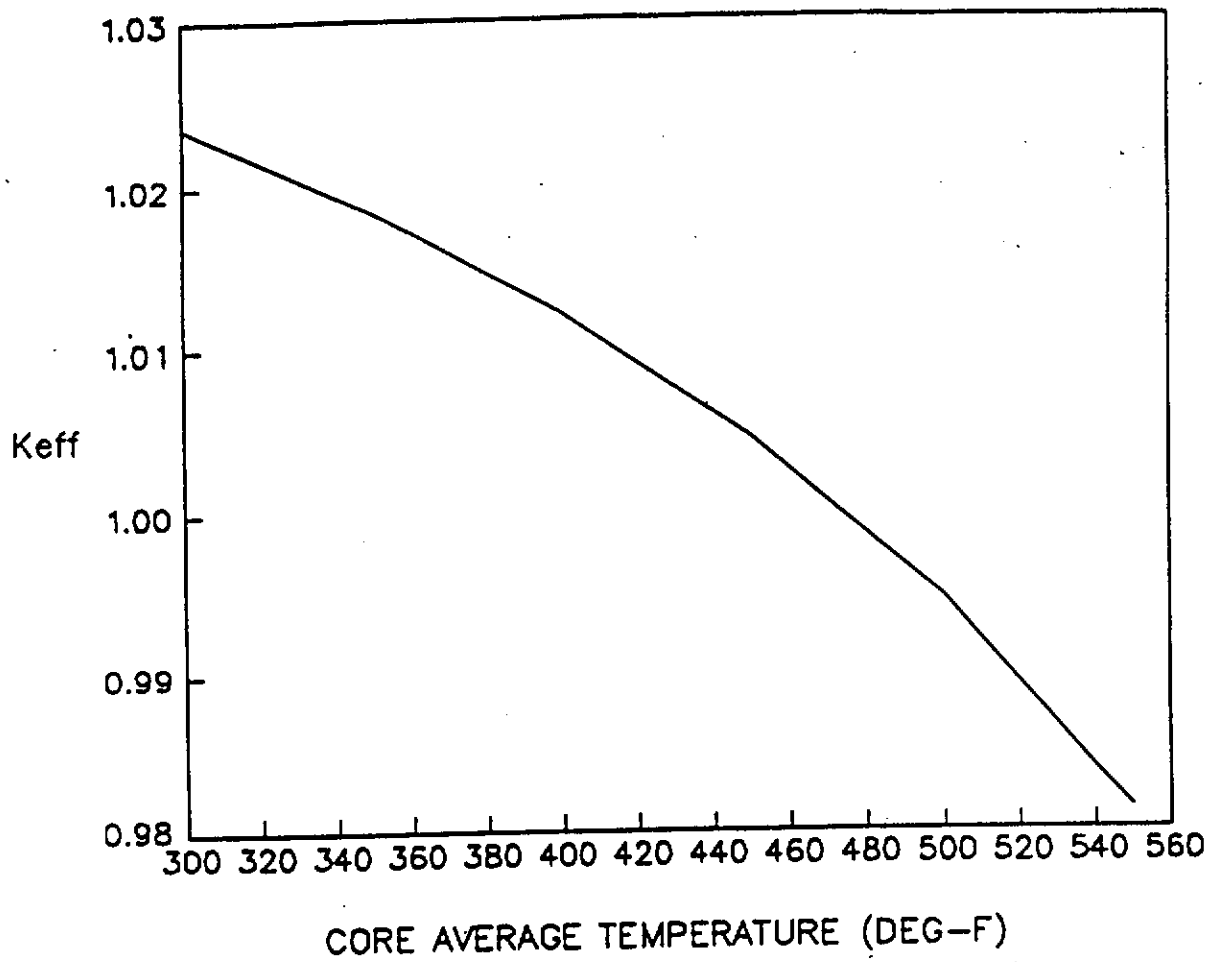
ASSUMPTIONS USED
FOR
STEAM LINE BREAK DOSE ANALYSIS

Power	2346 MWt
Reactor Coolant Noble Gas Activity Prior to Accident	1.0% Fuel Defect Level
Reactor Coolant Iodine Activity Prior to Accident:	
Pre-Accident Spike	60µCi/gm of DE I-131
Accident	1.0µCi/gm of DE I-131
Reactor Coolant Iodine Activity Increase Due to Accident Initiated Spike	500 times equilibrium release rate from fuel for initial 1.6 hours after SLB
Secondary Coolant Activity Prior to Accident	0.10 µCi/gm of DE I-131
SG Tube Leak Rate During Accident	500 gallons per day per SG
Iodine Partition Factor:	
Faulted SG	1.0 (SG assumed to steam dry)
Intact SGs	0.01
Duration of Activity Release Secondary System	24 hr
Offsite Power	Lost
Steam Release from SGs:	
Faulted	84,128 lb (0-2 hr)
Intact SGs	269,700 lb (0-2 hr) 369,300 lb (2-8 hr) 984,700 lb (8-24 hr)

TABLE 14.2.5-6

STEAM LINE BREAK OFFSITE DOSES

	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-24 Hours)
Thyroid Dose (rem) (Accident Initiated Spike)	4.2 E-1	1.1 E-1
Thyroid Dose (rem) (Pre-Accident Spike)	5.2 E-1	1.1 E-1
Whole Body Dose (rem)	1.9 E-4	4.6 E-5



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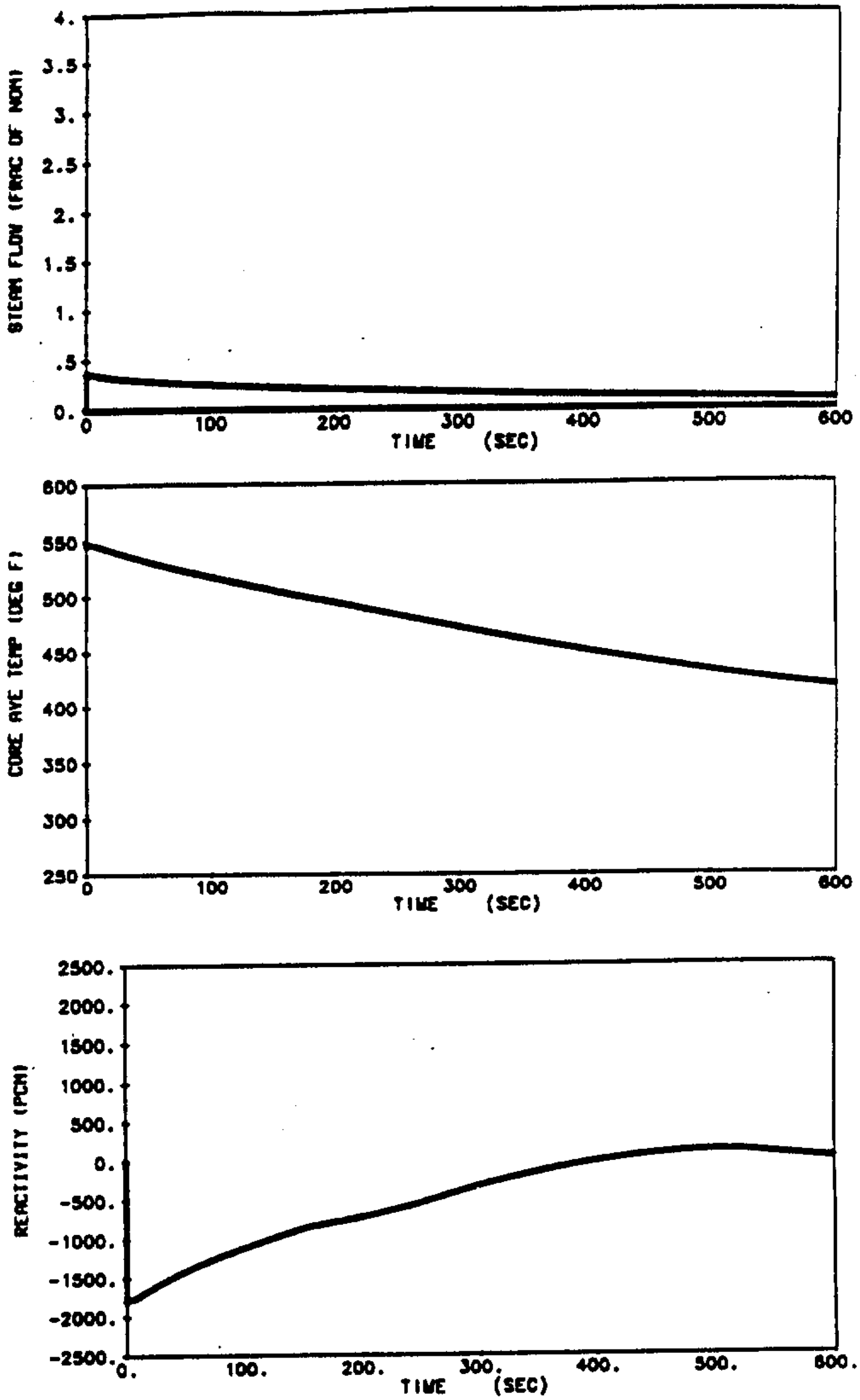
K_{eff} VS TEMPERATURE

FIGURE 14.2.5-1



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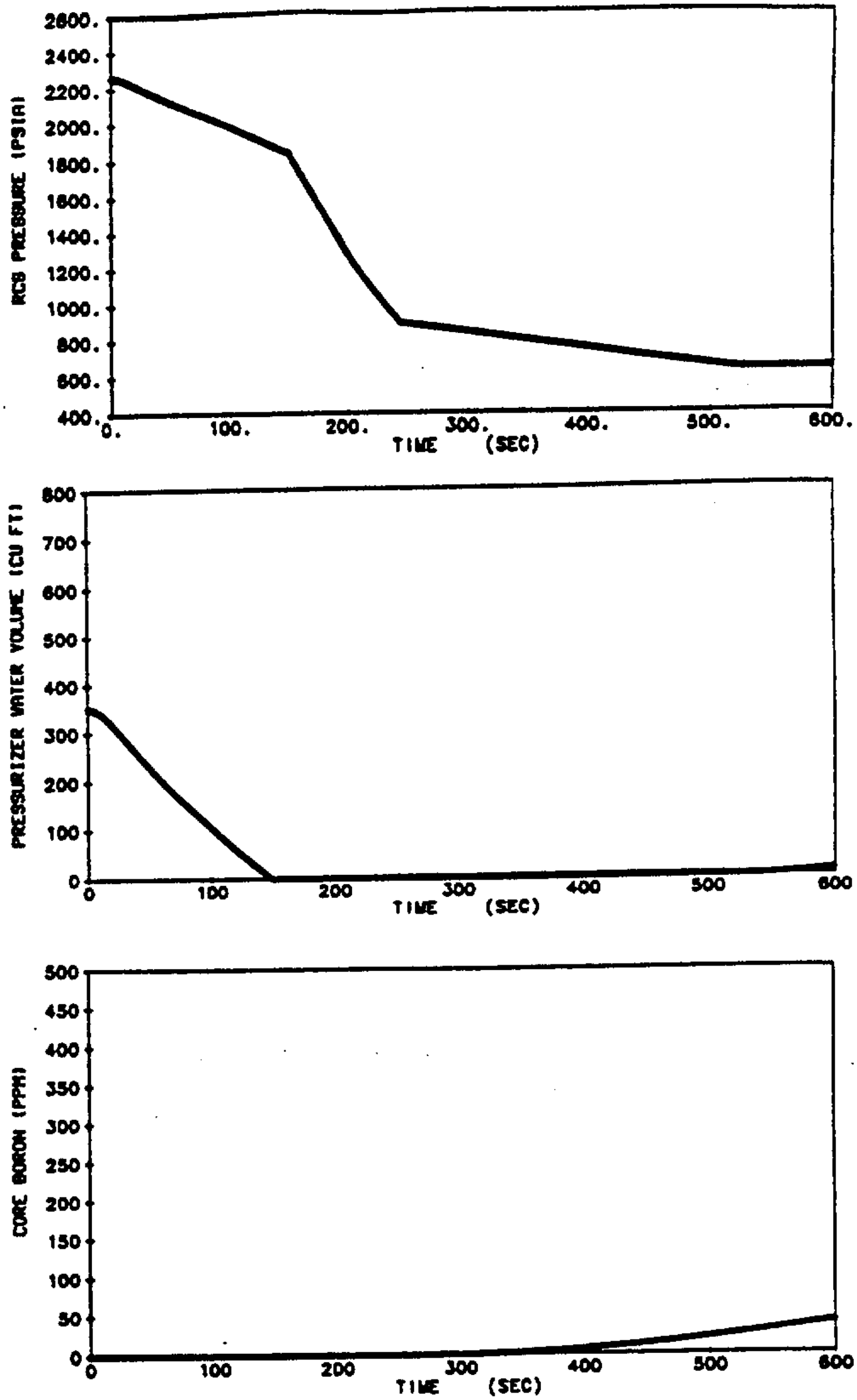
FAILURE OF STEAM GENERATOR SAFETY OR RELIEF VALVE

FIGURE 14.2.5-2



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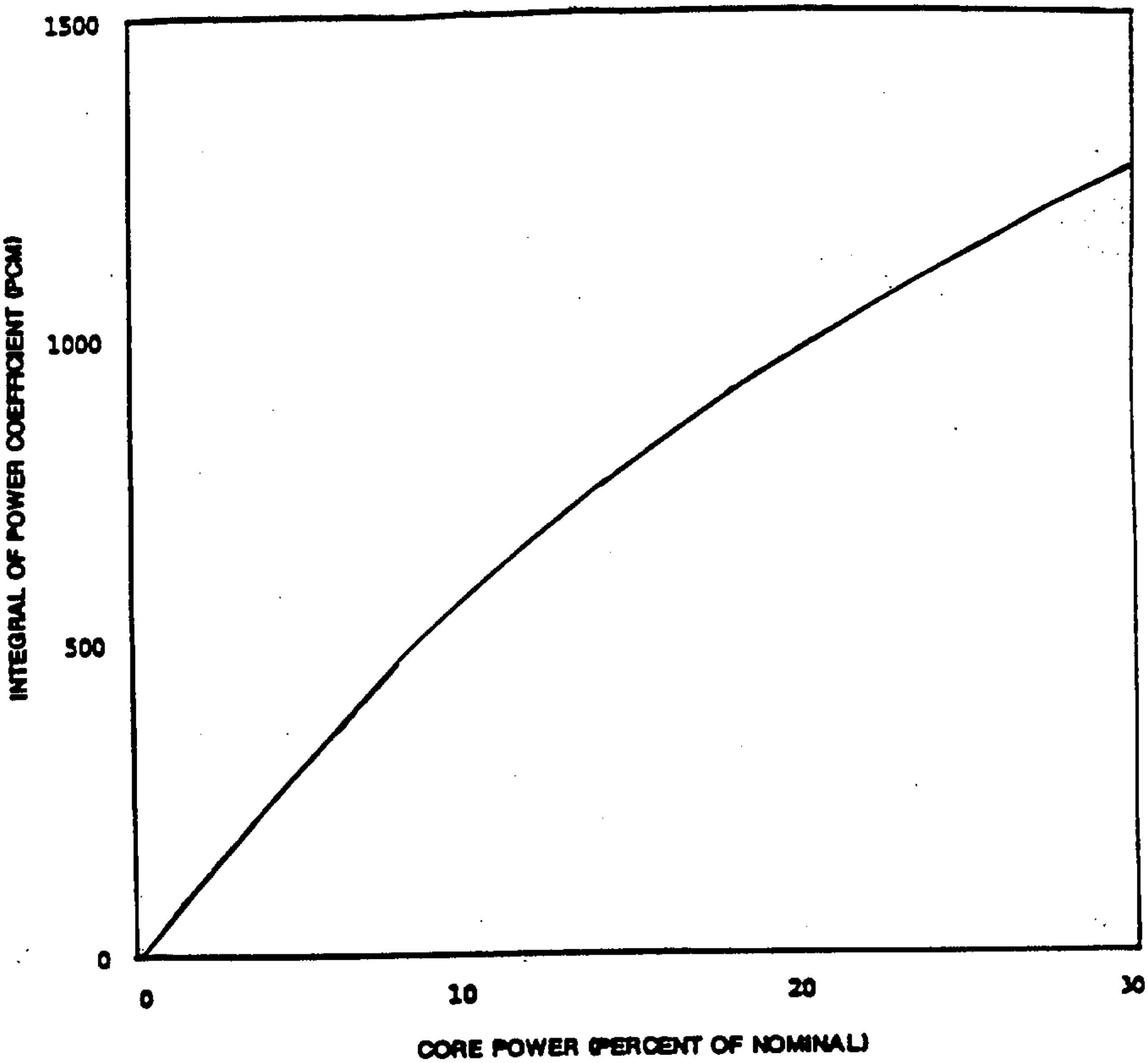
FAILURE OF STEAM GENERATOR
SAFETY OR RELIEF VALVE

FIGURE 14.2.5-3



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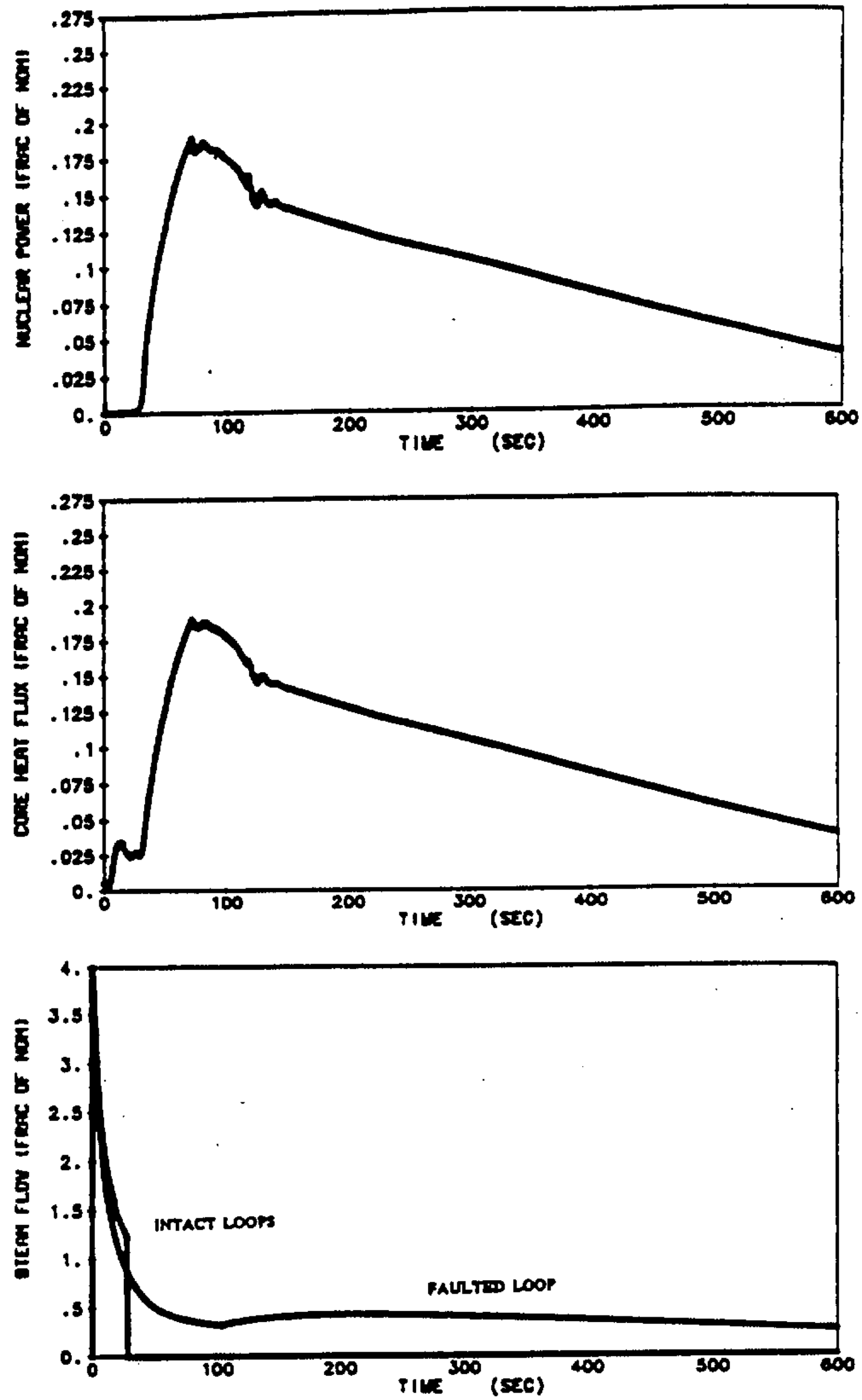
DOPPLER POWER FEEDBACK

FIGURE 14.2.5-4



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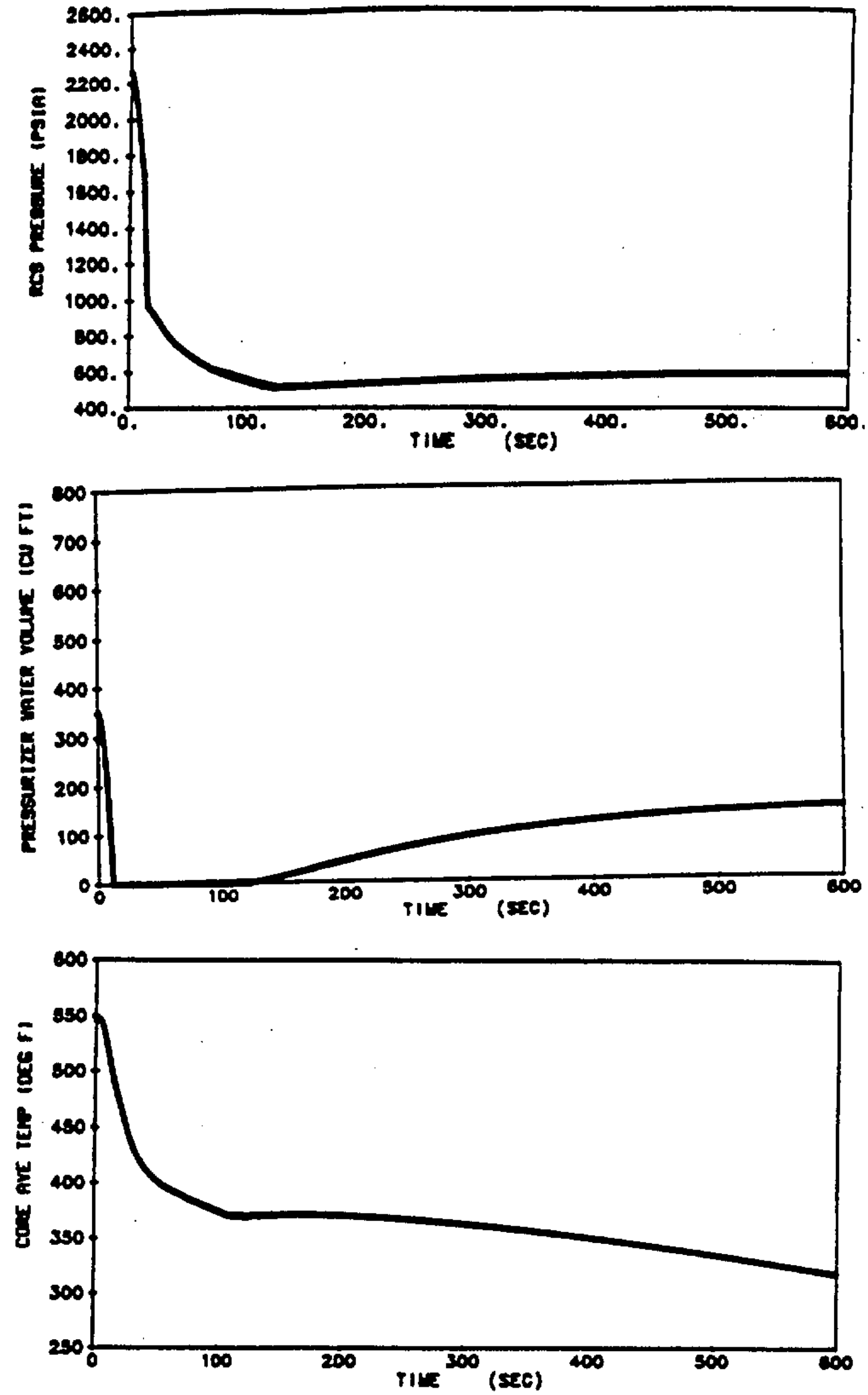
1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER AVAILABLE

FIGURE 14.2.5-5



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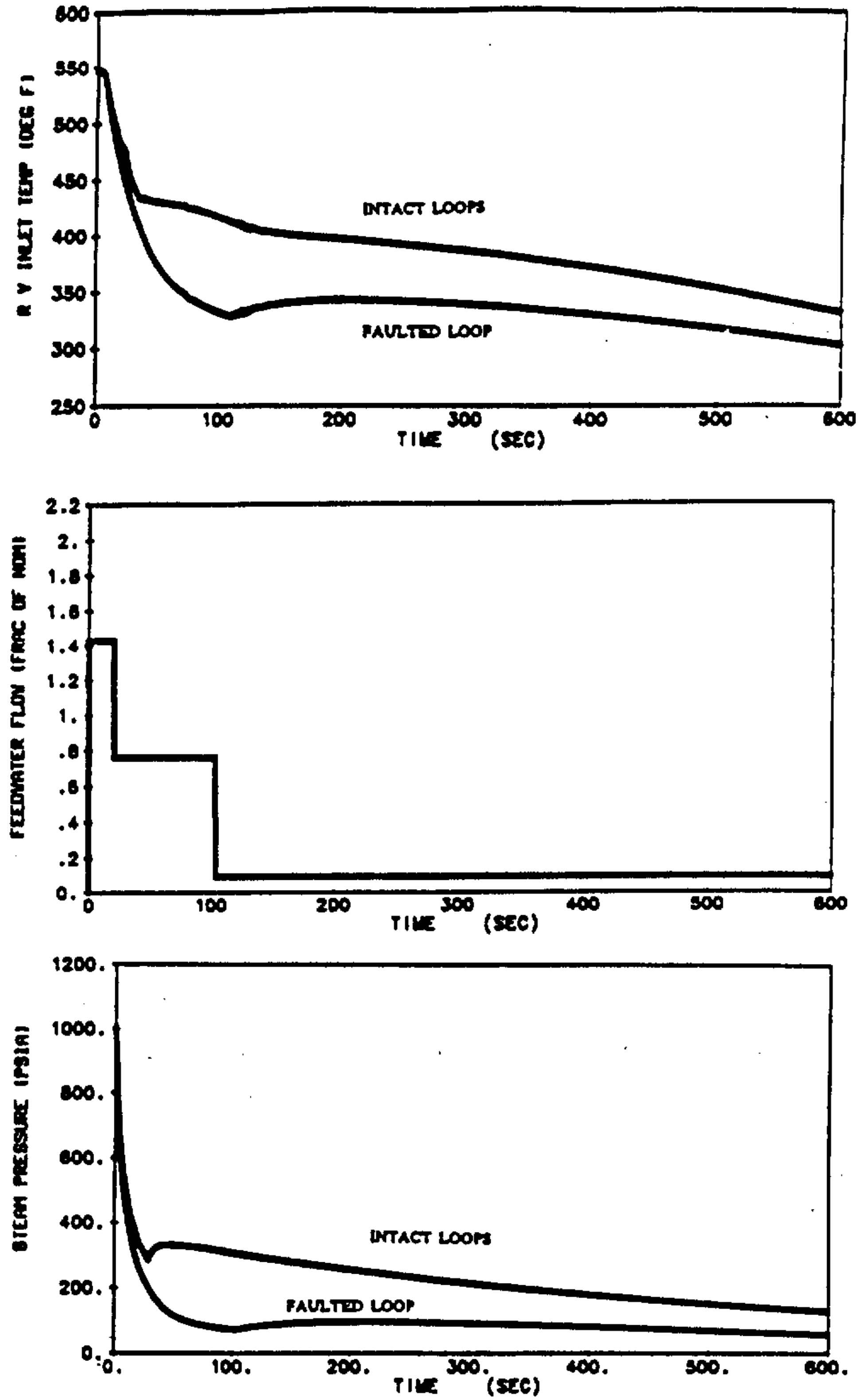
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER AVAILABLE

FIGURE 14.2.5-6



REV. 13 (10/96)

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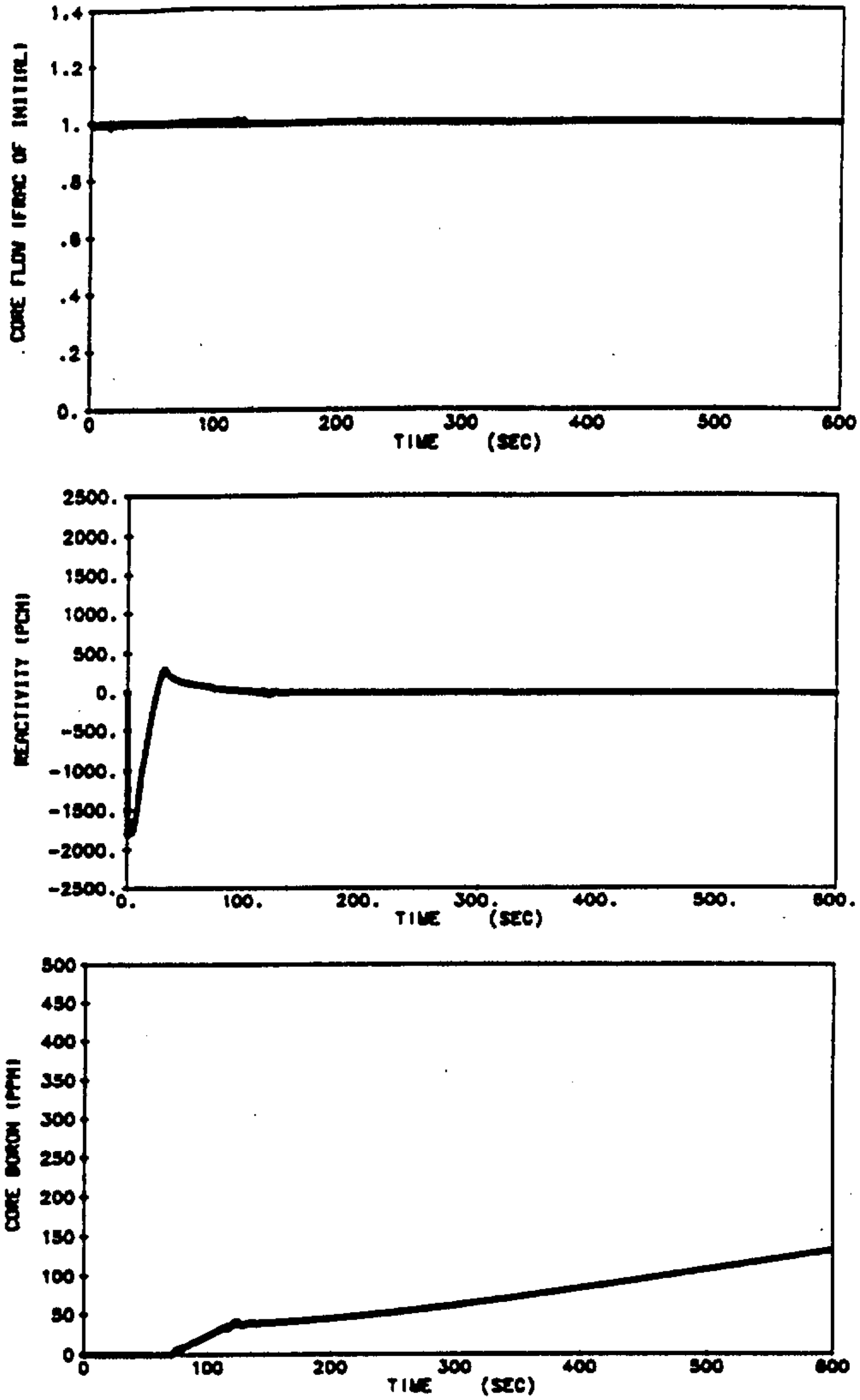
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER AVAILABLE

FIGURE 14.2.5-7



REV. 13 (10/96)

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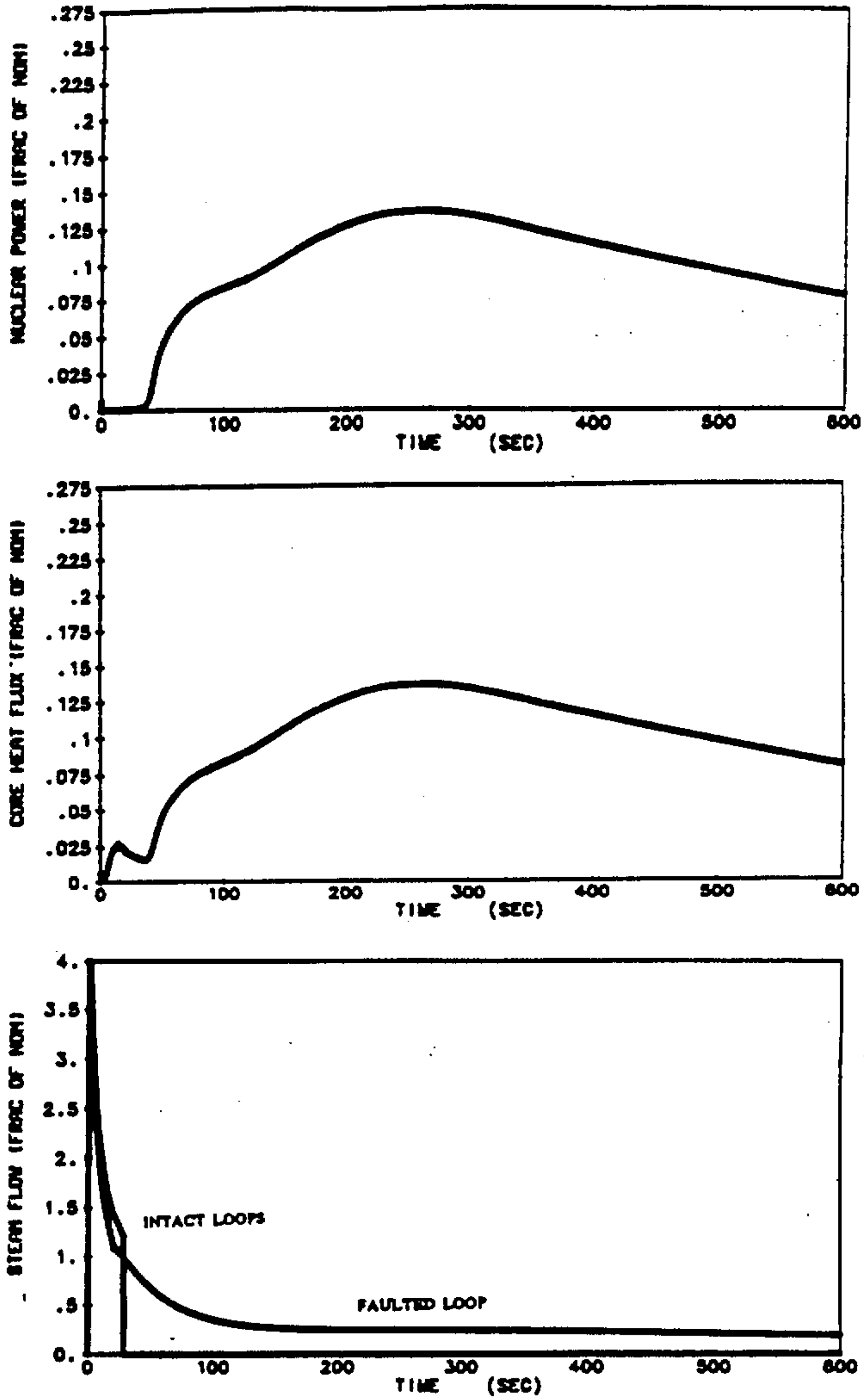
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER AVAILABLE

FIGURE 14.2.5-8



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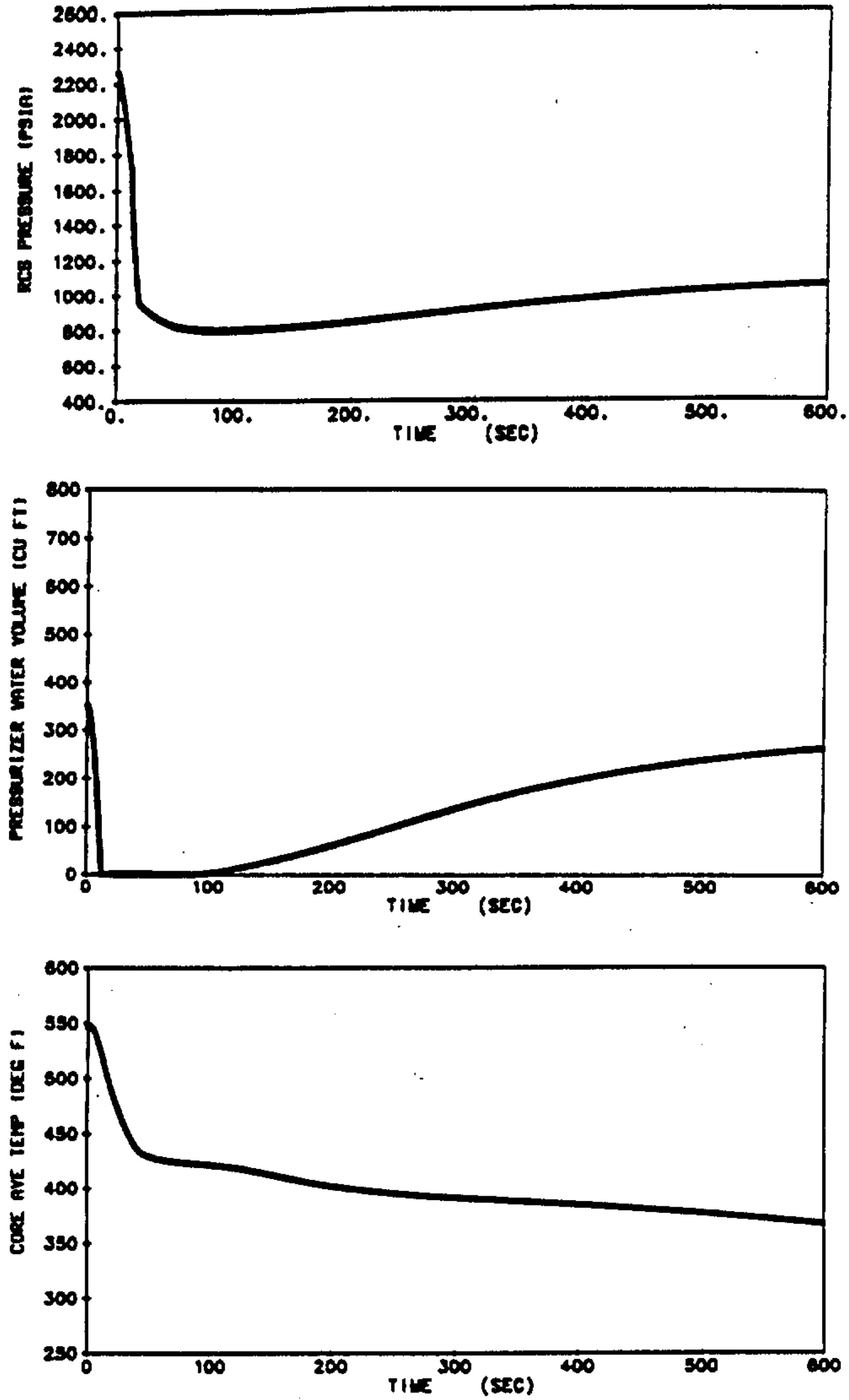
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER NOT AVAILABLE

FIGURE 14.2.5-9



REV. 13 (10/96)

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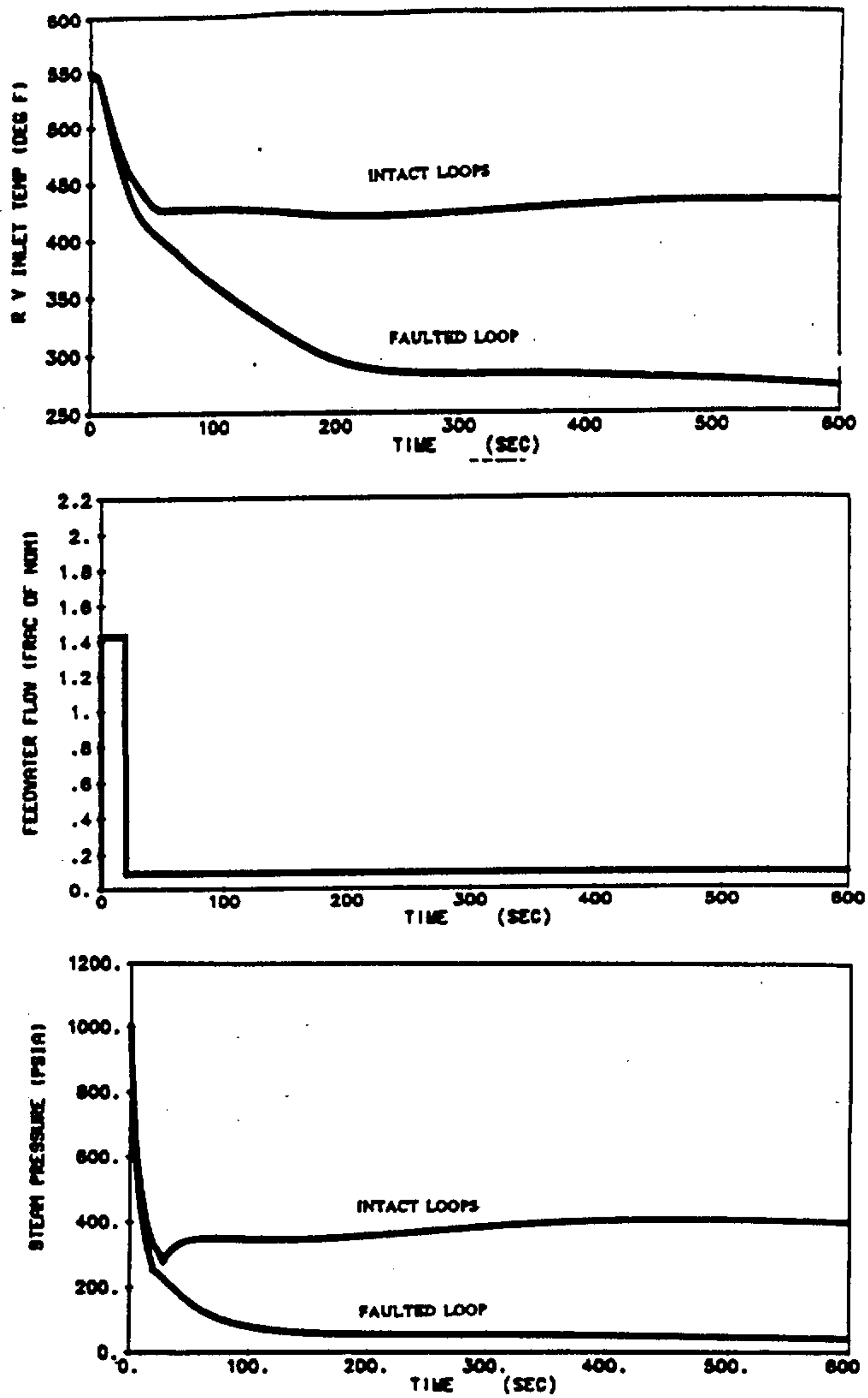
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER NOT AVAILABLE

FIGURE 14.2.5-10



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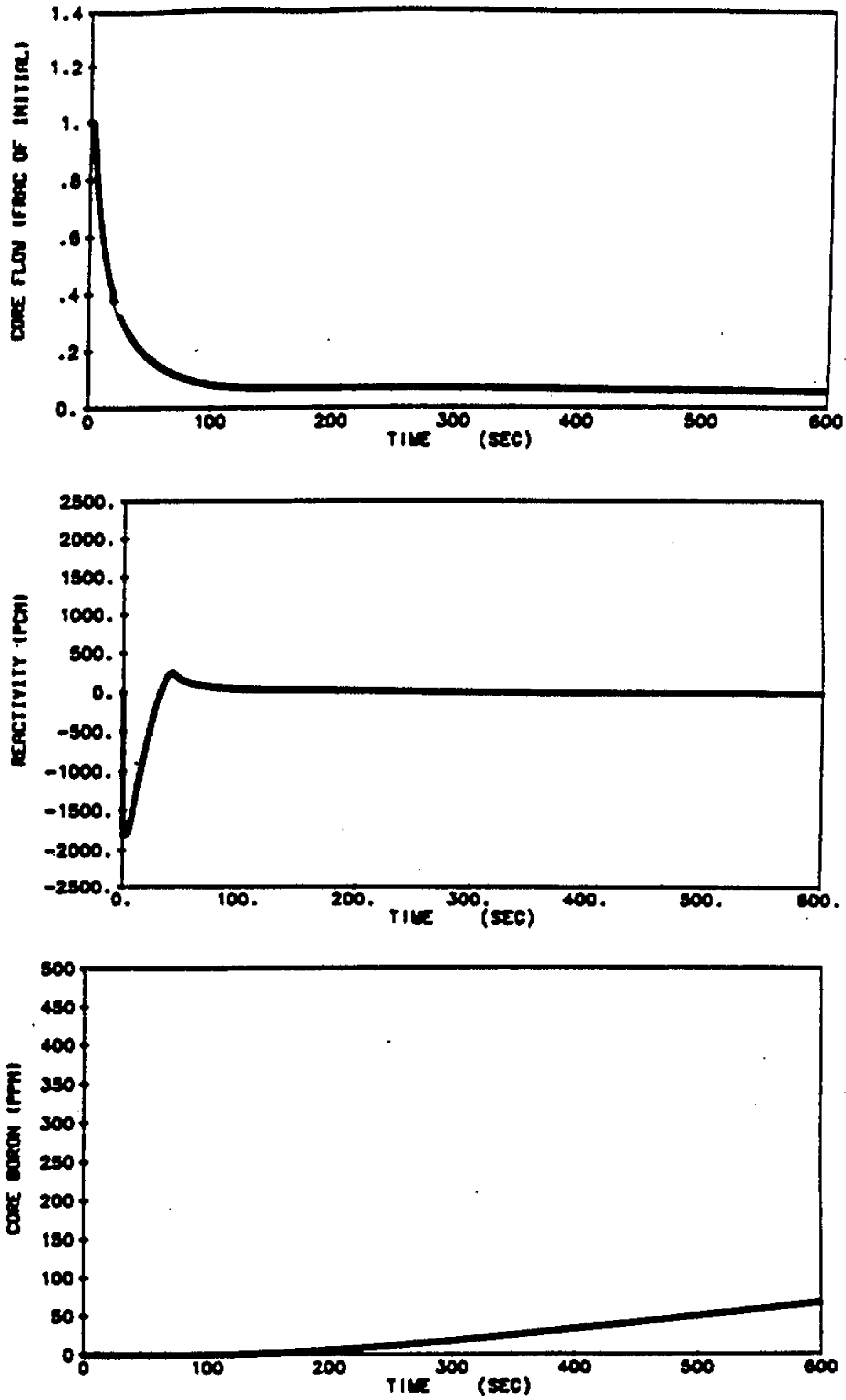
1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER NOT AVAILABLE

FIGURE 14.2.5-11



REV. 13 (10/96)

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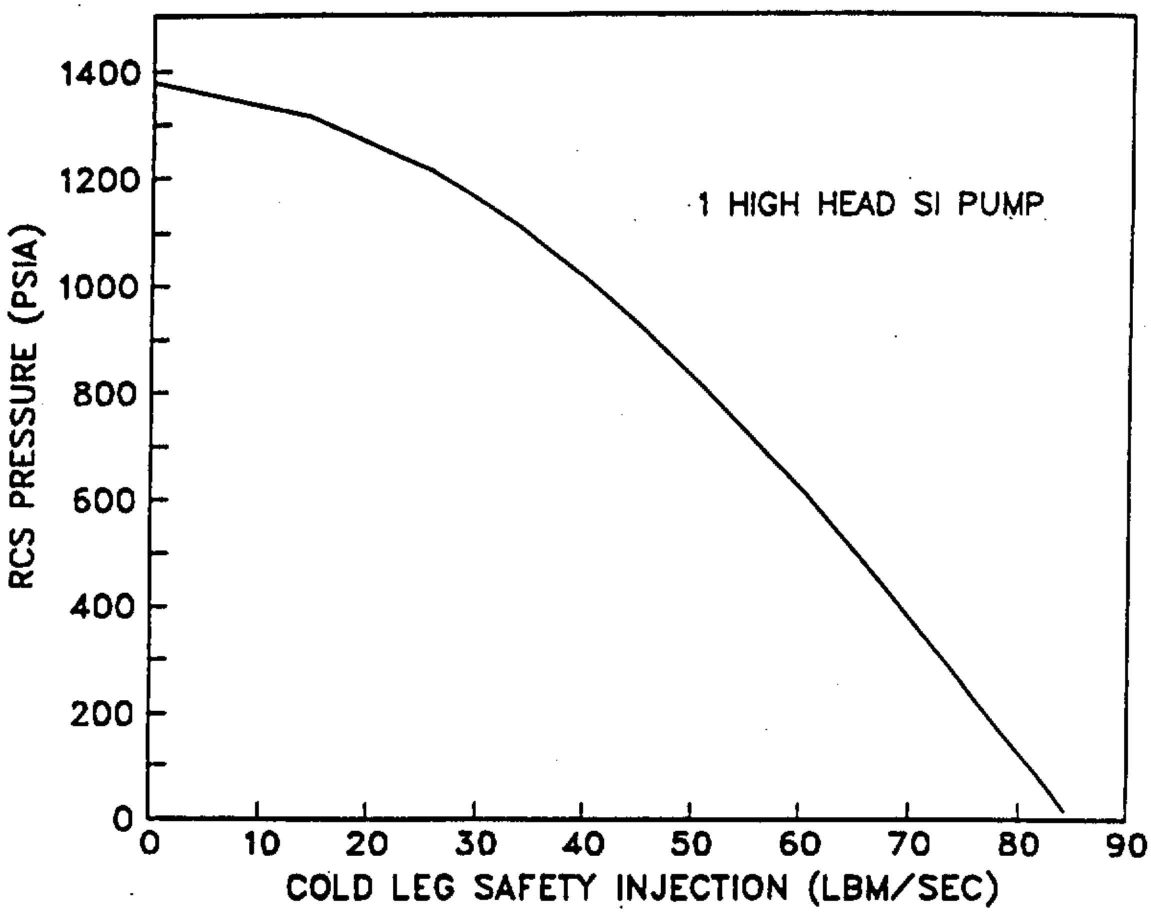
TURKEY POINT PLANT UNITS 3 & 4

1.4 FT^2 STEAM LINE RUPTURE
OFFSITE POWER NOT AVAILABLE

FIGURE 14.2.5-12



REV. 13 (10/96)

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TURKEY POINT PLANT UNITS 3 & 4

STEAM LINE BREAK
SAFETY INJECTION FLOW

FIGURE 14.2.5-13

[DELETED]

REV. 16 (10/99)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

MAIN STEAM LINE BREAK
0% POWER 1.4/2.8 FT^2 MSCV FAILURE
WITH OFFSITE POWER

FIGURE 14.2.5-14

[DELETED]

REV. 16 (10/99)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

MAIN STEAM LINE BREAK
0% POWER 1.4/2.8 FT^2 MSCV FAILURE
WITH OFFSITE POWER

FIGURE 14.2.5-15

[DELETED]

REV. 16 (10/99)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

MAIN STEAM LINE BREAK
0% POWER 1.4/2.8 FT^2 MSCV FAILURE
WITH OFFSITE POWER

FIGURE 14.2.5-16

14.2.6 RUPTURE OF A CONTROL ROD MECHANISM HOUSING-RCCA EJECTION

This accident is defined as a mechanical failure of a control rod drive mechanism pressure housing resulting in the ejection of the rod cluster control assembly (RCCA) and drive shaft. The consequence of this mechanical failure is a rapid positive reactivity insertion together with an adverse core power distribution, possibly leading to localized fuel rod damage. The resultant core thermal power excursion is limited by the Doppler reactivity effect of the increased fuel temperature and terminated by reactor trip actuated by high nuclear power signals.

A failure of a control rod mechanism housing sufficient to allow a control rod to be rapidly ejected from the core is not considered credible for the following reasons:

a) Each control rod drive mechanism housing is completely assembled and shop-tested at 3450 psig.

b) The mechanism housings are individually hydrotested after they are installed on the reactor vessel head to the head adapters, and checked during the hydrotest of the completed Reactor Coolant System.

c) Stress levels in the mechanism are not affected by anticipated system transients at power, or by thermal movement of the coolant loops. Moments induced by the design earthquake can be accepted within the allowable primary working stress range specified by the ASME Code, Section III, for Class 1 components.

d) The latch mechanism housing and rod travel housing are each a single length of forged type-304 stainless steel. This material exhibits excellent notch toughness at all temperatures that will be encountered.

A significant margin of strength in the elastic range, together with the large energy absorption capability in the plastic range, gives additional assurance that the gross failure of the housing will not occur. The joints between the latch mechanism and the head adapter and between the latch mechanism and the rod travel housing are threaded joints, reinforced using canopy type seal welds.

The operation of a chemical shim plant is such that the severity of an ejection accident is inherently limited. Since control rod clusters are used to control load variations only and core depletion is followed with boron dilution, there are only a few rods in the core at full power. Proper positioning of these rods is monitored by a control room alarm system. There are low and low-low RCCA insertion limit alarms. Operating instructions require boration at the low level alarm and emergency boration at the low-low level alarm. The control rod position monitoring and alarm systems are described in detail in Section 7.3 and in Reference 1.

Due to the extremely low probability of a rod cluster control assembly ejection accident, some fuel damage could be considered an acceptable consequence.

Comprehensive studies of the threshold of fuel failure and of the threshold of significant conversion of the fuel thermal energy to mechanical energy have been carried out as part of the SPERT project by the Idaho Nuclear Corporation (Reference 2). Extensive tests of UO_2 zirconium-clad fuel rods representative of those present in pressurized water reactor-type cores have demonstrated failure thresholds in the range of 240 to 257 cal/gm. However, other rods of a slightly different design exhibited failure as low as 225 cal/gm. These results differ significantly from the TREAT (Reference 3) results which indicated a failure threshold of 280 cal/gm. Limited results have indicated that this threshold decreased ~10 percent with fuel burnup. The clad failure mechanism appears to be melting for unirradiated (zero burnup) rods and brittle fracture for irradiated rods. The conversion ratio of thermal to mechanical energy is also important. This ratio becomes marginally detectable above 300 cal/gm for unirradiated rods and 200 cal/gm for irradiated rods; catastrophic failure (large fuel dispersal, large pressure rise), even for irradiated rods, did not occur below 300 cal/gm.

The real physical limits of this accident are that the rod ejection event and any consequential damage to either the core or the Reactor Coolant System must not prevent long-term core cooling and any offsite dose consequences must be within the guidelines of 10 CFR 100. More specific and restrictive criteria are applied to ensure fuel dispersal in the coolant, gross lattice distortion or severe shock waves will not occur. In view of the above experimental results, the conclusion of WCAP-7588 Rev I-A (Reference 1), and Reference 4, the limiting criteria are:

A. Average fuel pellet enthalpy at the hot spot must be maintained below 225 cal/gm for unirradiated and 200 cal/gm for irradiated fuel,

B. Peak reactor coolant pressure must be less than that which could cause RCS stresses to exceed the faulted-condition stress limits,

C. Fuel melting is limited to less than 10 percent of the fuel volume at the hot spot even if the average fuel pellet enthalpy is below the limits of Criterion A.

14.2.6.1 METHOD OF ANALYSIS

This section describes the models used and the results obtained. Only the initial few seconds of the power transient are discussed, since the long term considerations are the same as for a loss of coolant accident.

The calculations of the RCCA ejection transient is performed in two stages, first an average core channel calculation and then a hot region calculation. The average core calculation is performed using spatial neutron kinetics methods to determine the average power generation with time including the various total core feedback effects; i.e., Doppler reactivity and moderator reactivity. Enthalpy and temperature transients in the hot spot are then determined by multiplying the average core energy generation by the hot channel factor and performing a fuel rod transient heat transfer calculation. The power distribution calculated without feedback is pessimistically assumed to persist throughout the transient. A detailed discussion of the method of analysis can be found in Reference 1.

Average Core

The spatial kinetics computer code, TWINKLE (Reference 5), is used for the average core transient analysis. This code solves the two-group neutron diffusion theory kinetic equation in one, two, or three spatial dimensions (rectangular coordinates) for six delayed neutron groups and up to 2000 spatial points. The computer code includes a detailed multiregion, transient fuel clad coolant heat transfer model for calculation of pointwise Doppler and moderator feedback effects. In this analysis, the code is used as a one-dimensional axial kinetics code since it allows a more realistic representation of the spatial effects of axial moderator feedback and RCCA movement. However, since the radial dimension is missing, it is still necessary to employ very conservative methods (described below) of calculating the ejected rod worth and hot channel factor.

Hot Spot Analysis

In the hot spot analysis, the initial heat flux is equal to the nominal heat flux times the design hot channel factor. During the transient, the heat flux hot channel factor is linearly increased to the transient value in 0.1 second, the time for full ejection of the rod. Therefore, the assumption is made that the hot spot before and after ejection are coincident. This is very conservative since the peak after ejection will occur in or adjacent to the assembly with the ejected rod, and prior to ejection the power in this region will necessarily be depressed.

The average core energy addition, calculated as described above, is multiplied by the appropriate hot channel factors. The hot spot analysis is performed using the detailed fuel and clad transient computer code, FACTRAN (Reference 6). This computer code calculates the transient temperature distribution in a cross section of a metal clad UO_2 fuel rod, and the heat flux at the surface of the rod, using as input the nuclear power versus time and the local coolant conditions. The zirconium-water reaction is explicitly represented, and all material properties are represented as functions of temperature. A parabolic radial power distribution is used within the fuel rod.

FACTRAN uses the Dittus-Boelter or Jens-Lottes correlation to determine the film heat transfer before DNB, and the Bishop-Sandberg-Tong correlation (see Reference 7) to determine the film boiling coefficient after DNB. The Bishop-Sandberg-Tong correlation is conservatively used assuming zero bulk fluid quality. The DNB ratio is not calculated, instead the code is forced into DNB by specifying a conservative DNB heat flux. The gap heat transfer coefficient can be calculated by the code; however, it is adjusted in order to force the full power steady-state temperature distribution to agree with the fuel heat transfer design codes.

Calculation of Basic Parameters

Input parameters for the analysis are conservatively selected on the basis of values calculated for this type of core. The more important parameters are discussed below. Table 14.2.6-1 presents the parameters used in this analysis.

Ejected Rod Worths and Hot Channel Factors

The values for ejected rod worths and hot channel factors are calculated using either three-dimensional static methods or by a synthesis method employing one-dimensional and two-dimensional calculations. Standard nuclear design codes are used in the analysis. No credit is taken for the flux flattening effects of reactivity feedback. The calculation is performed for the maximum allowed bank insertion at a given power level, as determined by the rod insertion limits. Adverse xenon distributions are considered in the calculation to provide worst case results.

Appropriate margins are added to the ejected rod worth and hot channel factors to account for any calculational uncertainties, including an allowance for nuclear power peaking due to densification.

Power distribution before and after ejection for a "worst case" can be found in Reference 1. During plant startup physics testing, ejected rod worths and power distributions have been measured in the zero and full power configurations and compared to values used in the analysis. Experience has shown that the ejected rod worth and power peaking factors are consistently overpredicted in the analysis.

<u>Delayed Neutron Fraction, ß</u>

Calculations of the effective delayed neutron fraction ($ß_{eff}$) typically yield values no less than 0.70 percent at beginning-of-life and 0.50 percent at end-of-life. The ejected rod accident (in the zero power transients) is sensitive to $ß_{eff}$ if the ejected rod worth is equal to or greater than $ß_{eff}$. In order to allow for future cycles, conservative estimates of $ß_{eff}$ of 0.50 percent at beginning of cycle and 0.42 percent at end of cycle are used in the analysis.

<u>Reactivity Weighting Factor</u>

The largest temperature rises, and hence the largest reactivity feedbacks, occur in channels where the power is higher than average. Since the weight of a region is dependent on flux, these regions have high weights. This means that the reactivity feedback is larger than that indicated by a simple single channel analysis. Physics calculations have been carried out for temperature changes with a flat temperature distribution, and with a large number of axial and radial temperature distributions. Reactivity changes were compared and effective weighting factors determined. These weighting factors take the form of multipliers which, when applied to single channel feedbacks, correct them to effective whole core feedbacks for the appropriate flux shape. In this analysis, since a one-dimensional (axial) spatial kinetics method is employed, thus axial weighting is not necessary if the initial condition is made to match the ejected rod configuration. In addition, no weighting is applied to the moderator feedback. A conservative radial weighting factor is applied to the transient fuel temperature to obtain an effective fuel temperature as a function of time accounting for the missing spatial dimension. These weighting factors have also been shown to be conservative compared to three-dimensional analysis.

Moderator and Doppler Coefficient

The critical boron concentrations at the beginning-of-life and end-of-life are adjusted in the nuclear code in order to obtain moderator density coefficient curves which are conservative compared to actual design conditions for the plant. As discussed above, no weighting factor is applied to these results. The resulting moderator temperature coefficient is at least +7 pcm/°F at the appropriate zero or full power nominal average temperature for the beginning-of-life cases.

The Doppler reactivity defect is determined as a function of power level using a one-dimensional steady-state computer code with a Doppler weighting factor of 1.0. The Doppler weighting factor will increase under accident conditions, as discussed above.

Heat Transfer Data

The FACTRAN (Reference 6) code used to determine the hot spot transient contains standard curves of thermal conductivity versus fuel temperature. During a transient, the peak centerline fuel temperature is independent of the gap conductances during the transient. The cladding temperature is however strongly dependent on the gap conductance and is highest for high gap conductances. For conservatism a high gap heat transfer coefficient value of 10,000 Btu/hr-ft^2-F has been used during transients. This value corresponds to a negligible gap resistance and a further increase would have essentially no effect on the rate of heat transfer.

Coolant Mass Flow Rates

When the core is operating at full power, all three reactor coolant pumps will always be operating. However, for zero power conditions, the system may be operating with two pumps. The principal effect of operating at reduced flow is to reduce the film boiling heat transfer coefficient. This results in higher peak cladding temperatures, but does not affect the peak centerline fuel temperature. Reduced flow also lowers the critical heat flux. However, since DNB is always assumed at the hot spot, and since the heat flux rises very rapidly during the transient, this produces only second order changes in the cladding and centerline fuel temperatures. All zero power analyses for both average core and the hot spot have been conducted assuming two pumps in operation.

Trip Reactivity Insertion

The rods were assumed to be released 0.5 seconds after reaching the power range high neutron flux trip setpoint. The delay is constituted of 0.2 seconds for the instrumentation to produce a signal, 0.15 seconds for the trip breaker to open and 0.15 seconds for coil release. In calculating the shape of the insertion versus time curve all the rods are assumed to be dropped as a single bank from the fully withdrawn position. This means that the initial movement is through the low worth region at the extreme top of the core, which results in a conservatively slow reactivity insertion versus time curve.

Fuel Densification Effects

Fuel densification effects on rod ejection are accounted for according to the methods described in Reference 8.

Lattice Deformations

A large temperature gradient exists in the region of the hot spot. Since the fuel rods are free to move in a vertical direction, differential expansion between separate rods cannot produce distortion. However, the temperature gradients across individual rods may produce a force tending to bow the midpoint of the rods toward the hot spot. Physics calculations indicate that the net result of this would be a negative reactivity insertion. In practice, no significant bowing is anticipated, since the structural rigidity of the core is more than sufficient to withstand the forces produced.

Boiling in the hot spot region will produce a net fluid flow away from that region. However, the fuel heat is released to the water slowly, and it is considered inconceivable that cross flow will be sufficient to produce significant lattice forces. Even if massive and rapid boiling, sufficient to distort the lattice, is hypothetically postulated, the large void fraction in the hot spot region would produce a reduction in the total core moderator to fuel ratio, and a large reduction in this ratio at the hot spot. The net effect would therefore be a negative feedback. It is concluded that no conceivable mechanism exists for a net positive feedback resulting from lattice deformation. In fact, a small negative feedback may result. The effect is conservatively addressed in the following analyses.

Results

Cases are presented for both beginning and end-of-life at zero and full power.

A. Beginning of Cycle, Full Power

Control bank D was assumed to be inserted to its insertion limit. The worst ejected rod worth and hot channel factor were conservatively calculated to be 0.35 percent ΔK and 5.48, respectively. The peak hot spot average fuel pellet enthalpy was 190 cal/gm. The peak clad average temperature was 2660°F and the peak fuel centerline temperature is 5000°F. However, fuel melting was well within the limiting criterion of 10 percent of the pellet volume at the hot spot.

B. Beginning of Cycle, Zero Power

For this condition, control bank D was assumed to be fully inserted and banks B and C were at their insertion limits. The worst ejected rod is located in control bank D and has a worth of 0.71 percent ΔK and a hot channel factor of 8.0. The peak hot spot average fuel pellet enthalpy was 116 cal/gm. The peak clad average temperature reached 2033°F, the fuel centerline temperature was 3267°F.

C. End of Cycle, Full Power

Control bank D was assumed to be inserted to its insertion limit. The ejected rod worth and hot channel factors were conservatively calculated to be 0.30 percent ΔK and 5.52, respectively. The peak hot spot average fuel pellet enthalpy was 147 cal/gm. This resulted in a peak clad average temperature of 2072°F and the peak fuel centerline temperature was 4508°F.

D. End of Cycle, Zero Power

The ejected rod worth and hot channel factor for this case were obtained assuming control bank D to be fully inserted and banks B and C at their insertion limits. The results were 0.84 percent ΔK and 14.3, respectively. The peak hot spot average fuel pellet enthalpy was 110 cal/gm. The peak clad average and fuel centerline temperatures were 1967°F and 3098°F.

The effects on the overall transient behavior due to a change of the clad material from Zircaloy-4 to ZIRLO were determined in Reference 9 to be negligible. The ZIRLO cladding results in a negligible benefit in both the fraction of fuel melting at the hot spot and the fuel peak stored energy when compared to the results for Zircaloy-4

A summary of the cases presented above is given in Table 14.2.6-1. The nuclear power and hot spot fuel and clad temperature transients for the worst cases (beginning-of-life full power and zero power cases) are presented in Figures 14.2.6-1 and 14.2.6-2, and a time sequence of events is given in Table 14.2.6-2.

14.2.6.2 FISSION PRODUCT RELEASE

It is conservatively assumed that fission products are released from the gaps of all rods entering DNB. In all cases considered, less than 10 percent of the rods entered DNB based on a detailed three-dimensional THINC analysis. Although limited fuel melting at the hot spot was predicted for the BOL full power cases, melting is not expected since the analysis conservatively assumed that the hot spots before and after ejection were coincident.

14.2.6.3 PRESSURE SURGE

A detailed calculation of the pressure surge for an ejected worth of one dollar at beginning-of-life, hot full power, indicates that the peak pressure does not exceed that which would cause reactor pressure vessel stress to exceed the faulted condition stress limits (Reference 1). Since the severity of the present analysis does not exceed the "worst case" analysis, the accident for this plant will not result in an excessive pressure rise or further damage to the RCS.

14.2.6.4 DOSE EVALUATION

It is assumed that a mechanical failure of a control rod mechanism pressure housing has occurred, resulting in the ejection of a rod cluster control assembly and drive shaft. As a result of the accident fuel clad damage and a small amount of fuel melt are assumed to occur. Due to the pressure differential between the primary and secondary systems,radioactive reactor coolant is discharged from the primary into the secondary system. A portion of this radioactivity is released to the outside atmosphere through either the main condenser or the atmospheric dump valves (ADV)/safety valves. Iodine activity is contained in the secondary coolant prior to the accident and some of this activity is released to atmosphere as a result of steaming of the steam generators following the accident. Finally, radioactive reactor coolant is discharged to the containment leakage to the environment.

A pre-accident iodine spike is assumed to occur. For the pre-accident iodine spike it is assumed that a reactor transient has occurred prior to the rod ejection and has raised the RCS iodine concentration to 60 µCi/gm of dose equivalent (DE) I-131.

The noble gas activity concentration in the RCS at the time the accident occurs is based on a fuel defect level of 1.0%. The iodine activity concentration of the secondary coolant at the time the rod ejection accident occurs is assumed to be equivalent to the Technical Specification limit of 0.10 µCi/gm of (DE) I-131.

As a result of the rod ejection accident less than 10% of the fuel rods in the core undergo DNB. In determining the offsite doses following rod ejection accident, it is conservatively assumed that 10% of the fuel rods in the core suffer sufficient damage that all of their gap activity is released to the RCS. Ten percent of the total core activity for both iodines and noble gases is assumed to be in the fuel-cladding gap.

A small fraction (i.e. 0.25%) of the fuel in the core is assumed to melt as a result of the rod ejection accident. One-half of the iodine activity in the melted fuel is released to the RCS, while all of the noble gas activity in the melted fuel is released to the RCS.

Conservatively, all the iodine and noble gas activity (from prior to the accident and resulting from the accident) is assumed to be in the RCS when determining offsite doses due to the primary to secondary steam generator tube leakage, and all of the iodine and noble gas activity is assumed to be in the containment when determining offsite doses due to containment leakage. However, 50% of the iodine activity released to the containment is assumed to instantaneously plate out on containment surfaces.

The primary to secondary steam generator tube leak used in the analysis is the Technical Specification limit of 1.0 gpm.

No credit for iodine removal is taken for any steam released to the condenser prior to reactor trip and concurrent loss of offsite power.

An iodine partition factor in the steam generator of 0.01 (Ci/gm steam)/(Ci/gm water) is used (Reference 5).

All noble gas activity carried over to the secondary side through steam generator tube leakage is assumed to be immediately released to the outside atmosphere.

The steam release from the steam genertors following the rod ejection accident is based on the maximum relief rate of 10.67xE+6 lb/hr through the main steam safety valves and a steam release duration of 95 seconds. This results in a steam release of 281,569 lb.

The Technical Specification design basis containment leak rate of 0.25% by weight of containment air is used for the initial 24 hours. Thereafter the containment leak rate is assumed to be one-half the design value, or 0.125% per day.

In addition to the immediate plate-out on containment surfaces of 50% of the iodine activity released to containment, the time-dependent deposition on containment surfaces of the remaining elemental iodine is considered. This is independent of containment spray operation. An elemental iodine deposition coefficient of 5.94 hr^{-1} is determined. Credit is taken for deposition until a decontamination factor of 100 in the containment inventory of elemental iodine is reached.

The rod ejection accident is similar to a 2 inch diameter LOCA. Analysis of the 2-inch LOCA at Turkey Point shows that the high containment pressure SI signal of 4.0 psig would be reached at approximately 100 seconds. The safety analysis limit for high containment pressure SI signal of 6.0 psig would be reached at approximately 150 seconds. The SI signal would start the emergency containment filtration (ECF) system filter fans. To account for time to allow the fans to reach operating speed and to add conservatism, credit for the ECF system filters is not taken in the initial 300 seconds following the rod ejection accident. After 2 hours following the accident, no further iodine removal is assumed by the ECF system filters.

The major assumption and parameters used in the analysis are itemized in Table 14.2.6-3. The thyroid dose conversion factors, breathing rates, and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4.

The dose limits for a rod ejection accident are "well within" the 10 CFR 100 guideline values, or 75 rem thyroid and 6 rem whole body.

The offsite thyroid and whole body doses due to the rod ejection accident are given in Table 14.2.6-4. The offsite doses due to the rod ejection accident do not exceed the acceptance criteria.

14.2.6.5 CONCLUSIONS

Despite the conservative assumptions, the analyses indicate that the described fuel and clad limits are not exceeded. It is concluded that there is no danger of sudden fuel dispersal into the coolant. Since the peak pressure does not exceed that which would cause stresses to exceed the faulted condition stress limits, it is concluded that there is no danger of further consequential damage to the RCS. The analyses demonstrate that the fission product release as a result of fuel rods entering DNB is limited to less than 10 percent of the fuel rods in the core.

14.2.6.6 REFERENCES

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3. Liimatainen, R. C and Testa, F. J., "Studies in TREAT of Zircaloy 2- Clad, UO_2-Core Simulated Fuel Elements," ANL-7225, P 177, November 1966.

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7. Bishop, A. A., Sandberg, R. O. and Tong, L. S., "Forced Convection Heat Transfer at High Pressure After the Critical Heat Flux", ASME 65-HT-31, August 1965.

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9. S.S. Witter to J.L. Perryman "Turkey Point Units 3 & 4-ZIRLO Safety Assessment Revision 1," 98FP-G-0054, June 9, 1998.

TABLE 14.2.6-1

RESULTS OF THE
ROD CLUSTER CONTROL ASSEMBLY (RCCA)
EJECTION ACCIDENT ANALYSIS

	Beginning of Cycle	Beginning of Cycle	End of Cycle	End of Cycle
Power level, percent	102	0	102	0
Ejected rod worth, percent ΔK	0.35	0.71	0.30	0.84
Delayed neutron fraction, percent	0.50	0.50	0.42	0.42 |
Feedback reactivity weighting	1.3	1.42	1.3	2.32
Trip reactivity percent ΔK,	4.0	2.0	4.0	2.0
Hot Channel Factor before rod ejection	2.694	---	2.694	---
Hot Channel Factor after rod ejection	5.48	8.0	5.52	14.3
Number of operational pumps	3	2	3	2
Max fuel pellet average temperature, °F	4286	2815	3457	2698 |
Max fuel centerline temperature, °F	5000	3267	4508	3098 |
Max clad average temperature, °F	2660	2033	2072	1967 |
Max fuel stored energy, cal/gm	190	116	147	110 |
Fuel melt in hot pellet, percent	7.65	0	0	0 |

TABLE 14.2.6-2

SEQUENCE OF EVENTS
RCCA EJECTION ACCIDENT

CASE	EVENT	TIME (SEC)
BOL, full power	Initiation of Rod Ejection	0.00
	Power Range High Neutron Flux Setpoint Reached	0.03
	Peak Nuclear Power Occurs	0.13
	Rods Begin to Fall	0.53
	Peak Clad Temperature Occurs	2.19
	Peak Heat Flux Occurs	2.20
	Peak Fuel Center Temperature Occurs	3.98
BOL, zero power	Initiation of Rod Ejection	0.00
	Power Range High Neutron Flux Setpoint Reached	0.25
	Peak Nuclear Power Occurs	0.30
	Rods Begin to Fall	0.75
	Peak Clad Temperature Occurs	2.31
	Peak Heat Flux Occurs	2.38
	Peak Fuel Center Temperture Occurs	3.40

TABLE 14.2.6-3

ASSUMPTIONS USED
FOR
ROD EJECTION ACCIDENT DOSE ANALYSIS

Power	2346 MWt
Reactor Coolant Noble Gas Activity Prior to Accident	1.0% Fuel Defect Level
Reactor Coolant Iodine Activity Prior to Accident	60 μCi/gm of DE I-131
Activity Released to Reactor Coolant And Containment From Failed Fuel (Noble Gas & Iodine)	10% of Core Gap Activity
Fraction of Core Activity in Gap (Noble Gas & Iodine)	0.10
Activity Released to Reactor Coolant and Containment from Melted Fuel:	
Iodine	0.125% of Core Activity
Noble Gas	0.25% of Core Activity
Secondary Coolant Activity Prior to Accident	0.10 μCi/gm of DE I-131
Total SG Tube Leak Rate During Accident	1.0 gpm
Iodine Partition Factor in SGs	0.01
Steam Release from SGs	281,569 lb (0-95 sec)

TABLE 14.2.6-3

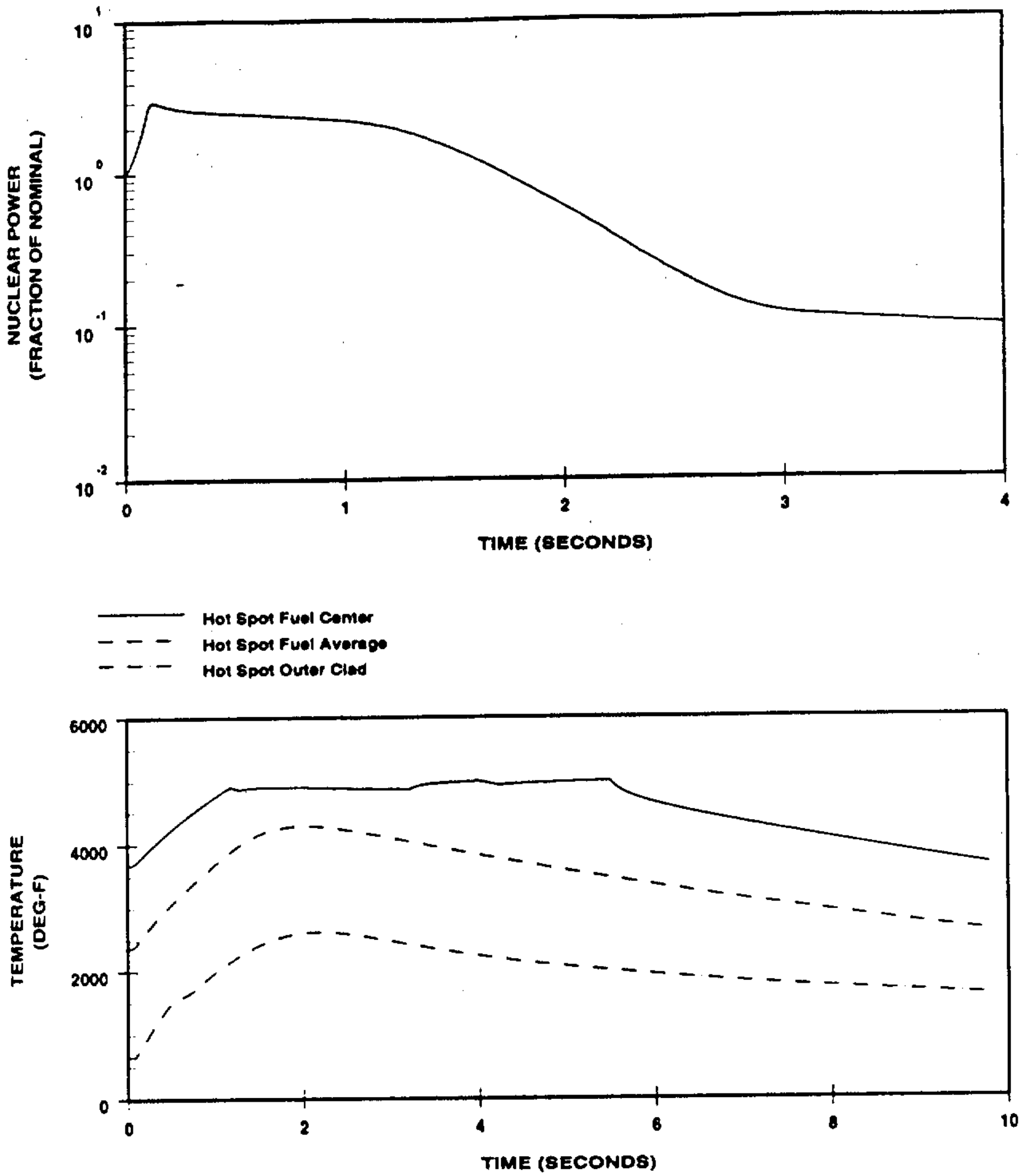
ASSUMPTIONS USED
FOR
ROD EJECTION ACCIDENT DOSE ANALYSIS

Iodine Removal in Containment:	
Instantaneous Iodine Plateout	50%
Elemental Iodine Deposition	5.94 hr^{-1} for DF $\leq$ 100
Emergency Containment Filters:	
Start Delay Time	300 sec
Number of Units	2
Flow Rate per Unit	33,750 cfm
Elemental	90%
Methyl	30%
Particulate	95%
Operating Time	2 hr
Containment Free Volume	1.55 x 10^6 ft^3
Containment Leak Rate:	
0-24 hr	0.25%/day
> 24 hr	0.125%

TABLE 14.2.6-4

ROD EJECTION ACCIDENT OFFSITE DOSES

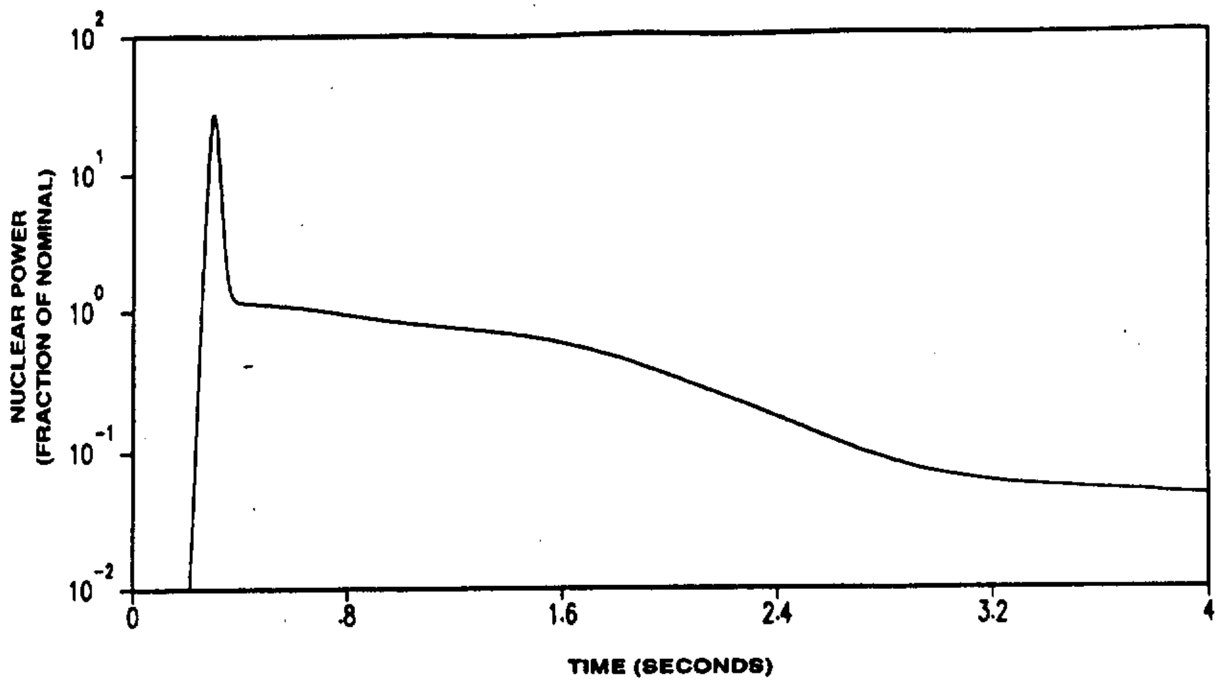
	Exclusion Boundary (EB) (0-2 Hours)	Low Population Zone (LPZ) (0-30 Days)
Thyroid Dose (rem)	5.9 E-1	6.9 E-2
Whole Body Dose (rem)	1.6 E-2	2.3 E-3

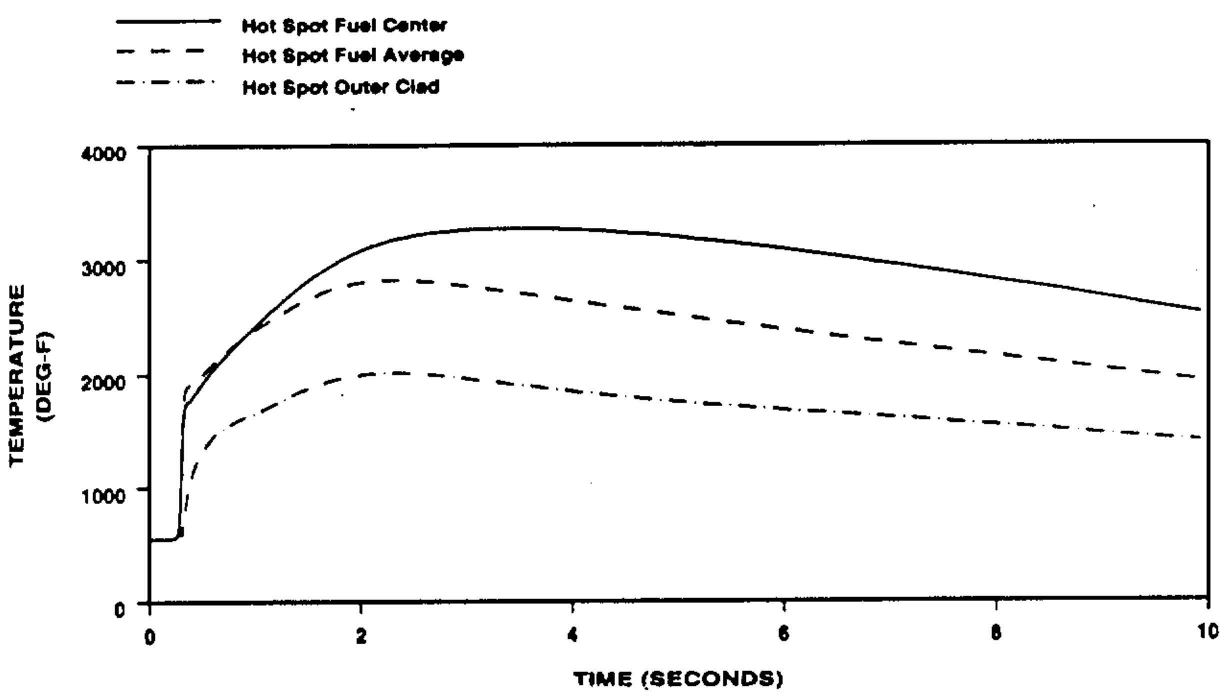


REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

RCCA EJECTION TRANSIENT
BEGINNING OF LIFE
FULL POWER
FIGURE 14.2.6-1





REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

RCCA EJECTION TRANSIENT
BEGINNING OF LIFE
ZERO POWER
FIGURE 14.2.6-2

14.3 REACTOR COOLANT SYSTEM PIPE RUPTURE

A comprehensive safety analysis of postulated pipe ruptures within the Reactor Coolant System (RCS) boundary has been performed. This analysis has included cases of the Loss of Coolant Accident (LOCA) resulting from a broad spectrum of small and large pipe ruptures including the Maximum Hypothetical Accident (MHA) case of the double ended break of the largest RCS pipe.

The objective of the analysis has been to determine the condition of the RCS, core, and containment in the event of a postulated LOCA, and to determine that the various Emergency Core Cooling Systems (ECCS) have the capability to control each LOCA, including the MHA.

14.3.1 GENERAL

A LOCA would result from a rupture of the Reactor Coolant System (RCS) or of any line connected to that system up to the first closed valve. The charging pumps have the capability to make up for leakage resulting from ruptures of a small cross section, thus permitting an orderly shutdown. The coolant released would remain in the containment.

For a postulated large break, reactor trip is initiated when the pressurizer low pressure set point is reached while the Safety Injection System (SIS) signal is actuated by pressurizer low pressure. The reactor trip and SIS actuation are also initiated by a high containment pressure signal. The consequences of the accident are limited in two ways:

a) Reactor trip and borated water injection supplement void formation in causing rapid reduction of the nuclear power to a residual level corresponding to the delayed fission and fission product decay.

b) Injection of borated water ensures sufficient flooding of the core to prevent excessive temperatures.

Before the reactor trip occurs, the reactor is in an equilibrium condition, i.e., the heat generated in the core is being removed via the secondary system. After reactor trip and turbine trip, core heat, heat from hot internals and the vessel is transferred to the RCS fluid, and then to the secondary system. The secondary system pressure increases and steam dump may occur. Make-up to the secondary side is automatically provided by the auxiliary feedwater pumps. The SIS signal stops normal feedwater flow by closing the main feedwater line isolation valves and initiates auxiliary feedwater flow by starting the auxiliary feedwater pumps. The secondary flow aids in the reduction of RCS pressure. When the RCS pressure falls below 600 psia, the accumulators begin to inject borated water. The reactor coolant pumps are assumed to be tripped at the initialization of the accident and effects of pump coastdown are included in the blowdown analyses.

Performance Criteria for Emergency Core Cooling System

The reactor is designed to withstand thermal effects caused by a loss of coolant accident including the double ended severance of the largest Reactor Coolant System pipe. The reactor core and internals together with the Emergency Core Cooling System are designed so that the reactor can be safety shutdown and the essential heat transfer geometry of the core preserved following the accident. The Emergency Core Cooling System, even when operating during the injection mode with the most severe single active failure is designed to meet the Acceptance Criteria.(1)

The ECCS is designed to limit the cladding temperature to 2200°F in accordance with 10CFR50.46. In addition, the core metal-water reaction is limited to less than 1% of the available Zircaloy, and the oxidation to less than 17% of the cladding thickness.

14.3.2 THERMAL ANALYSIS

The analysis specified by 10 CFR 50.46,"Acceptance Criteria for Emergency Core Cooling Systems for Light Water Power Reactors," (Reference 14.3.2.3-1) is presented in this section. The results of the loss of coolant accident analyses are summarized in Tables 14.3.2.1-3 and 14.3.2.2-2 and show compliance with the Acceptance Criteria.

The potential for adverse boric acid concentration occurring in the reactor vessel during the long term recirculation phase following LOCA has been analyzed. The analysis showed that there is no problem in maintaining long term core cooling.

The boundary considered for loss of coolant accidents as related to connecting piping is defined in Section 4.1.

The method of analysis to determine peak cladding temperature is divided into two types of analysis: (1) large break LOCA; and (2) small break LOCA. The method of analysis for large and small break LOCA is described below and results are given.

14.3.2.1 LARGE BREAK LOCA

14.3.2.1.1 LARGE BREAK LOCA ANALYSIS

Should a major break occur, depressurization of the RCS would result in a pressure decrease in the pressurizer. The reactor trip signal would subsequently occur when the pressurizer low pressure trip setpoint is reached. An SI signal is generated when the appropriate setpoint (high containment pressure or low pressurizer pressure) is reached. These countermeasures will limit the consequences of the accident in two ways:

A. Reactor trip and borated water injection supplement void formation in causing rapid reduction of power to the residual level corresponding to fission product decay heat. An average RCS/sump mixed boron concentration is calculated to ensure that the post-LOCA core remains subcritical. In addition, the insertion of control rods to shut down the reactor is neglected in the large break analysis.

B. Injection of borated water provides for heat transfer from the core and prevents excessive clad temperatures.

In the current best-estimate analysis, the most limiting large break LOCA single failure is the loss of one high-head pump and one low-head pump. This assumption is consistent with the previous procedure for large break analyses.

For the large break analysis, one ECCS train, including one high-head SI pump and one RHR (low-head) pump, starts and delivers flow through the injection lines (one for each loop) with one branch injection line spilling to the containment backpressure. Both emergency diesel generators (EDGs) are assumed to start in the modeling of the containment fan coolers and spray pumps. Modeling full containment heat removal systems operation is required by Branch Technical Position CSB 6-1 and is conservative for the large break LOCA.

To minimize delivery to the reactor, the branch line chosen to spill is selected as the one with the minimum resistance. In addition, the high-head SI pump performance curve was degraded.

In the large break ECCS analysis presented here, single failure is conservatively accounted for via the loss of an ECCS train, the spilling of the minimum resistance injection line, and by assuming all containment spray pumps and fan coolers are available. Therefore, the analysis assumed one high-head SI pump, one RHR pump, two containment spray pumps, and three fan coolers are operating.

Prior to the accident, the RCS is assumed to be operating normally at full power. A large cold leg break is assumed to open nearly instantaneously in one of the main coolant pipes. Calculations where the location and size of the break have been varied indicate that a break in the cold leg between the pump and the vessel leads to the most severe transient. For this break location, a rapid depressurization occurs, along with a core flow reversal as subcooled liquid flows out of the vessel into the broken cold leg. Boiling begins in the core, and the reactor core begins to shut down. Within approximately two seconds, the core is highly voided, and core fission is terminated. The cladding temperature rises rapidly as heat transfer from the fuel rods is reduced.

Within approximately six seconds, the pressure in the pressurizer has fallen to the point where reactor trip and safety injection signals are initiated. It is likely that these signals will have been initiated sooner as a result of a high containment pressure signal. Along with the safety injection signal, the containment isolation signal is also initiated.

In the first five seconds, the coolant in all regions of the vessel begins to flash. In addition, the break flow becomes saturated and is substantially reduced. This reduces the depressurization rate, and may also lead to a period of positive core flow or reduced downflow as the reactor coolant pumps in the intact loops continue to supply water to the vessel, and as flashing begins in the vessel lower plenum and downcomer. Cladding temperatures may be reduced, and some portions of the core may rewet during this period.

This positive core flow or reduced downflow period ends as two-phase conditions occur in the reactor coolant pumps, reducing their effectiveness. Once again, the core flow reverses as most of the vessel mass flows out through the broken cold leg. Core cooling occurs as a result of the reverse flow.

Approximately 12 seconds after the break, the pressure falls to the point where accumulators begin injecting cold water into the cold legs. Because the break flow is still high, much of the injected ECCS water, which flows into the downcomer of the vessel, is bypassed out to the break.

Approximately 30 seconds after the break, most of the original RCS inventory has been ejected. The system pressure and break flow are reduced and the ECCS water, which has been filling the downcomer, begins to fill the lower plenum of the vessel. Additional ECCS water pumped from the refueling water storage tank (RWST) begins to flow into the vessel. During this time, core heat transfer is relatively poor and cladding temperatures increase.

The blowdown phase of the transient ends when the RCS pressure (initially assumed at 2250 psia) falls to a value approaching that of the containment atmosphere. Prior to or at the end of the blowdown, termination of bypass occurs and refill of the reactor vessel lower plenum begins. Refill is completed when ECCS water has filled the lower plenum of the reactor vessel.

Approximately 40 seconds after the break, the lower plenum has refilled, and ECCS water enters the core. The flow into the core is oscillatory, as cold water rewets the hot fuel cladding, generating steam. This steam and entrained water must pass through the vessel upper plenum, the hot legs, the steam generator, and the reactor coolant pump before it can be vented out the break. The resistance of this flow path to the steam flow is balanced by the driving force of water filling the downcomer. Shortly after reflood begins, the accumulators exhaust their inventory of water, and begin to inject the nitrogen gas which was used to pressurize the accumulators. This results in a short period of improved heat transfer as the nitrogen forces water from the downcomer into the core. When the accumulators have exhausted their supply of nitrogen, the reflood rate may be reduced and peak cladding temperatures may again rise. This heatup may continue until the core has reflooded to several feet. Approximately three minutes after the break, all locations in the core begin to cool. The core is completely quenched within ten minutes, and long-term cooling and decay heat removal begin. Long term cooling for the next several minutes is characterized by continued boiling in the vessel as decay power and residual heat in the reactor structures are removed. The sequence of events described above is summarized in Table 14.3.2.1-4.

14.3.2.1.2 LARGE BREAK LOCA ANALYTICAL MODEL

In 1988, as a result of the improved understanding of LOCA thermal-hydraulic phenomena gained by extensive research programs, the NRC staff amended the requirements of 10CFR50.46 and Appendix K, "ECCS Evaluation Models," so that a realistic evaluation model may be used to analyze the performance of the ECCS during a hypothetical LOCA (Reference 14.3.2.3-2). Under the amended rules, best estimate thermal-hydraulic models may be used in place of models with Appendix K features. The rule change also requires, as part of the analysis, an assessment of the uncertainty of the best estimate calculations. It further requires that this analysis uncertainty be included when comparing the results of the calculations to the prescribed acceptance limits. Further guidance for the use of best estimate codes was provided in Regulatory Guide 1.157 (Reference 14.3.2.3-3).

To demonstrate use of the revised ECCS rule, the NRC and its consultants developed a method called the Code Scaling, Applicability, and Uncertainty (CSAU) evaluation methodology (Reference 14.3.2.3-4). This method outlined an approach for defining and qualifying a best estimate thermal-hydraulic code and quantifying the uncertainties in a LOCA analysis.

A LOCA evaluation methodology for three- and four-loop PWR plants based on the revised 10 CFR 50.46 rules was developed by Westinghouse with the support of EPRI and Consolidated Edison and was recently approved by the NRC (Reference 14.3.2.3-5). The methodology is documented in WCAP-12945, "Code Qualification Document for Best Estimate LOCA Analysis" (Reference 14.3.2.3-6), the Revised Methodology Report (Reference 14.3.2.3-7), and other references as cited in the NRC's Safety Evaluation Report.

The thermal-hydraulic computer code which was reviewed and approved for the calculation of fluid and thermal conditions in the PWR during a large break LOCA is WCOBRA/TRAC Version MOD7A, Rev. 1 (Reference 14.3.2.3-6).

WCOBRA/TRAC combines two-fluid, three-field, multi-dimensional fluid equations used in the vessel with one-dimensional drift-flux equations used in the loops to allow a complete and detailed simulation of a PWR. This best estimate computer code contains the following features:

- Ability to model transient three-dimensional flows in different geometries inside the vessel

- Ability to model thermal and mechanical non-equilibrium between phases

- Ability to mechanistically represent interfacial heat, mass, and momentum transfer in different flow regimes

- Ability to represent important reactor components such as fuel rods, steam generators, reactor coolant pumps, etc.

The two-fluid formulation uses a separate set of conservation equations and constitutive relations for each phase. The effects of one phase on another are accounted for by interfacial friction and heat and mass transfer interaction terms in the equations. The conservation equations have the same form for each phase; only the constitutive relations and physical properties differ. Dividing the liquid phase into two fields is a convenient and physically accurate way of handling flows where the liquid can appear in both film and droplet form. The droplet field permits more accurate modeling of thermal-hydraulic phenomena such as entrainment, de-entrainment, fallback, liquid pooling, and flooding.

WCOBRA/TRAC also features a two-phase, one-dimensional hydrodynamics formulation. In this model, the effect of phase slip is modeled indirectly via a constitutive relationship which provides the phase relative velocity as a function of fluid conditions. Separate mass and energy conservation equations exist for the two-phase mixture and for the vapor.

The reactor vessel is modeled with the three-dimensional, three field model, while the loop, major loop components, and safety injection points are modeled with the one-dimensional model.

All geometries modeled using the three-dimensional model are represented as a matrix of cells. The number of mesh cells used depends on the degree of detail required to resolve the flow field, the phenomena being modeled, and practical restrictions such as computing costs and core storage limitations.

The equations for the flow field in the three-dimensional model are solved using a staggered difference scheme on the Eulerian mesh. The velocities are obtained at mesh cell faces, and the state variables (e.g., pressure, density, enthalpy, and phasic volume fractions) are obtained at the cell center. This cell is the control volume for the scalar continuity and energy equations. The momentum equations are solved on a staggered mesh with the momentum cell centered on the scalar cell face.

The basic building block for the mesh is the channel, a vertical stack of single mesh cells. Several channels can be connected together by gaps to model a region of the reactor vessel. Regions that occupy the same level form a section of the vessel. Vessel sections are connected axially to complete the vessel mesh by specifying channel connections between sections. Heat transfer surfaces and solid structures that interact significantly with the fluid can be modeled with rods and unheated conductors.

One-dimensional components are connected to the vessel. The basic scheme used also employs the staggered mesh cell. Special purpose components exist to model specific components such as the steam generator and pump.

A typical calculation using WCOBRA/TRAC begins with the establishment of a steady-state, initial condition with all loops intact. The input parameters and initial conditions for this steady-state calculation are discussed in the next section.

Following the establishment of an acceptable steady-state condition, the transient calculation is initiated by introducing a break into one of the loops. The evolution of the transient through blowdown, refill, and reflood follows continuously, using the same computer code (WCOBRA/TRAC) and the same modeling assumptions. Containment pressure is modeled with the BREAK component using a time dependent pressure table. Containment pressure is calculated using the COCO code (Reference 14.3.2.3-8) and mass and energy releases from the WCOBRA/TRAC calculation.

The methods used in the application of WCOBRA/TRAC to the large break LOCA are described in References 14.3.2.3-5, -6, and -7. A detailed assessment of the computer code WCOBRA/TRAC was made through comparisons to experimental data. These assessments were used to develop quantitative estimates of the code's ability to predict key physical phenomena in a PWR large break LOCA. Modeling of a PWR introduces additional uncertainties which are identified and quantified in the plant specific analysis (Reference 14.3.2.3-9). The final step of the best estimate methodology is to combine all the uncertainties related to the code and plant parameters and estimate the PCT at 95 percent probability. The steps taken to derive the PCT uncertainty estimate are summarized below:

1. Plant Model Development

   In this step, a WCOBRA/TRAC model of the plant is developed. A high level of noding detail is used, in order to provide an accurate simulation of the transient. However, specific guidelines are followed to assure that the model is consistent with models used in the code validation. This results in a high level of consistency among plant models, except for specific areas dictated by hardware differences such as in the upper plenum of the reactor vessel or the ECCS injection configuration.

2. Determination of Plant Operating Conditions

   In this step, the expected or desired operating range of the plant to which the analysis applies is established. The parameters considered are based on a "key LOCA parameters" list which was developed as part of the methodology. A set of these parameters, at mostly nominal values, is chosen for input as initial conditions to the plant model. A transient is run utilizing these parameters and is known as the "initial transient." Next, several confirmatory runs are made, which vary a subset of the key LOCA parameters over their expected operating range in one-at-a-time sensitivities. The most limiting input conditions, based on these confirmatory runs, are then combined into a single transient, which is then called the "reference transient."

3. PWR Sensitivity Calculations

   A series of PWR transients are performed in which the initial fluid conditions and boundary conditions are ranged around the nominal conditions used in the reference transient. The results of these calculations for Turkey Point Units form the basis for the determination of the initial condition bias and uncertainty discussed in Section 6 of Reference 14.3.2.1-9.

Next, a series of transients are performed which vary the power distribution, taking into account all possible power distributions during normal plant operation. The results of these calculations for Turkey Point Units form the basis for the determination of the power distribution bias and uncertainty discussed in Section 7 of Reference 14.3.2.1-9.

Finally, a series of transients are performed which vary parameters that affect the overall system response ("global" parameters) and local fuel rod response ("local" parameters). The results of these calculations for Turkey Point Units form the basis for the determination of the model bias and uncertainty discussed in Section 8 of Reference 14.3.2.1-9.

4. Response Surface Calculations

Regression analyses are performed to derive PCT response surfaces from the results of the power distribution run matrix and the global model run matrix. The results of the initial conditions run matrix are used to generate a PCT uncertainty distribution.

5. Uncertainty Evaluation

The total PCT uncertainty from the initial conditions, power distribution, and model calculations is derived using the approved methodology (Reference 14.3.2.3-5). The uncertainty calculations assume certain plant operating ranges which may be varied depending on the results obtained. These uncertainties are then combined to determine the initial estimate of the total PCT uncertainty distribution for the double-ended cold leg guillotine and split breaks. The results of these initial estimates of the total PCT uncertainty are compared to determine the limiting break type. If the split break is limiting, an additional set of split transients are performed which vary overall system response ("global" parameters) and local fuel rod response ("local" parameters). The results of these calculations form the basis for the determination of the model bias and uncertainty for split breaks discussed in Section 8 of Reference 14.3.2.1-9. Finally, an additional series of runs is made to quantify the bias and uncertainty due to assuming that the above three uncertainty categories are independent. The final PCT uncertainty distribution is then calculated for the limiting break type, and the 95th percentile PCT is determined.

6. Plant Operating Range

   The plant operating range over which the uncertainty evaluation applies is defined. Depending on the results obtained in the above uncertainty evaluation, this range may be the desired range established in step 2, or may be narrower for some parameters to gain additional margin.

There are three major uncertainty categories or elements:

1. Initial condition bias and uncertainty
2. Power distribution bias and uncertainty
3. Model bias and uncertainty

Conceptually, these elements may be assumed to affect the reference transient PCT as shown below

$$PCT_i = PCT_{REF,i} + \Delta PCT_{IC,i} + \Delta PCT_{PD,i} + \Delta PCT_{MOD,i} \; 1 \qquad (14.3.2\text{-}1)$$

where,

$PCT_{REF,i}$ 2 = **Reference transient PCT:** The reference transient PCT is calculated using WCOBRA/TRAC at the nominal conditions identified in Table 14.3.2.1-1, for blowdown (i=1), first reflood (i=2), and second reflood (i=3).

$\Delta PCT_{IC,i}$ 3 = **Initial condition bias and uncertainty:** This bias is the difference between the reference transient PCT, which assumes several nominal or average initial conditions, and the average PCT taking into account all possible values of the initial conditions. This bias takes into account plant variations which have a relatively small effect on PCT. The elements which make up this bias and its uncertainty are plant-specific.

$\Delta PCT_{PD,i}$ 4 = **Power distribution bias and uncertainty:** This bias is the difference between the reference transient PCT, which assumes a nominal power distribution, and the average PCT taking into account all possible power distributions during normal plant operation. Elements which contribute to the uncertainty of this bias are calculational uncertainties, and variations due to transient operation of the reactor.

$\Delta PCT_{MOD,i}$ 5 = **Model bias and uncertainty:** This component accounts for uncertainties in the ability of the WCOBRA/TRAC code to accurately predict important phenomena which affect the overall system response ("global" parameters) and the local fuel rod response ("local" parameters). The code and model bias is the difference between the reference transient PCT, which assumes nominal values for the global and local parameters, and the average PCT taking into account all possible values of global and local parameters.

The separability of the uncertainty components in the manner described above is an approximation, since the parameters in each element may be affected by parameters in other elements. The bias and uncertainty associated with this assumption is quantified as part of the overall uncertainty methodology and included in the final estimates of $PCT^{95\%}$.

14.3.2.1.3 RESULTS OF LARGE BREAK LOCA ANALYSIS

A series of WCOBRA/TRAC calculations was performed using the Turkey Point Units 3 & 4 plant input model, to determine the effect on peak cladding temperature (PCT) of variations in several key LOCA parameters. From these studies, an assessment was made of the parameters which had a significant effect as will be described in the following sections.

The expected PCT and its uncertainty developed above is valid for a range of plant operating conditions. In contrast to current Appendix K calculations, many parameters in the base case calculation are at nominal values. The range of variation of the operating parameters has been accounted for in the estimated PCT uncertainty. Table 14.3.2.1-6 summarizes the operating ranges for the Turkey Point Units. If operation is maintained within these ranges, the LOCA analyses developed in this report are considered to be valid.

Nominal Split Break Transient Description

The plant-specific analysis performed for the Turkey Point Units indicated that the split break is more limiting than the double-ended guillotine break. The plant conditions used in the nominal split break transient are listed in Table 14.3.2.1-1. Since many of these parameters are at their bounded values, the calculated results are a conservative representation of the response to a large break LOCA.

The LOCA transient can be divided into time periods in which specific phenomena are occurring. A convenient way to divide the transient is in terms of the various heatup and cooldown transients that the hot assembly undergoes. For each of these phases, specific phenomena and heat transfer regimes are important, as discussed below. Results are shown in Figures 14.3.2.1-1 to 14.3.2.1-14.

Critical Heat Flux (CHF) Phase

Immediately following the cold leg rupture, the break flowrate is subcooled and high. The regions of the RCS with the hottest initial temperatures (core, upper plenum, upper head, and hot legs) begin to flash to steam within the first 0.5 seconds following the break. Flow in the core reverses, and the fuel rods begin to go through departure from nucleate boiling (DNB). Voiding in the core also causes the fission power to drop rapidly. The discharge flowrate decreases sharply as the break flow becomes two-phase (Figure 14.3.2.1-3). This phase is terminated when the water in the lower plenum and downcomer begin to flash.

Upward Core Flow Phase

Flashing in the lower plenum and pumped flow supplied by the intact loops re-establishes upward core flow for a brief period of time (Figure 14.3.2.1-4). This phase ends as the lower plenum mass is depleted, the loops become two-phase, and the intact loop pump head degrades because of two-phase conditions (Figure 14.3.2.1-5).

Downward Core Flow Phase

Downward flow into the core begins as the pump head continues to be degraded and upward flow in the downcomer is firmly established (Figure 14.3.2.1-6).

Due to the downflow during this phase, the cladding temperature was turned around at about 9 seconds after the initiation of the transient. The accumulators on the intact loops begin to inject at 12.5 seconds after the break (Figure 14.3.2.1-7). Initially, the injected water is bypassed around the downcomer and out of the break. As the system pressure continues to fall (Figure 14.3.2.1-8), the break flow and consequently the downward core flow are reduced. The vessel pressure reaches the containment pressure at the end of this phase, which occurs about 25 to 30 seconds after the initiation of the transient. The core begins to heat up as the system approaches containment pressure and the vessel begins to fill with ECCS water.

Refill Phase

When the steam flow up the downcomer is sufficiently reduced, the cold ECCS water begins to penetrate the downcomer (Figure 14.3.2.1-9) and refill the lower plenum. The refill period is characterized by a rapid increase in the lower plenum liquid level and the vessel fluid mass (Figures 14.3.2.1-10 and 14.3.2.1-11). In this period, the cladding temperature at all elevations increases rapidly due to the lack of liquid and steam flow in the core region and resulting poor cooling (Figure 14.3.2.1-1). This phase ends when the lower plenum fills with water and the ECCS water enters the core (Figure 14.3.2.1-12). This initiates the reflood phase, where entrainment begins, with a resulting improvement in heat transfer.

Reflood Phase

At the beginning of this phase, the accumulators empty (Figure 14.3.2.1-7) and nitrogen enters the system, which causes a surge of water into the core (Figure 14.3.2.1-12), and a temporary cooldown (Figure 14.3.2.1-1). The early part of this period is characterized by a significant vapor generation as the lower elevations of the core quench. This temporarily increases the core pressure, reversing the core inlet flow. As the steam generated in the core is vented through the loops and the downcomer level rises further, the downcomer pressure increases above the core pressure, and positive core flow is re-established. The resulting core/downcomer level oscillations can be seen in the core and downcomer liquid level plots (Figures 14.3.2.1-12 and 14.3.2.1-9). At approximately 100 to 120 seconds, ECCS water accumulated in the lower plenum and downcomer starts to boil because of heat transfer from the vessel internals, causing a reduction in the core and downcomer liquid levels and the vessel mass, as the two-phase level swell pushes water out the break (Figure 14.3.2.1-3). The cold water from the pumped safety injection (Figure 14.3.2.1-13) eventually collapses the voids sufficiently for the downcomer to resume refilling (Figure 14.3.2.1-9). However, because the RCS pressure follows the containment pressure, which is decreasing (Figure 14.3.2.1-14), the downcomer level swelling and loss of inventory out of the break occurs several times before the reflood heatup is terminated. The reflooding of the core proceeds, with the limiting PCT elevation moving upward with time (Figures 14.3.2.1-1 and 14.3.2.1-2). The final reflood PCT for the nominal split break is 1848°F, reached at the 10.2-foot elevation on the hot rod, about 200 seconds after the break.

Sensitivity Studies

A number of sensitivity calculations were carried out to investigate the effect of the key LOCA parameters, and to develop the required data for the uncertainty evaluation. In the sensitivity studies performed, LOCA parameters were varied one at a time. For each sensitivity study, a comparison between the base case and the sensitivity case transient results was made.

The results of the sensitivity studies for cold leg split break area are summarized in Table 14.3.2.1-5. A full report on the results for all sensitivity study results is included in Reference 14.3.2.1-9. The results of these analyses lead to the following conclusions:

1. No loss of offsite power, with the assumption that the reactor coolant pumps continue to run during the LOCA, results in the highest PCT.

2. A cold leg split break with a break area of 1.2 times the cold leg area results in the highest PCT. This split break then becomes the reference transient for the determination of uncertainties.

3. Generic results presented in References 14.3.2.3-6 and 14.3.2.1-7 indicated that cooling of the hot assembly was mainly provided by axial flow. LOCA parameters that can increase the upward or the downward axial flows through the hot assembly can therefore be expected to have a significant impact on the PCT. These effects are accounted for in the global model studies for the Turkey Point Units.

<u>Initial Conditions Sensitivity Studies</u>

Several calculations were performed to evaluate the effect of change in the initial conditions on the calculated LOCA transient. These calculations analyzed key initial plant conditions over their expected range of operation. These studies included effects of ranging Tavg, RCS pressure, and ECCS temperatures, and accumulator pressure and volumes. The results of these studies are presented in Section 6 of Reference 14.3.2.1-9.

The calculated results were used to develop initial condition uncertainty distributions for the blowdown, first and second reflood peaks. These distributions are then used in the uncertainty evaluation, to predict the PCT uncertainty component resulting from initial conditions uncertainty, $\Delta PCT_{IC,i}$.

Power Distribution Sensitivity Studies

Several calculations were performed to evaluate the effect of power distribution on the calculated LOCA transient. The power distribution attributes which were analyzed are the peak linear heat rate relative to the core average, the maximum relative rod power, the relative power in the bottom third of the core, and the relative power in the middle third of the core. The choice of these variables and their ranges are described and justified in References 14.3.2.3-7 and 14.3.2.3-10.

A run matrix was developed in order to vary the power distribution attributes singly and in combination. The calculated results are presented in Section 7 of Reference 14.3.2.1-9. The results indicated that power distributions with peak powers shifted towards the top of the core produced higher PCTs.

The calculated results were used to develop response surfaces, as described in Step 4 of Section 14.3.2.1.2 which could be used to predict the change in PCT for various changes in the power distributions, for the blowdown, first and second reflood peaks. These were then used in the uncertainty evaluation, to predict the PCT uncertainty component resulting from uncertainties in power distribution parameters, $\Delta PCT_{PD,i}$.

Global Model Sensitivity Studies

Several calculations were performed to evaluate the effect of broken loop resistance, break discharge coefficient, and condensation rate on the PCT for the guillotine break. As in the power distribution study, these parameters were varied singly and in combination in order to obtain a data base which could be used for response surface generation. The run matrix and ranges of the break flow parameters are described in Reference 14.3.2.1-7. The limiting split break was also identified using the approved methodology (Reference 14.3.2.3-5). The calculated results are presented in Section 8 of Reference 14.3.2.1-9.

The results of these studies indicated that a split break with an area equal to 1.2 times the cold leg area results in the highest PCT. This requires that the effect of broken loop resistance and condensation must be re-evaluated for the limiting split break area. The calculated results from these additional split breaks are presented in Reference 14.3.2.1-9.

The calculated results were used to develop response surfaces as described in Section 14.3.2.1.2, which could be used to predict the change in PCT for various changes in the flow conditions. These were then used in the uncertainty evaluation to predict the PCT uncertainty component resulting from uncertainties in global model parameters, $\Delta PCT_{MOD,i}$.

Uncertainty Evaluation and Results

The PCT equation was presented in Section 14.3.2.1.2. Each element of uncertainty is initially considered to be independent of the other. Each bias component is considered a random variable, whose uncertainty and distribution is obtained directly, or is obtained from the uncertainty of the parameters of which the bias is a function. For example, $\Delta PCT_{PD,i}$ is a function of FQ, $F_{\Delta H}$, P_{BOT} and P_{MID}. Its distribution is obtained by sampling the plant FQ, $F_{\Delta H}$, P_{BOT} and P_{MID} distributions and using a response surface to calculate $\Delta PCT_{PD,i}$. Since ΔPCT_i is the sum of these biases, it also becomes a random variable. Separate initial PCT frequency distributions are constructed as follows for the guillotine break and the limiting split break size:

1) Generate a random value of each ΔPCT element.

2) Calculate the resulting PCT using Equation 14.3.2-1.

3) Repeat the process many times to generate a histogram of PCT's.

For the Turkey Point Units, the results of this assessment showed the split break to be potentially limiting. Additional split break calculations were then performed, a more detailed description of $\Delta PCT_{MOD,i}$ was developed, and steps 1 through 3 repeated for the limiting split break. This analysis confirmed the split break to be the limiting break type.

A final verification step is performed in which additional calculations (known as "superposition" calculations) are made with WCOBRA/TRAC for the limiting split break size, simultaneously varying several parameters which were previously assumed independent (for example, power distributions and models).

Predictions using Equation 14.3.2-1 are compared to this data, and additional biases and uncertainties are applied.

The estimate of the PCT at 95 percent probability is determined by finding that PCT below which 95 percent of the calculated PCT's reside. This estimate is the licensing basis PCT, under the revised ECCS rule.

The results for the Turkey Point Units are given in Table 14.3.2.1-3. As expected, the difference between the 95 percent value and the average value increases with increasing time, as more parameter uncertainties come into play.

Containment Purging Evaluations

The Turkey Point Units will have 48- and 54-inch diameter containment purge valves open for the initial seconds of the large break LOCA transient. The open valves will reduce the containment pressure response during the large break LOCA, which is an adverse effect upon the calculated PCT. The calculated PCT effect is an increase of 27°F.

ZIRLO Evaluations

Fuel assembly changes including ZIRLO cladding and a change in the IFBA backfill pressure from 200 psig to 100 psig were implemented beginning with Turkey Point Unit 3 Cycle 17.

Safety analyses with ZIRLO cladding properties and 100 psig backfill pressure IFBA were performed independently for a plant specific transient, which has a reflood PCT in excess of the estimated 95th percentile PCT. This is the same WCOBRA/TRAC transient selected for the maximum local oxidation and maximum hydrogen generation calculations in Sections 10-2 and 10-3 of Reference 14.3.2.3-9.

These sensitivities showed an increase in Best-Estimate Large Break LOCA PCT of 8°F at Reflood 1 PCT elevation and a 22°F at Reflood 2 PCT elevation for ZIRLO cladding, and non-IFBA fuel remains limiting (Reference 14.3.2.3-21). The net effect is an overall Best Estimate Large Break LOCA PCT of 2035°F at the reflood 1 PCT elevation and 2089°f at the reflood 2 PCT elevation. The maximum local metal-water reaction is less than 17 percent. The total core metal-water reaction is less than 1 percent. The temperature transient is terminated at a time when core geometry is still amenable to cooling. As a result, the core temperature will continue to drop and the ability to remove decay heat generated in the fuel for an extended period of time will be provided. Therefore, the 10 CFR 50.46 Acceptance Criteria continue to be satisfied for Turkey Point Units 3 and 4 with ZIRLO clad fuel.

T_{avg} Coastdown

As the core reactivity decreases at the end of a fuel cycle, it is possible to extend power operation by reducing the RCS water temperature. This type of operation is commonly referred to as a Tavg coastdown. A typical scenario would have a plant maintaining 100% power for several days by reducing Tavg on the order of 1°F per day, then decreasing power on the order of 1% per day as Tavg continues to be reduced. The total length of the coastdown is typically less than two weeks.

The LBLOCA results presented in this report assume that the LOCA occurs early in the fuel cycle. These results bound operation of Turkey Point Units 3 and 4 during a Tavg coastdown, for the following reasons:

1) At high power locations in the core, the cladding creeps down and makes contact with the fuel pellets during the first cycle of operation. This reduces the pellet average temperatures by 300-400°F at the highest power locations. This reduction in initial stored energy is a significant

LBLOCA benefit.

2) During a Tavg coastdown, the plant will not be undergoing load follow maneuvers or other operational transients which can increase peaking factors substantially. Therefore, the maximum expected FQ during a Tavg coastdown will be below the FQ ranges assumed in the BELOCA uncertainty analysis.

3) The expected FdH at the end of a fuel cycle is typically lower than what has been assumed in the analysis (nominal value equal to the Tech Spec limit minus uncertainties).

14.3.2.1.4 LARGE BREAK LOCA CONCLUSIONS

It must be demonstrated that there is a high level of probability that the limits set forth in 10 CFR 50.46 are met. The demonstration that these limits are met for the Turkey Point Units is as follows:

1) There is a high level of probability that the peak cladding temperature (PCT) shall not exceed 2200°F. The results presented in Table 14.3.2.1-3 indicate that this regulatory limit has been met.

2) The maximum calculated total oxidation of the cladding shall nowhere exceed 17% of the total cladding thickness before oxidation. The approved Best Estimate LOCA methodology assesses this requirement using a plant-specific transient which has a PCT in excess of the estimated 95 percentile PCT. Based on this conservative calculation, a maximum total oxidation of <17% is calculated, which meets the regulatory limit.

3) The calculated total amount of hydrogen generated from the chemical reaction of the cladding with water or steam shall not exceed 0.01 times the hypothetical amount that would be generated if all of the metal in the cladding cylinders surrounding the fuel were to react. This requirement was assessed using the approved analysis option described in Section 10-3 of Reference 14.3.2.1-9. The total amount of hydrogen generated, based on this conservative assessment is 0.009 times the maximum theoretical amount, which meets the regulatory limit.

4) Calculated changes in core geometry shall be such that the core remains amenable to cooling. This requirement is met by demonstrating that the PCT does not exceed 2200°F, and the seismic and LOCA forces are not sufficient to distort the fuel assemblies to the extent that the core cannot be cooled. The FSAR for the Turkey Point Units includes structural analyses in Section 14.3.3 which indicate that grid deformation does not occur. Therefore, this regulatory limit is met.

5) After any calculated successful initial operation of the ECCS, the calculated core temperature shall be maintained at an acceptably low value and decay heat shall be removed for the extended period of time required by the long lived radioactivity remaining in the core. While WCOBRA/TRAC is typically not run past full core quench, all base calculations are run well past PCT turnaround and past the point where increasing vessel inventories are calculated. The conditions at the end of the WCOBRA/TRAC calculations indicates that the transition to long term cooling is underway even before the entire core is quenched.

14.3.2.2 SMALL BREAK LOCA (SMALL RUPTURED PIPES OR CRACKS IN LARGE PIPES) WHICH ACTUATE THE EMERGENCY CORE COOLING SYSTEM

This section presents the results of the 1995 small break loss-of-coolant accident (LOCA) analysis of record performed to support the Turkey Point Units 3 and 4 uprating in conformance with 10 CFR 50.46 (Reference 14.3.2.3-11) and Appendix K to 10 CFR 50.

14.3.2.2.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

A LOCA is defined as a rupture of the reactor coolant system (RCS) piping or of any line connected to that system. A small break, as considered in this section, is defined as a rupture of the RCS piping with a cross sectional area of less than 1.0 ft^2, in which the normally operating charging system flow is not sufficient to sustain pressurizer level and pressure.

The most limiting single active failure assumed for a small break LOCA is that of an emergency power train failure which results in the loss of one complete train of Emergency Core Cooling System (ECCS) components. In addition, a loss-of-offsite power (LOOP) is assumed to occur coincident with reactor trip. This means that credit may be taken for at most two high head safety injection (SI) pumps and one low head (RHR) pump. However, in the analysis of the small break LOCA presented here, only the minimum delivered ECCS flow from a single high head SI pump was assumed.

The small break LOCA analysis performed for the Turkey Point Units 3 and 4 uprating program utilizes the NRC-approved NOTRUMP Evaluation Model (References 14.3.2.3-12 and 14.3.2.3-13), with appropriate modifications to model pumped SI and accumulator injection in the broken loop as well as an improved condensation model (COSI) for the pumped SI into the broken and intact loops(Reference 14.3.2.3-14).

The most limiting broken loop injection scenario has been identified. Given that a break at the bottom of the RCS piping is the most limiting location, and the fact that Westinghouse ECCS designs have the SI penetrations above the RCS cold leg center line, a break of an SI branch line cannot be limiting for PCT calculations. Since the most limiting break location cannot be in the SI lines, it is not necessary to perform small break LOCA analyses with and without SI to the broken loop in order to isolate the most limiting condition. Therefore, the small break LOCA analysis performed for the Turkey Point uprating program assumes SI is delivered to both the intact and broken loops at the RCS backpressure. These countermeasures limit the consequences of the small break LOCA accident in two ways:

1. Control rod insertion and borated injection (SI) supplement void formation in causing a rapid reduction of nuclear power to a residual level corresponding to the delayed fission and fission product decay.

2. Injection of borated water ensures sufficient flooding of the core to prevent excessive clad temperatures.

Prior to break initiation, the plant is assumed to be in a full power (102%) equilibrium condition, i.e., the heat generated in the core is being removed via the secondary system. Other initial plant conditions assumed in the analysis are given in Table 14.3.2.2-1. Subsequent to the break opening, a period of reactor coolant system blowdown ensues in which the heat from fission product decay, the hot reactor internals, and the reactor vessel continues to be transferred to the RCS. The heat transfer between the RCS and the secondary system may be in either direction and is a function of the relative temperatures of the primary and secondary. In the case of continuous heat addition to the secondary during a period of quasi-equilibrium, an increase in the secondary system pressure results in steam relief via the steam generator safety valves.

During the earlier part of the small break transient (prior to the assumed loss-of-offsite power coincident with reactor trip), the loss of flow through the break is not sufficient enough to overcome the positive core flow maintained by the reactor coolant pumps. During this period, upward flow through the core is maintained. However, following the reactor coolant pump trip (due to a LOOP) and subsequent pump coastdown, a partial period of core uncovery occurs. Ultimately, the small break transient analysis is terminated when the ECCS flow provided to the RCS exceeds the break flow rate.

The core heat removal mechanisms associated with the small break transient include not only the break itself and the injected ECCS water, but also that heat transferred from the RCS to the steam generator secondary side. Main Feedwater (MFW) is assumed to be isolated coincident with the safety injection signal, and the MFW pumps coast down to 0% flow in 10 seconds. A continuous supply of makeup water is also provided to the secondary using the auxiliary feedwater (AFW) system. An AFW actuation signal occurs coincident with the safety injection signal, resulting in the assumed delivery of AFW system flow of 200 gpm to the affected unit 120 seconds following the signal. The heat transferred to the secondary side of the steam generator aids in the reduction of the RCS pressure.

Should the RCS depressurize to approximately 600 psig, as in the case of the limiting 3-inch break and the 4-inch break, the cold leg accumulators begin to inject borated water into the reactor coolant loops. In the case of the 2-inch break however, the vessel mixture level is recovered without the aid of accumulator injection.

14.3.2.2.2 ANALYSIS OF EFFECTS AND CONSEQUENCES

Method of Analysis

For small breaks (less than 1.0 ft^2) the NOTRUMP (References 14.3.2.3-12 and 14.3.2.3-13) digital computer code was employed to calculate the transient depressurization of the Reactor Coolant System as well as to describe the mass and enthalpy of the fluid flow through the break. The NOTRUMP computer code is a state-of-the-art one-dimensional general network code incorporating a number of advanced features. Among these are calculation of thermal non-equilibrium in all fluid volumes, flow regime-dependent drift flux calculations with counter-current flooding limitations, mixture level tracking logic in multiple-stacked fluid nodes and regime-dependent heat transfer correlations. The NOTRUMP small-break LOCA Emergency Core Cooling System (ECCS) evaluation model was developed to determine the RCS response to design basis small break LOCAs, and to address NRC concerns expressed in NUREG-0611 (Reference 14.3.2.3-16).

The reactor coolant system model is nodalized into volumes interconnected by flow paths. The broken loop is modelled explicitly, while the two intact loops are lumped into a second "unbroken" loop. The transient behavior of the system is determined from the governing conservation equations of mass, energy, and momentum. The multinode capability of the program enables explicit, detailed spatial representation of various system components which, among other capabilities, provides a proper calculation of the behavior of the loop seal during a loss-of-coolant accident. The reactor core is represented as heated control volumes with associated phase separation models to permit transient mixture height calculations. A more detailed description of the NOTRUMP code, its models, and the associated small break evaluation model is provided in References 14.3.2.3-12 and -13.

After the small break LOCA is initiated, reactor trip is calculated to occur due to a low pressurizer pressure signal at 1805 psia (including uncertainties). Soon after the reactor trip signal is generated, the safety injection signal is calculated to actuate due to a low pressurizer pressure of 1615 psia (including uncertainties). Safety injection systems consist of accumulator tanks pressurized with Nitrogen gas, and pumped injection systems. The small break LOCA analysis assumed an accumulator water volume of 892 ft^3

with a cover gas pressure of 600 psig. As stated earlier, a minimum emergency core cooling system capability (i.e., only one high head safety injection pump) was assumed for the analysis. The assumed pumped safety injection flow to the broken and intact loops of the RCS as a function of RCS pressure is shown in Figures 14.3.2.2-9 and -8, respectively. The safety injection flow rates presented are based upon a single high head safety injection degraded pump performance curve without any branch line imbalances. The effect of flow from the RHR pumps was not considered in any of the Turkey Point small break LOCA analyses, since the shutoff head is much lower than the RCS pressure during the critical portion of the small break transient. The onset of full safety injection flow was assumed to be delayed 35 seconds following the occurrence of the injection signal to account for emergency diesel generator startup and emergency power bus loading in the case of a loss-of-offsite-power coincident with a LOCA. A rod drop time of 3.0 seconds was assumed in addition to a 2.0 second signal processing delay time, resulting in a total delay time of 5.0 seconds from the time of the reactor trip signal (1805 psia) to full rod insertion.

Peak clad temperature calculations were performed with the LOCTA-IV (Reference 14.3.2.3-17) code using the NOTRUMP calculated core pressure, fuel rod power history, uncovered core steam and mixture heights as boundary conditions. Figure 14.3.2.2-2 depicts the hot rod axial power shape used to perform the small break LOCA analysis. This shape was chosen because it represents a distribution with power concentrated in the upper regions of the core. Such a distribution is limiting for small-break LOCAs, because it minimizes coolant level swell, while maximizing vapor superheating and fuel rod heat generation at the uncovered elevations. The small break LOCA analysis assumes full power operation of the core until the control rods are completely inserted. Figure 14.3.2.2-3 represents the code interface between LOCTA and NOTRUMP.

<u>Results - Limiting Break Case</u>

This section presents the results of the limiting small break LOCA analysis (as determined by the highest calculated peak clad temperature), and fulfills the requirements of NUREG-0737 (Reference 14.3.2.3-18), Section II.K.3.31, which requires a plant-specific small break LOCA analysis using an Evaluation Model revised per Section II.K.3.30. In accordance with NRC Generic Letter 83-35

(Reference 14.3.2.3-19), generic Westinghouse analyses using NOTRUMP (References 14.3.2.3-12 and 14.3.2.3-13) were performed and are presented in WCAP-11145 (Reference 14.3.2.3-20). The results of Reference 20 demonstrate that the cold leg break location is limiting with respect to postulated cold leg, hot leg and pump suction leg break locations. First, a break spectrum was performed at the high RCS vessel average temperature, as this condition is typically limiting. Based on these conditions, the limiting break for the Turkey Point Units was found to be a 3-inch diameter break in the cold leg with a peak cladding temperature attained during the transient of 1688°F (refer to Table 14.3.2.2-2). Inherent in the limiting small break analysis are several input assumptions, a summary of which is provided in Table 14.3.2.2-1, while Table 14.3.2.2-3 provides the key transient event times.

A summary of the transient response for the limiting three-inch break case is shown in Figures 14.3.2.2-4 through 14.3.2.2-12. These figures present the response of the following parameters:

1. RCS Pressure Transient.
2. Core Mixture Level.
3. Peak Clad Temperature.
4. Cold Leg Break Mass Flow Rate.
5. Safety Injection Mass Flow Rate.
6. Top Core Node Vapor Temperature.
7. Hot Spot Rod Surface Heat Transfer Coefficient.
8. Hot Spot Fluid Temperature.

During the initial period of the small-break transient, the effect of the break flow rate is not sufficient to overcome the flow rate maintained by the reactor coolant pumps as they coast down. As such, normal upward flow is maintained through the core and core heat is adequately removed. Following reactor trip, the removal of the heat generated as a result of fission products decay, is accomplished via a two-phase mixture level covering the core. From the clad temperature transients for the 3-inch break calculations given in Figures 14.3.2.2-5 and 14.3.2.2-6, it is seen that the peak clad temperature occurs near the time when the core is most deeply uncovered and the top of the core is being cooled by steam. This time is characterized by the highest vapor superheating above the mixture level (refer to Figure

14.3.2.2-7). A comparison of the flow provided by the safety injection system (Figures 14.3.2.2-8 and 14.3.2.2-9) to the total break mass flow rate at the end of the transient (Figures 14.3.2.2-10), shows that at the time the transient was terminated, the safety injection flow rate that was delivered to the RCS exceeds the mass flow rate out the break (70.1 lbm/s versus 61.2 lbm/s). In addition, the inner vessel core mixture level has recovered the top of the core (Figure 14.3.2.2-5).

Figures 14.3.2.2-11 and 14.3.2.2-12 provide additional information on the hot rod surface heat transfer coefficient at the hot spot and fluid temperature at the hot spot, respectively.

Additional Break Cases

Studies documented in Reference 14.3.2.3-20 have determined that the limiting small-break transient occurs for breaks of less than 10 inches in diameter. To ensure that the 3-inch diameter break was indeed the most limiting, calculations were also performed with break equivalent diameters of 2 inches and 4 inches. The results of each of these cases are given in Tables 14.3.2.2-2 and 14.3.2.2-3.

Plots of the following parameters for each case are also given in Figures 14.3.2.2-13 through 14.3.2.2-15 for the 2-inch break case and Figures 14.3.2.2-16 through 14.3.2.2-18 for the 4-inch break.

1. RCS Pressure Transient.
2. Core Mixture Level.
3. Peak Clad Temperature.

As seen in Table 14.3.2.2-2, the peak clad temperature for each of these cases was calculated to be less than that for the 3-inch break case.

Limiting Temperature Conditions

Reduced operating temperature typically results in a PCT benefit for the small break LOCA. However, due to competing effects and the complex nature of small break LOCA transients, there have been some instances where more limiting results have been observed for the reduced operating temperature case.

For this reason, a small break LOCA transient based on a lower bound RCS vessel average temperature was performed.

The temperature window analyzed was based on a nominal vessel average temperature of 574.2°F, with ± 3°F for an operating window and ± 8.5°F to bound uncertainties. The break spectrum was performed at the high vessel average temperature (585.7°F), as this case was expected to yield limiting results. Then, a sensitivity analysis for the low vessel average temperature (562.7°F) was performed based on the limiting 3-inch break case from the break spectrum analyses previously described.

Plots of the following parameters are given in Figures 14.3.2.2-19 through 14.3.2.2-21 for the 3-inch break case at low T_{AVG} conditions:

1. RCS Pressure Transient,
2. Core Mixture Level, and
3. Peak Cladding Temperature.

The PCT for the 3-inch break case based on low vessel average temperature was 1619°F (see Table 14.3.2.2-2). Therefore, the PCT for this case was calculated to be less than that for the 3-inch break case with high vessel average temperature conditions.

<u>Evaluations</u>

Upon completion of the small break LOCA analysis, an evaluation was performed for automatic containment spray actuation during small break LOCA. This evaluation accounts for the fact that Turkey Point Units 3 and 4 may be subject to SI interruption for up to 2 minutes while switching over to cold leg recirculation. The evaluation for containment spray actuation in small break LOCA resulted in no PCT penalty assessment. The basis for this conclusion was that the SI interruption occurs late in the transient when cladding temperatures are shown to be sufficiently low. Therefore, a conservative adiabatic heatup due to SI interruption would not exceed calculated PCT.

The DRFA fuel stack height above the lower core plate was explicitly modeled for the various cases analyzed.

The 5°F full-power T_{AVG} coastdown does not impact the small break LOCA analysis since lower vessel average temperature is non-limiting for the Turkey Point small break LOCA analysis.

An analysis of the limiting 3 inch Small Break LOCA with ZIRLO cladding was performed in Reference 14.3.2.3-21. The calculated PCT is 1683°F. Previous generic assessments have determined that IFBA analysis is not required for Small Break LOCA, regardless of initial backfill pressure. The maximum local metal-water reaction is less than 17 percent. The total core metal-water is less than 1.0 percent. The temperature transient is terminated at a time when core geometry is still amenable to cooling. As a result, the core temperature will continue to drop and the ability to remove decay heat generated in the fuel for an extended period of time will be provided. It was determined that ZIRLO cladding resulted in a limiting PCT 5°F less than Zircalloy-4 cladding, and is therefore bounded by the analysis performed with Zircalloy cladding. The 10 CFR 50.46 Acceptance Criteria continue to be satisfied for Turkey Point Units 3 and 4 with ZIRLO clad fuel.

The analyses and evaluation presented in this section show that the high head safety injection subsystems of the Emergency Core Cooling System, together with the heat removal capability of the steam generator, provide sufficient core heat removal capability to maintain the calculated peak-clad temperatures below the required limit of 10 CFR 50.46. Hence, adequate protection is afforded by the emergency core cooling system in the event of a small break loss-of-coolant accident.

14.3.2.2.3 CONCLUSIONS - SMALL BREAK LOCA ANALYSIS

For small breaks in the reactor coolant system pipe up to a cross sectional area of less than 1.0 ft^2, the Emergency Core Cooling System will meet the Acceptance Criteria presented to 10 CFR 50.46. That is:

1. The calculated peak fuel cladding temperature provides for a substantial margin to the requirement of 2200°F.

2. The amount of fuel cladding that reacts chemically with the water or steam does not exceed 1% of the hypothetical amount that would be generated if all the zirconium metal in the cladding cylinders surrounding the fuel, excluding the cladding surrounding the plenum volume, were to react.

3. The localized cladding oxidation limit of 17% is not exceeded during or after quenching.

4. The core remains amenable to cooling during and after the LOCA.

5. The core temperature is reduced and decay heat is removed for an extended period of time. This is required to remove the heat produced by the long-lived radioactivity remaining in the core.

14.3.2.3 REFERENCES

1. "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Cooled Nuclear Power Reactors," 10 CFR 50.46 and Appendix K of 10 CFR 50, Federal Register, Volume 39, Number 3, January 1974, as amended in Federal Register, Volume 53, September 1988.

2. Federal Register, "Emergency Core Cooling Systems: Revisions to Acceptance Criteria," V53, N180, pp. 35996-36005, September 16, 1988.

3. USNRC Regulatory Guide 1.157, "Best-Estimate Calculations of Emergency Core Cooling System Performances," May 1989.

4. Boyack, B., et al., "Qualifying Reactor Safety Margins: Application of Code Scaling Applicability and Uncertainty (CSAU) Evaluation Methodology to a Large Break Loss-of-Coolant-Accident," NUREG/CR-5249, 1989.

5. Letter, R. C. Jones (USNRC) to N. J. Liparulo (W), "Acceptance for Referencing of the Topical Report WCAP-12945 (P), Westinghouse Code Qualification Document for Best Estimate Loss-of-Coolant Analysis, " June 28, 1996.

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7. Letter, N. J. Liparulo (W) to R. C. Jones (USNRC), "Revisions to Westinghouse Best-Estimate Methodology," NTD-NRC-95-4575, October 13, 1995.

8. "Containment Pressure Analysis Code (COCO)," WCAP-8327 (Proprietary) and WCAP-8326 (Non-Proprietary), June 1974.

9. "Best Estimate Analysis of the Large Break Loss of Coolant Accident for Turkey Point Units 3 & 4 Nuclear Plant for Power Uprate," WCAP-14159, Rev. 0, October 1996.

10. Letter, N. J. Liparulo (W) to F. R. Orr (USNRC), "Re-Analysis Work Plans Using Final Best-Estimate Methodology," NSD-NRC-96-4746, June 13, 1996.

11. "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Nuclear Power Reactors," 10CFR50.46 and Appendix K of 10 CFR 50, Federal Register, Vol. 39, Number 3, January 4, 1974.

14.3.2.3 REFERENCES (Continued)

12. Meyer, P. E., "NOTRUMP - A Nodal Transient Small Break and General Network Code," WCAP-10079-P-A (proprietary) and WCAP-10080-NP-A (non-proprietary), August 1985.

13. Lee, N., et al., "Westinghouse Small Break ECCS Evaluation Model Using The NOTRUMP Code," WCAP-10054-P-A (proprietary) and WCAP-10081-NP-A (non-proprietary), August 1985.

14. Thompson, C. M., et al, "Addendum to the Westinghouse Small Break ECCS Evaluation Model Using the NOTRUMP Code: Safety Injection in the Broken Loop and COSI Condensation Model," WCAP-10054-P, Addendum 2, Revision 1 (proprietary) and WCAP-10081-NP, Addendum 2, Revision 1, (non-proprietary), October, 1995.

15. Shimeck, D. J., "COSI SI/Steam Condensation Experiment Analysis," WCAP-11767-P (proprietary), March 1988.

16. "Generic Evaluation of Feedwater Transients and Small Break Loss-of-Coolant Accidents in Westinghouse - Designed Operating Plants," NUREG-0611, January 1980.

17. Bordelon, F. M., et al., "LOCTA-IV Program: Loss-of-Coolant Transient Analysis," WCAP-8301 (proprietary) and WCAP-8305 (non-proprietary), June 1974.

18. "Clarification of TMI Action Plan Requirements," NUREG-0737, November 1980.

19. NRC Generic Letter 83-35 from D. G. Eisenhut, "Clarification of TMI Action Plan Item II.K.3.31," November 2, 1983.

20. Rupprecht, S. D. et al., "Westinghouse Small Break LOCA ECCS Evaluation Model Generic Study with the NOTRUMP Code;" WCAP-11145-P-A (proprietary) and WCAP-11372-NP-A (non-proprietary), October 1986.

21. S.S. Witter to J.L. Perryman “Turkey Point Units 3 & 4–ZIRLO Safety Assessment Revision 1,“ 98FP-G-0054, June 9, 1998.

TABLE 14.3.2.1-1

KEY INPUT PARAMETERS AND REFERENCE TRANSIENT ASSUMPTIONS USED IN THE LARGE BREAK LOCA ANALYSIS

Parameter	Reference Transient	Uncertainty or Bias
1.0 Plant Physical Description		
a. Dimensions	Nominal	ΔPCT_{MOD}
b. Flow resistance	Nominal	ΔPCT_{MOD}
c. Pressurizer location	Opposite broken loop	Bounded
d. Hot assembly location	Under limiting location	Bounded
e. Hot assembly type	15x15 OFA	Bounded
f. SG tube plugging level	High (20%)	Bounded*
2.0 Plant Initial Operating Conditions		
2.1 Reactor Power		
a. Core average linear heat rate (AFLUX)	Nominal - 100% of uprated power (2300 MWt)	ΔPCT_{PD}
b. Peak linear heat rate (PLHR)	Derived from desired Tech Spec (TS) limit and maximum baseload FQ	ΔPCT_{PD}
c. Hot rod average linear heat rate (HRFLUX)	Derived from TS $F_{\Delta H}$	ΔPCT_{PD}
d. Hot assembly average heat rate (HAFLUX)	HRFLUX/1.04	ΔPCT_{PD}
e. Hot assembly peak heat rate (HAPHR)	PLHR/1.04	ΔPCT_{PD}
f. Axial power distribution (PBOT, PMID)	Figure 3-2-8 of Reference 14.3.2.3-9	ΔPCT_{PD}

- Confirmed by plant-specific analysis

TABLE 14.3.2.1-1 (Cont'd)

KEY INPUT PARAMETERS AND REFERENCE TRANSIENT ASSUMPTIONS USED IN THE LARGE BREAK LOCA ANALYSIS

Parameter			Reference Transient	Uncertainty or Bias
	g.	Low power region relative power (PLOW)	0.2	Bounded*
	h.	Hot assembly burnup	BOL	Bounded
	i.	Prior operating history	Equilibrium decay heat	Bounded
	j.	Moderator Temperature Coefficient (MTC)	Tech Spec Maximum (0)	Bounded
	k.	HFP boron	800 ppm	Generic
2.2	Fluid Conditions			
	a.	T_{avg}	Min. nominal T_{avg} = 571.2°F	Nominal is bounded, unc'y is in ΔPCT_{IC}*
	b.	Pressurizer pressure	Nominal (2250.0 psia)	ΔPCT_{IC}
	c.	Loop flow	85000 gpm	ΔPCT_{MOD}
	d.	T_{UH}	Best-Estimate	0
	e.	Pressurizer level	Nominal (53.3% of span)	0
	f.	Accumulator temperature	Nominal (95°F)	ΔPCT_{IC}
	g.	Accumulator pressure	Nominal (652.2 psia)	ΔPCT_{IC}
	h.	Accumulator liquid volume	Nominal (6665 gallons)	ΔPCT_{IC}
	i.	Accumulator line resistance	Nominal	ΔPCT_{IC}
	j.	Accumulator boron	Minimum	Bounded

• Confirmed by plant-specific analysis

TABLE 14.3.2.1-1 (Cont'd)

KEY INPUT PARAMETERS AND REFERENCE TRANSIENT ASSUMPTIONS USED IN THE LARGE BREAK LOCA ANALYSIS

Parameter			Reference Transient	Uncertainty or Bias
3.0	Accident Boundary Conditions			
	a.	Break location	Cold leg	Bounded
	b.	Break type	Guillotine**	ΔPCT_{MOD}
	c.	Break size	Nominal (cold leg area)	ΔPCT_{MOD}
	d.	Offsite power	On (RCS pumps running)	Bounded*
	e.	Safety injection flow	Minimum	Bounded
	f.	Safety injection temperature	Nominal (69.5°F)	ΔPCT_{IC}
	g.	Safety injection delay	Max delay (23 sec)	Bounded
	h.	Containment pressure	Min based on W Cobra/Trac M&E	Bounded
	i.	Single failure	ECCS: Loss of 1 SI train	Bounded
	j.	Control rod drop time	No control rods	Bounded
4.0	Model Parameters			
	a.	Critical Flow	Nominal (as coded)	ΔPCT_{MOD}
	b.	Resistance uncertainties in broken loop	Nominal (as coded)	ΔPCT_{MOD}
	c.	Initial stored energy/fuel rod behavior	Nominal (as coded)	ΔPCT_{MOD}
	d.	Core heat transfer	Nominal (as coded)	ΔPCT_{MOD}

* Confirmed by plant-specific analysis
** Split break determined to be limiting

TABLE 14.3.2.1-1 (Cont'd)

KEY INPUT PARAMETERS AND REFERENCE TRANSIENT ASSUMPTIONS USED IN THE LARGE BREAK LOCA ANALYSIS

Parameter	Reference Transient	Uncertainty or Bias
e. Delivery and bypassing of ECC	Nominal (as coded)	Conservative
f. Steam binding/entrainment	Nominal (as coded)	Conservative
g. Non-condensible gases/accumulator nitrogen	Nominal (as coded)	Conservative
h. Condensation	Nominal (as coded)	ΔPCT_{MOD}

Notes:

1. ΔPCT_{MOD} indicates this uncertainty is part of code and global model uncertainty.
2. ΔPCT_{PD} indicates this uncertainty is part of power distribution uncertainty.
3. ΔPCT_{IC} indicates this uncertainty is part of initial condition uncertainty.

* Confirmed by plant-specific analysis

TABLE 14.3.2.1-2

LARGE BREAK LOCA - CONTAINMENT DATA USED FOR PCT CALCULATION

Net Free Volume	1,550,000 ft^3
Initial Conditions Containment Pressure Temperature RWST temperature Temperature outside containment Initial spray temperature	 12.7 psia 90.0 °F 35.0 °F 39.0 °F 39.0 °F
Spray System Maximum flow for one spray pump Number of spray pumps operating Post-accident spray system initiation delay	 1821.5 gal/min 2 36 seconds
Containment Fan Coolers Post-accident initiation fan coolers Number of fan coolers operating	 11 seconds 3*

* Conservative assumption only for analysis: Operability requirements are specified in Technical Specifications.

TABLE 14.3.2.1-3

BEST ESTIMATE LARGE BREAK LOCA RESULTS
(FUEL CLADDING RESULTS)

Component	Reflood 1	Reflood 2	Criteria
$PCT^{average}$	<1685 ◦F	<1607 ◦F	N/A
$PCT^{95\%}$	<2027 ◦F*	<2067 ◦F*	<2200 ◦F
Maximum Oxidation	<17%		<17%
Maximum Hydrogen Generation	<0.9%		<1%

* Includes a 27◦F PCT increase to account for containment purge valve closing time

Note: Reflood 1 $PCT^{95\%}$ is used for the 10 CFR 50.46 Report.

See Appendix 14A or 14B for latest PCT.

TABLE 14.3.2.1-4

LARGE BREAK LOCA ANALYSIS - TIME SEQUENCE OF EVENTS

BLOWDOWN	0 sec.	BREAK OCCURS
		REACTOR TRIP (PRESSURIZER PRESSURE OR HIGH CONT. PRESSURE)
		PUMPED SI SIGNAL (PRESSURIZER PRESSURE OR HIGH CONT. PRESSURE)
		ACCUMULATOR INJECTION BEGINS
		PUMPED ECCS INJECTION BEGINS (ASSUMING OFFSITE POWER AVAIL.)
		CONTAINMENT HEAT REMOVAL SYSTEM STARTS (OFFSITE POWER AVAIL.)
	20-30 sec.	END OF BYPASS
		END OF BLOWDOWN
REFILL		PUMPED ECCS INJECTION BEGINS (LOSS OF OFFSITE POWER)
		CONTAINMENT HEAT REMOVAL SYSTEM STARTS (LOSS OF OFFSITE POWER)
	30-40 sec.	BOTTOM OF CORE RECOVERY
REFLOOD		ACCUMULATORS EMPTY
	10 min.	CORE QUENCHED
LONG TERM COOLING		SWITCH TO COLD LEG RECIRCULATION ON RWST LOW LEVEL ALARM
		SWITCH TO HOT LEG/COLD LEG RECIRCULATION
	24 hr.	

TABLE 14.3.2.1-5

PARTIAL SUMMARY OF TURKEY POINT SENSITIVITY STUDIES

Type of Study	Parameter Varied	Value	PCT Results (°F)	
			RFLD1	RFLD2
Global Models	DECLG, C_D (Reference Transient)	1.0	1756	1684
	SPLIT, C_D	1.0	1767	1680
		1.2	1839	1848
		1.4	1698	1649

TABLE 14.3.2.1-6

PLANT OPERATING RANGE ALLOWED BY THE LOCA ANALYSIS

Parameter		Operating Range
1.0	Plant Physical Description	
	a) Dimensions	No in-board grid deformation due to LOCA + SSE
	b) Flow resistance	N/A
	c) Pressurizer location	N/A
	d) Hot assembly location	Anywhere in core
	e) Hot assembly type	15X15 OFA DRFA (Zircalloy/ZIRLO cladding)
	f) SG tube plugging level	$\leq$ 20%
2.0	Plant Initial Operating Conditions	
	2.1 Reactor Power	
	a) Core average linear heat rate	Core power $\leq$ 102% of 2300 MWt
	b) Peak linear heat rate	$F_Q \leq 2.5$
	c) Hot rod avg linear heat rate	$F_{\Delta H} \leq 1.733$
	d) Hot assembly avg heat rate	$\overline{P}_{HA} \leq 1.733/1.04$
	e) Hot assembly peak heat rate	$F_{Q,HA} \leq 2.5/1.04$
	f) Axial power distribution (PBOT, PMID)	RAOC (0.20 $\leq$ PBOT $\leq$ 0.43, 0.30 $\leq$ PMID $\leq$ 0.43)
	g) Low power region rel pwr (PLOW)	0.2 $\leq$PLOW $\leq$ 0.8

TABLE 14.3.2.1-6 (Cont'd)

PLANT OPERATING RANGE ALLOWED BY THE LOCA ANALYSIS

Parameter		Operating Range
	h) Hot assembly burnup	$\leq$ 75000 MWD/MTU, lead rod
	i) Prior operating history	All normal operating histories
	j) MTC	$\leq$ 0 at HFP
	k) HFP boron	Normal letdown
	2.2 Fluid Conditions	
	a) T_{avg}	$562.7 \leq T_{avg} \leq 585.7$°F
	b) Pressurizer pressure	$2180 \leq P_{RCS} \leq 2320$ psia
	c) Loop flow	$\geq$ 85,000 gpm/loop
	d) T_{UH}	Current upper internals
	e) Pressurizer level	Normal level, automatic control
	f) Accumulator temperature	$\leq$ 130°F
	g) Accumulator pressure	$590 \leq P_{acc} \leq 715$ psia
	h) Accumulator volume	$6007 \leq V_{acc} \leq 7338$ gallons
	i) Accumulator fL/D	Current line configuration
	j) Minimum ECC boron	$\geq$ 1950 ppm

TABLE 14.3.2.1-6 (CONT'D)

PLANT OPERATING RANGE ALLOWED BY THE LOCA ANALYSIS

Parameter		Operating Range
3.0	Accident Boundary Conditions	
	a) Break location	N/A
	b) Break type	N/A
	c) Break size	N/A
	d) Offsite power	On or Off
	e) Safety injection flow	≥ values used in reference case
	f) Safety injection temperature	≤ 105°F
	g) Safety injection delay	≤ 23 seconds (with offsite power) ≤ 35 seconds (without offsite power)
	h) Containment pressure	Current Technical Specifications
	i) Single failure	All trains operable
	j) Control rod drop time	N/A

TABLE 14.3.2.2-1

INPUT PARAMETERS USED IN THE SMALL BREAK LOCA ANALYSIS

Parameter	High TAVG	Low TAVG
Reactor Core Rated Thermal Power (MWt) (1)	2300	2300
Peak Linear Power (kw/ft) (1,2)	14.9	14.9
Total Peaking Factor (F_Q^T) at peak (2)	2.50	2.50
Power Shape (2)	See Figure 14.3.2.2-2	See Figure 14.3.2.2-2
$F_{\Delta H}$	1.70	1.70
Fuel (3)	15x15 DRFA	15x15 DRFA
Accumulator Water Volume, nominal (ft^3/acc.)	892	892
Accumulator Tank Volume, nominal (ft^3/acc.)	1200	1200
Accumulator Gas Pressure, minimum (psig)	600	600
Pumped Safety Injection Flow	See Figure 14.3.2.2-1	See Figure 14.3.2.2-1
Steam Generator Tube Plugging Level (%) (4)	20	20
Thermal Design Flow/loop, (gpm)	85,000	85,000
Vessel Avg. Temperature w/ uncertainty (°F)	585.7	562.7
Reactor Coolant Pressure w/ unc. (psia)	2320	2320
Min. Aux. Feedwater Flowrate/loop (lb/sec) (5)	9.26	9.26

NOTES:

1. Two percent is added to this power to account for calorimetric error. Reactor coolant pump heat is not modeled in the SBLOCA analyses.

2. This represents a power shape corresponding to a one-line segment peaking factor envelope, K(z), based on $F_Q^T = 2.50$.

3. Evaluation performed to address minor differences between:
   a) DRFA and OFA fuel types and,
   b) Zircalloy 4 and ZIRLO clad types.

4. Maximum plugging level in any one or all steam generators.

5. Flowrates per steam generator.

TABLE 14.3.2.2-2

SMALL BREAK LOCA ANALYSIS
FUEL CLADDING RESULTS

Break Spectrum, (High T_{AVG})

	BREAK SIZE		
	2-inch	3-inch	4-inch
Peak Clad Temperature (°F)	1656	1688(2)	1583
Peak Clad Temperature Location (ft)(1)	11.75	11.75	11.50
Peak Clad Temperature Time (sec)	2627	1188	668
Local Zr/H_2O Reaction, Max (%)	2.0188	1.5535	0.6679
Local Zr/H_2O Reaction Location (ft)(1)	11.75	11.50	11.25
Total Zr/H_2O Reaction (%)	< 1.0	< 1.0	< 1.0
Hot Rod Burst Time (sec)	No Burst	No Burst	No Burst
Hot Rod Burst Location (ft)(1)	N/A	N/A	N/A

Results for the Limiting 3-inch Break Size

	High TAVG	Low TAVG
Peak Clad Temperature (°F)	1688(2)	1619
Peak Clad Temperature Location (ft)(1)	11.75	11.50
Peak Clad Temperature Time (sec)	1188	1229
Local Zr/H_2O Reaction, Max (%)	1.5535	1.1034
Local Zr/H_2O Reaction Location (ft)(1)	11.50	11.50
Total Zr/H_2O Reaction (%)	< 1.0	< 1.0
Hot Rod Burst Time (sec)	No Burst	No Burst
Hot Rod Burst Location (ft)(1)	N/A	N/A

Notes:

1. Height from bottom of active fuel.
2. Analysis performed with Zircalloy 4 cladding bounds the use of ZIRLO clad fuel.
3. See Appendix 14A or 14B for latest PCT.

TABLE 14.3.2.2-3

SMALL BREAK LOCA ANALYSIS
TIME SEQUENCE OF EVENTS

Break Spectrum, (High T_{AVG})

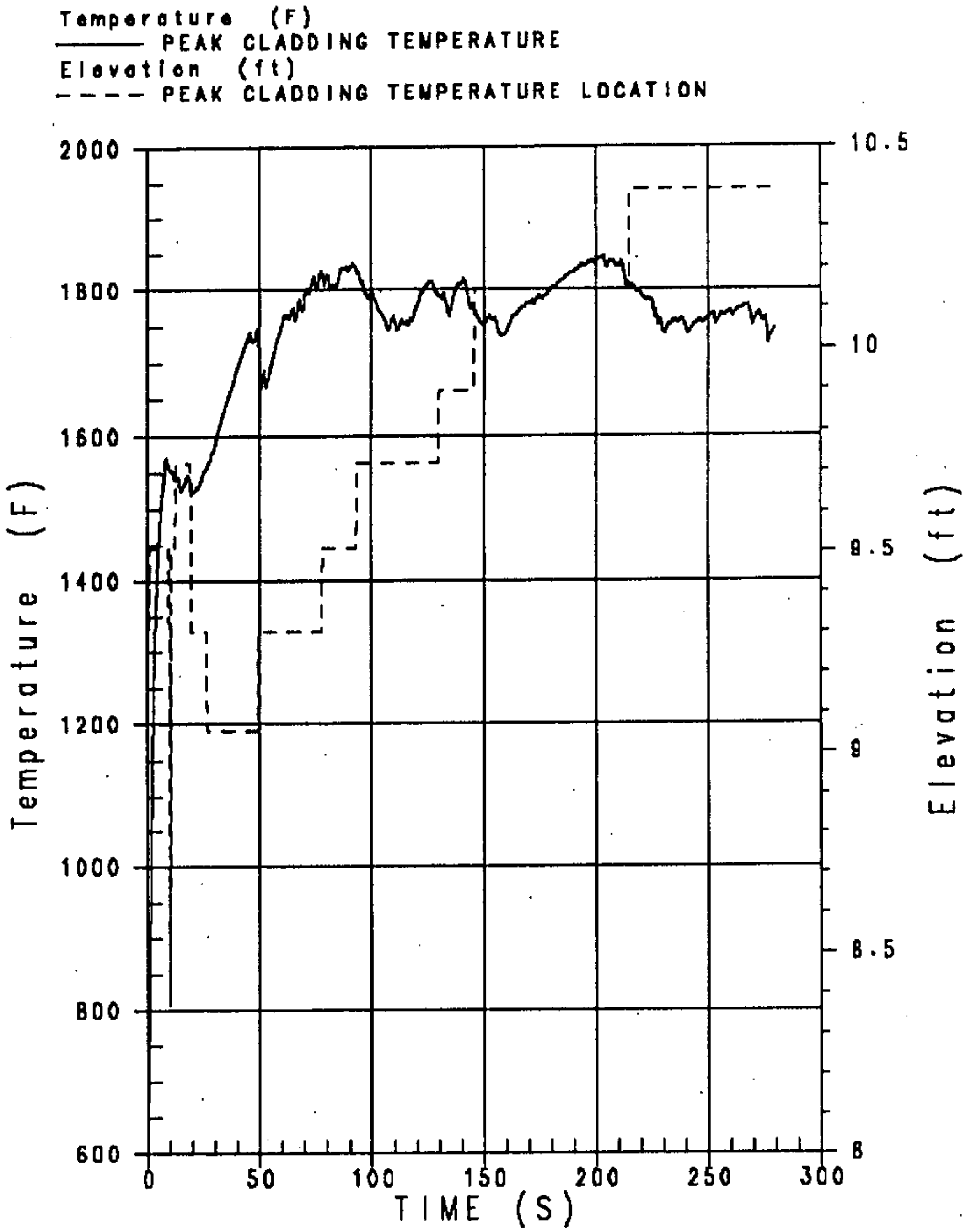
	BREAK SIZE		
	2-inch	3-inch	4-inch
Break Occurs (sec)	0.0	0.0	0.0
Reactor Trip Signal (sec)	40.6	17.0	10.4
Safety Injection Signal (sec)	58.9	30.4	21.4
Top Of Core Uncovered (sec)	1402	482	278(1)
Accumulator Injection Begins (sec)	N/A	1040	525
Peak Cladding Temperature Occurs (sec)	2627	1188	668
Top Of Core Covered (sec)	4554	2363	965

Results for the Limiting 3-inch Break Size

	High TAVG	Low TAVG
Break Occurs (sec)	0.0	0.0
Reactor Trip Signal (sec)	17.0	14.4
Safety Injection Signal (sec)	30.4	21.8
Top Of Core Uncovered (sec)	482	526
Accumulator Injection Begins (sec)	1040	1086
Peak Cladding Temperature Occurs (sec)	1188	1229
Top Of Core Covered (sec)	2363	2343

NOTE:

1. Momentary core uncovery occurred at 213 seconds during prelude to loop seal clearing. The beginning of the subsequent extended core uncovery at 278 seconds is the time listed.

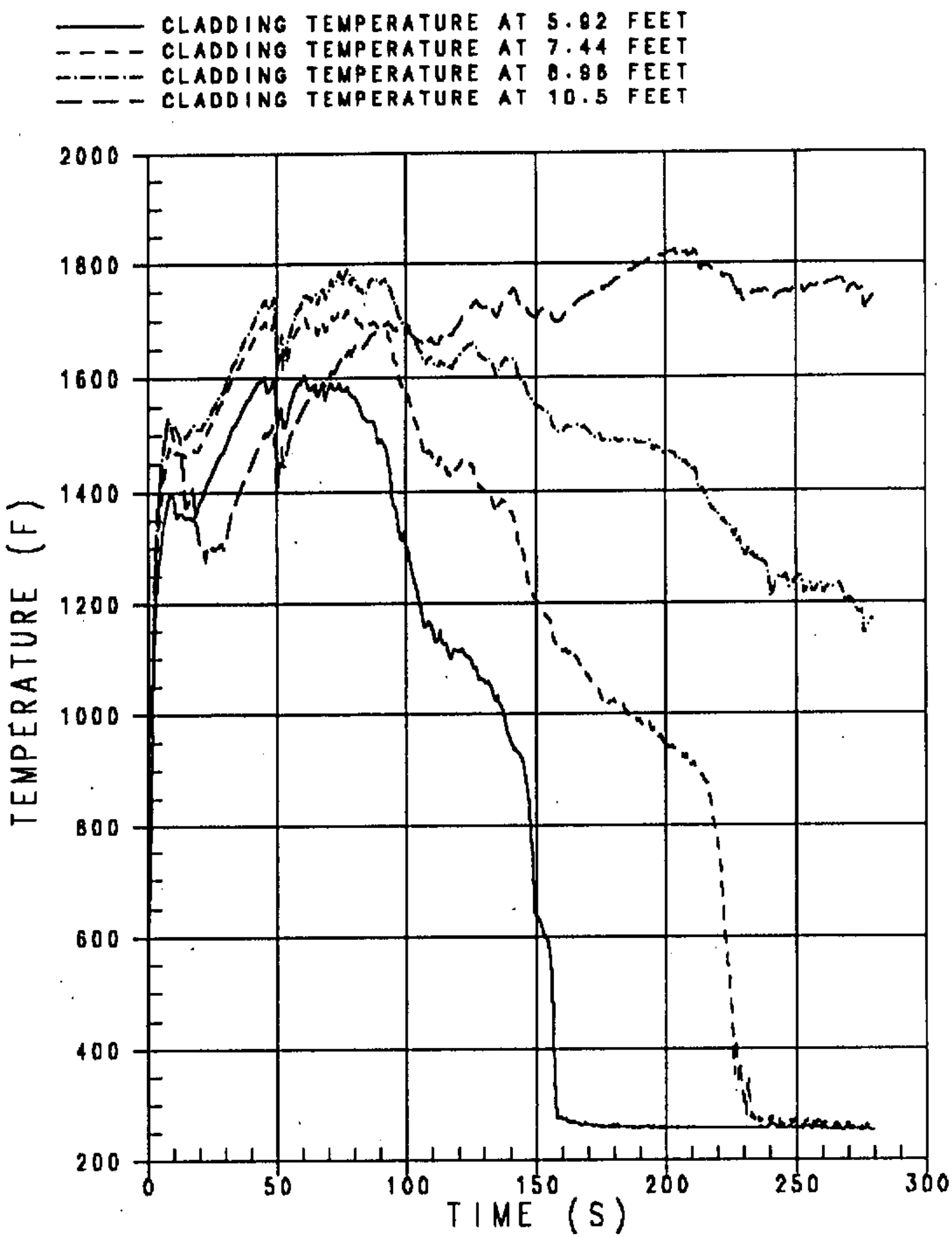


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

PEAK CLADDING TEMPERATURE
AND ELEVATION FOR THE NOMINAL
SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-1

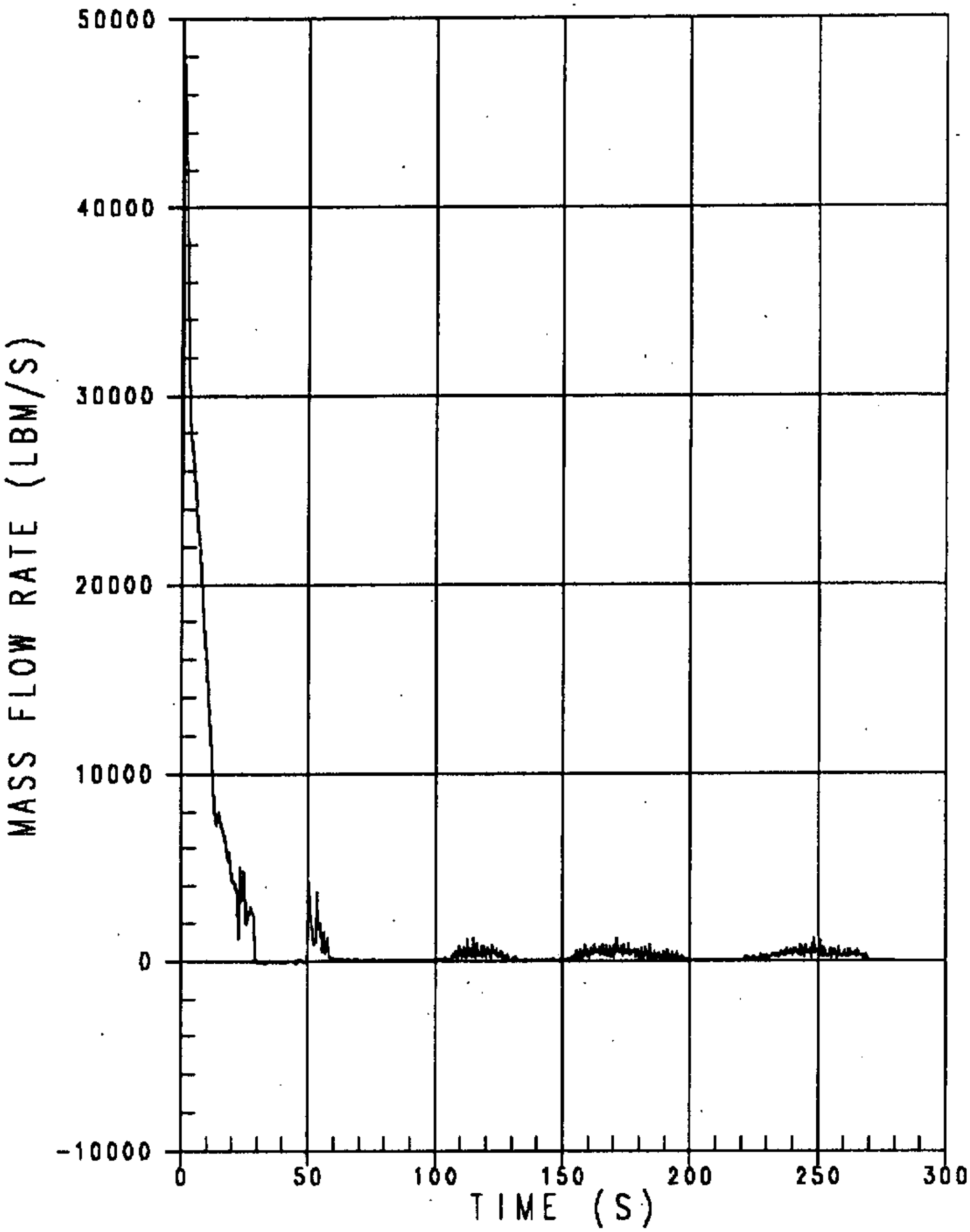


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CLADDING TEMPERATURE AT SELECT LOCATIONS FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-2



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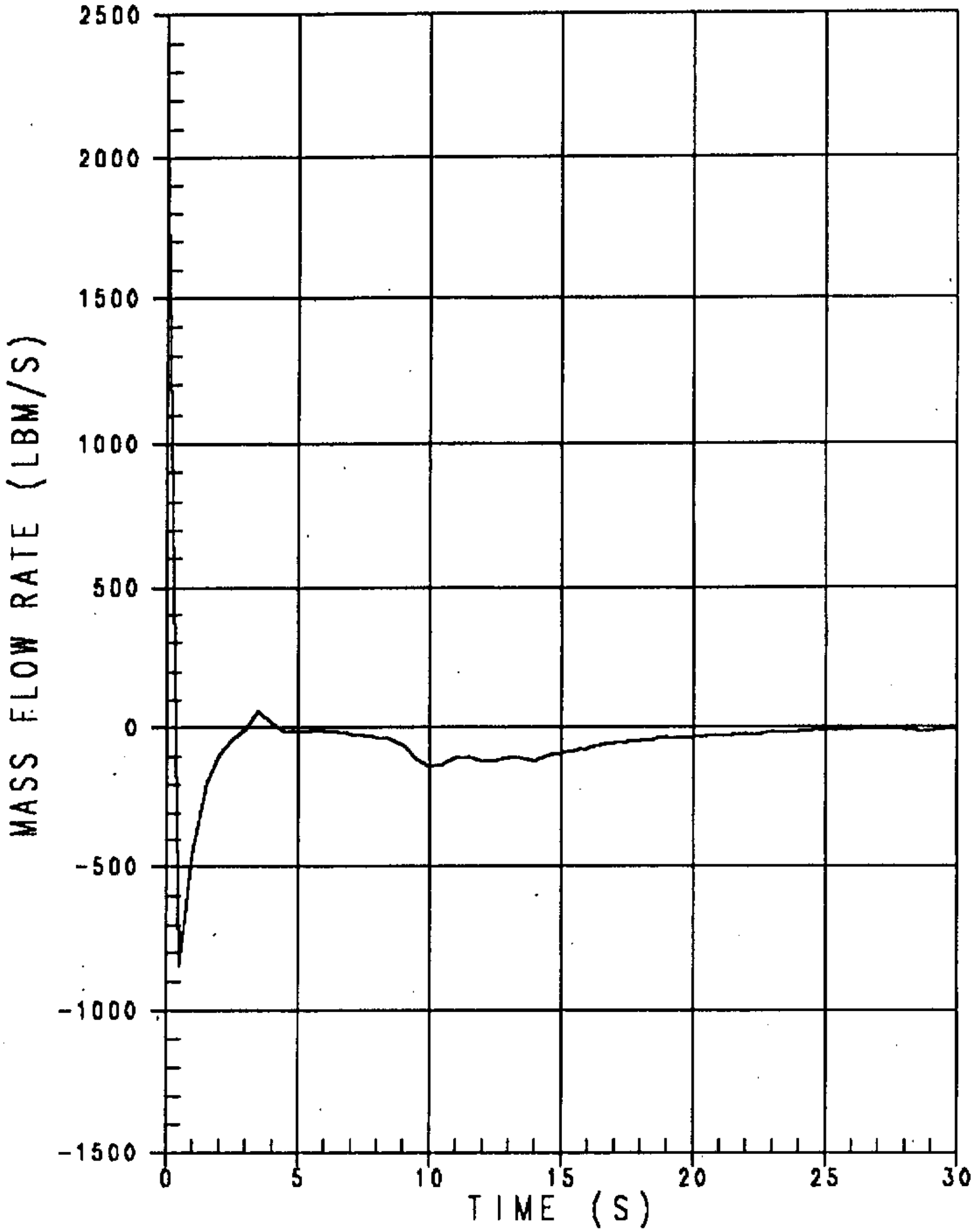
TURKEY POINT PLANT UNITS 3 & 4

BREAK FLOW FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-3

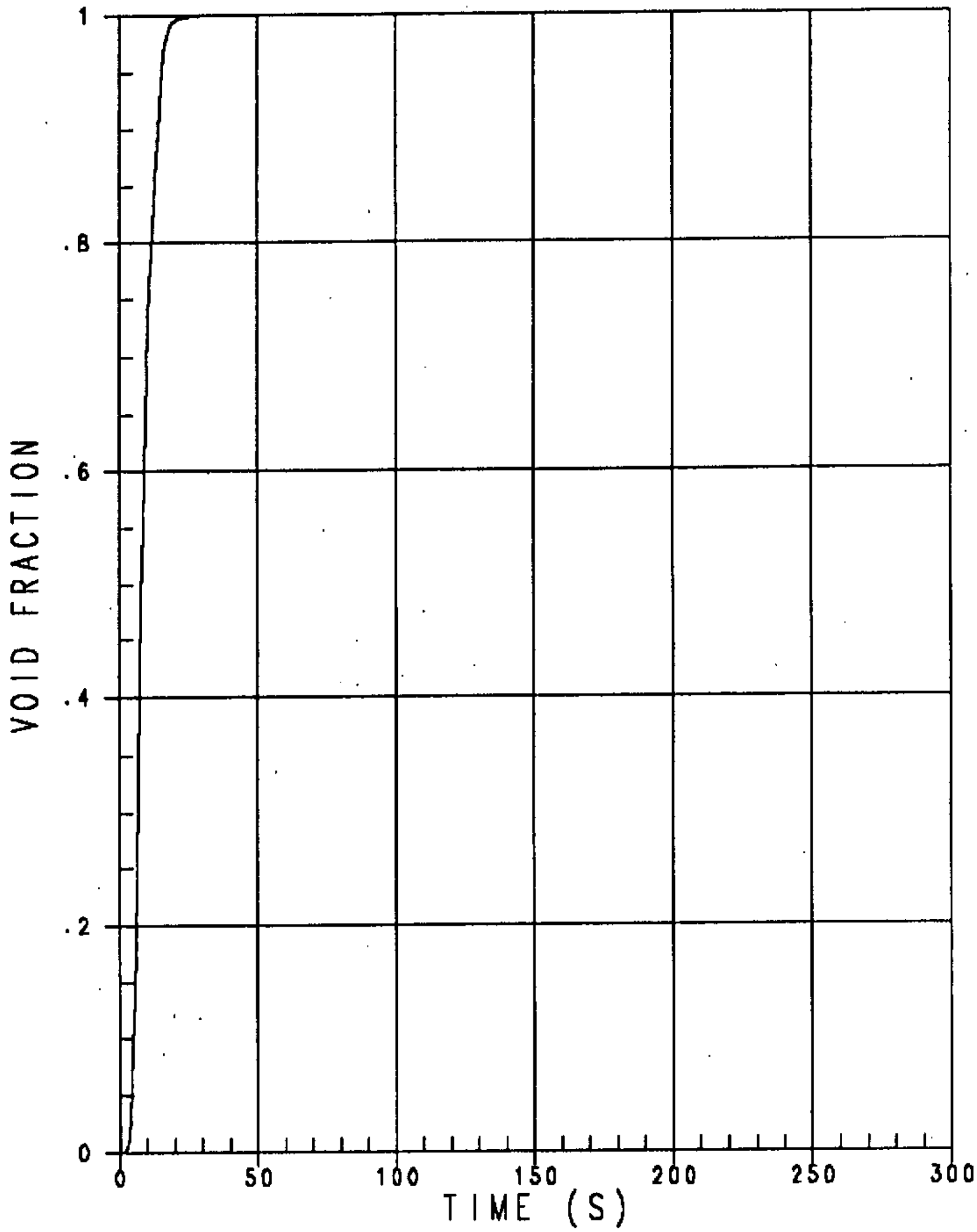


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

BLOWDOWN FLOW AT THE
BOTTOM OF THE CORE FOR THE
NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-4

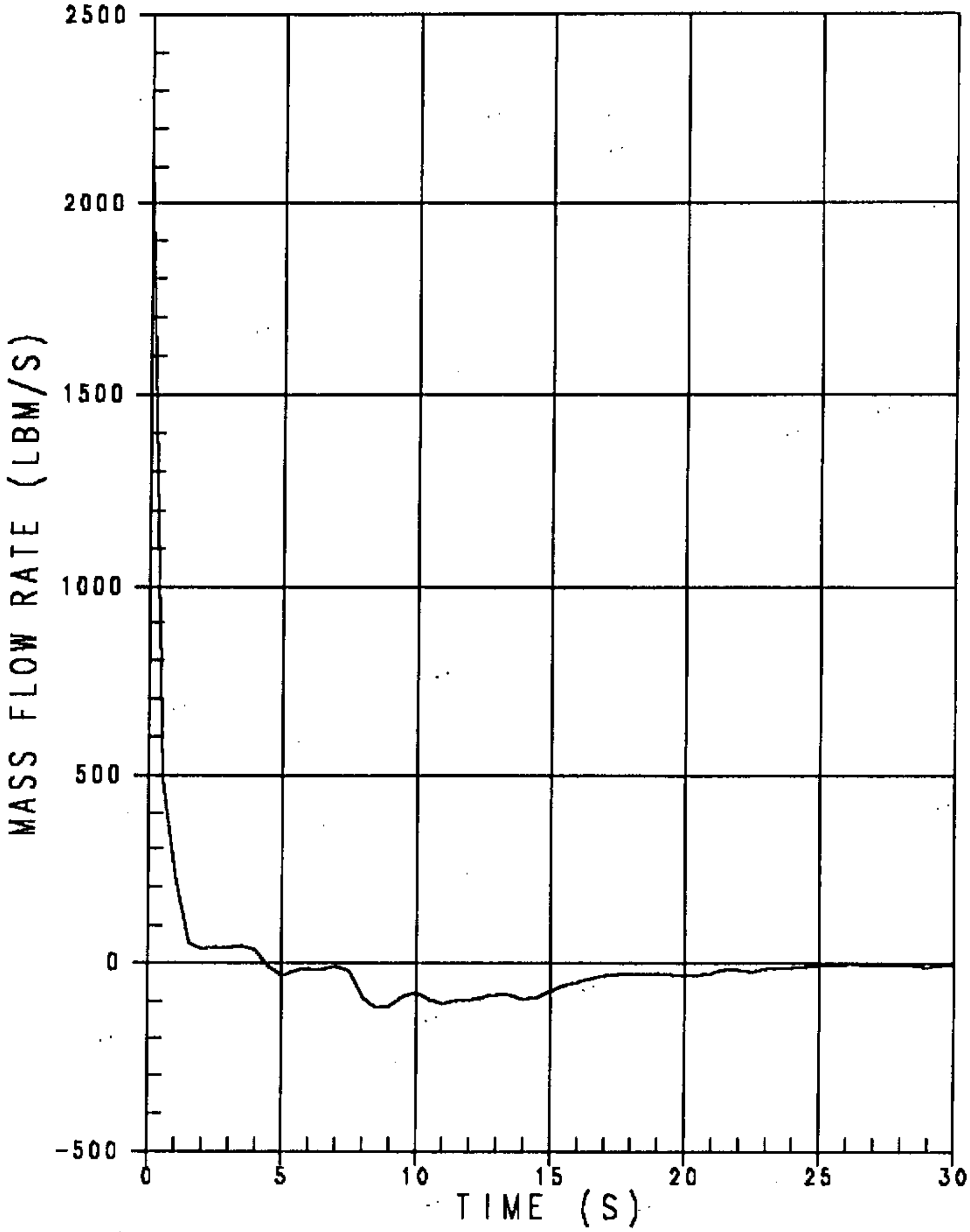


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

VOID FRACTION AT THE INTACT LOOP PUMP FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-5

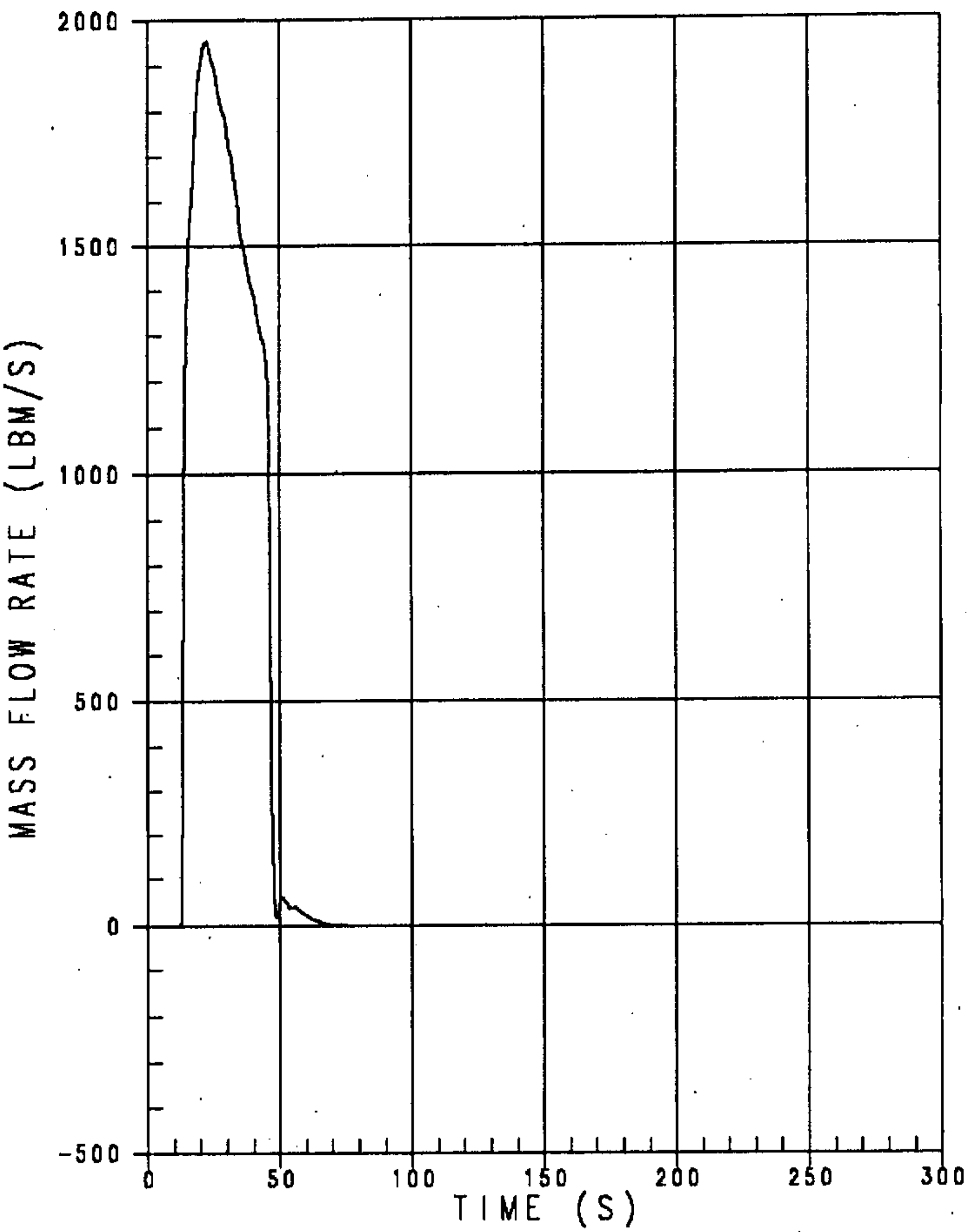


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

BLOWDOWN FLOW AT THE TOP OF THE CORE FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-6

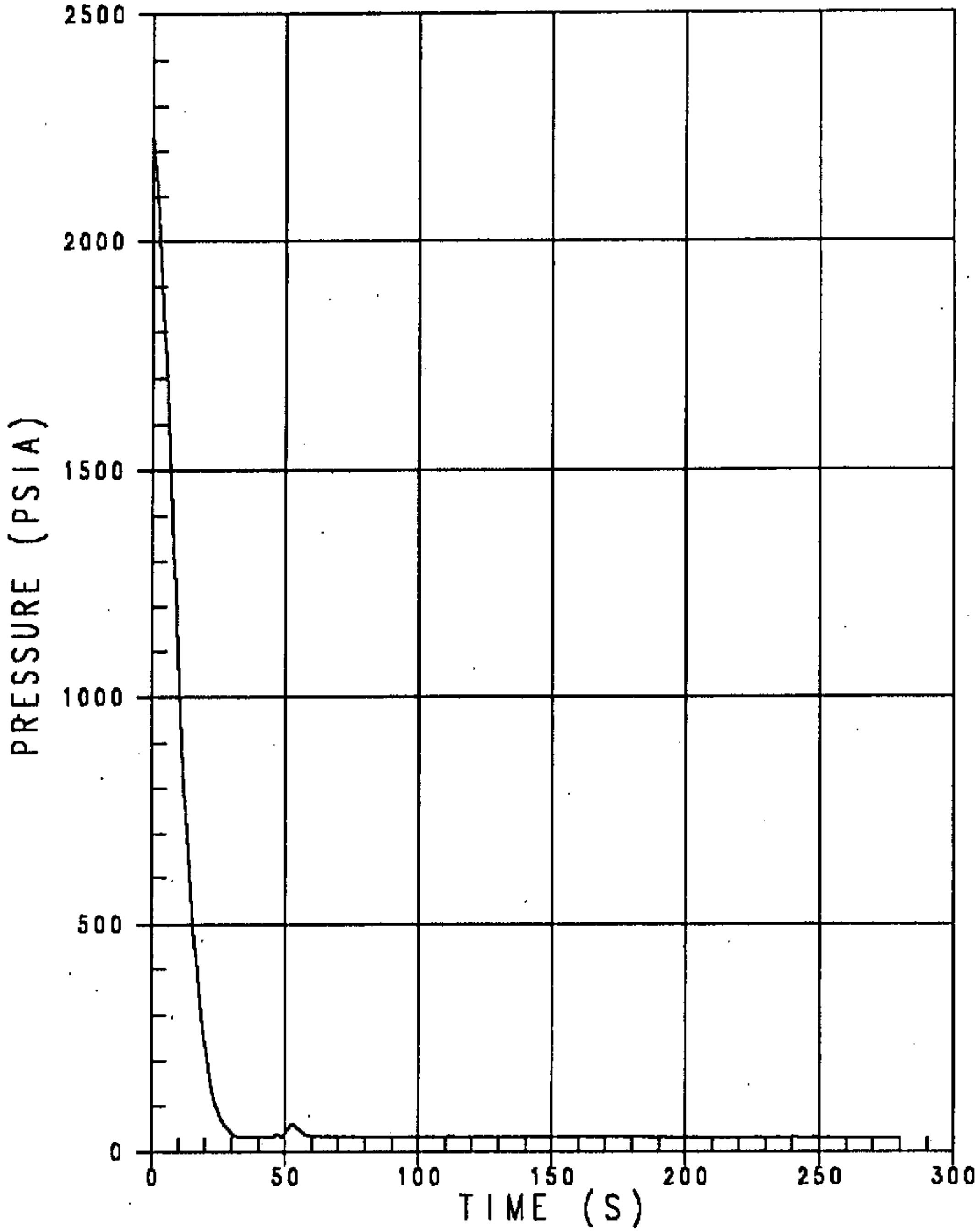


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ACCUMULATOR FLOW FOR THE
NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-7

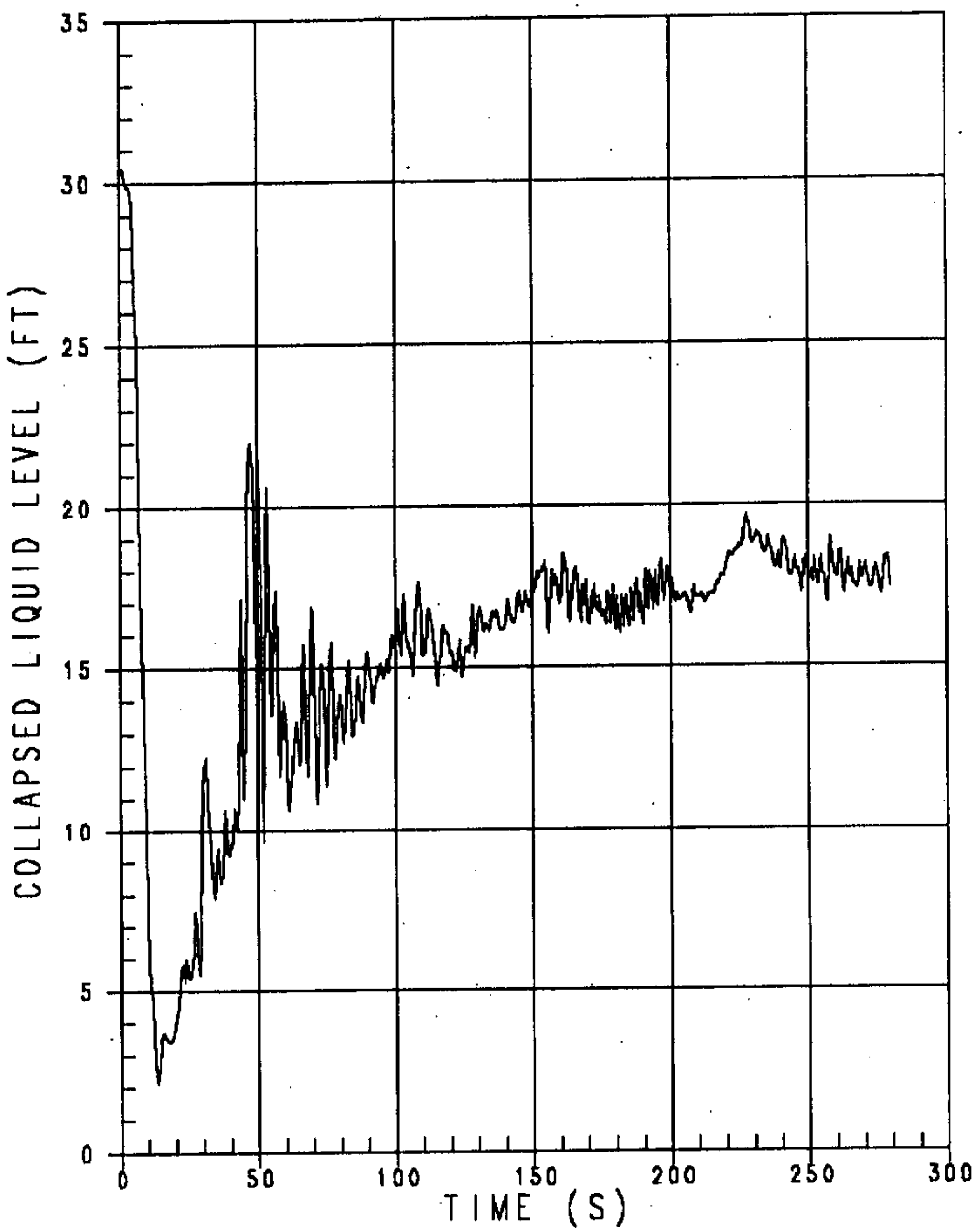


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

PRESSURIZER PRESSURE FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-8

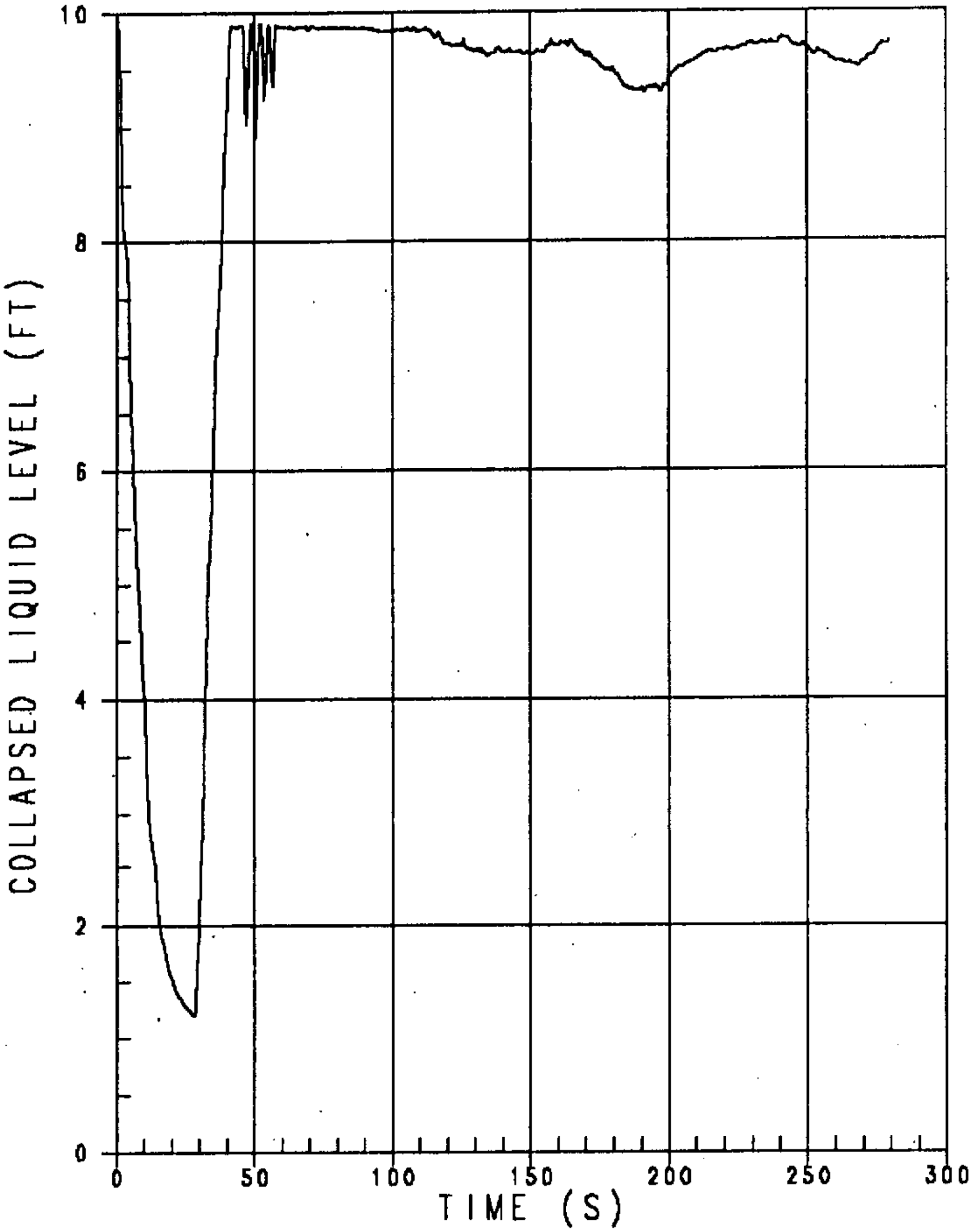


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

COLLAPSED LIQUID LEVEL IN THE DOWNCOMER FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-9

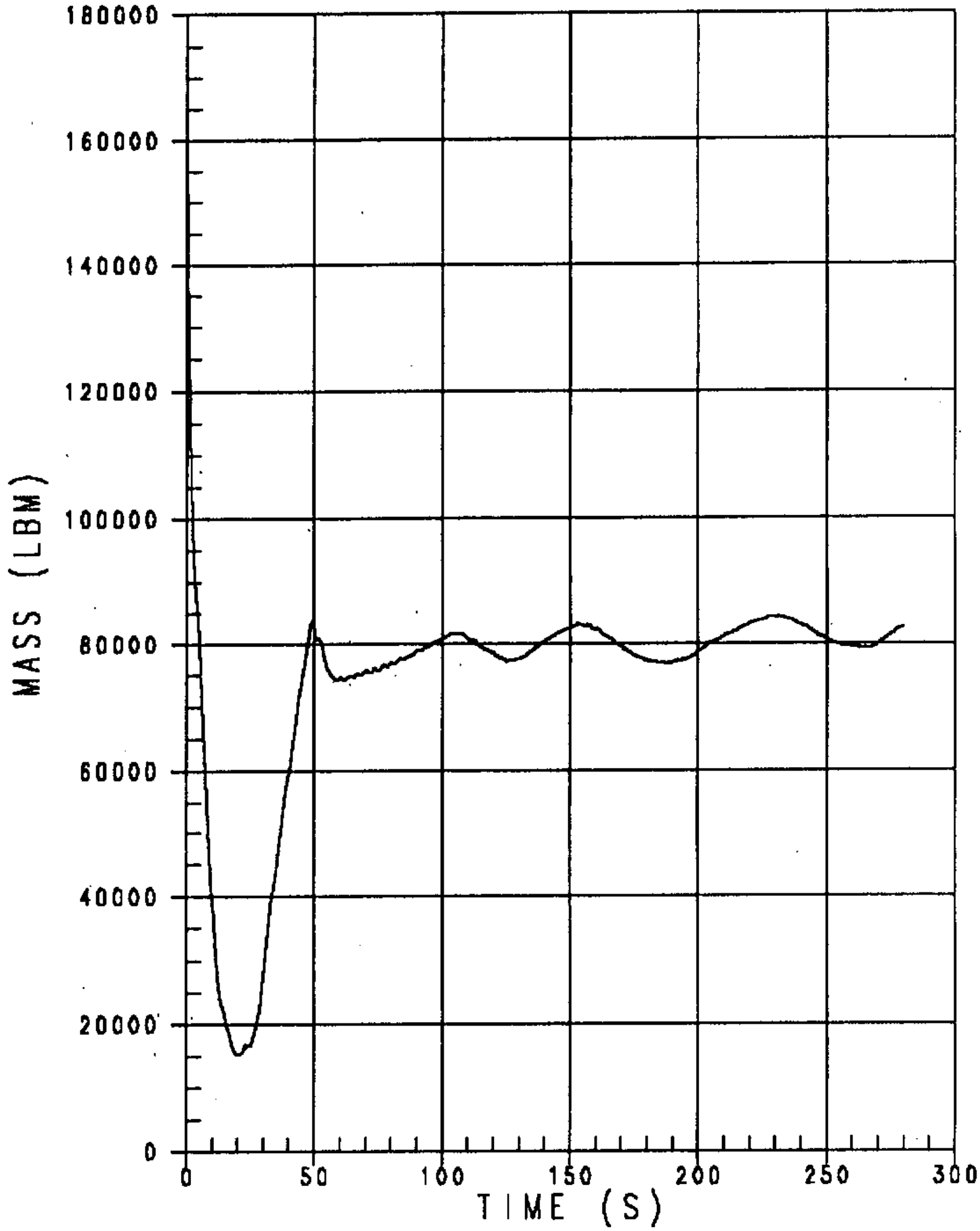


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

COLLAPSED LIQUID LEVEL IN THE LOWER PLENUM FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-10

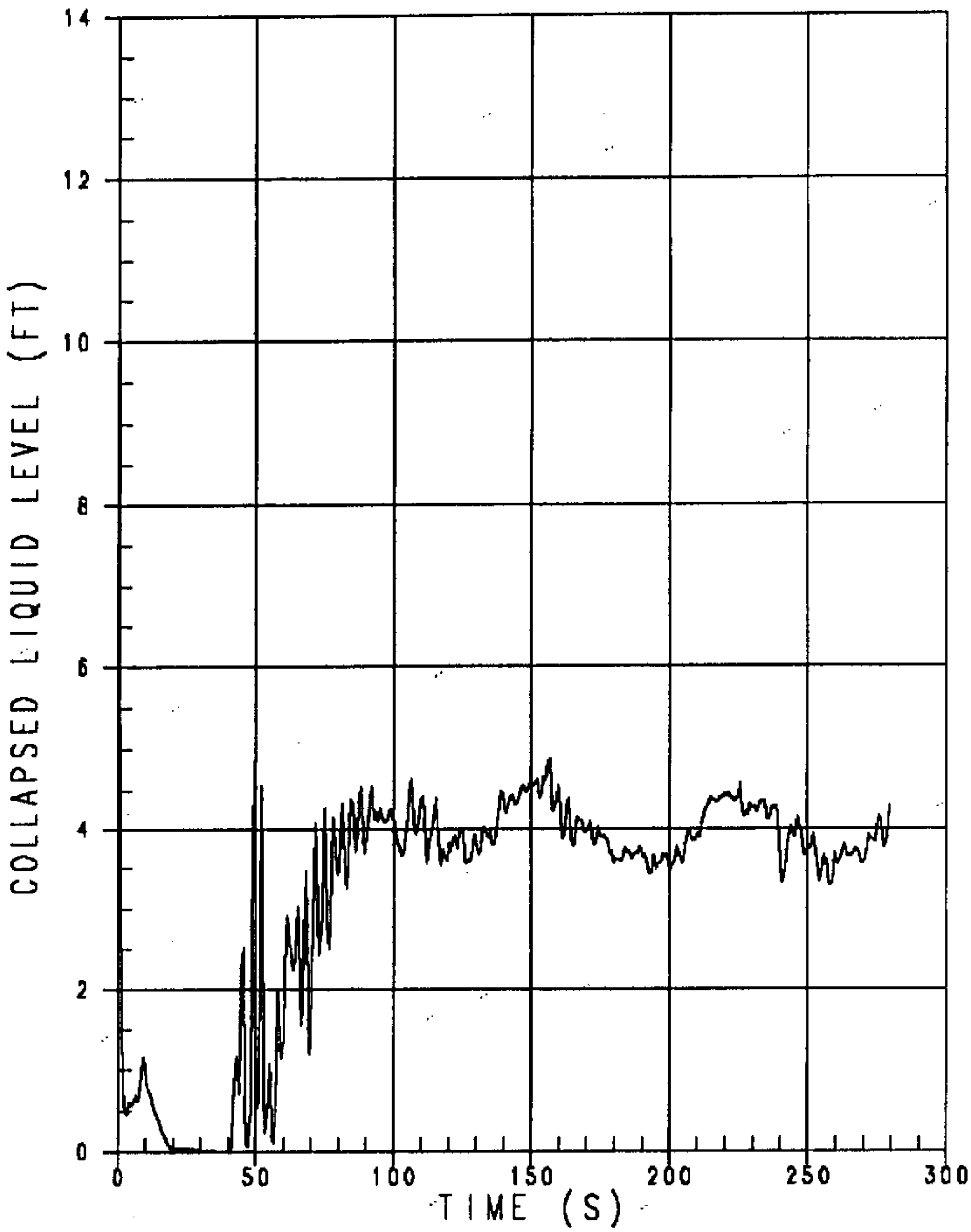


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

VESSEL MASS FOR THE
NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-11

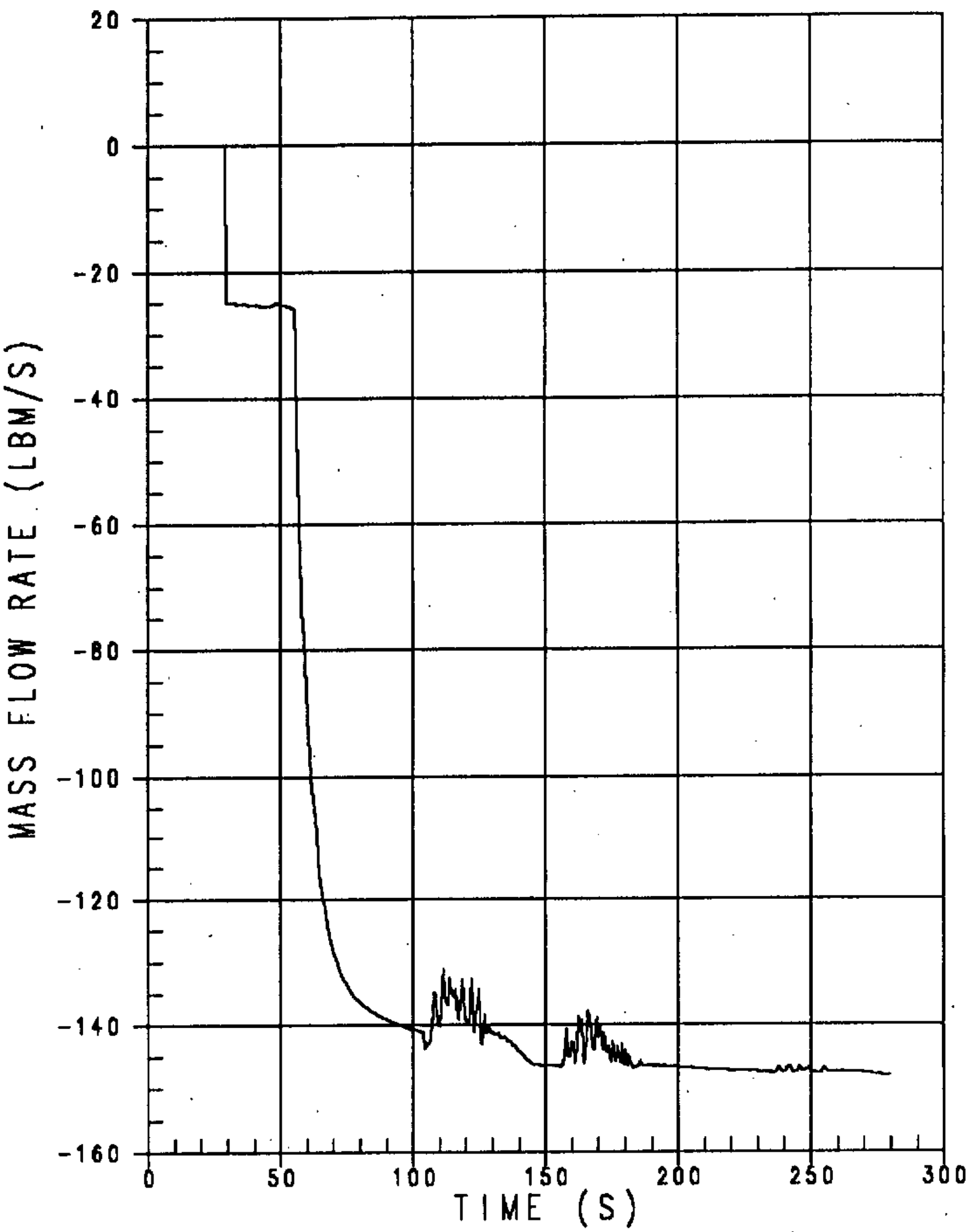


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

COLLAPSED LIQUID LEVEL IN
THE CORE FOR THE NOMINAL
SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-12

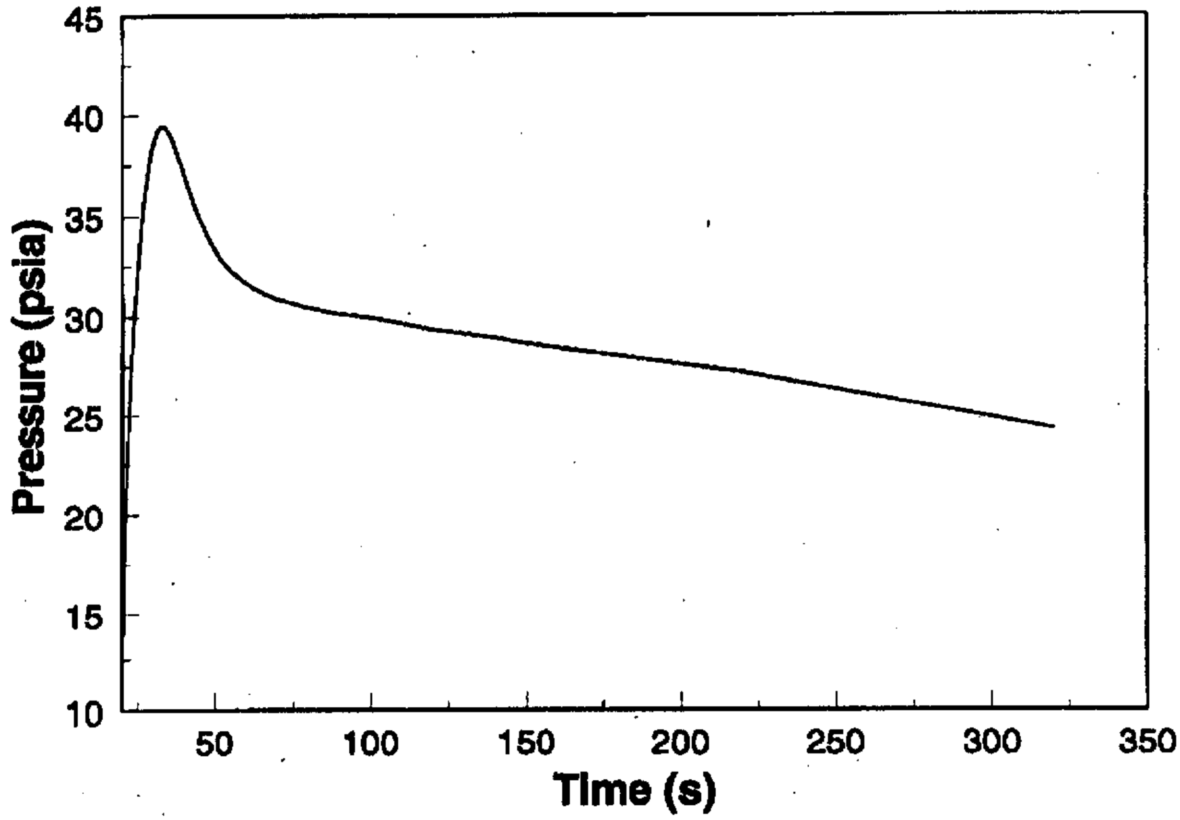


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

PUMPED SAFETY INJECTION FLOW FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-13

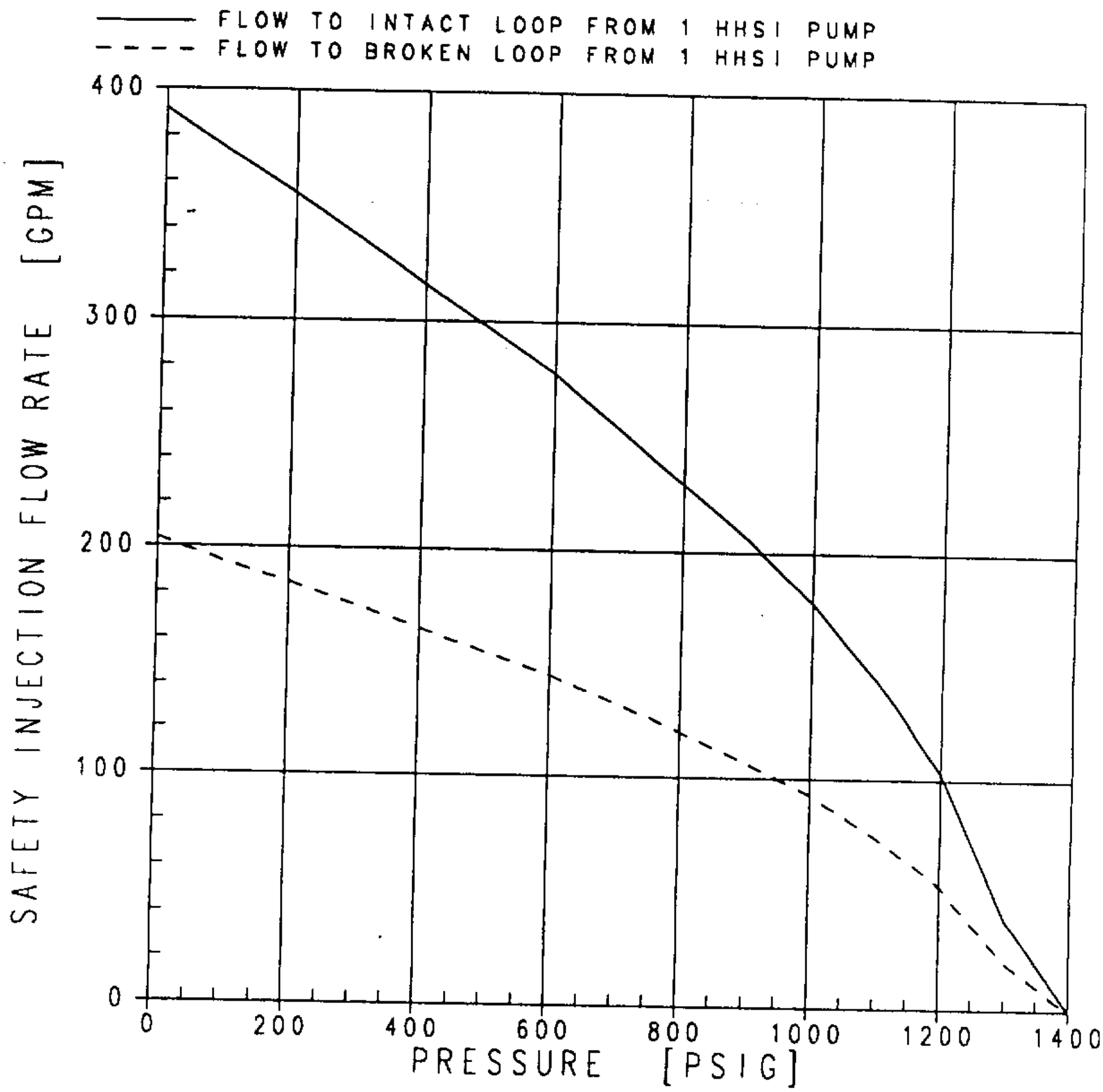


REV. 15 (4/98)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT BACKPRESSURE FOR THE NOMINAL SPLIT BREAK TRANSIENT

FIGURE 14.3.2.1-14



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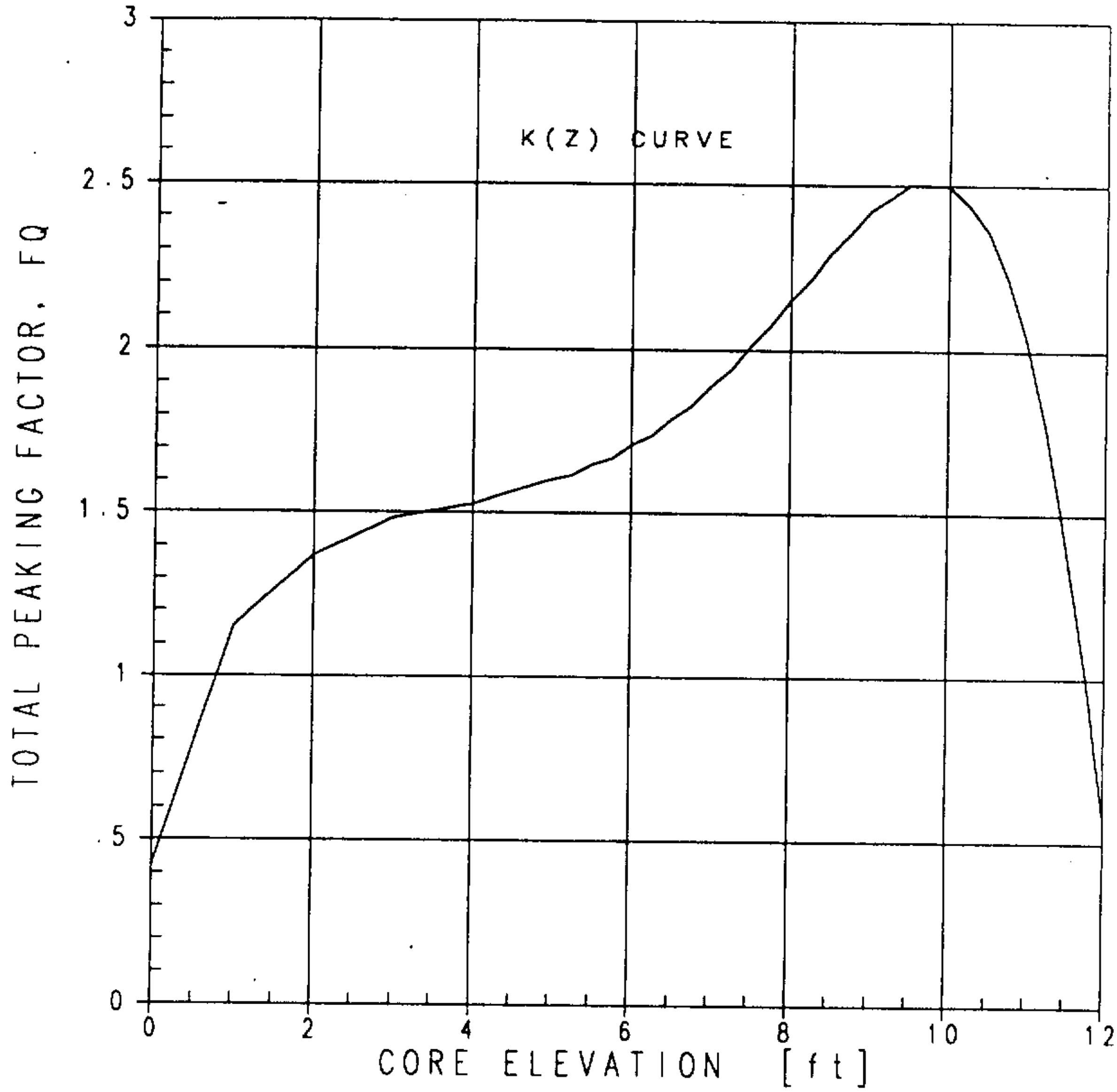
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

SMALL BREAK SAFETY INJECTION
FLOW RATE
ONE HHSI PUMP
FIGURE 14.3.2.2-1



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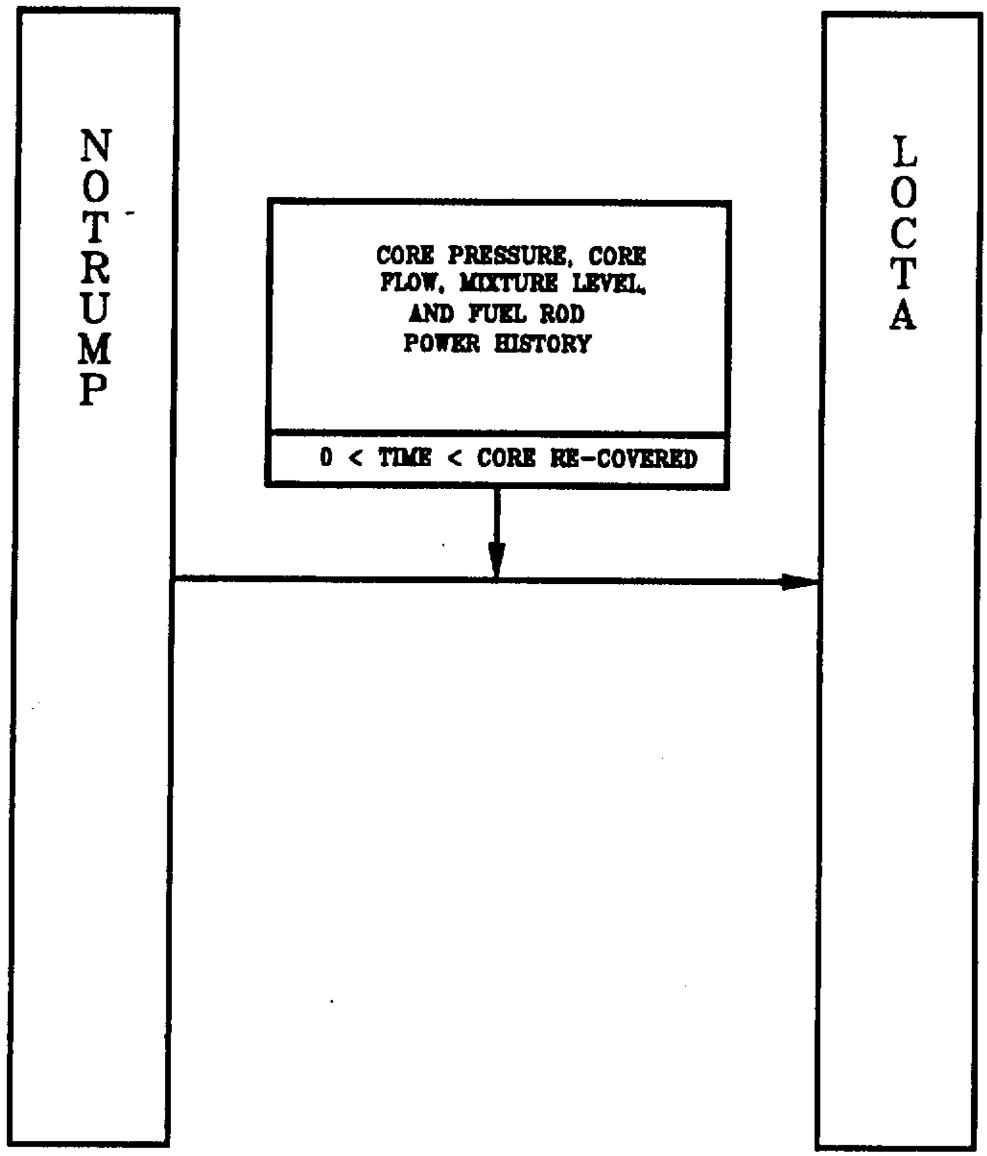
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

SMALL BREAK
HOT ROD POWER SHAPE

FIGURE 14.3.2.2-2



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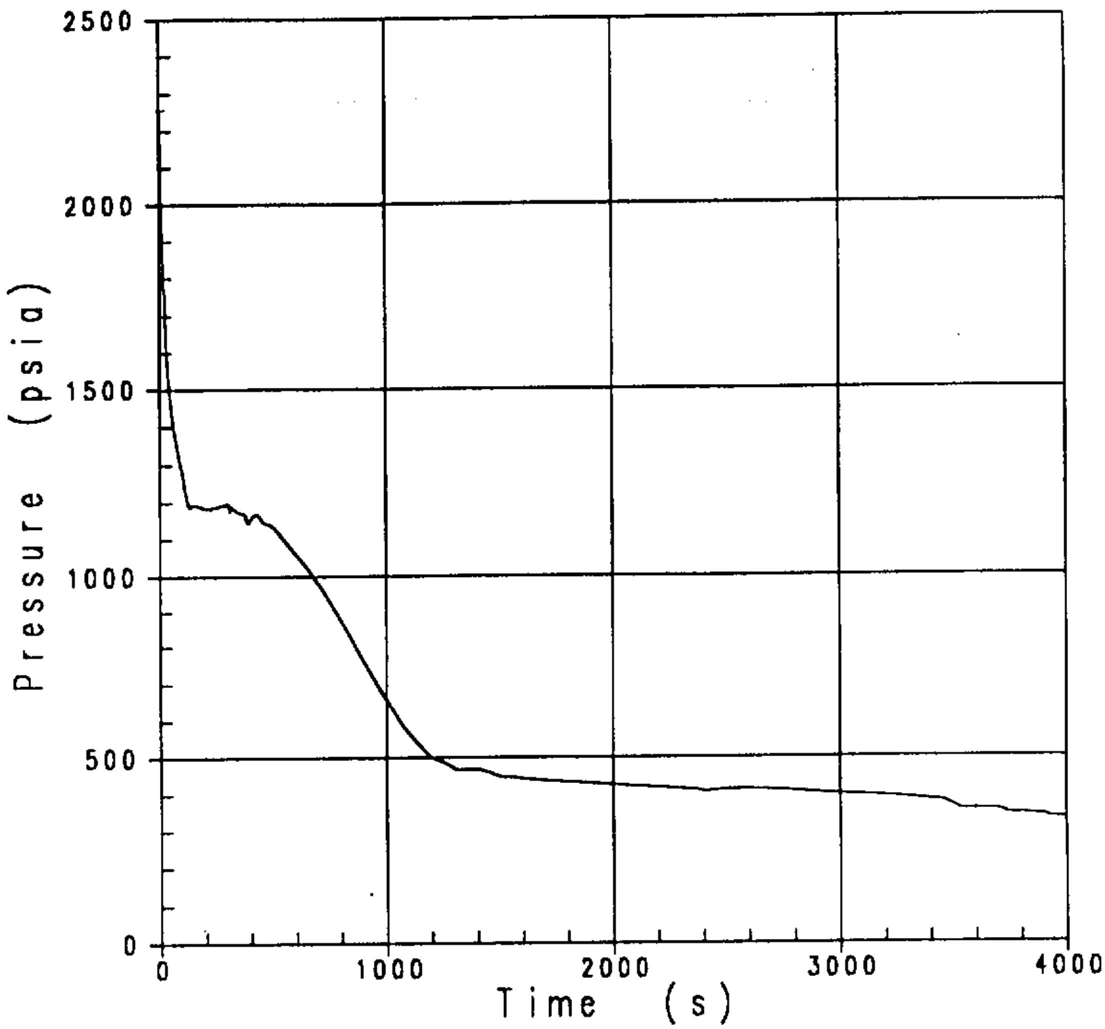
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CODE INTERFACE DESCRIPTION
FOR THE SMALL BREAK
LOCA MODEL
FIGURE 14.3.2.2-3

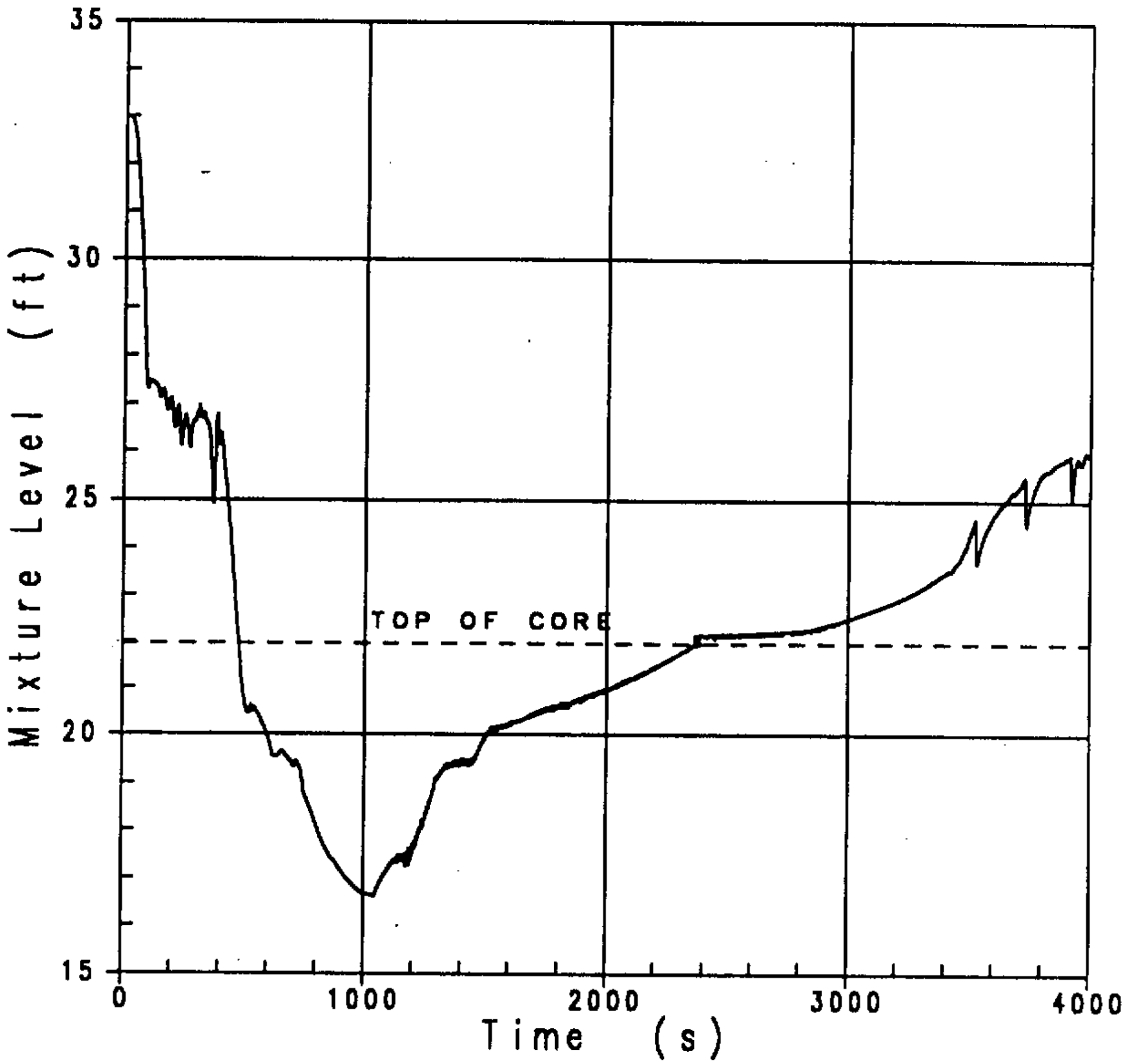


REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

RCS DEPRESSURIZATION TRANSIENT
LIMITING 3-INCH BREAK
WITH HIGH T_{AVG}

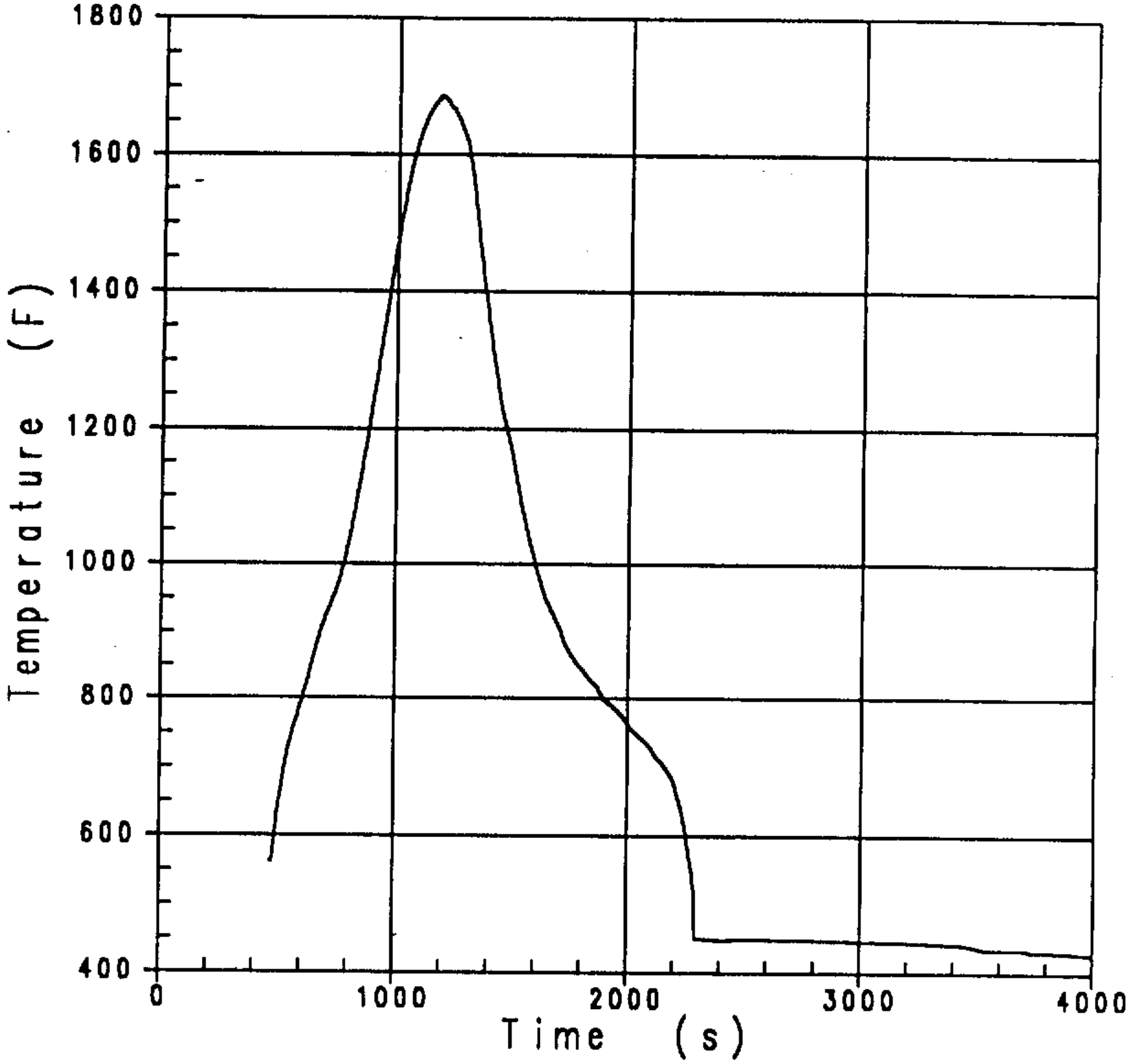
FIGURE 14.3.2.2-4



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

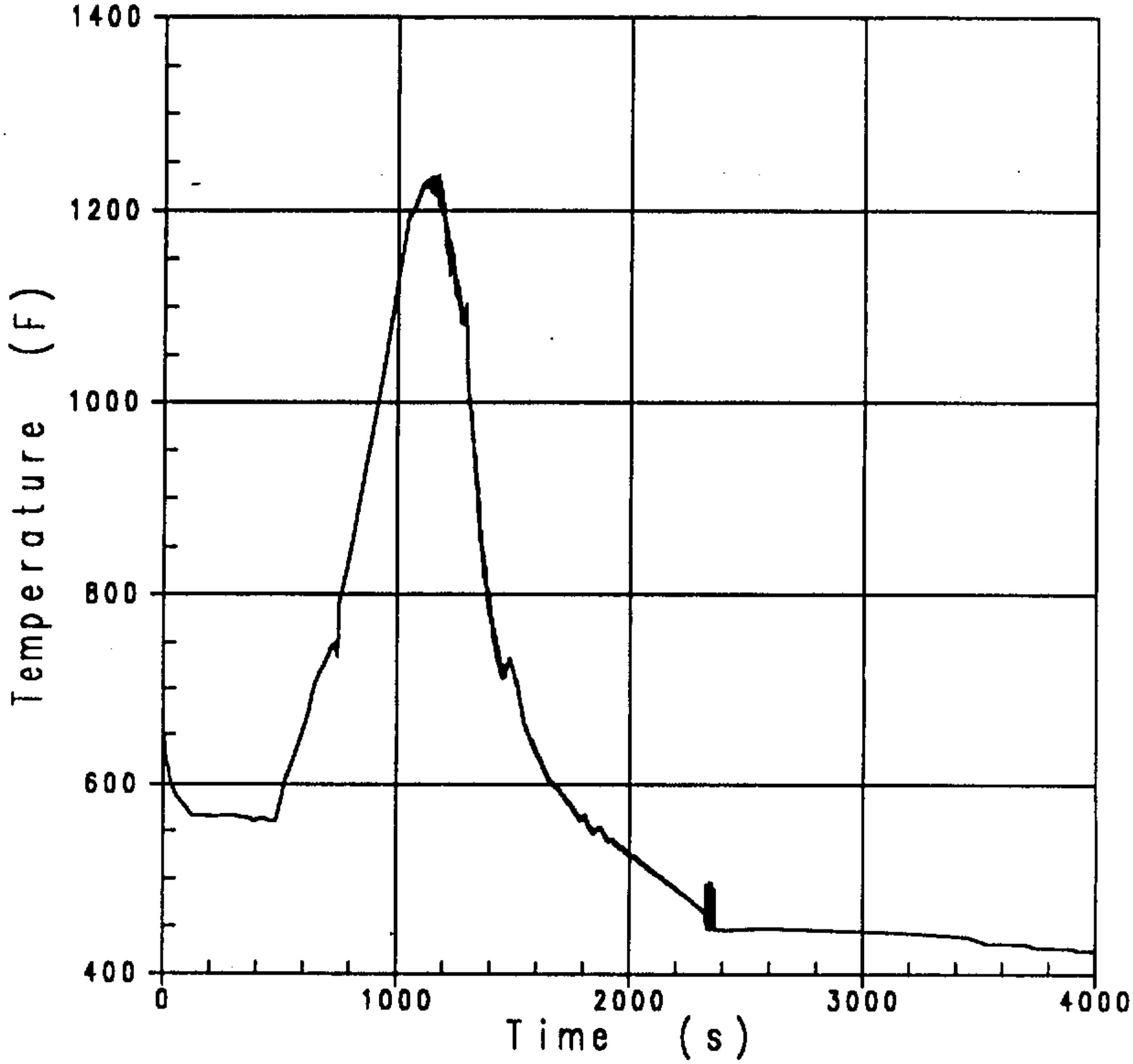
CORE MIXTURE LEVEL
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-5



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

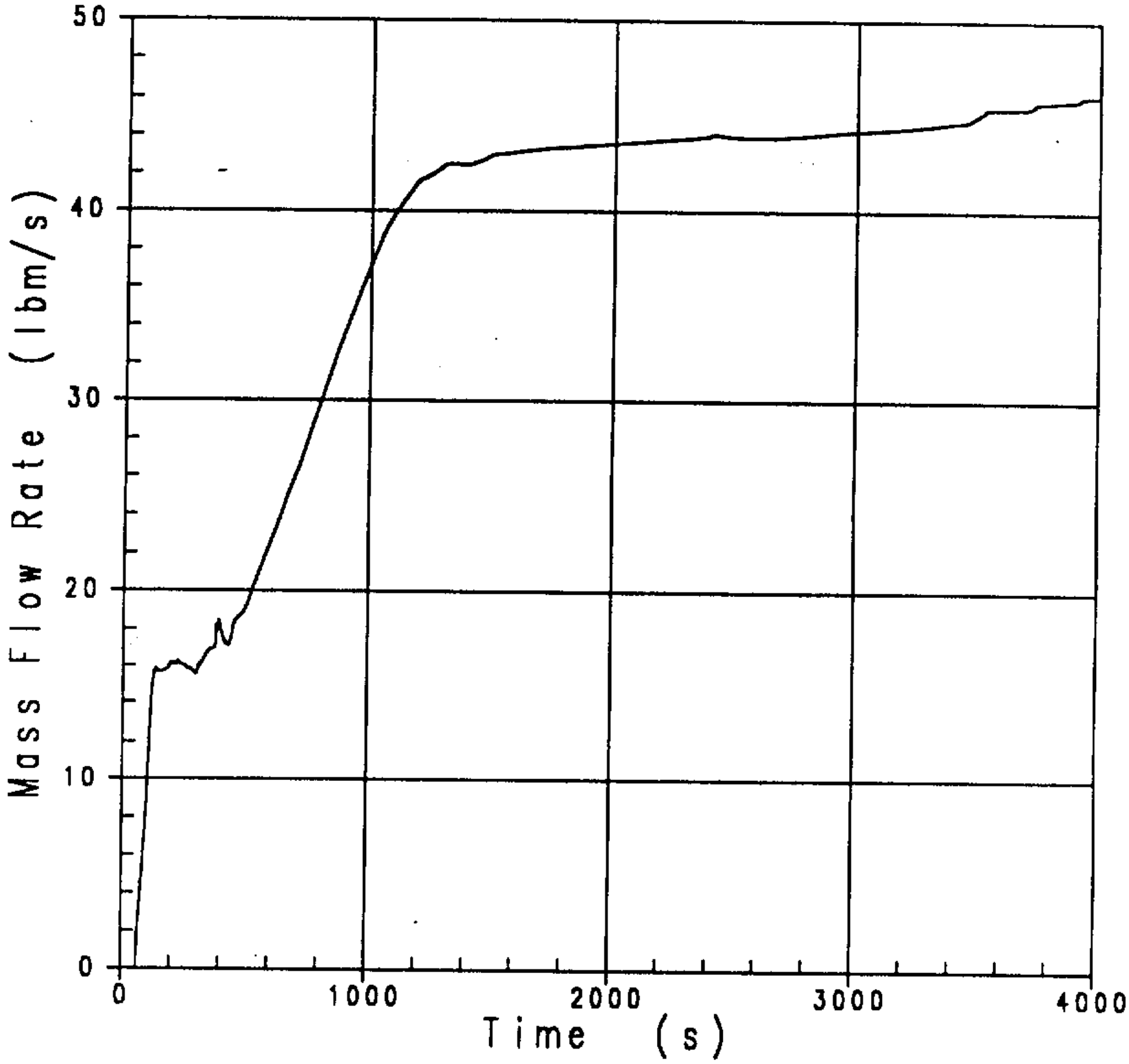
PEAK CLADDING TEMPERATURE-HOT ROD
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-6



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

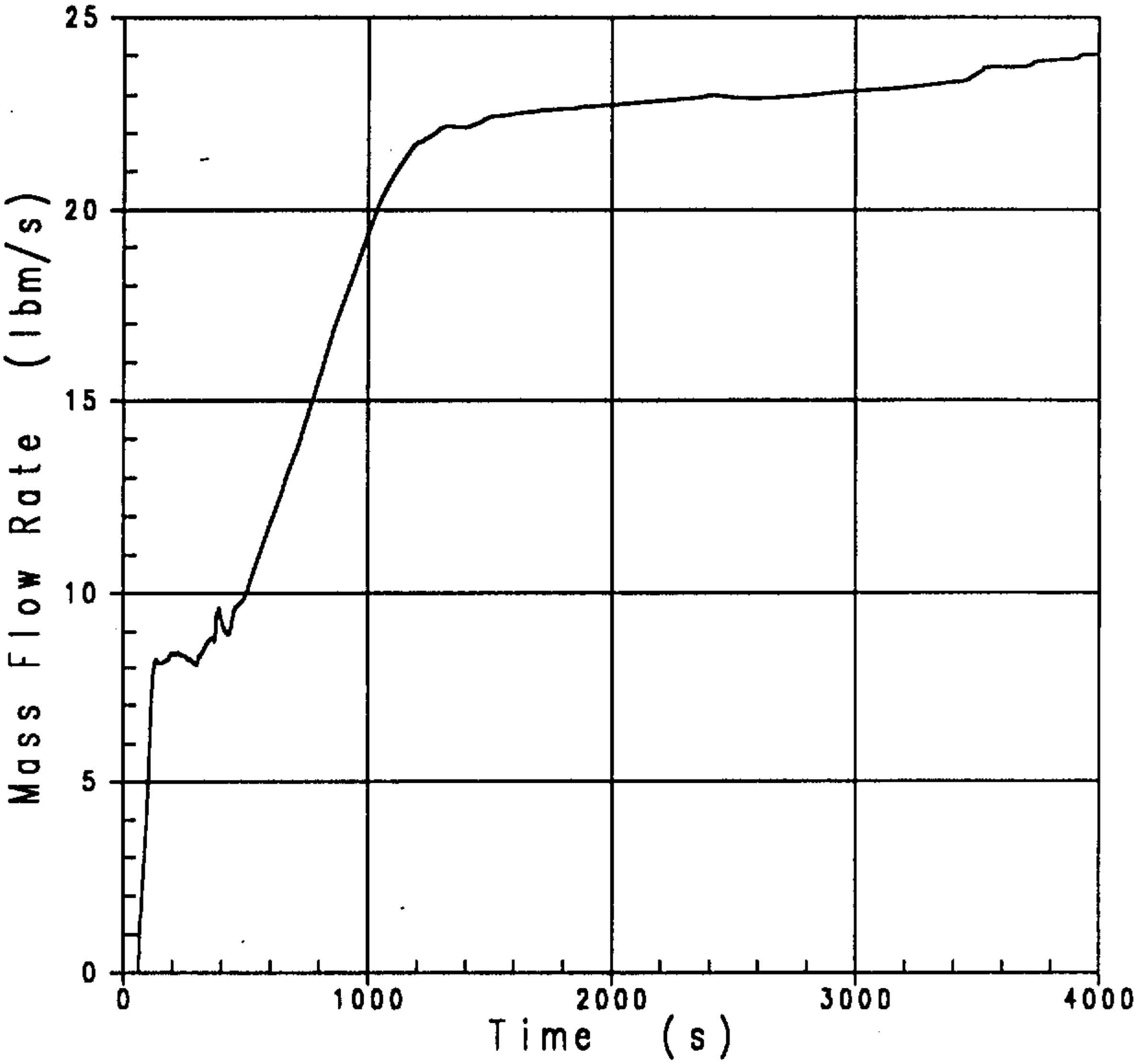
TOP CORE NODE VAPOR TEMPERATURE
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-7



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

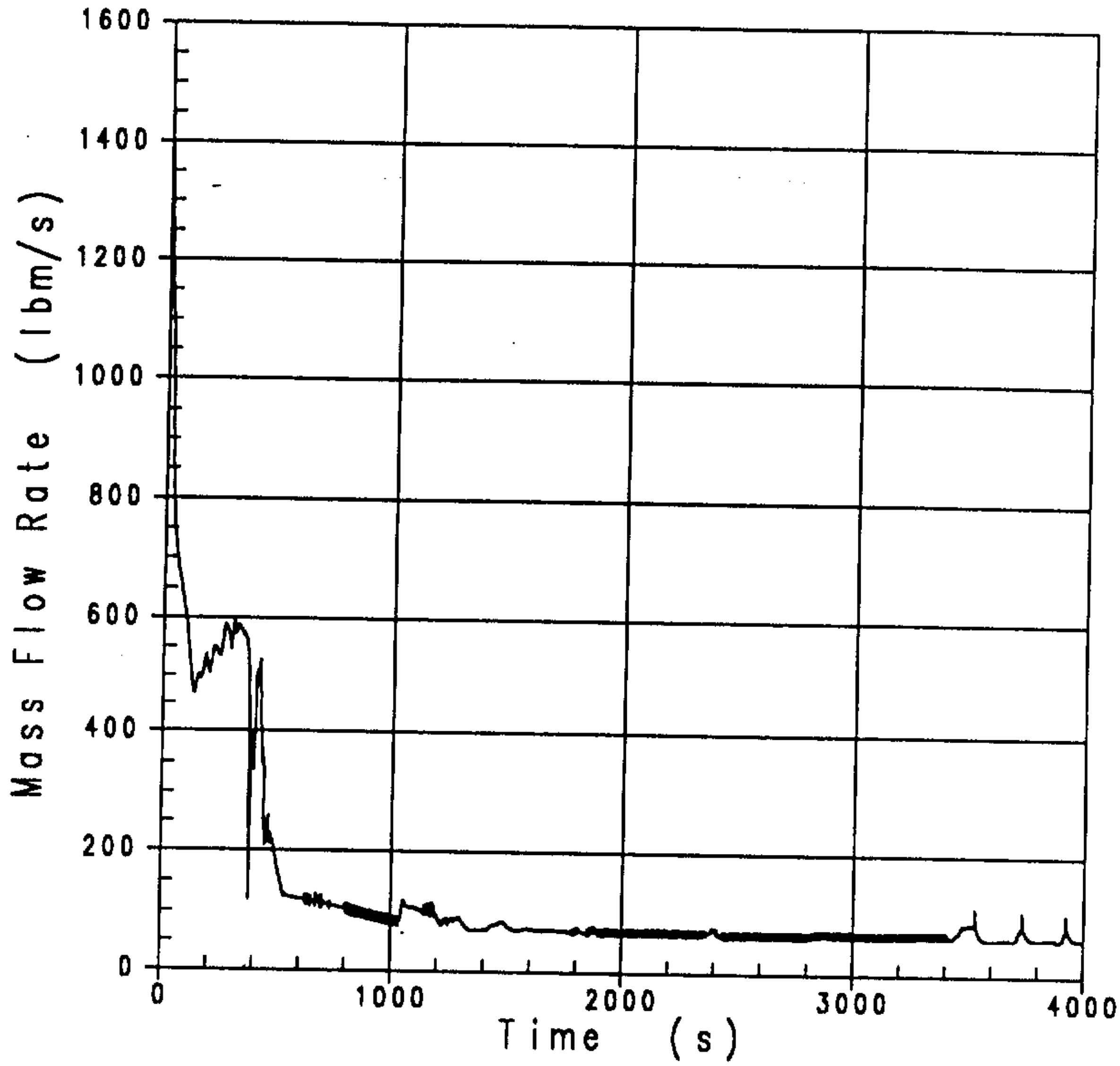
ECCS PUMPED SI - INTACT LOOP
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-8



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

ECCS PUMPED SI - BROKEN LOOP
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-9

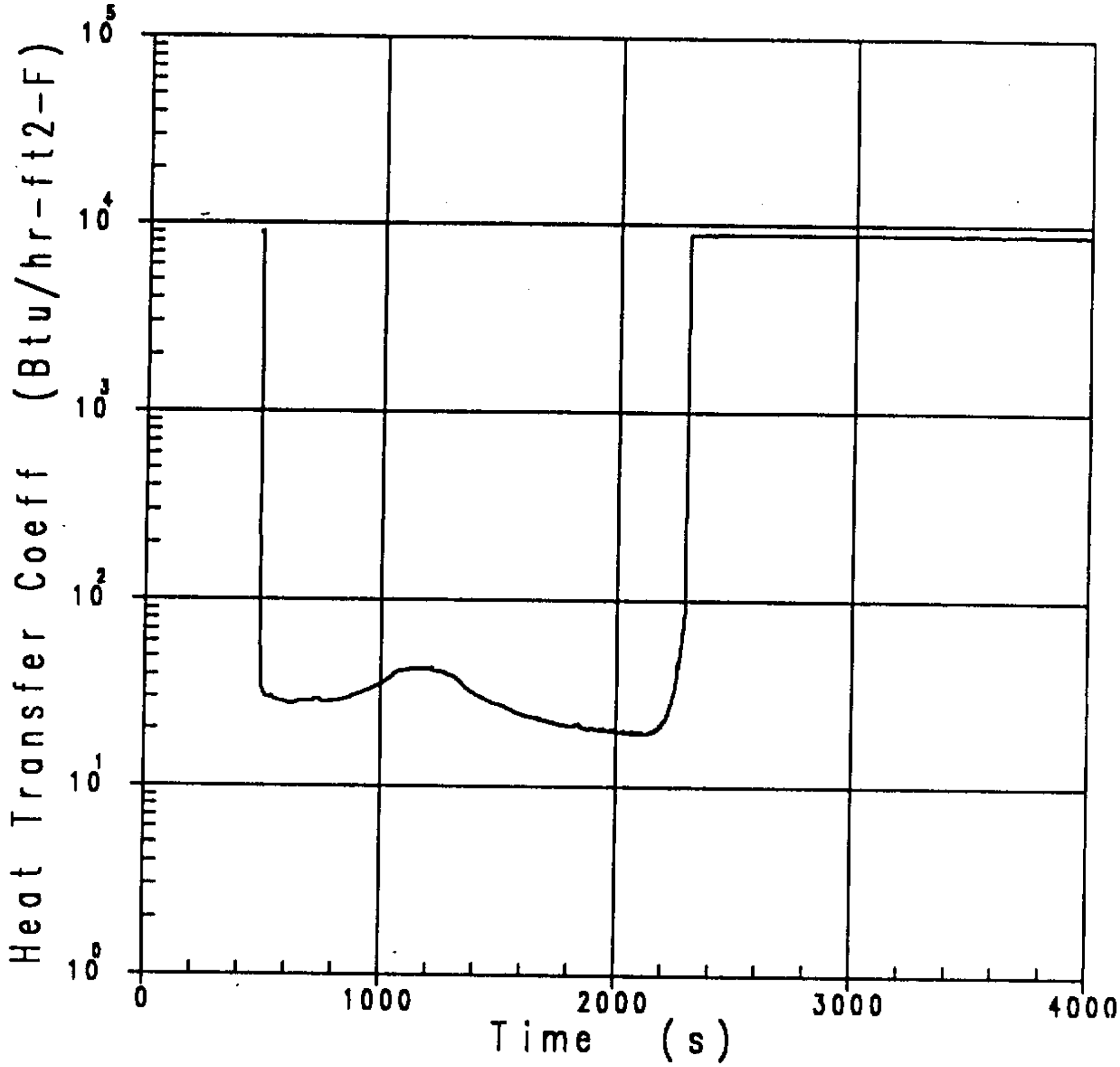


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

COLD LEG BREAK MASS FLOW
3-INCH BREAK
WITH HIGH T_{AVG}

FIGURE 14.3.2.2-10

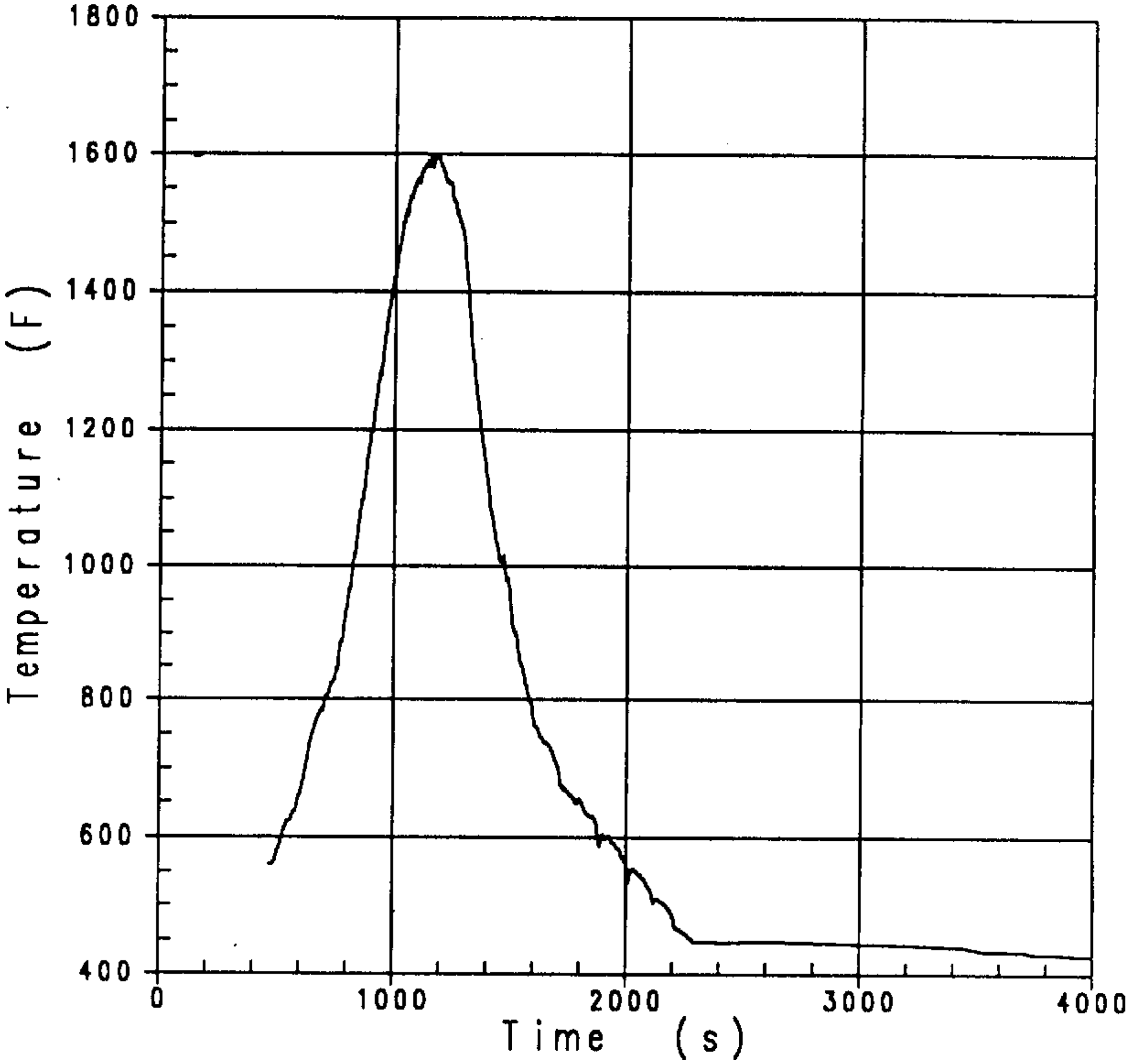


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

HOT ROD SURFACE HEAT TRANSFER COEFFICIENT - HOT SPOT
3-INCH BREAK
WITH HIGH T_{AVG}

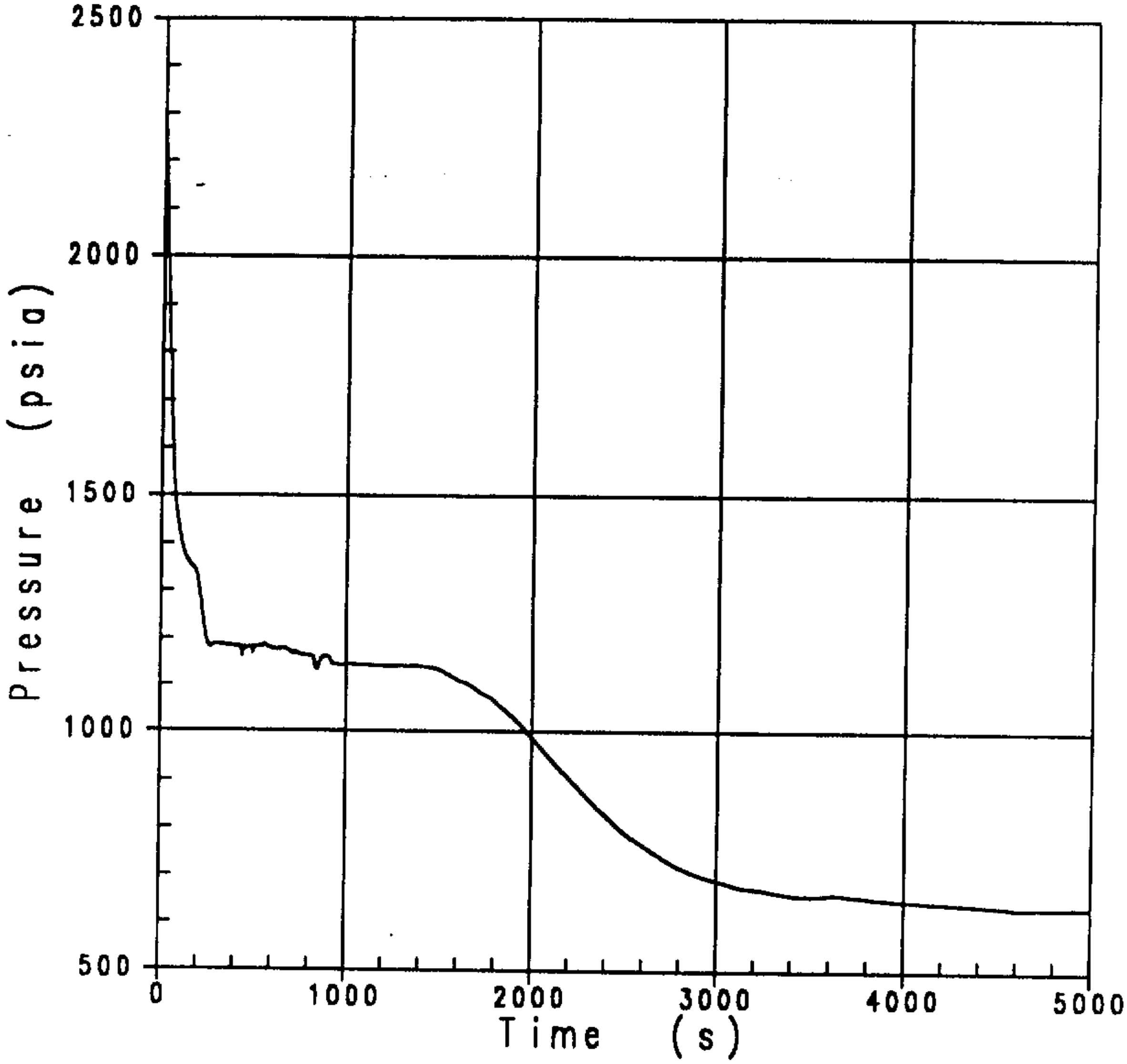
FIGURE 14.3.2.2-11



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

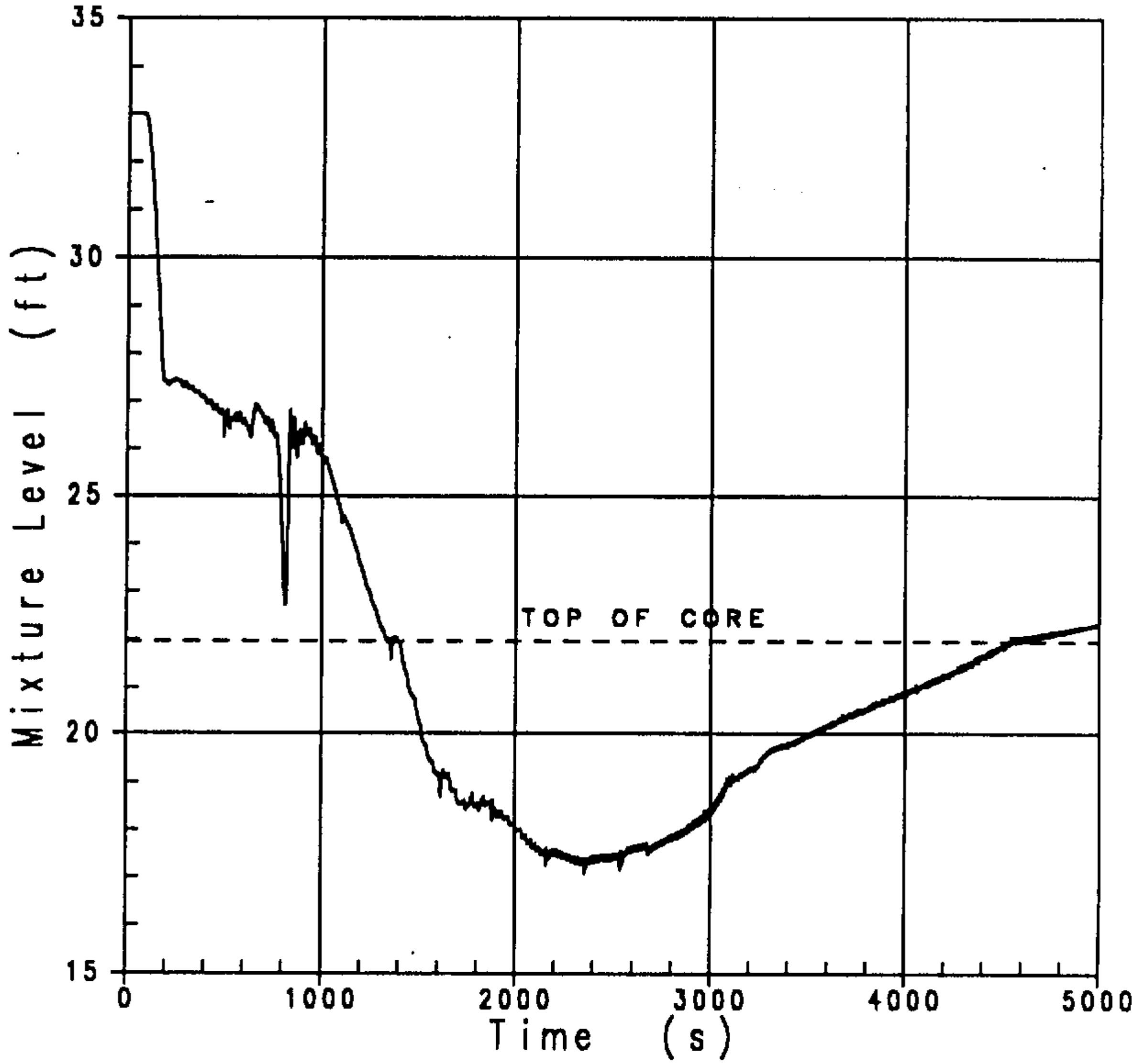
FLUID TEMPERATURE - HOT SPOT
3-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-12



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

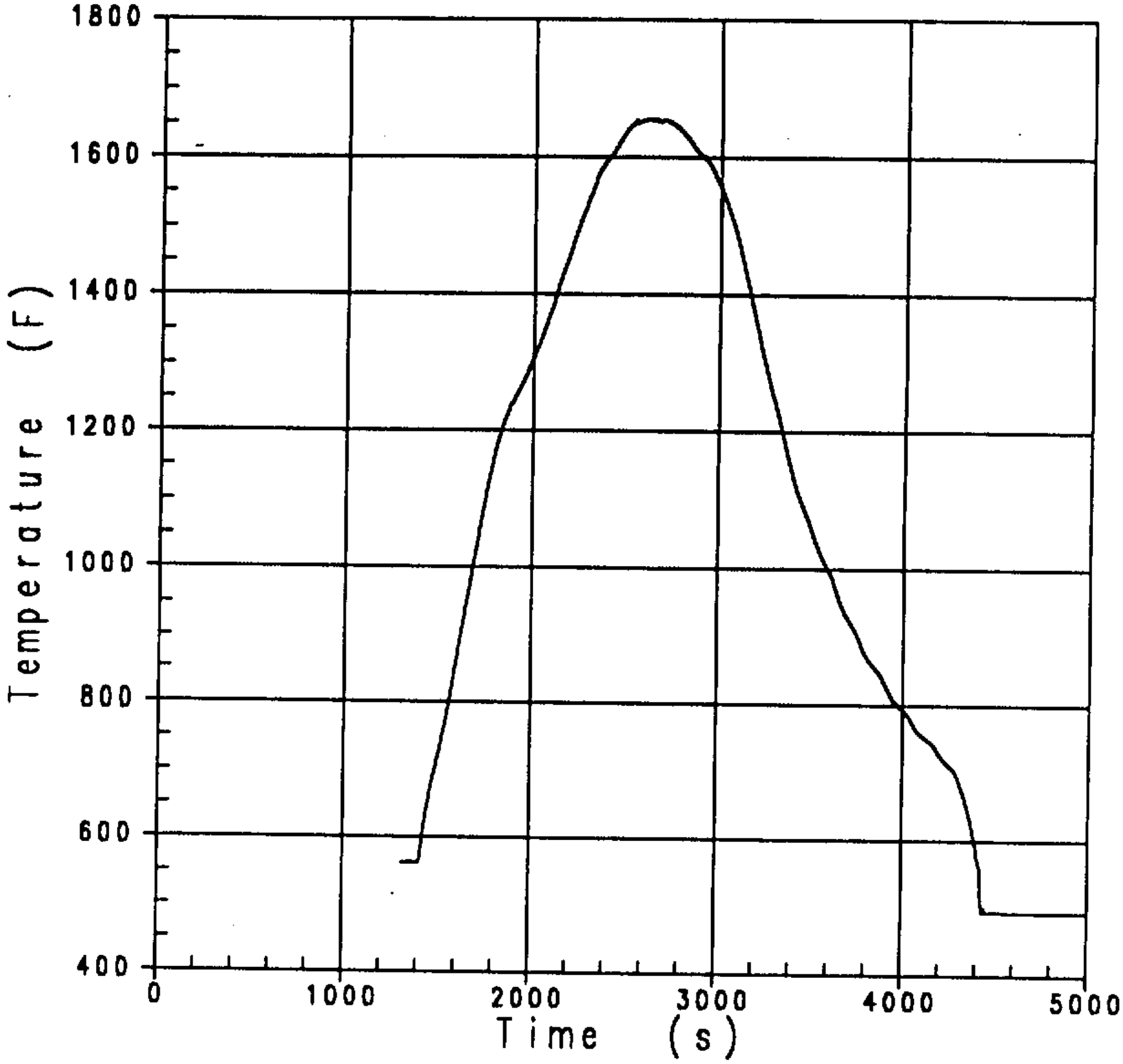
RCS DEPRESSURIZATION TRANSIENT
2-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-13



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CORE MIXTURE LEVEL
2-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-14

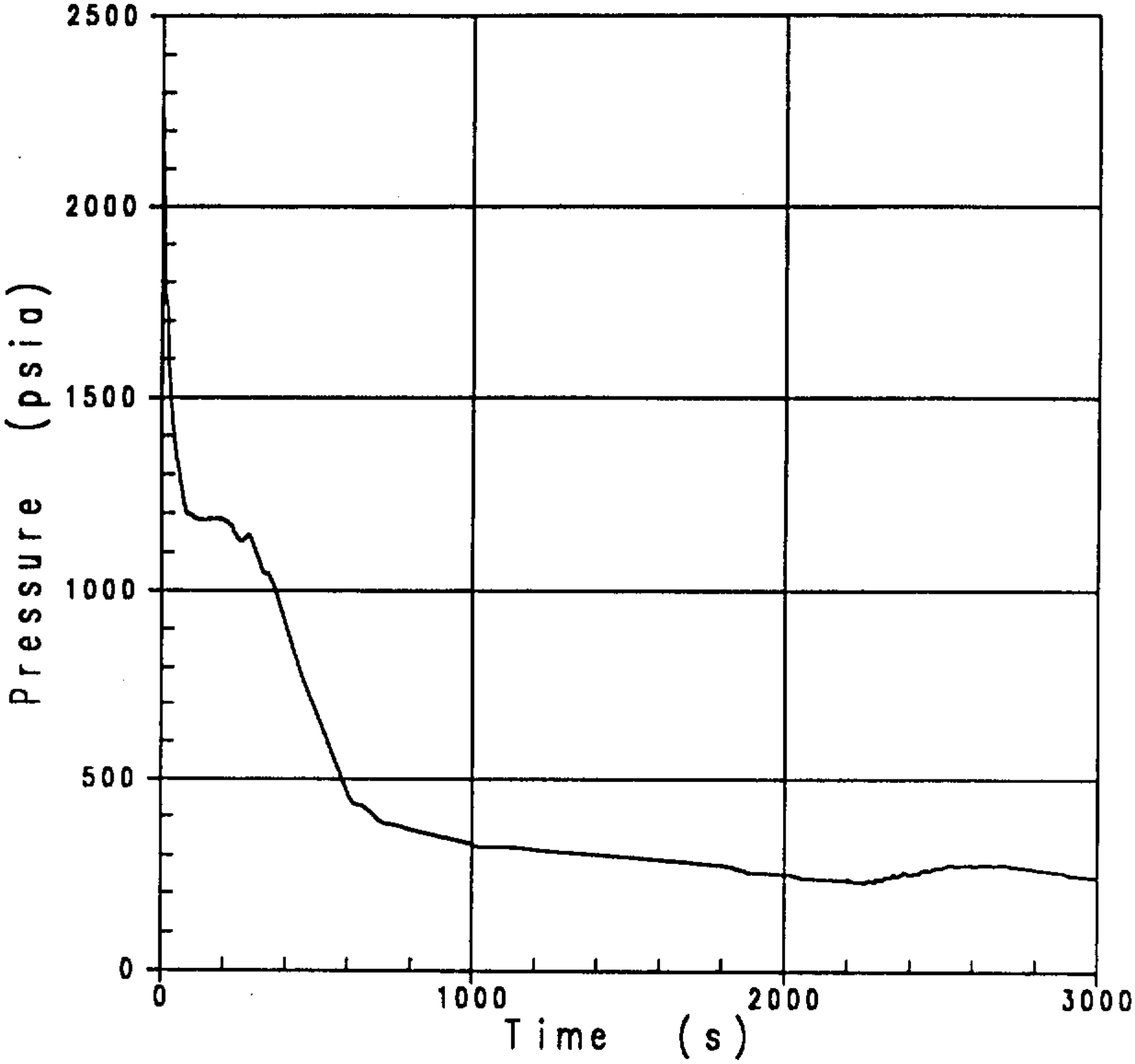


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

PEAK CLADDING TEMPERATURE - HOT ROD
2-INCH BREAK
WITH HIGH T_{AVG}

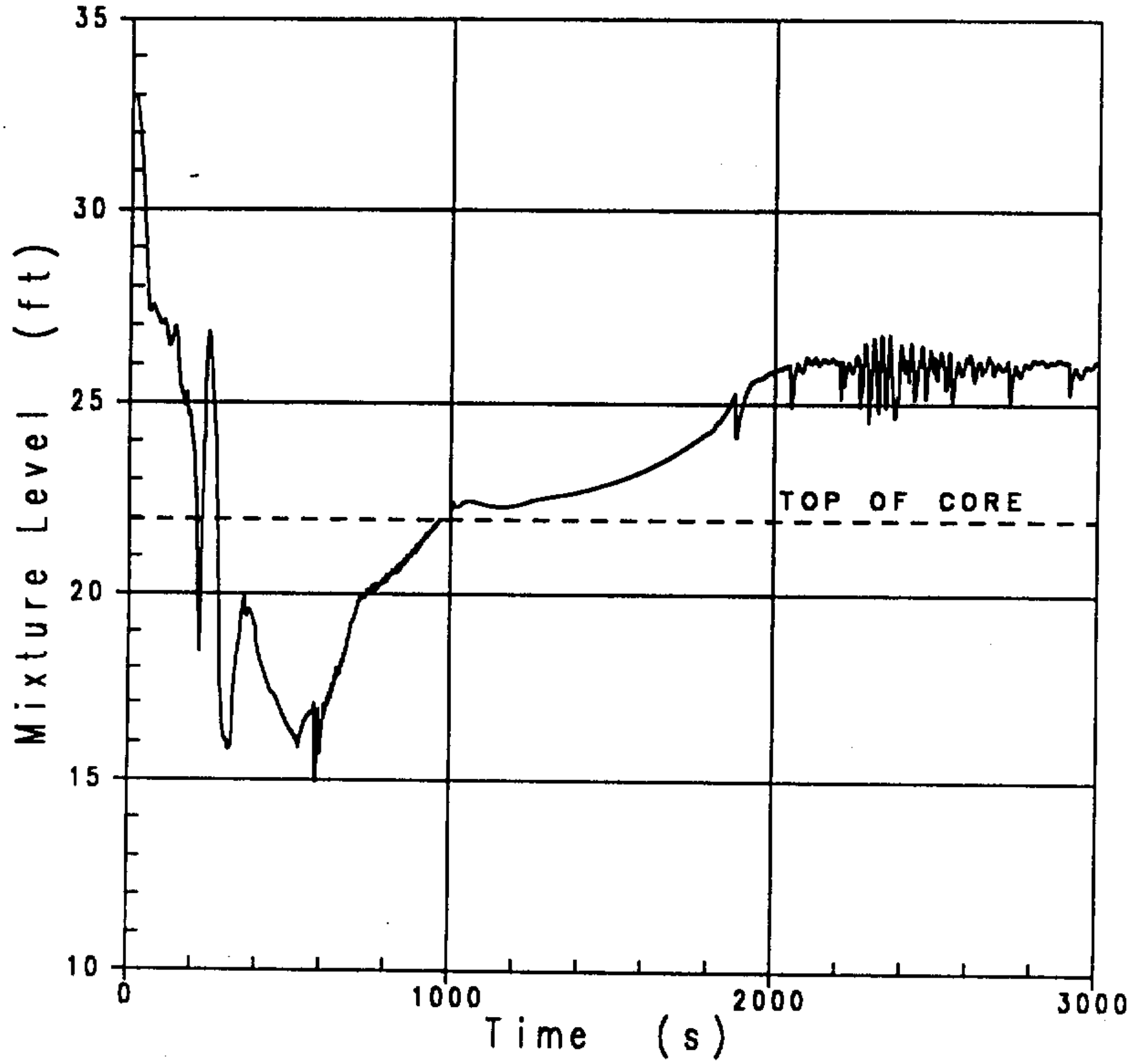
FIGURE 14.3.2.2-15



REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

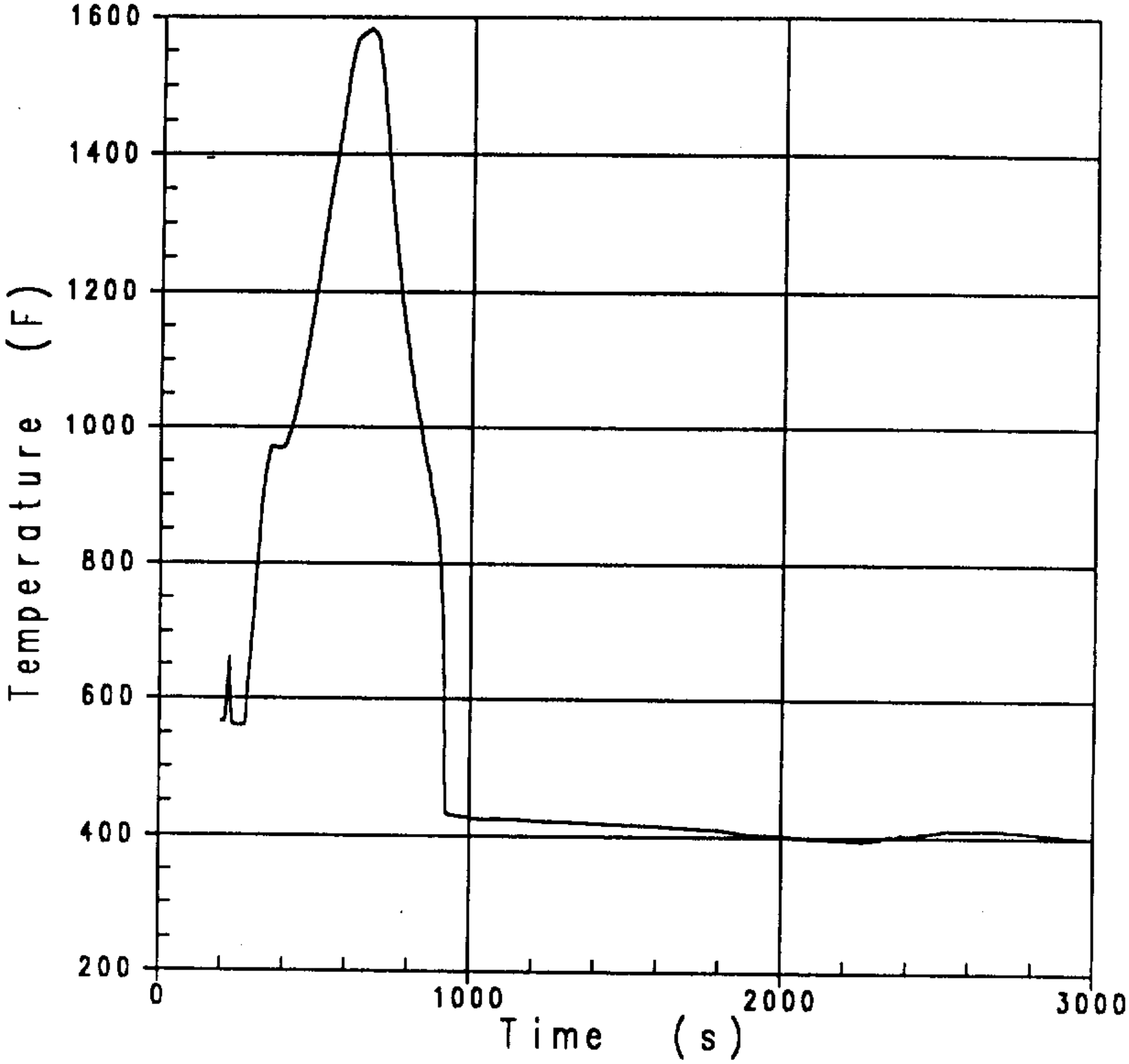
RCS DEPRESSURIZATION TRANSIENT
4-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-16



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

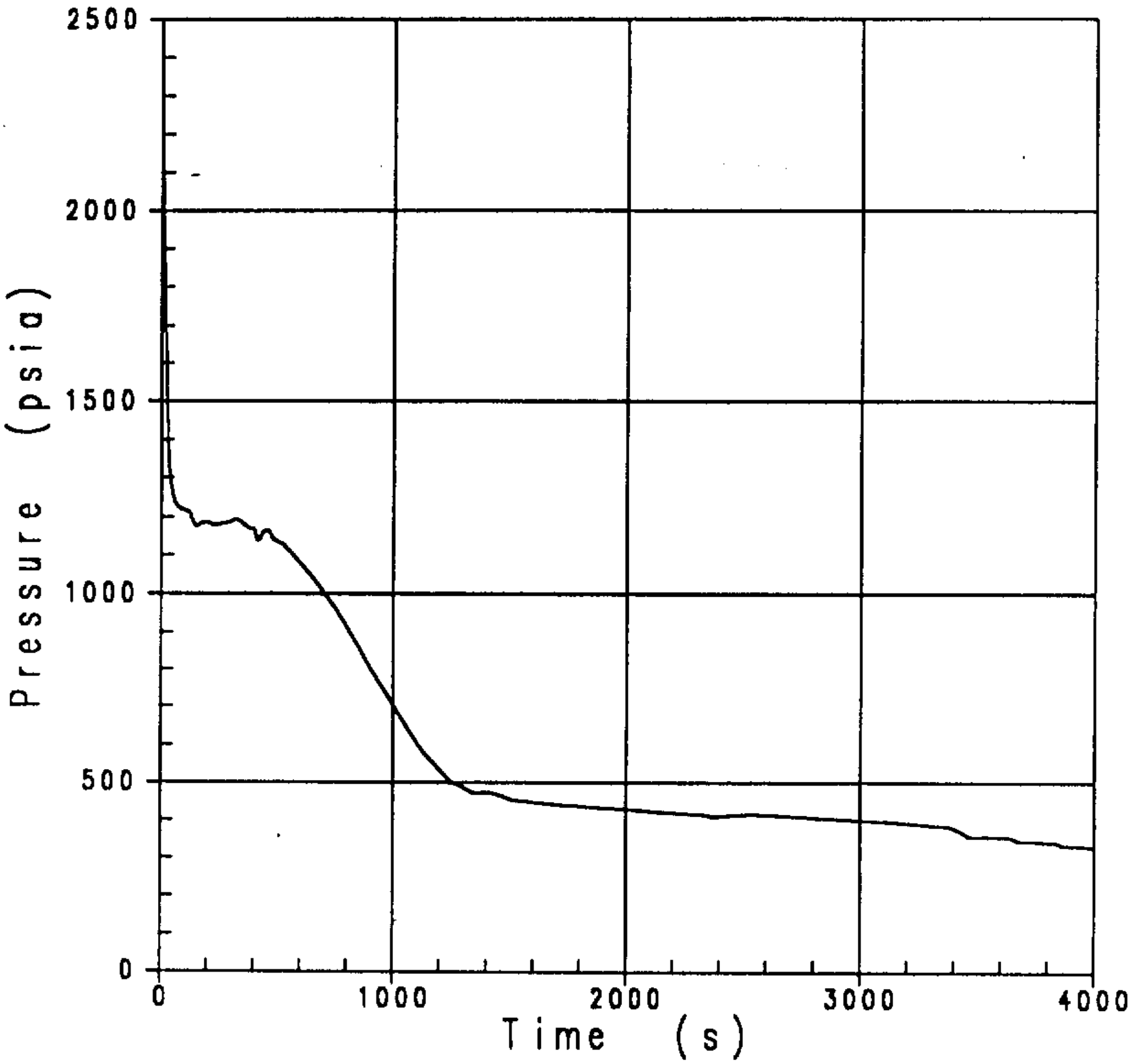
CORE MIXTURE LEVEL
4-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-17



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

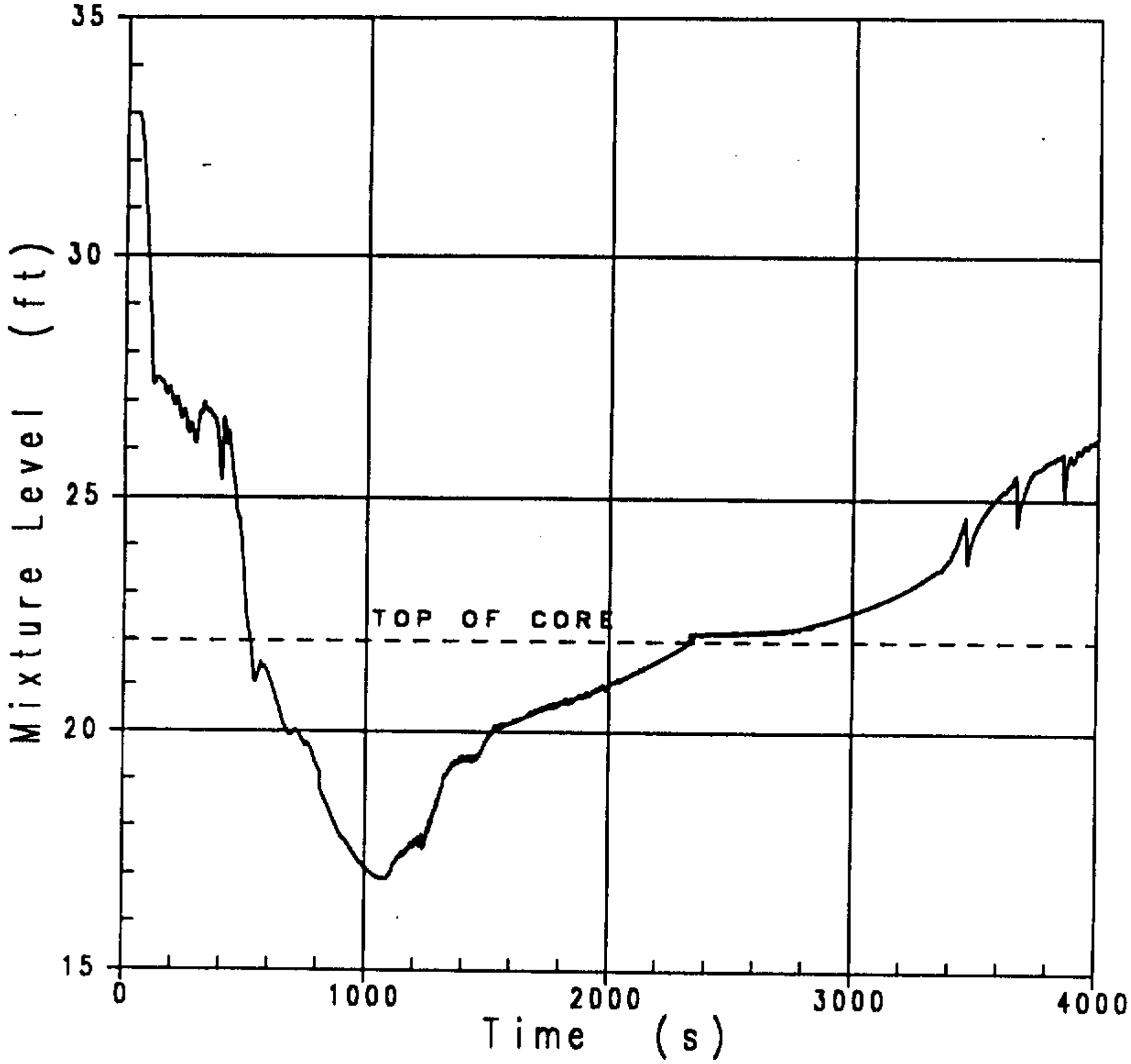
PEAK CLADDING TEMPERATURE - HOT ROD
4-INCH BREAK
WITH HIGH T_{AVG}
FIGURE 14.3.2.2-18



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

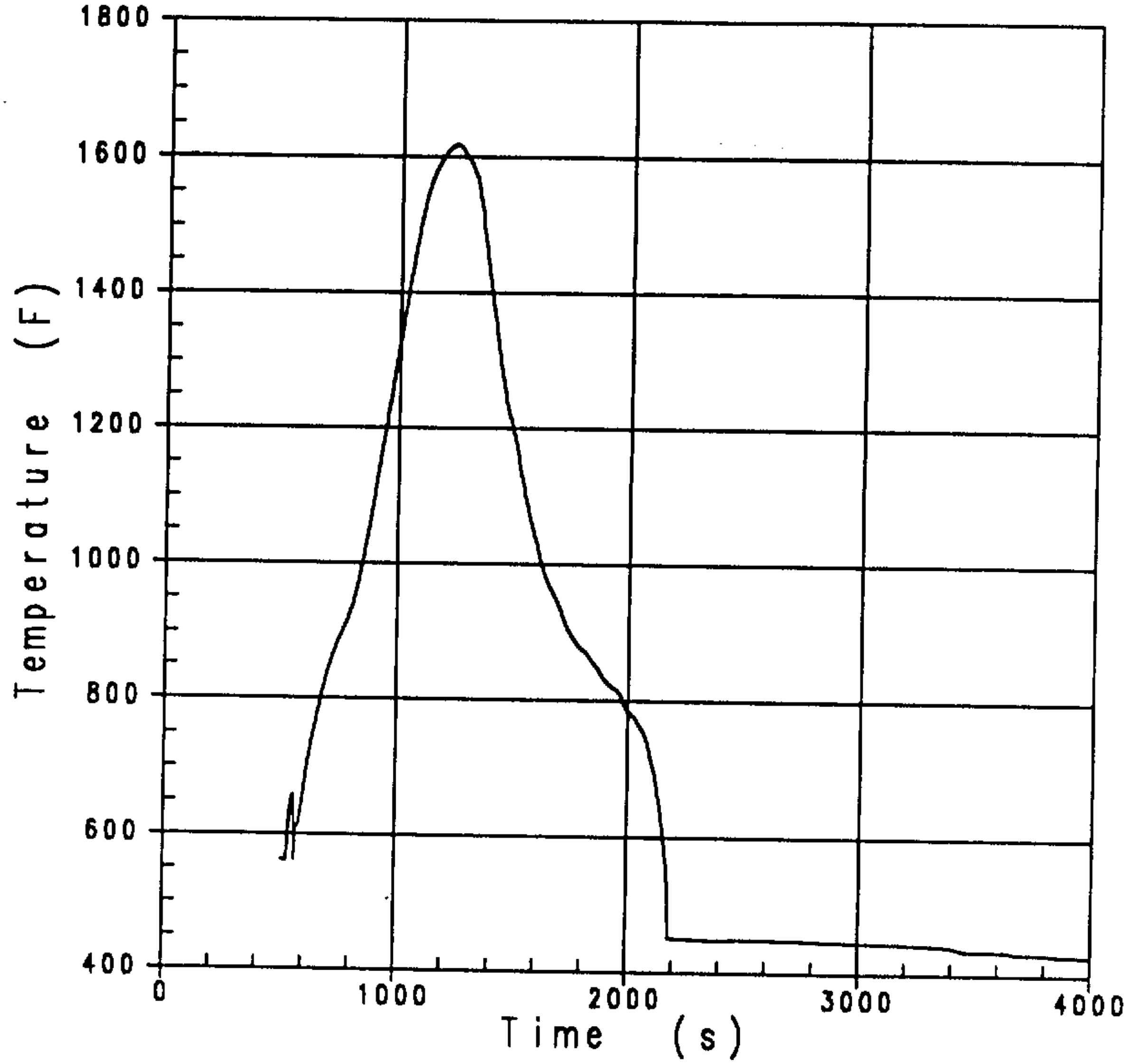
RCS DEPRESSURIZATION TRANSIENT
3-INCH BREAK
WITH LOW T_{AVG}
FIGURE 14.3.2.2-19



REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CORE MIXTURE LEVEL
3-INCH BREAK
WITH LOW T_{AVG}
FIGURE 14.3.2.2-20



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

PEAK CLADDING TEMPERATURE - HOT ROD
3-INCH BREAK
WITH LOW T_{AVG}
FIGURE 14.3.2.2-21

14.3.3 CORE AND INTERNALS INTEGRITY ANALYSIS

Internals Evaluation

The forces exerted on reactor internals and core, following a loss-of-coolant accident, are computed by employing the MULTIFLEX digital computer program developed for the space-time-dependent analysis of multi-loop PWR plants.

Design Criteria

The criteria for acceptability are that the core should be coolable and intact following a pipe rupture up to and including a double ended rupture of the Reactor Coolant System. This implies that core cooling and adequate core shutdown must be assured. Consequently, the limitations established on the internals are concerned principally with the maximum allowable deflections and/or stability of the parts.

Critical Internals

Upper Barrel

The upper barrel deformation has the following limits:

To assure reactor trip and to avoid disturbing the RCC guide structure, the barrel should not interfere with any guide tubes. This condition requires a stability check to assure that the barrel will not buckle under the accident loads.

RCC Guide Tubes

The RCC guide tubes in the upper core support package have the following allowable limits. Tests on guide tubes show that when the transverse deflection of the guide tube becomes significant, the cross section of the RCC guide tube changes. An allowable transient maximum transverse deflection of 1.0 inch has been established for the blowdown accident. Beam deflections above these limits produce cross section changes with increasing delay in scram time until the control rod will not scram due to interference between the rods and the guide. The no loss of function limit is established as 1.75 inches. With a maximum transient transverse deflection of 1.75 inches, the cross section distortion will not exceed 0.072", after load removal. This cross section distortion allows control rod insertion. For a maximum transient transverse deflection of 1.0 inch, a cross section distortion not in excess of 0.035" is anticipated.

Fuel Assemblies

The limitations for this case are related to the stability of the thimbles at the upper end. During the accident, the fuel assembly will have a vertical displacement and could touch the upper package subjecting the components to dynamic stresses.

The upper end of the thimbles shall not experience stresses above the buckling compressive stresses because any buckling of the upper end of the thimbles will distort the guide line and could affect the fall of the control rod.

Upper Package

The maximum allowable local deformation of the upper core plate where a guide tube is located is 0.100 inch. This deformation will cause the plate to contact the guide tube since the clearance between plate and guide tube is 0.100 inch. This limit will prevent the guide tubes from being put in compression. In order to maintain the straightness of the guide tube a maximum allowable total deflection of 1" for the upper support plate and deep beam has been established. The corresponding no loss of function deflection is above 2".

<u>Allowable Stress Criteria</u>

The allowable stress criteria fall into two categories dependent upon the nature of the stress state: membrane or bending. A direct state of stress (membrane) has a uniform stress distribution over the cross section. The allowable (maximum) membrane or direct stress is taken to be equal to the stress corresponding to 0.2 of the uniform material strain or the yield strength, whichever is higher. For unirradiated 304 stainless steel at operating temperature the stress corresponding to 20% of the uniform strain is:

$$(S_m)_{allowable} = 39500 \text{ psi}$$

For irradiated materials, the limit stress is higher.

For a bending state of stress, the strain is linearly distributed over a cross-section. The average strain value is, therefore, one half of the outer fiber strain where the stress is a maximum. Thus, by requiring the average strain to satisfy an allowable criterion similar to that for the direct state of stress, the outer fiber strain may be 0.4 times the uniform strain. The maximum allowable outer fiber bending stress is then taken to be equal to the stress corresponding to 40% of the uniform strain or the yield strength, whichever is higher. For unirradiated 304 stainless steel at operating temperature, we obtain from the stress strain curve:

$$(S_b)_{allowable} = 50{,}000 \text{ psi}$$

For combinations of membrane and bending stresses, the maximum allowable stress is taken to be equal to the stress corresponding to the maximum outer fiber strain not in excess of 40% uniform strain and average strain not in excess of 20% uniform strain.

In comparing this criterion with the concept of fully plastic moment, the shape factors for rectangular cross section in Resistance of Materials, by Seely and Smith (Wiley, 1956) p. 232 is:

$$\frac{\sigma_2}{\sigma_1} = 1.5$$

where σ_1 = maximum allowable stress for pure axial tension.

σ_2 = fictitious outer fiber stress assuming linear stress distribution in the cross section, under the fully plastic moment.

For the faulted condition, the ratio adopted is 50,000/39,500 = 1.25 which is less than the real shape factor of 1.5.

The reference made to corresponding strains when the allowable stresses are selected (0.2 ϵ_u and 0.4 ϵ_u) is directed primarily to show margins.

Blowdown and Force Analysis

Blowdown Model

The NRC approved MULTIFLEX computer program (Reference 1) was employed to generate the blowdown thermal-hydraulic transient in the primary reactor coolant system due to a postulated pipe rupture, or Loss-Of-Coolant-Accident (LOCA) in both the reactor coolant system hot and cold legs. The computer program considers subcooled, transition, and two-phase (saturated) blowdown regimes, employing the method of characteristics to solve the conservation laws, assuming one dimensional flow and a homogeneous liquid-vapor mixture. With its ability to model flow branches and a large number of nodes, MULTIFLEX has the required flexibility to represent various flow passages within the primary reactor coolant system. The reactor coolant system is divided into subregions in which the fluid flows along longitudinal axes. While each subregion is regarded as an equivalent pipe, a complex network of these equivalent pipes is used to represent the entire primary RCS.

A coupled fluid-structure interaction is incorporated into the analysis by accounting for the deflection of the constraining boundaries, which are represented by separate spring-mass oscillator systems. The reactor core barrel is modeled as an equivalent beam with the structural properties of the core barrel in a plane parrallel to the broken inlet nozzle. Horizontally, the barrel is divided into ten segments, with each segment consisting of three walls. Mass and stiffness matrices that are then calculated by applying the spatial pressure variation to the wall area at each of the elevations representative of the ten mass points of the beam model. The resultant core barrel motion is then translated into an equivalent change in flow area in each downcomer annulus flow channel. At every time increment, MULTIFLEX iterates between the hydraulic and structural subroutines for each location confined by a flexible wall.

Because of the applicability of leak-before-break licensing to the Turkey Point units, large double ended guillotine (DEG) breaks are excluded from the design basis and only limiting auxiliary line breaks are considered. For the Turkey Point units, the limiting auxiliary line breaks are the pressurizer surge line break on the hot leg and the accumulator line break on the cold leg. Postulated RHR auxiliary line breaks are bounded by the accumulator line break.

<u>Horizontal Force Model</u>

MULTIFLEX evaluates the pressure and velocity transients for a maximum of 2000 locations throughout the system. These pressure and velocity transients are stored as a computer file and are made available to the program FORCE-2 which utilizes a detailed geometric description in evaluating the vertical loading on the reactor internals.

Each reactor component for which force calculations are required is designated as an element and assigned an element number. Forces acting upon each of the elements are calculated summing the effects of:

1. The pressure differential across the element.

2. Flow stagnation on, and unrecovered orifice losses across the element.

3. Friction losses along the element.

Input to the code, in addition to the MULTIFLEX calculated blowdown pressure and velocity transients, includes the effective area of each element on which acts the vertical force due to the pressure differential across the element, a coefficient to account for flow stagnation and unrecovered orifice losses, and the total area of the element along which the shear forces act.

The horizontal forces on the vessel wall, core barrel, and thermal shield are computed using the LATFORC code.

During blowdown, significant asymmetrical loadings on the reactor vessel internals can be generated as a result of variation of the fluid pressure distribution in the downcomer annulus region. To determine these horizontal forces, LATFORC utilizes MULTIFLEX generated field pressures,together with geometric vessel information (component radial and axial lengths). In LATFORC, the downcomer annulus is subdivided into cylindrical segments, formed by dividing this region into circumferential and axial zones. The X (or Y) component of the hydraulic force acting on each segment is determined by multiplying the mean pressure acting over the segment by the X (or Y) projected segment area. In LATFORC, the X-axis coincides with the axis of the broken loop's inlet nozzle and the positive direction is directed away from this nozzle.

Vertical Excitation

Structural Model and Method of Analysis

The response of reactor internals components due to an excitation produced by complete severance of a reactor coolant loop pipe is analyzed. Assuming a double-ended pipe break occurs in a very short period of time, the rapid drop of pressure at the break produces a disturbance which propagates along the reactor coolant loop and excites the internal structure.

The internal structure is simulated by a multi-mass system connected with springs and dashpots representing the viscous damping due to structural and impact losses. The gaps between various components, as well as Coulomb type of friction, is also incorporated into the overall model. Since the fuel elements in the fuel assemblies are kept in position by friction forces originating from the preloaded fuel assembly grid fingers, any sliding that occurs between the fuel rods and assembly is considered as Coulomb type of friction. A series of mechanical model of local structures were developed and analyzed so that certain basic nonlinear phenomena previously mentioned could be understood. Using the results of these models, a final eleven-mass model is adopted to represent the internals structure under vertical excitation. Figure 14.3.3-1 is a schematic representation of the internals structures. The eleven-mass model is shown in Figure 14.3.3-2. A comparison between Figure 14.3.3-1 and 14.3.3-2 shows the parallel between the plant and the model. The modeling is conducted in such a way that uniform masses are lumped into easily identifiable discrete masses while elastic elements are represented by springs. A legend for the different masses is given in Table 14.3.3-1. The masses are readily recognized as Items W1 through W11. The core barrel and the lower package are easily discernable. The fuel assemblies have been segregated into two groups. The majority of the fuel mass, W4, is indirectly connected to the deep beam structure represented by mass W8. There is also a portion of the fuel mass, W6, which connects through the long columns to the top plate. The stiffness of the top plate panels is represented by K8. The hold down spring, K1, is bolted-up between the flange of the deep beam structure and the core barrel flange with the preload, P1. After preloading the hold down spring, a clearance, G1, exists between the core barrel flange and the solid height of the hold down spring. Within the fuel assemblies, the fuel elements W4 and W6 are held in place by frictional contact with the grid spring fingers. Coulomb damping is provided in the analysis to represent this frictional restraint.

The analytical model is also provided with viscous terms to represent the structural damping of the elastic elements. The viscous dampers are represented by C1 through C11.

Restrictions are placed on the displacement amplitudes by specifying the free travel available to the dynamic masses. Available displacements are designated by symbols G1 through G8.

The displacements are tested during the solution of the problem to see if the available travel has been achieved. When the limit of travel has been attained, stops are engaged to arrest further motion of the dynamic masses. The stops or snubbers are designated by the symbols S1 through S11.

Contact with the snubbers results in some damping of the motion of the model. The impact damping of the snubbers is represented by the devices D1 through D11.

During the assembly of the reactor, bolt-up of the closure head presets the spring loading of the core barrel and the spring loading on the fuel assemblies. Since the fuel assemblies in the model have been segregated into two groups two preload values are provided in the analysis. Preload values P1, P3, and P5 represent the hold down spring preload on the core barrel and the top nozzle springs preload values on the fuel assemblies.

The formulation of the transient motion response problem with a digital computer programming were performed. The effects of an earthquake vertical excitation are also incorporated into the program.

In order to program the multi-mass system, the appropriate spring rates, weights, and forcing function for the various masses were determined. The spring rates and weights of the reactor components are calculated separately for each plant. The forcing functions for the masses are obtained from the LATFORC and FORCE-2 program described in the previous section. It calculates the transient forces on reactor internals during blowdown using transient pressures and fluid velocities.

For the blowdown analysis the forcing functions are applied directly to the various internal masses.

For the earthquake analysis of the reactor internals, the forcing function, which is simulated earthquake response, is applied to the multi-mass system at the ground connections (the reactor vessel). Therefore, the external excitation is transmitted to the internals through the springs at the ground connections.

Results

Analysis has been performed for similar unit designs for variations in rupture opening time, and for hot leg and cold leg breaks. The response of the structure to these excitations indicates that the vertical motion is irregular with peaks of very short duration. The deflections and motion of the reactor internals are limited by the solid height of lower core plate springs and by the hold down spring located above the barrel flange.

The internals behave as a nonlinear system during the vertical oscillations produced by the blowdown forces. The nonlinearities are due to the Coulomb frictional forces between grids and rods, and to gaps between components causing discontinuities in force transmission. The frequency response is consequently a function not only of the exciting frequencies in the system, but also of the amplitude. Different break conditions excite different frequencies in the system. This situation can be seen clearly when the response under blowdown forces is compared with the one due to vertical seismic acceleration. Under seismic excitation, the system behaves almost linearly because component motion is not sufficient to cause closing of the various gaps in the structure or slippage in the fuel rods.

Under certain blowdown excitation conditions the core moves upward, touches the core plate, and falls down on the lower structure causing oscillations in all the components. During the time that the oscillations occur and, depending on its initial position, the fuel rods slide on the fuel assembly. The response shows that the case could be represented as two large vibrating masses (the core and the barrel), the rest of the system oscillating with respect to the barrel and the core.

Damping effects have also been considered; it appears that the higher frequencies disappear rapidly after each impact or slippage.

The results of the computer program give not only the frequency response of the components, but also the maximum impact force and deflections. From these results, the stresses are computed using the standard "Strength of Material" formulas. The impact stresses are obtained in an analogous manner using the maximum forces seen by the various structures during impact.

Analysis of the response of the vessel internals under seismic exitation (vertical and horizontal), superimposed on that of the loading imposed by the limiting branch line break, has been done (Reference 2). Results show the internals are adequate to withstand blowdown forces.

Analysis of Effects of Loss of Coolant and Safety Injection on the Reactor Vessel and Internals

The following information was provided as part of the orginal plant licensing process and is considered historical in nature.

The analysis of the effects of injecting safety injection water into the reactor coolant system following a postulated loss of coolant accident are being incorporated into a WCAP report to be submitted to the AEC.

For the reactor vessel, three modes of failure are considered including the ductile mode, brittle mode and fatigue mode.

a) Ductile Mode - the failure criterion used for this evaluation is that there shall be no gross yielding across the vessel wall using the material yield stress specified in Section III of the ASME Boiler and Pressure Vessel Code. The combined pressure and thermal stresses during injection through the vessel thickness as a function of time have been calculated and compared to the material yield stress at the times during the safety injection transient.

   The results of the analyses showed that local yielding may occur in approximately the inner 12 per cent of the base metal and in the cladding.

b) Brittle Mode - the possibility of a brittle fracture of the irradiated core region has been considered from both a transition temperature approach and a fracture mechanics approach.

   The failure criteria used for the transition temperature evaluation is that a local flaw cannot propagate beyond any given point where the applied stress will remain below the critical propagation stress at the applicable temperature at that point.

   The results of the transition temperature analysis showed that the stress-temperature condition in the outer 65 per cent of the base metal wall thickness remains in the crack arrest region at all times during the safety injection transient. Therefore, if a defect were present in the most detrimental location and orientation (i.e., a crack on the inside surface and circumferentially directed) it could not propagate any farther than approximately 35 per cent of the wall thickness, even considering the worst case assumptions used in this analysis.

The results of the fracture mechanics analysis, considering the effects of water temperature, heat transfer coefficients and fracture toughness of the material as a function of time, temperature and irradiation will be included in the report. Both a local crack effect and a continuous crack effect have been considered with the latter requiring the use of a rigorous finite element axisymmetric code.

c) Fatigue Mode - the failure criterion used for the failure analysis was the one presented in Section III of the ASME Boiler and Pressure Vessel Code. In this method the piece is assumed to fail once the combined usage factor at the most critical location for all transients applied to the vessel exceeds the code allowable usage factor of one.

The results of this analysis showed that the combined usage factor never exceeded 0.2, even after assuming that the safety injection transient occurred at the end of plant life.

In order to promote a fatigue failure during the safety injection transient at the end of plant life, it has been estimated that a wall temperature of approximately 1100°F is needed at the most critical area of the vessel (instrumentation tube welds in the bottom head).

The design basis of the Safety Injection System ensures that the maximum cladding temperature does not exceed the melting temperature of the cladding. This is achieved by prompt recovery of the core through flooding, with the passive accumulator and the injection systems. Under these conditions, a vessel temperature of 1100°F is not considered a credible possibility and the evaluation of the vessel under such elevated temperatures is for a hypothetical case.

For the ductile failure mode, such hypothetical rise in the wall temperature would increase the depth of local yielding in the vessel wall.

The results of these analyses show that the integrity of the reactor vessel is never violated.

The safety injection nozzles have been designed to withstand ten postulated safety injection transients without failure. This design and associated analytical evaluation were made in accordance with the requirements of Section III of the ASME Boiler and Pressure Vessel Code.

The maximum calculated pressure plus thermal stress in the safety injection nozzle during the safety injection transient was calculated to be approximately 50,900 psi. This value compares favorably with the code allowable stress of 80,000 psi.

These ten safety injection transients are considered along with all the other design transients for the vessel in the fatigue analysis of the nozzles. This analysis showed the usage factor for the safety injection nozzles was 0.47 which is well below the code allowable value of 1.0.

The safety injection nozzles are not in the highly irradiated region of the vessel and thus they are considered ductile during the safety injection transient.

The effect of the safety injection water on the fuel assembly grid springs has been evaluated and due to the fact tht the springs have a large surface area to volume ratio, being in the form of thin strips, and are expected to follow the coolant temperature transient with very little lag, no thermal shock is expected and the core cooling is not compromised.

Evaluations of the core barrel and thermal shield have also shown that core cooling is not jeopardized under the postulated accident conditions.

An analysis has been made of the thermal stresses in the core support components. Analysis shows that the highest thermal stress case occurs in the core barrel. The barrel is affected by the cold water in the downcomer and the somewhat hotter water in the compartments between barrel and baffle, producing a thermal gradient across barrel wall. The lower support structure is cooled more uniformly because of the large and numerous flow holes and consequently thermal stresses are lower.

The method used to obtain the maximum barrel stresses is as follows:

1) temperature distribution across the barrel wall is computed as a function of time taking into consideration water temperatures and film coefficients.

2) assuming that the obtained thermal gradients are axisymetrically distributed, which is conservative for stresses, maximum thermal stresses are computed in the barrel considered as an infinite cylinder.

3) thermal stresses are added to primary stresses including seismic in order to obtain the maximum stress state of the barrel.

Results of studies performed for different conditions show that maximum thermal stresses in the barrel wall are below the allowable criteria given for design by Section III of the ASME Code.

REFERENCES

1. Takeuchi, K. et.al., "MULTIFLEX- A fortran-IV computer program for analyzing Thermal-Hydraulic- Structure System Dynamics," WCAP-8708-P-A, September 1977.

2. Amendment 18 to CP&L Application (FSAR Amendment 9) Docket 50-261.

TABLE 14.3.3-1

MULTI-MASS VIBRATIONAL MODEL-DEFINITION OF SYMBOLS

W1 - Core Barrel & Thermal Shield
W2 - Lower Package
W3 - Fuel Assemblies Major
W4 - Fuel Rods Major
W5 - Fuel Assemblies Minor
W6 - Fuel Rods Minor
W7 - Core Plate & Short Column
W8 - Deep Beam
W9 - Core Plate & Long Columns
W1O - Top Plate (Ctr.)
W11 - Core Barrel

Kl - Hold Down Spring
K2 - Lower Package Major
K3 - Top Nozzle Springs Major
K5 - Top Nozzle Springs Minor
K7 - Short Columns
K8 - Upper Core Plate
K9 - Long Columns
K1O - Top Plate
K11 - Core Barrel

Snubbers

S1 - Core Barrel Flange
S2 - Hold Down Spring
S3 - Top Nozzles Bars, Major
S4 - Pedestal Bars, Major
S5 - Top Nozzles Bars, Minor
S6 - Pedestal Bars, Minor
S7 - Top Nozzle Bumpers, Major
S8 - Top Nozzle Bumpers, Minor
S9 - Pedestals, Major
S10 - Pedestals, Minor
S11 - Deep Beam Flange

Impact Dampers

D1 - Barrel Flange
D2 - Hold Down Spring
D3 - Top Nozzle Bars, Major
D4 - Pedestal Bars, Major
D5 - Top Nozzle Bars, Minor
D6 - Pedestal Bars, Minor
D7 - Top Nozzles, Major
D8 - Top Nozzles, Minor
D9 - Pedestal, Major
D10 - Pedestal, Minor
D11 - Deep Beam Flange

Structural Dampers

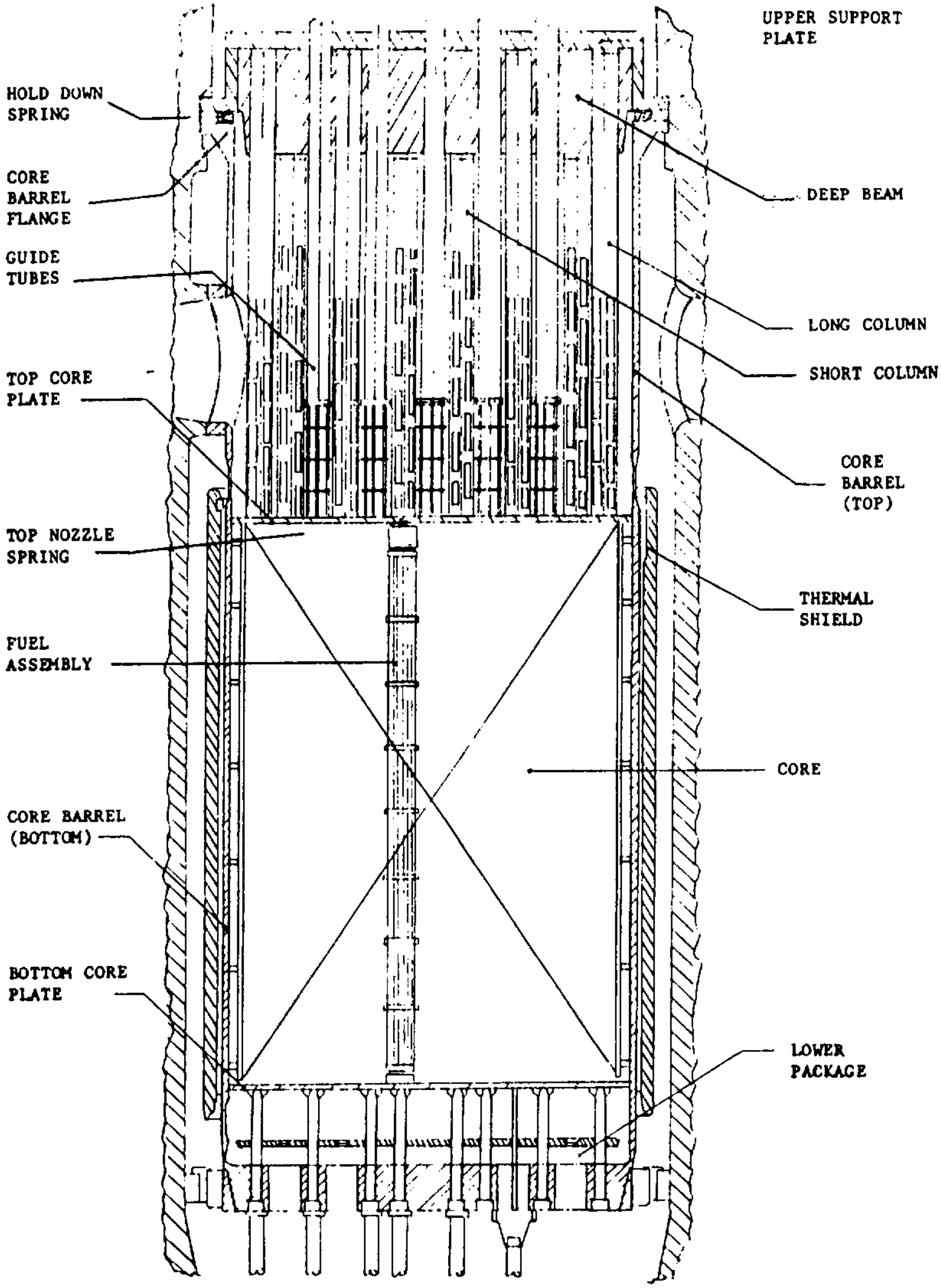
Cl - Hold Down Springs
C2 - Lower Package
C3 - Top Nozzle, Major
C5 - Top Nozzle, Minor
C7 - Short Columns
C8 - Upper Core Plate
C9 - Long Columns
C10 - Top Plate
Cll - Core Barrel

Clearances

Gl - Hold Down Spring
G3 - Fuel Rod Top, Major
G4 - Fuel Rod Bottom, Major
G5 - Fuel Rod Top, Minor
G6 - Fuel Rod Bottom, Minor
G7 - Fuel Assembly Major
G8 - Fuel Assembly Minor

Preloads

Pl - Hold Down Spring
P3 - Top Nozzle Springs Major
P5 - Top Nozzle Springs Minor



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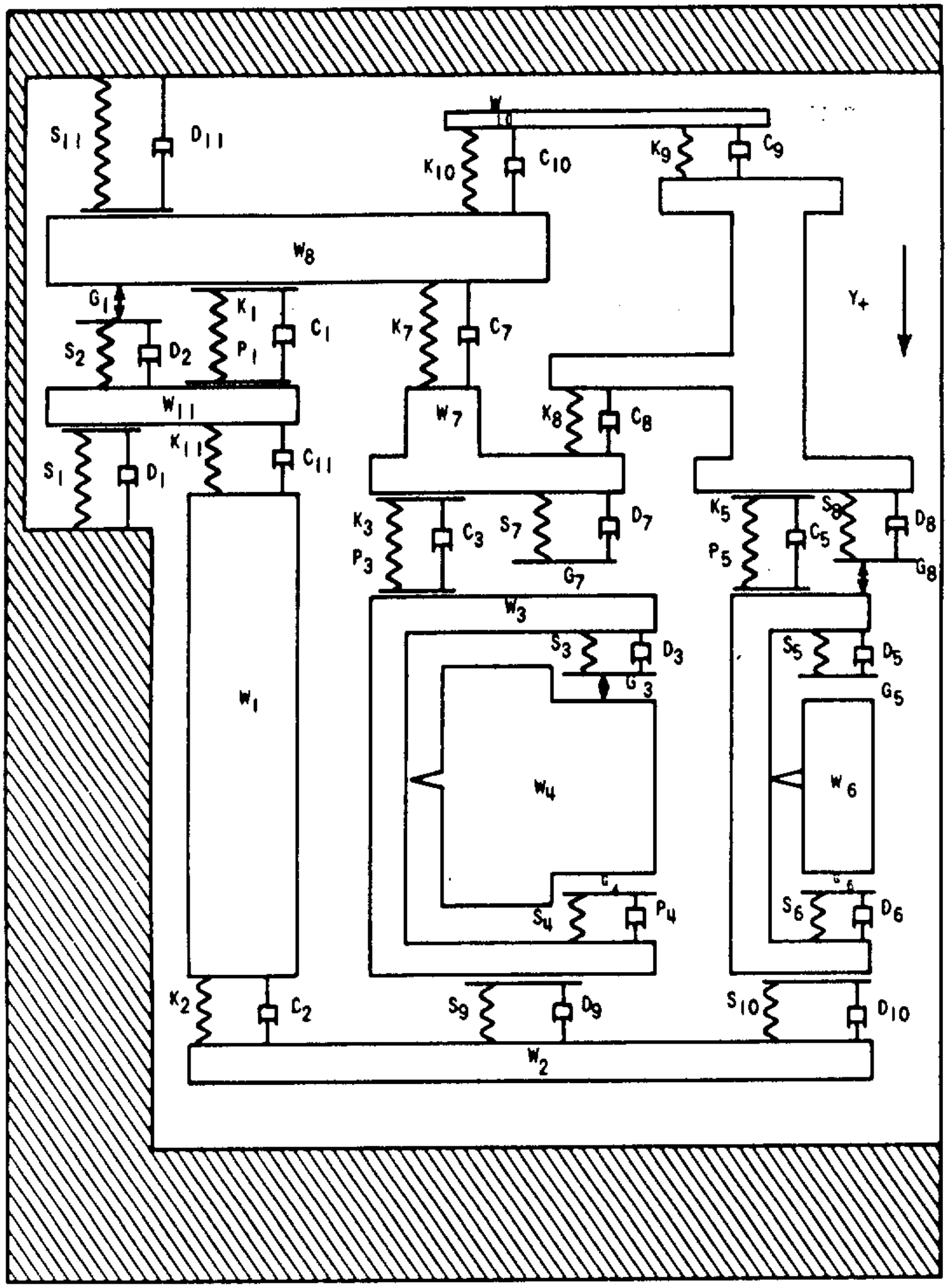
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

REACTOR VESSEL INTERNALS

FIGURE 14.3.3-1



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TURKEY POINT PLANT UNITS 3 & 4

MULTI-MASS VIBRATIONAL MODEL

FIGURE 14.3.3-2

14.3.4 CONTAINMENT INTEGRITY EVALUATION

Method of Analysis

The containment system is designed such that for all break sizes, up to and including the double-ended severance of a reactor coolant pipe or secondary system pipe, the containment peak pressure is below the design pressure with margin. This section details the mass and energy releases and resulting containment response subsequent to a hypothetical loss of coolant accident (LOCA) or a main steamline break (MSLB).

14.3.4.1 MASS AND ENERGY RELEASE ANALYSIS FOR POSTULATED LOCAs

14.3.4.1.1 INTRODUCTION

The purpose of this analysis was to calculate the long-term Loss-of-Coolant Accident (LOCA) mass and energy releases for the hypothetical double-ended pump suction (DEPS) rupture and double-ended hot leg (DEHL) rupture break cases with the uprated conditions for the Turkey Point Units 3 and 4 Thermal Uprating Program.

The uncontrolled release of pressurized high temperature reactor coolant, termed a LOCA, will result in release of steam and water into the containment. This, in turn, will result in an increase in the containment pressure and temperature. The mass and energy release rates described in this section form the basis of further computations to evaluate the structural integrity of the containment following a postulated accident (see Section 14.3.4.3).

14.3.4.1.2 INPUT PARAMETERS AND ASSUMPTIONS

The mass and energy release analysis is sensitive to the assumed characteristics of various plant systems, in addition to other key modeling assumptions. Some of the most-critical items are the reactor coolant system (RCS) initial conditions, core decay heat, safety injection flow, and primary and secondary metal mass and steam generator heat release modeling. Specific assumptions concerning each of these items are discussed below. Tables 14.3.4.1-1 and 14.3.4.1-2 present key data assumed in the analysis.

For the long-term mass and energy release calculations, operating temperatures which bound the highest average coolant temperature range were used as bounding analysis conditions. The modeled core rated power of 2346 MWt. which contains an adjustment for calorimetric error (+2 percent of power), was the basis in the analysis. The use of higher temperatures is conservative because the initial fluid energy is based on coolant temperatures which are at the maximum levels attained in steady state operation. Additionally, an allowance to account for instrument error and deadband is reflected in the initial RCS temperatures. The initial reactor coolant system (RCS) pressure in this analysis is based on a nominal value of 2250 psia plus an allowance which accounts for the measurement uncertainty on pressurizer pressure. The selection of 2250 psia as the limiting pressure is considered to affect the blowdown phase results only, since this represents the initial pressure of the RCS. The RCS rapidly depressurizes from this value until the point at which it equilibrates with containment pressure.

The rate at which the RCS blows down is initially more severe at the higher RCS pressure. Additionally the RCS has a higher fluid density at the higher pressure (assuming a constant temperature) and subsequently has a higher RCS mass available for releases. Thus, 2250 psia plus uncertainty was selected for the initial pressure as the limiting case for the long-term mass and energy release calculations.

The selection of the fuel design features for the long-term mass and energy calculation is based on the need to conservatively maximize the core stored energy. The margin in core stored energy was chosen to be +15 percent. Thus, the analysis very conservatively accounts for the stored energy in the core.

Margin in RCS volume of 3% (which is composed of 1.6% allowance for thermal expansion and 1.4% for uncertainty) is modeled.

Regarding safety injection flow, the mass and energy calculation considered configurations/failures to conservatively bound respective alignments. A spectrum of cases included:

1. Diesel Failure Case (1 HHSI, 1 RHR, & 1 CS Pump)
2. Containment Spray Pump Failure Case (2 HHSI, 2 RHR, & 1 CS Pump)
3. No Failure Case (2 HHSI, 2 RHR, & 2 CS Pumps).

The following assumptions were employed to assure that the mass and energy releases are conservatively calculated, thereby maximizing energy release to containment.

1. Maximum expected operating temperature of the reactor coolant system (100% full-power conditions).

2. An allowance in temperature for instrument error and dead-band (+7.4°F).

3. Margin in volume of 3% (which is composed of 1.6% allowance for thermal expansion, and 1.4% for uncertainty).

4. An allowance of +2 percent of power for calorimetric error; that is, 102% of core rated thermal power, 2346 MWt.

5. Conservative coefficient of heat transfer (i.e., steam generator primary/secondary heat transfer and reactor coolant system metal heat transfer).

6. Allowance in core stored energy for effect of fuel densification.

7. A margin in core stored energy (+15 percent included to account for manufacturing tolerances).

8. An allowance for RCS initial pressure uncertainty (+70 psi).

9. A maximum containment backpressure equal to design pressure.

10. Allowance for RCS flow uncertainty (-3.5%).

11. Steam generator tube plugging leveling (0% uniform)

    - Maximizes reactor coolant volume and fluid release.
    - Maximizes heat transfer area across the SG tubes.
    - Reduces coolant loop resistance, which reduces the Δp upstream of the break and increases break flow.

Thus, based on the previously discussed conditions and assumptions, a bounding analysis of Turkey Point Units 3 and 4 is made for the release of mass and energy from the RCS in the event of a LOCA at 2346 MWt.

14.3.4.1.3 DESCRIPTION OF ANALYSES

The evaluation model used for the long-term LOCA mass and energy release calculations was the March 1979 model described in Reference 1. This evaluation model has been reviewed and approved generically by the NRC. It has also been utilized and approved on the plant-specific dockets for other Westinghouse PWRs such as Catawba Units 1 and 2, Beaver Valley Unit 2, McGuire Units 1 and 2, Millstone Unit 3, Sequoyah Units 1 and 2, Watts Bar Units 1 and 2, Surry Units 1 and 2, and Indian Point Unit 2.

This section presents the long-term LOCA mass and energy releases that were generated in support of the Turkey Point Units 3 and 4 thermal uprating program. These mass and energy releases are then subsequently used in the containment integrity analysis presented in Section 14.3.4.3.

14.3.4.1.4 LOCA MASS AND ENERGY RELEASE PHASES

The containment system receives mass and energy releases following a postulated rupture in the RCS. These releases continue over a time period, which, for the LOCA mass and energy analysis, is typically divided into four phases.

1. Blowdown - the period of time from accident initiation (when the reactor is at steady state operation) to the time that the RCS and containment reach an equilibrium state.

2. Refill - the period of time when the lower plenum is being filled by accumulator and ECCS water. At the end of blowdown, a large amount of water remains in the cold legs, downcomer, and lower plenum. To conservatively consider the refill period for the purpose of containment mass and energy releases, it is assumed that this water is instantaneously transferred to the lower plenum along with sufficient

accumulator water to completely fill the lower plenum. This allows an uninterrupted release of mass and energy to containment. Thus, the refill period is conservatively neglected in the mass and energy release calculation.

3. Reflood - begins when the water from the lower plenum enters the core and ends when the core is completely quenched.

4. Post-reflood (Froth) - describes the period following the reflood transient. For the pump suction break, a two-phase mixture exits the core, passes through the hot legs, and is superheated in the steam generators. After the broken loop steam generator cools, the break flow becomes two phase.

14.3.4.1.5 COMPUTER CODES

The Reference 1 mass and energy release evaluation model is comprised of mass and energy release versions of the following codes: SATAN VI, WREFLOOD, and FROTH. These codes were used to calculate the long-term LOCA mass and energy releases for Turkey Point Units 3 and 4.

The SATAN VI code calculates blowdown, the first portion of the thermal-hydraulic transient following break initiation, including pressure, enthalpy, density, mass and energy flowrates, and energy transfer between primary and secondary systems as a function of time.

The WREFLOOD code addresses the portion of the LOCA transient where the core reflooding phase occurs after the primary coolant system has depressurized (blowdown) due to the loss of water through the break and when water supplied by the Emergency Core Cooling refills the reactor vessel and provides cooling to the core. The most-important feature is the steam/water mixing model (refer to Section 14.3.4.1.8.2).

FROTH models the post-reflood portion of the transient. The FROTH code is used for the steam generator heat addition calculation from the broken and intact loop steam generators.

14.3.4.1.6 BREAK SIZE AND LOCATION

Generic studies have been performed with respect to the effect of postulated break size on the LOCA mass and energy releases. The double ended guillotine break has been found to be limiting due to larger mass flow rates during the blowdown phase of the transient. During the reflood and froth phases, the break size has little effect on the releases.

Three distinct locations in the reactor coolant system loop can be postulated for pipe rupture:

1. Hot leg (between vessel and steam generator).
2. Cold leg (between pump and vessel).
3. Pump suction (between steam generator and pump).

The break locations analyzed for this program are the double-ended pump suction (DEPS) rupture (10.48 ft^2), and the double-ended hot leg (DEHL) rupture (9.19 ft^2). Break mass and energy releases have been calculated for the blowdown, reflood, and post-reflood phases of the LOCA for the DEPS cases. For the DEHL case, the releases were calculated only for the blowdown. The following information provides a discussion on each break location.

The DEHL rupture has been shown in previous studies to result in the highest blowdown mass and energy release rates. Although the core flooding rate would be the highest for this break location, the amount of energy released from the steam generator secondary is minimal because the majority of the fluid which exits the core bypasses the steam generators venting directly to containment. As a result, the reflood mass and energy releases are reduced significantly as compared to either the pump suction or cold leg break locations where the core exit mixture must pass through the steam generators before venting through the break. For the hot leg break, generic studies have confirmed that there is no reflood peak (i.e., from the end of the blowdown period the containment pressure would continually decrease). Therefore, only the mass and energy releases for the hot leg break blowdown phase are calculated and presented in this section.

The cold leg break location has also been found in previous studies to be much less limiting in terms of the overall containment energy releases. The cold leg blowdown is faster than that of the pump suction break, and more mass is released into the containment. However, the core heat transfer is greatly reduced, and this results in a considerably lower energy release into containment. Studies have determine that the blowdown transient for the cold leg is, in general, less limiting than that for the pump suction break. During reflood, the flooding rate is greatly reduced and the energy release rate into the containment is reduced. Therefore, the cold leg break is not included in the scope of this uprating.

The pump suction break combines the effects of the relatively high core flooding rate, as in the hot leg break, and the addition of the stored energy in the steam generators. As a result, the pump suction break yields the highest energy flow rates during the post-blowdown period by including all of the available energy of the Reactor Coolant System in calculating the releases to containment.

14.3.4.1.7 APPLICATION OF SINGLE-FAILURE CRITERION

An analysis of the effects of the single-failure criterion has been performed on the mass and energy release rates for each break analyzed. An inherent assumption in the generation of the mass and energy release is that offsite power is lost. This results in the actuation of the emergency diesel generators, required to power the safety injection system. This is not an issue for the blowdown period which is limited by the DEHL break.

Three cases have been analyzed for the effects of a single failure. The first case postulated the single failure as the loss of an emergency diesel generator. This results in the loss of one pumped safety injection train. The second case is the assumed failure of a containment spray pump. As compared to the first case, the SI flow would be greater and the time of RWST depletion would be earlier. For the third case, no failure is postulated to occur that would impact the amount of ECCS flow. The analysis of the cases described provides confidence that the effect of credible single failures is bounded.

14.3.4.1.8 MASS AND ENERGY RELEASE DATA

14.3.4.1.8.1 BLOWDOWN MASS AND ENERGY RELEASE DATA

A version of the SATAN-VI code is used for computing the blowdown transient. The code utilizes the control volume (element) approach with the capability for modeling a large variety of thermal fluid system configurations. The fluid properties are considered uniform and thermodynamic equilibrium is assumed in each element. A point kinetics model is used with weighted feedback effects. The major feedback effects include moderator density, moderator temperature, and Doppler broadening. A critical flow calculation for subcooled (modified Zaloudek), two-phase (Moody), or superheated break flow is incorporated into the analysis. The methodology for the use of this model is described in Reference 1.

Table 14.3.4.1-3 presents the calculated mass and energy release for the blowdown phase of the DEHL break. For the hot leg break mass and energy release tables, break path 1 refers to the mass and energy exiting from the reactor vessel side of the break; break path 2 refers to the mass and energy exiting from the steam generator side of the break.

Table 14.3.4.1-6 presents the calculated mass and energy releases for the blowdown phase of the DEPS break. For the pump suction breaks, break path 1 in the mass and energy release tables refers to the mass and energy exiting from the steam generator side of the break; break path 2 refers to the mass and energy exiting from the pump side of the break.

14.3.4.1.8.2 REFLOOD MASS AND ENERGY RELEASE DATA

The WREFLOOD code is used for computing the reflood transient. The WREFLOOD code consists of two basic hydraulic models - one for the contents of the reactor vessel, and one for the coolant loops. The two models are coupled through the interchange of the boundary conditions applied at the vessel outlet nozzles and at the top of the downcomer. Additional transient phenomena such as pumped safety injection and accumulators, reactor coolant pump performance, and steam generator release are included as auxiliary equations which interact with the basic models as required. The WREFLOOD code permits the capability to calculate variations during the core reflooding transient of basic parameters such as core flooding rate, core and downcomer water levels, fluid thermodynamic conditions (pressure, enthalpy, density) throughout the primary system, and mass flow rates through the primary system. The code permits hydraulic modeling of the two flow paths available for discharging steam and entrained water from the core to the break; i.e., the path through the broken loop and the path through the unbroken loops.

A complete thermal equilibrium mixing condition for the steam and emergency core cooling injection water during the reflood phase has been assumed for each loop receiving ECCS water. This is consistent with the usage and application of the Reference 1 mass and energy release evaluation model, in recent analyses, e.g., D.C. Cook docket (Reference 2). Even though the Reference 1 model credits steam/mixing only in the intact loop and not in the broken loop, justification, applicability, and NRC approval for using the mixing model in the broken loop has been documented (Reference 1). This assumption is justified and supported by test data, and is summarized as follows.

The model assumes a complete mixing condition (i.e., thermal equilibrium) for the steam/water interaction. The complete mixing process, however, is made up of two distinct physical processes. The first is a two-phase interaction with condensation of steam by cold ECCS water. The second is a single-phase mixing of condensate and ECCS water. Since the steam release is the most-important influence to the containment pressure transient, the steam condensation part of the mixing process is the only part that need be considered. (Any spillage directly heats only the sump.)

The most-applicable steam/water mixing test data has been reviewed for validation of the containment integrity reflood steam/water mixing model. This data is that generated in 1/3-scale tests (Reference 3), which are the largest scale data available and thus most-clearly simulates the flow regimes and gravitational effects that would occur in a PWR. These tests were designed specifically to study the steam/water interaction for PWR reflood conditions.

From the entire series of 1/3-scale tests, a group corresponds almost directly to containment integrity reflood conditions. The injection flowrates for this group cover all phases and mixing conditions calculated during the reflood transient. The data from these tests were reviewed and discussed in detail in Reference 1. For all of these tests, the data clearly indicate the occurrence of very effective mixing with rapid steam condensation. The mixing model used in the containment integrity reflood calculation is therefore wholly supported by the 1/3-scale steam/water mixing data.

Additionally, the following justification is also noted. The post-blowdown limiting break for the containment integrity peak pressure analysis is the pump suction double ended rupture break. For this break, there are two flowpaths available in the RCS by which mass and energy may be released to containment. One is through the outlet of the steam generator, the other via reverse flow through the reactor coolant pump. Steam which is not condensed by ECCS injection in the intact RCS loops passes around the downcomer and through the broken loop cold leg and pump in venting to containment. This steam also encounters ECCS injection water as it passes through the broken loop cold leg, complete mixing occurs and a portion of it is condensed. It is this portion of steam which is condensed that is taken credit for in this analysis. This assumption is justified based upon the postulated break location, and the actual physical presence of the ECCS injection nozzle. A description of the test and test results is contained in References 1 and 3.

Table 14.3.4.1-7 presents the calculated mass and energy release for the reflood phase of the pump suction double-ended rupture with a single limiting failure of a diesel generator. This failure case was the most-limiting for the LOCA containment integrity analysis (see Section 14.3.4.3) for the post-blowdown phase. Other failure scenarios were analyzed, but since the diesel failure is the most-limiting it will be presented. The other scenarios that were considered were a spray-pump-failure case and a no-safeguards-failure case.

The transients of the principal parameters during reflood are given in Table 14.3.4.1-8 for the DEPS diesel-failure case.

14.3.4.1.8.3 POST-REFLOOD MASS AND ENERGY RELEASE DATA

The FROTH code (Reference 4) is used for computing the post-reflood transient. The FROTH code calculates the heat release rates resulting from a two-phase mixture level present in the steam generator tubes. The mass and energy releases that occur during this phase are typically superheated due to the depressurization and equilibration of the broken loop and intact loop steam generators. During this phase of the transient, the RCS has equilibrated with the containment pressure, but the steam generators contain a secondary inventory at an enthalpy that is much higher than the primary side. Therefore, there is a significant amount of reverse heat transfer that occurs. Steam is produced in the core due to core decay heat. For a pump suction break, a two-phase fluid exits the core, flows through the hot legs and becomes superheated as it passes through the steam generator. Once the broken loop cools, the break flow becomes two phase. The methodology for the use of this model is described in Reference 1. The mass and energy release rates are calculated by FROTH until the time of containment depressurization. After containment depressurization (14.7 psia), the mass and energy release available to containment is generated directly from core boiloff/decay heat.

Table 14.3.4.1-9 presents the two-phase post-reflood (FROTH) mass and energy release data for the DEPS diesel-failure case.

14.3.4.1.8.4 DECAY HEAT MODEL

On November 2, 1978, the Nuclear Power Plant Standards Committee (NUPPSCO) of the American Nuclear Society approved ANS standard 5.1 (Reference 5) for the determination of decay heat. This standard was used in the mass and energy release model.

Significant assumptions in the generation of the decay heat curve for use in design basis containment integrity LOCA analyses include:

1. Decay heat sources considered are fission product decay and heavy element decay of U-239 and Np-239.

2. Decay heat power from fissioning isotopes other than U-235 is assumed to be identical to that of U-235.

3. Fission rate is constant over the operating history of maximum power level.

4. The factor accounting for neutron capture in fission products has been taken from Equation 11, of Reference 5 up to 10,000 seconds, and Table 10, of Reference 5 beyond 10,000 seconds.

5. The fuel has been assumed to be at full power for 10^8 seconds.

6. The number of atoms of U-239 produced per second has been assumed to be equal to 70% of the fission rate.

7. The total recoverable energy associated with one fission has been assumed to be 200 MeV/fission.

8. Two-sigma uncertainty (two times the standard deviation) has been applied to the fission product decay.

Based upon NRC staff review, Safety Evaluation Report (SER) of the March 1979 evaluation model, use of the ANS Standard-5.1, November 1979 decay heat model was approved for the calculation of mass and energy releases to the containment following a loss-of-coolant accident.

14.3.4.1.8.5 STEAM GENERATOR EQUILIBRATION AND DEPRESSURIZATION

Steam generator equilibration and depressurization is the process by which secondary side energy is removed from the steam generators in stages. The FROTH computer code calculates the heat removal from the secondary mass until the secondary temperature is Tsat at the containment design pressure. After the FROTH calculations, steam generator secondary energy is removed based on first and second stage rates. The first stage rate is applied until the steam generator reaches Tsat at the user specified intermediate equilibration pressure, when the secondary pressure is assumed to reach the actual containment pressure. Then the second stage rate is used until the final depressurization, when the secondary reaches the reference temperature of Tsat at 14.7 psia, or 212°F. The heat removal of the broken loop and intact loop steam generators are calculated separately.

During the FROTH calculations, steam generator heat removal rates are calculated using the secondary side temperature, primary side temperature and a secondary side heat transfer coefficient determined using a modified McAdam's correlation. Steam generator energy is removed during the FROTH transient until the secondary side temperature reaches saturation temperature at the containment design pressure. The constant heat removal rate used during the first heat removal stage is based on the final heat removal rate calculated by FROTH. The SG energy available to be released during the first stage interval is determined by calculating the difference in secondary energy available at the containment design pressure and that at the (lower) user specified intermediate equilibration pressure, assuming saturated conditions. This energy is then divided by the first stage energy removal rate, resulting in an intermediate equilibration time. At this time, the rate of energy release drops substantially to the second stage rate. The second stage rate is determined as the fraction of the difference in secondary energy available between the intermediate equilibration and final depressurization at 212°F, and the time difference from the time of the intermediate equilibration to the user specified time of the final depressurization at 212°F. With current methodology, all of the secondary energy remaining after the intermediate equilibration is conservatively assumed to be released by imposing a mandatory cooldown and subsequent depressurization down to atmospheric pressure at 3600 seconds, i.e., 14.7 psia and 212°F.

14.3.4.1.8.6 SOURCES OF MASS AND ENERGY

The sources of mass considered in the LOCA mass and energy release analysis are given in Table 14.3.4.1-10. These sources are the reactor coolant system, accumulators, and pumped safety injection.

The energy inventories considered in the LOCA mass and energy release analysis are given in Table 14.3.4.1-11. The energy sources include:

1. Reactor Coolant System Water

2. Accumulator Water

3. Pumped Injection Water

4. Decay Heat

5. Core Stored Energy

6. Reactor Coolant System Metal - Primary Metal (includes SG tubes)

7. Steam Generator Metal (includes transition cone, shell, wrapper, and other internals)

8. Steam Generator Secondary Energy (includes fluid mass and steam mass)

9. Secondary Transfer of Energy (feedwater into and steam out of the steam generator secondary)

Energy Reference Points

1. Available Energy: 212°F; 14.7 psia
2. Total Energy Content: 32°F; 14.7 psia

The mass and energy inventories are presented at the following times, as appropriate:

1. Time zero (initial conditions)

2. End of blowdown time

3. End of refill time

4. End of reflood time

5. Time of broken loop steam generator equilibration to pressure setpoint

6. Time of intact loop steam generator equilibration to pressure setpoint

7. Time of full depressurization (3600 seconds)

In the mass and energy release data presented, no Zirc-water reaction heat was considered, because the clad temperature is assumed not to rise high enough for the rate of the Zirc-water reaction heat to be of any significance.

14.3.4.1.9 CONCLUSIONS

The consideration of the various energy sources in the long-term mass and energy release analysis provides assurance that all available sources of energy have been included in this analysis. Thus, the review guidelines presented in Standard Review Plan Section 6.2.1.3 have been satisfied. Any other conclusions cannot be drawn from the generation of mass and energy releases directly since the releases are inputs to the containment integrity analyses. The containment response must be performed. See Section 14.3.4.3 for the LOCA containment integrity conclusions.

14.3.4.2 MASS AND ENERGY RELEASE ANALYSIS FOR POSTULATED SECONDARY SYSTEM PIPE RUPTURES INSIDE CONTAINMENT

14.3.4.2.1 INTRODUCTION

Steamline ruptures occurring inside a reactor containment structure may result in significant releases of high-energy fluid to the containment environment, possibly resulting in high containment temperatures and pressures. The quantitative nature of the releases following a steamline rupture is dependent upon the many possible configurations of the plant steam system and containment designs as well as the plant operating conditions and the size of the rupture. These variations make a reasonable determination of the single absolute worst case for both containment pressure and temperature evaluations following a steamline break difficult. The analysis considers a variety of postulated pipe breaks encompassing wide variations in plant operation, safety system performance, and break size in determining the containment response to a secondary system pipe rupture.

14.3.4.2.2 INPUT PARAMETERS AND ASSUMPTIONS

The postulated break area can have competing effects on blowdown results. Larger break areas will be more likely to result in large amounts of water being entrained in the blowdown. However, larger breaks also result in earlier generation of protective trip signals following the break and a reduction of both the power production by the plant and the amount of high-energy fluid available to be released to the containment.

To determine the effects of plant power level and break area on the mass and energy releases from a ruptured steamline, spectrums of both variables have been evaluated. At plant power levels of 102%, 70%, 30% and 0% of nominal full-load power, four break sizes have been defined. These break areas are defined as the following.

1. A full double-ended rupture (DER) downstream of the flow restrictor in one steamline. Note that a DER is defined as a rupture in which the steam pipe is completely severed and the ends of the break displace from each other.

2. A small break at the steam generator nozzle having an area just larger than that at which water entrainment occurs.

3. A small break at the steam generator nozzle having an area just smaller than that at which water entrainment occurs.

4. A small split rupture that will neither generate a steamline isolation signal from the Westinghouse Engineered Safety Features nor result in water entrainment in the break effluent.

The cases examined in this study were chosen based on the results of the analyses presented in Reference 6 for Turkey Point Units 3 and 4. The most-limiting case with respect to peak containment pressure was analyzed at the uprated power condition. Initial containment conditions for this limiting case were assumed to be +3.0 psig and 130°F. This case was a 1.4 ft^2 (based on the steam nozzle flow limiter cross-sectional area) DER at hot-zero-power (HZP) conditions. This DER steamline break was modeled assuming isolation is accomplished by the main steam isolation valve in each intact steamline. The important plant conditions and features that were assumed are discussed in the following paragraphs.

14.3.4.2.2.1 INITIAL POWER LEVEL

Steamline breaks can be postulated to occur with the plant in any operating condition ranging from hot shutdown to full power. Since steam generator mass decreases with increasing power level, breaks occurring at lower power levels will generally result in a greater total mass release to the containment. However, because of increased stored energy in the primary side of the plant, increased heat transfer in the steam generators, and additional energy generation in the fuel, the energy release to the containment from breaks postulated to occur during "at-power" operation may be greater than for breaks occurring with the plant in a hot-shutdown condition. Additionally, steam pressure and the dynamic conditions in the steam generators change with increasing power and have a significant influence on both the rate of blowdown and the amount of moisture entrained in the fluid leaving the break.

Because of the opposing effects (mass versus energy release) of changing power level on steamline break releases, no single power level can be singled out as a worst case initial condition for a steamline break event. Therefore, several different power levels spanning from full- to zero-power conditions have been investigated for Turkey Point Units 3 and 4 as discussed in Reference 6. For this power uprating analysis, only the power level corresponding to the steamline break mass and energy releases resulting in the limiting containment pressure response is included.

In general, the plant initial conditions are assumed to be at the nominal value corresponding to the initial power. Table 14.3.4.2-1 identifies the values assumed for RCS pressure, RCS vessel average temperature, pressurizer water volume, steam generator water level, and feedwater enthalpy corresponding to the limiting steamline break case analyzed.

14.3.4.2.2.2 SINGLE-FAILURE ASSUMPTION

To avoid unnecessary conservatism, bounding multiple failure assumptions were not made in the analysis. Only one single failure was considered in the analysis. The Main Steam Isolation Valve Assembly in each steamline consists of the main steam isolation valve (MSIV) and the main steam check valve (MSCV). The MSIV closes upon an isolation signal to terminate steam flow from the associated steam generator. The MSCV is designed to prevent reverse steam flow in the steamline, thus preventing blowdown from more than one steam generator for any break inside containment. However, if the MSCV in the faulted loop is assumed to fail, the intact steam generators would blow down through the break until the MSIVs in the intact loops close. This could result in significant additional mass and energy release to containment. The assumption that both the MSIV and the MSCV in the faulted loop fail exceeds the current UFSAR analysis assumptions. The intent of this assumption is to show that the protection logic which provides a signal to close the MSIVs, and the associated delay time, is adequate to limit the amount of steam mass and energy discharged into containment such that the containment pressure limit is not exceeded. To do this, no credit is taken for the proper functioning of the MSCV in preventing reverse steam flow from the intact steam generators.

14.3.4.2.2.3 MAIN FEEDWATER SYSTEM

Main feedwater flow was conservatively modeled by assuming an increase in feedwater flow in response to increases in steam flow following initiation of the steamline break. This maximizes the total mass addition prior to feedwater isolation. The steamline break case of Reference 6 which resulted in the limiting containment pressure response occurred from a hot-zero-power condition. During actual plant operation, the main feedwater valves are not in service at power levels up to approximately 15-20% of full power; rather, the 4-inch feedwater bypass valves are used to provide flow to the steam generators. The flows through the 4-inch feedwater bypass valves as a function of steam generator pressure were generated for both the faulted and the intact loops. The feedwater isolation response time was governed by the response time of the feedwater bypass valves and was assumed to be a total of 13 seconds following the safety injection signal.

Following feedwater isolation, as the steam generator pressure decreases, some of the fluid in the feedwater lines downstream of the isolation valve may flash to steam if the feedwater temperature exceeds the saturation pressure. This unisolable feedwater line volume is an additional source of high-energy fluid that was assumed to be discharged out of the break. The unisolable volume in the feedwater lines is maximized for the faulted loop and minimized for the intact loop. The following piping volumes available for steam flashing were calculated from plant drawings and assumed in the analysis.

- o Volume from SG nozzle to FCV (faulted loop) - 238 ft^3
- o Volume from SG nozzle to FCV (intact loops) - 75 ft^3/loop

14.3.4.2.2.4 AUXILIARY FEEDWATER SYSTEM

Generally, within the first minute following a steamline break, the auxiliary feedwater system will be initiated on any one of several protection system signals. Addition of auxiliary feedwater to the steam generators will increase the secondary mass available for release to containment as well as increase the heat transferred to the secondary fluid. The auxiliary feedwater flow control valves are set to supply a fixed flow to each steam generator, regardless of the back pressure in the steam generator. The maximum AFW flowrate has been determined to be 254 gpm/FCV (1 FCV per AFW train, 2 AFW trains per SG; therefore, the total AFW flowrate is 508 gpm/SG) for the first 120 seconds, decreasing to 140 gpm/FCV (total AFW flowrate is 280 gpm/SG) for the remainder of the event. A higher AFW flowrate to the faulted loop steam generator is conservative for the steamline break event; consequently, 254 gpm/FCV for 120 seconds decreasing to 140 gpm/FCV was assumed for the faulted loop steam generator AFW flowrate. Conversely, a lower AFW flowrate is conservative for the intact loop steam generators; thus, a constant 140 gpm/FCV was assumed for each intact loop for the entire transient.

14.3.4.2.2.5 STEAM GENERATOR FLUID MASS

Maximum initial steam generator masses in the faulted loop steam generator were used in both of the analyzed cases. The use of high initial steam generator masses maximizes the steam generator inventory available for release to containment. The initial masses were calculated as the mass corresponding to the programmed level +6% narrow range span. Minimum initial steam generator masses in the intact loops steam generators were used in both of the analyzed cases. The use of reduced initial steam generator masses minimizes the availability of the heat sink afforded by the steam generators on the intact loops. The initial masses were calculated as the mass corresponding to the programmed level -6% narrow range span. All steam generator fluid masses are calculated corresponding to 0% tube plugging which is conservative with respect to the RCS cooldown through the faulted loop steam generator resulting from the steamline break. The water mass defined by the unisolable portion of the steam generator blowdown recovery system is accounted for as part of an overall mass uncertainty applied to the steam generator initial conditions. This mass uncertainty is applied to both the faulted and intact steam generators and is in addition to the programmed 6% narrow range span level uncertainty previously mentioned.

14.3.4.2.2.6 STEAM GENERATOR REVERSE HEAT TRANSFER

Once the steamline isolation is complete, those steam generators in the intact steam loops become sources of energy which can be transferred to the steam generator with the broken line. This energy transfer occurs via the primary coolant. As the primary plant cools, the temperature of the coolant flowing in the steam generator tubes drops below the temperature of the secondary fluid in the intact steam generators resulting in energy being returned to the primary coolant. This energy is then available to be transferred to the steam generator with the broken steamline. The effects of reverse steam generator heat transfer are included in the results.

14.3.4.2.2.7 BREAK FLOW MODEL

Piping discharge resistances were not included in the calculation of the releases resulting from the steamline ruptures (Moody Curve for an $f(\ell/D) = 0$ was used).

14.3.4.2.2.8 CORE DECAY HEAT

Core decay heat generation assumed is based on the 1979 ANS Decay Heat + 2σ model (Reference 5).

14.3.4.2.2.9 STEAMLINE VOLUME BLOWDOWN

The contribution to the mass and energy releases from the secondary plant steam piping was included in the mass and energy release calculations. The flowrate was determined using the Moody correlation, the pipe cross-sectional area, and the initial steam pressure. For the limiting steamline break DER case analyzed for the power uprating, the unisolable steamline mass is included in the mass exiting the break from the time of steamline isolation until the unisolable mass is completely released to containment.

14.3.4.2.2.10 MAIN STEAMLINE ISOLATION

The postulated single failure for these two cases is the failure to close the MSCV in the faulted loop. In this instance, MSIV closure in the intact loops is required to terminate the blowdown. A delay time of 7 seconds was assumed (2-second signal processing plus 5-second valve closure) with full steam flow assumed through the valve during the valve stroke. The assumption of full steam flow for this time conservatively accounts for the effects of the unisolable steamline volume which would be released following closure of the MSIVs.

14.3.4.2.2.11 REACTOR COOLANT SYSTEM METAL HEAT CAPACITY

As the primary side of the plant cools, the temperature of the reactor coolant drops below the temperature of the reactor coolant piping, the reactor vessel, and the reactor coolant pumps. As this occurs, the heat stored in the metal is available to be transferred to the steam generator with the broken line. Stored metal heat does not have a major impact on the calculated mass and energy releases. The effects of this RCS metal heat are included in the results using conservative thick metal masses and heat transfer coefficients.

14.3.4.2.2.12 ROD CONTROL

The rod control system was assumed to be in manual operation for the steamline break analyses.

14.3.4.2.2.13 PROTECTION SYSTEM ACTUATIONS

The protection systems available to mitigate the effects of a MSLB accident inside containment include reactor trip, safety injection, steamline isolation, feedwater isolation, emergency fan coolers, and containment spray. The first protection system signal actuated was High Containment Pressure (2-of-3 signals) which initiated safety injection; the safety injection signal produced a reactor trip signal. Feedwater isolation occurred as a result of the safety injection signal. Finally, steamline isolation occurred via a High Steam Flow in 2-of-3 steamlines (1-of-2 signals per steamline) coincident with a Low T-avg SI signal in 2-of-3 loops.

14.3.4.2.2.14 SAFETY INJECTION SYSTEM

Minimum safety injection system (SIS) flowrates corresponding to the failure of one SIS train (2-of-4 pumps) were assumed in this analysis. A minimum SI flow is conservative since the reduced boron addition maximizes a return to power resulting from the RCS cooldown. The higher power generation increases heat transfer to the secondary side, maximizing steam flow out of the break. The delay time to achieve full SI flow was assumed to be 23 seconds for this analysis.

14.3.4.2.2.15 CORE REACTIVITY COEFFICIENTS

Conservative core reactivity coefficients corresponding to end-of-cycle conditions, including HZP stuck-rod moderator density coefficients, were used to maximize the reactivity feedback effects resulting from the steamline break. Use of maximum reactivity feedback results in higher power generation if the reactor returns critical, thus maximizing heat transfer to the secondary side of the steam generators.

14.3.4.2.3 DESCRIPTION OF ANALYSIS

The break flows and enthalpies of the steam release through the steamline break are analyzed with the LOFTRAN (Reference 7) computer code. Blowdown mass and energy releases determined using LOFTRAN include the effects of core power generation, main and auxiliary feedwater additions, engineered safeguards systems, reactor coolant system thick metal heat storage, and reverse steam generator heat transfer.

The Turkey Point NSSS is analyzed using LOFTRAN to determine the transient steam mass and energy releases inside containment following a steamline break event. The tables of mass and energy releases are used as input conditions to the analysis of the containment response as discussed in Section 14.3.4.3.

The single most-limiting case analyzed with respect to peak containment pressure, based on the results in Reference 6, was a 1.4 ft^2 DER at hot-zero-power (HZP) conditions.

The DER steamline break event was modeled taking credit only for MSIV closure on the intact loops for steamline isolation.

14.3.4.2.4 RESULTS

Using Reference 6 as a basis, including parameter changes associated with the power uprating, the mass and energy releases rates were developed to determine the containment pressure response for the limiting steamline break case noted in Section 14.3.4.2.3. The mass and energy releases from the 1.4 ft^2 DER at HZP conditions resulted in the highest containment pressure. The steam mass and energy releases discussed in this section provide the basis for the containment response described in Section 14.3.4.3 of this report. Table 14.3.4.3-6 provides the sequence of events for the limiting steamline break inside containment.

14.3.4.2.5 CONCLUSIONS

The mass and energy releases from the steamline break case, resulting in the limiting containment pressure response identified in Reference 6, have been analyzed at the uprated power conditions. The assumptions delineated in Section 14.3.4.2.2 have been included in the steamline break analysis such that the applicable acceptance criteria are met. The steam mass and energy releases discussed in this section provide the basis for the containment response described in Section 14.3.4.3.

14.3.4.3 CONTAINMENT RESPONSE

14.3.4.3.1 IDENTIFICATION OF CAUSES AND ACCIDENT DESCRIPTION

The containment response analysis demonstrates the acceptability of the containment safeguards systems to mitigate the consequences of a high-energy line break inside containment. The impact of MSLB or LOCA mass and energy releases on the containment pressure is addressed to assure that the containment pressure remains below its design pressure at the uprated 2300 MWt core power conditions.

14.3.4.3.2 INPUT PARAMETERS AND ASSUMPTIONS

An analysis of containment response to the rupture of the RCS or main steamline must start with knowledge of the initial conditions in the containment. The pressure, temperature, and humidity of the containment atmosphere prior to the postulated accident are specified in the analysis.

Also, values for the initial temperature of the component cooling water (CCW) and temperature of the intake cooling water (ICW) and refueling water storage tank (RWST) solution are assumed, along with the initial water inventory of the RWST. All of these values are chosen conservatively, as shown in Table 14.3.4.3-1.

The following are the major assumptions made in the analysis.

1. The mass and energy released to the containment are described in Sections 14.3.4.1 for LOCA and 14.3.4.2 for MSLB.

2. Homogeneous mixing is assumed. The steam-air mixture and the water phases each have uniform properties. More specifically, thermal equilibrium between the air and the steam is assumed. However, this does not imply thermal equilibrium between the steam-air mixture and the water phase.

3. Air is taken as an ideal gas, while compressed water and steam tables are employed for water and steam thermodynamic properties.

4. For the steamline break analysis and the blowdown portion of the LOCA analysis, the discharge flow separates into steam and water phases at the breakpoint. The saturated water phase is at the total containment pressure, while the steam phase is at the partial pressure of the steam in the containment. For the post-blowdown portion of the LOCA analysis, steam and water releases are input separately.

14.3.4.3.2.1 PASSIVE HEAT REMOVAL

The most significant heat removal mechanism during the early portion of the transient is the transfer of heat to the containment structural heat sinks. Provision is made in the containment pressure transient analysis for heat transfer through, and heat storage in, both interior and exterior walls. Every wall is divided into a large number of nodes. For each node, a conservation of energy equation expressed in finite-difference form accounts for transient conduction into and out of the node and temperature rise of the node. Table 14.3.4.3-2 is the summary of the containment structural heat sinks used in the analysis. The thermal properties of each heat sink material are shown in Table 14.3.4.3-3.

The heat transfer coefficient to the containment structure is calculated based primarily on the work of Tagami (Reference 8). From this work, it was determined that the value of the heat transfer coefficient increases parabolically to a peak value. The value then decreases exponentially to a stagnant heat transfer coefficient which is a function of steam-to-air-weight ratio.

Tagami presents a plot of the maximum value of heat transfer coefficient, h, as a function of "coolant energy transfer speed," defined as follows:

$$h = \frac{\text{total coolant energy transferred into containment}}{\text{(containment volume) (time interval to peak pressure)}}$$

From this, the maximum heat transfer coefficient (h) of steel is calculated:

$$h_{max} = 75\left(\frac{E}{t_p V}\right)^{0.60} \qquad \text{(Equation 14.3.4.3-1)}$$

where:

h_{max} = maximum value of h (Btu/hr ft^2 °F).

t_p = time from start of accident to end of blowdown for LOCA and steam line isolation for secondary breaks (sec).

V = containment volume (ft^3).

E = coolant energy discharge (Btu).

The parabolic increase to the peak value is given by:

$$h_s = h_{max}\left(\frac{t}{t_p}\right)^{0.5}, 0 \le t \le t_p \qquad \text{(Equation 14.3.4.3-2)}$$

where:

h_s = heat transfer coefficient for steel (Btu/hr ft^2 °F).

t = time from start of accident (sec).

For concrete, the heat transfer coefficient is taken as 40 percent of the value calculated for steel.

The exponential decrease of the heat transfer coefficient is given by:

$$h_s = h_{stag} + (h_{max} - h_{stag})e^{-0.05(t-tp)} \quad t>t_p \qquad \text{(Equation 14.3.4.3-3)}$$

where:

h_{stag} = $2 + 50X$, $0 \leq X \leq 1.4$.

h_{stag} = h for stagnant conditions (Btu/hr ft^2 °F).

X = steam-to-air weight ratio in containment.

For a large break, the engineered safety features are quickly brought into operation. Because of the brief period of time required to depressurize the reactor coolant system or the main steam system, the containment safeguards are not a major influence on the blowdown peak pressure; however, they reduce the containment pressure after the blowdown and maintain a low long-term pressure. Also, although the containment structure is not a very effective heat sink during the initial reactor coolant system blowdown, it still contributes significantly as a form of heat removal throughout the rest of the transient.

14.3.4.3.2.2 ACTIVE HEAT REMOVAL

During the injection phase of post-accident operation, the emergency core cooling system pumps water from the refueling water storage tank into the reactor vessel. Since this water enters the vessel at refueling water storage tank and accumulator ambient temperature, which is less than the temperature of the water in the vessel, it can absorb heat from the core until saturation temperature is reached. During the recirculation phase of operation, water is taken from the containment sump and cooled in the residual heat removal heat exchanger. The cooled water is then pumped back to the reactor vessel to absorb more decay heat. The heat is removed from the residual heat removal system heat exchanger by the CCW system.

Another containment heat removal system is the containment spray. Containment spray is used for rapid pressure reduction and for containment iodine removal. During the injection phase of operation, the containment spray pumps draw water from the RWST and spray it into the containment through nozzles mounted high above the operating deck. As the spray droplets fall, they absorb heat from the containment atmosphere. Since the water comes from the RWST, the entire heat capacity of the spray from the RWST temperature to the temperature of the containment atmosphere is available for energy absorption. During the recirculation phase of post-accident operation, water can be drawn from the residual heat removal heat exchanger outlet and sprayed into the containment atmosphere via the recirculation spray system. The modeled spray flow rate is shown in Table 14.3.4.3-4.

When a spray droplet enters the hot, saturated, steam-air containment environment following a loss-of-coolant accident, the vapor pressure of the water at its surface is much less than the partial pressure of the steam in the atmosphere. Hence, there will be diffusion of steam to the drop surface and condensation on the droplet. This mass flow will carry energy to the droplet. Simultaneously, the temperature difference between the atmosphere and the droplet will cause the droplet temperature and vapor pressure to rise. The vapor pressure of the droplet will eventually become equal to the partial pressure of the steam, and the condensation will cease. The temperature of the droplet will essentially equal the temperature of the steam-air mixture.

The equations describing the temperature rise of a falling droplet are as follows.

$$\frac{d}{dt}(Mu) = mh_g + q \qquad \text{(Equation 14.3.4.3-4)}$$

$$\frac{d}{dt}(M) = m \qquad \text{(Equation 14.3.4.3-5)}$$

where,

$q = h_cA * (T_s - T)$,

$m = k_gA * (P_s - P_v)$.

The coefficients of heat transfer (h_c) and mass transfer (k_g) are calculated from the Nusselt number for heat transfer, Nu, and the Nusselt number for mass transfer, Nu'.

Both Nu and Nu' may be calculated from the equations of Ranz and Marshall (Reference 9).

$$\underline{N}u = 2 + 0.6(\underline{Re})^{1/2}(\underline{Pr})^{1/3} \quad \text{(Equation 14.3.4.3-6)}$$

$$\underline{N}u' = 2 + 0.6(\underline{Re})^{1/2}(\underline{Sc})^{1/3} \quad \text{(Equation 14.3.4.3-7)}$$

Thus, Equations 14.3.4.3-4 and 14.3.4.3-5 can be integrated numerically to find the internal energy and mass of the droplet as a function of time as it falls through the atmosphere. Analysis shows that the temperature of the (mass) mean droplet produced by the spray nozzles rises to a value within 99 percent of the bulk containment temperature in less than 2 seconds.

Droplets of this size will reach temperature equilibrium with the steam-air containment atmosphere after falling through less than half the available spray fall height.

Detailed calculations of the heatup of spray droplets in post-accident containment atmospheres by Parsly (Reference 10) show that droplets of all sizes encountered in the containment spray reach equilibrium in a fraction of their residence time in a typical pressurized water reactor containment.

These results confirm the assumption that the containment spray will be 100 percent effective in removing heat from the atmosphere. Nomenclature used in this section is as follows:

Nomenclature

A = area
h_c = coefficient of heat transfer
k_g = coefficient of mass transfer
h_g = steam enthalpy

<u>Nomenclature</u>
(continued)

M = droplet mass
m = diffusion rate
<u>Nu</u> = Nusselt number for heat transfer
<u>Nu'</u> = Nusselt number for mass transfer
P_s = steam partial pressure
P_v = droplet vapor pressure
<u>Pr</u> = Prandtl number
q = heat flow rate
<u>Re</u> = Reynolds number
<u>Sc</u> = Schmidt number
T = droplet temperature
T_s = steam temperature
t = time
u = internal energy

The emergency containment coolers (ECCs) are a final means of heat removal. The ECCs consist of the fan and the banks of cooling coils. The fans draw the dense post-accident atmosphere through banks of cooling coils and mix the cooled steam/air mixture with the rest of the containment atmosphere. The coils are kept at a low temperature by a constant flow of component cooling water (CCW). Since this system does not use water from the RWST, the mode of operation remains the same both before and after the spray system and emergency core cooling system change to the recirculation mode. However, CCW is also used to cool the RHR heat exchanger(s) during recirculation. This will adversely affect fan cooler performance due to increased CCW temperatures and lower CCW flowrates to the fan coolers. See Table 14.3.4.3-5 for ECC heat removal capability for the design basis containment integrity analyses.

With these assumptions, the heat removal capability of the passive and active containment heat removal systems are sufficient to absorb the energy releases and still keep the maximum calculated pressure below the design pressure for the LOCA and MSLB containment integrity transients. The assumptions made for the CCW thermal performance analyses are more than adequate to demonstrate the heat removal capability of the CCW system.

14.3.4.3.3 DESCRIPTION OF ANALYSIS

Calculation of containment pressure and temperature response is accomplished by use of the computer code COCO (Reference 11). For analytical rigor and convenience, the containment air-steam-water mixture is separated into a water phase and a steam-air phase. Sufficient relationships to describe the transient are provided by the equations of conservation of mass and energy as applied to each system, together with the appropriate boundary conditions.

14.3.4.3.3.1 LOCA CONTAINMENT INTEGRITY

A series of cases was performed for the LOCA containment integrity. Section 14.3.4.1 documented the mass and energy releases for the most-limiting single failure of a diesel generator for a DEPS break and the releases from the blowdown of a DEHL break. Each of these cases was performed at an initial containment pressure of +0.3 psig and +3.0 psig. These two pressures represent the nominal assumed and maximum operating pressures in the containment.

Two additional DEPS cases with a diesel failure were performed. These cases were performed with only 1 ECC actuating from the auto-start signal, a second ECC manually actuated at 24 hours after accident initiation, and continuous operation of the recirculation sprays upon actuation during the cold leg recirculation switchover sequence. This differs from the other DEPS cases such that each of those cases assumed that the recirculation sprays would be terminated no later than 18 hours after accident initiation.

The sequence of events for each of the limiting LOCA cases is shown in Tables 14.3.4.3-7 through 14.3.4.3-9.

14.3.4.3.3.2 MSLB CONTAINMENT INTEGRITY

The MSLB mass and energy releases that were assumed for the 1.4 ft^2 DER at Hot Zero Power (HZP) as discussed in Section 14.3.4.2 were used to analyze the containment response. The failure of a MSCV was the limiting single failure for MSLB containment integrity. Since the failure was postulated to occur in the secondary steam system safety equipment, all of the containment heat removal equipment was assumed to be operational. This case was analyzed to the time of steam generator dryout. The sequence of events for this case is shown in Table 14.3.4.3-6.

14.3.4.3.4 RESULTS

The results of the transient analysis of the containment at an initial pressure of +0.3 psig for the LOCA cases are shown in Figures 14.3.4.3-1 through 14.3.4.3-6. Figures 14.3.4.3-1 and 14.3.4.3-2 show the response to the DEPS case with 2 ECCs assumed to be operating initially. The containment response to the DEHL blowdown is presented in Figures 14.3.4.3-3 and 14.3.4.3-4. The results of the long term DEPS transient with only 1 ECC operating initially and a second ECC manually actuated at 24 hours are presented in Figures 14.3.4.3-5 and Figure 14.3.4.3-6. The containment pressure transient for the 1.4 ft^2 DER MSLB at 0% power with a MSCV failure is shown in Figure 14.3.4.3-7. All of these cases show that the containment pressure will remain below design pressure of 55 psig.

In addition, all of the cases performed at the maximum initial containment pressure of +3.0 psig were also below the design pressure. After the peak pressure is attained, the operation of the safeguards system reduced the containment pressure. For the LOCA, at 24 hours following the accident, the containment pressure has been reduced to a value well below 50 percent of the peak calculated value. The containment integrity results are shown in Table 14.3.4.3-10 for LOCA and the MSLB ruptures.

14.3.4.3.5 CONCLUSIONS

The containment integrity analyses have been performed for the thermal uprate program at Turkey Point Units 3 & 4. The analyses included both long-term MSLB and LOCA transients. As described in the results Section 14.3.4.3.4, all cases resulted in a peak containment pressure that was less than 55 psig. In addition, all long-term cases were well below 50% of the peak value within 24 hours. Based on these results, all applicable acceptance criteria have been met and Turkey Point Units 3 & 4 are safe to operate at 2300 MWt (core).

14.3.4.4 CONTAINMENT COMPARTMENTS

The compartments within the containment which enclose or surround the various portions of the reactor coolant system consists of a reactor cavity and three steam generator enclosures.

The compartments pressure buildup following LOCA are calculated by the use of Bechtel proprietary computer program COPRA. This program calculates the mass and energy balance of the two-phase mixture as it discharges into the compartment and leaves through openings into the main containment atmosphere. This calculation does not account for heat sinks or engineered safeguards system as their influence is negligible for such short time transient. In all blowdown cases, the largest possible reactor coolant pipe rupture that could occur within the compartments was assumed. The reactor cavity free volume was taken as 9350 ft^3 and the main containment $1.55x10^6$ ft^3. The initial containment condition was assumed to be 120^oF[1] and 14.7 psia.

The reactor cavity has four different types of openings for pressure relief and flow expansion into the main containment atmosphere. However, the cavity blowdown is conservatively assumed to be able to vent to the main containment only through three, these are: (1) The annular clearances around the reactor coolant pipe penetrations (2) The annular space between concrete surface and the reactor vessel flange, and (3) The pipe chase connected with the reactor cavity.

It is assumed that the plugs for nozzle weld inspection remain in place and do not provide additional vent area.

NOTE:

1. Refer to Reference 12 and FSAR Section 14.0 for discussion of effects of operation with elevated normal bulk containment temperatures up to 125^oF for short periods of time.

Both double-ended and slot type of reactor coolant pipe ruptures have been postulated. For the double-ended break at the reactor nozzle, the lateral separation of the ruptured pipe end is restricted by the size of the pipe sleeve through the reactor shield walls. The re-straining effect allows an opening area from the primary system of about 0.5 ft^2 in each direction. On the other hand, slot type failure having a maximum failure length of two times the inside pipe diameter gives an opening area of about 4.75 $ft.^2$ (equivalent to the cross-sectional flow area of the pipe.). This break produces the higher differential pressure across the cavity wall. The coolant released into the annulus splits into two paths, one leading into the reactor cavity, and the other leading into the secondary compartment.

The steam generator compartments are vented through the baffle wall geometry of the secondary shield walls.

Per Reference 13, Leak-Before-Break (LBB) Technology can be applied to the calculations of the short term mass and energy releases. Under LBB, the most limiting break would be a double-ended rupture of one of the largest RCS loop branch lines (i.e., the pressurizer surge line, the accumulator/SI line, or the RHR suction line). The mass and energy released from these breaks are bounded by the current design basis ruptures discussed above.

14.3.4.5 REFERENCES

1. "Westinghouse LOCA Mass and Energy Release Model for Containment Design - March 1979 Version," WCAP-10325-P-A, May 1983 (Proprietary), WCAP-10326-A (Nonproprietary).

2. Docket No. 50-315, "Amendment No. 126, Facility Operating License No. DPR-58 (TAC No. 7106), for D.C. Cook Nuclear Plant Unit 1," June 9, 1989.

3. EPRI 294-2, Mixing of Emergency Core Cooling Water with Steam; □-Scale Test and Summary, (WCAP-8423), Final Report June 1975.

4. "Westinghouse Mass and Energy Release Data For Containment Design," WCAP-8264-P-A, Rev. 1, August 1975 (Proprietary), WCAP-8312-A (Nonproprietary).

5. ANSI/ANS-5.1 1979, "American National Standard for Decay Heat Power in Light Water Reactors," August 1979.

6. Gresham, J. A., Heberle, G. H., Wills, M. E. and Scobel, J. H., "Analysis of Containment Response Following a Main Steam Line Break for Turkey Point Units 3 and 4," WCAP-12262 (Nonproprietary), August 1989.

7. Burnett, T. W. T., et al., "LOFTRAN Code Description," WCAP-7907-P-A (Proprietary) and WCAP-7907-A (Nonproprietary), April 1984.

8. Tagami, Takashi, "Interim Report on Safety Assessments and Facilities Establishment Project in Japan for Period Ending June 1965, (No. 1)."

9. Ranz, W. E., and Marshall, W. R., Jr., "Evaporation from Drops." Chemical Engineering Progress, vol. 48, pp. 141-146, March 1952.

10. Parsly, L. F., "Spray Tests at the Nuclear Safety Pilot Plant," in "Nuclear Safety Program Annual Progress Report for Period Ending December 31, 1970," ORNL-4647, 1971, p. 82.

11. "Containment Pressure Analysis Code", WCAP-8327 (Proprietary), WCAP-8326 (Nonproprietary), June 1974.

14.3.4.5 REFERENCES (Continued)

12. Safety Evaluation JPN-PTN-SENJ-88-052,"Safey Evaluation for Containment Bulk Amendment Temperatures," Revison 3, dated April 13, 1989.

13. Letter, G. E. Edison (NRC) to W. F. Conway (FPL), "NRC Generic Letter 84-04, Asymetric LOCA Loads for Turkey Point Units 3 and 4", dated November 28, 1988.

TABLE 14.3.4.1-1

SYSTEM PARAMETERS
INITIAL CONDITIONS FOR THERMAL UPRATE

PARAMETERS	VALUE
Core Thermal Power (MWt)	2346
Reactor Coolant System Total Flowrate (lbm/sec)	25,813.75
Vessel Outlet Temperature (^oF) [1]	615.2
Core Inlet Temperature (^oF) [1]	554.0
Vessel Average Temperature (^oF) [1]	584.6
Initial Steam Generator Steam Pressure (psia)	832
Steam Generator Design	Model 44F
Steam Generator Tube Plugging (%)	0
Initial Steam Generator Secondary Side Mass (lbm)	103,501.2
Assumed Maximum Containment Backpressure (psia)	69.7
Accumulator	
Water Volume (ft^3)	920
N_2 Cover Gas Pressure (psia)	615
Temperature (^oF)	130
Safety Injection Delay (sec)	35.0

NOTE:

1. Analysis value includes an additional +7.4^oF allowance for instrument error and dead-band.

TABLE 14.3.4.1-2

SAFETY INJECTION FLOW
DIESEL FAILURE (SINGLE TRAIN)

INJECTION MODE (REFLOOD PHASE)

RCS Pressure (psig)	Total Flow (gpm)
0	3581.0
20	3318.0
40	3028.0
60	2705.0
80	2324.0
100	1772.0
120	562.0
140	557.0
160	551.0
180	546.0
200	540.0
300	511.0

INJECTION MODE (POST-REFLOOD PHASE)

RCS Pressure (psig)	Total Flow (gpm)
40	584.0

COLD LEG RECIRCULATION MODE

RCS Pressure (psig)	Total Flow (gpm)
0	2455.0

TABLE 14.3.4.1-3

DOUBLE-ENDED HOT LEG BREAK
BLOWDOWN MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW (1)		BREAK PATH NO.2 FLOW (2)	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
0.0000	0.0	0.0	0.0	0.0
0.0502	52052.2	33058.2	27440.1	17291.7
0.100	43931.8	27888.9	26452.2	16683.2
0.150	35897.9	22981.1	24471.6	15407.5
0.200	33326.1	21354.7	22866.7	14346.6
0.251	33160.2	21218.4	21435.1	13371.4
0.350	32570.3	20826.9	19771.1	12155.6
0.451	31951.0	20439.4	18862.1	11414.2
0.651	31684.6	20310.2	17657.4	10398.5
0.801	30915.5	19905.6	17137.6	9933.6
1.00	30269.1	19678.5	16589.6	9456.4
1.10	29886.8	19540.7	16459.6	9316.9
1.30	28980.0	19164.3	16433.3	9188.6
1.50	27877.9	18666.2	16584.7	9178.9
1.70	26631.5	18065.9	16804.4	9225.9
2.00	24669.1	17049.7	17091.4	9307.7
2.50	21669.7	15305.2	17288.0	9354.7
3.00	19519.6	13836.8	17132.6	9254.5
3.50	18277.5	12801.6	16707.8	9031.4
4.00	18070.1	12415.4	16017.6	8682.6
4.50	18724.0	12411.8	14976.4	8157.9
5.00	19164.9	12391.8	13787.8	7561.6
5.50	19629.4	12455.8	12448.7	6872.4
6.00	15408.3	10487.2	11153.4	6194.5
6.50	15291.3	10332.4	10052.0	5613.3
7.00	14964.2	10046.5	9145.5	5132.4
7.50	14560.5	9662.1	8373.0	4722.1
8.00	14559.9	9506.0	7684.5	4358.0
8.50	14274.3	9216.9	7061.7	4031.3
9.00	13796.2	8844.6	6486.8	3733.1
9.50	13107.5	8386.8	5951.6	3459.7
10.0	12278.3	7880.0	5457.8	3212.0
10.5	11394.0	7366.6	5005.0	2989.3
11.5	9639.3	6403.6	4216.3	2611.3
12.0	8625.7	5886.7	3817.1	2422.7
13.0	6475.6	4922.5	2860.2	2002.0
13.5	5475.6	4495.4	2455.6	1809.2
14.0	4478.2	4080.0	2185.7	1656.7
14.5	3403.7	3450.0	2013.2	1544.0
15.0	2756.9	2981.2	1881.0	1447.5

TABLE 14.3.4.1-3

DOUBLE-ENDED HOT LEG BREAK
BLOWDOWN MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW [1]		BREAK PATH NO.2 FLOW [2]	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
15.5	2343.8	2615.5	1717.5	1367.6
16.5	1705.4	2030.6	1409.9	1224.6
17.0	1456.8	1771.1	1284.2	1163.4
17.5	1076.7	1329.4	924.0	1092.3
18.0	993.6	1238.0	602.9	743.5
19.0	530.3	670.7	280.3	348.7
19.5	402.8	514.4	185.2	231.7
20.0	298.7	382.6	0.0	0.0
20.5	141.1	182.2	0.0	0.0
21.5	0.0	0.0	0.0	0.0

NOTES:

1. Mass and energy exiting from the reactor vessel side of the break.

2. Mass and energy exiting from the steam generator vessel side of the break.

TABLE 14.3.4.1-4

DOUBLE-ENDED HOT LEG MASS BALANCE

	Time (Seconds)	0.00	21.50 (1)	21.50 (2)
		Mass (Thousands Lb_m)		
Initial Mass	In RCS and Accum.	579.16	579.16	579.16
Added Mass	Pumped Injection	0.00	0.00	0.00
	TOTAL ADDED	0.00	0.00	0.00
TOTAL AVAILABLE ----->		579.16	579.16	579.16
Distribution	Reactor Coolant	403.94	50.05	93.69
	Accumulator	175.22	138.53	94.90
	TOTAL CONTENTS	579.16	188.58	188.58
Effluent	Break Flow	0.00	390.56	390.56
	ECCS Spill	0.00	0.00	0.00
	TOTAL EFFLUENT	0.00	390.56	390.56
TOTAL ACCOUNTABLE --->		579.16	579.15	579.15

NOTES:

1. Mass balance at the end of the blowdown phase.
2. Mass balance at the end of the refill phase.

TABLE 14.3.4.1-5

DOUBLE-ENDED HOT LEG ENERGY BALANCE

	Time (seconds)	0.00	21.50 (1)	21.50 (2)
		Energy (Million Btu)		
Initial Energy	In RCS, ACC, SGen	623.75	623.75	623.75
Added Energy	Pumped Injection	0.00	0.00	0.00
	Decay Heat	0.00	4.75	4.75
	Heat From Secondary	0.00	-6.15	-6.15
	TOTAL ADDED	0.00	-1.40	-1.40
TOTAL AVAILABLE ----->		623.75	622.35	622.35
Distribution	Reactor Coolant	237.49	13.09	17.43
	Accumulator	17.43	13.78	9.44
	Core Stored	23.36	11.01	11.01
	Primary Metal	118.73	111.46	111.46
	Secondary Metal	58.66	57.22	57.22
	Steam Generator	168.07	162.68	162.68
	TOTAL CONTENTS	623.75	369.25	369.25
Effluent	Break Flow	0.00	253.09	253.09
	ECCS Spill	0.00	0.00	0.00
	TOTAL EFFLUENT	0.00	253.09	253.09
TOTAL ACCOUNTABLE ---->		623.75	622.33	622.33

NOTES:

1. Energy balance at the end of the blowdown phase.
2. Energy balance at the end of the refill phase.

TABLE 14.3.4.1-6

DOUBLE-ENDED PUMP SUCTION BREAK
BLOWDOWN MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW (1)		BREAK PATH NO.2 FLOW (2)	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
0.0000	0.0	0.0	0.0	0.0
0.0501	40934.2	22404.7	28380.4	15458.8
0.100	40700.7	22324.0	21635.0	11808.3
0.201	41067.2	22685.4	23122.8	12635.5
0.301	41492.3	23129.7	24162.3	13211.5
0.400	41955.2	23638.6	24282.2	13283.4
0.500	42113.5	23999.6	23792.6	13020.9
0.601	41711.5	24037.8	23164.5	12682.8
0.701	40664.3	23672.0	22675.9	12421.6
0.900	38327.9	22702.5	22172.8	12156.4
1.10	36612.3	22054.7	21699.8	11902.2
1.30	34733.0	21285.3	21198.8	11629.2
1.40	33920.5	20944.9	20986.1	11512.9
1.80	31411.9	20017.1	20217.2	11089.5
2.00	29608.8	19271.9	19522.5	10705.4
2.50	20674.6	14138.4	17630.2	9660.9
3.00	15463.2	10687.9	15998.0	8765.6
3.50	12005.3	8469.4	14856.0	8144.9
4.00	10540.3	7553.9	13742.1	7539.0
4.50	9597.1	6963.7	13632.1	7489.7
5.00	9075.7	6638.5	13489.2	7411.9
5.50	8756.9	6481.3	13343.6	7336.9
6.00	8375.5	6316.8	13102.9	7207.7
6.50	8050.8	6145.7	12836.6	7061.0
7.00	7616.5	6460.3	12539.9	6895.5
7.50	6973.8	5903.2	12126.7	6665.1
8.00	7093.6	5690.8	11756.4	6459.0
8.50	7105.0	5535.3	11390.8	6254.7
9.00	6896.8	5428.5	11005.6	6041.0
9.50	6453.3	5244.3	10589.0	5811.2
10.0	6068.8	4998.9	10162.7	5576.7
11.0	5543.0	4523.6	9373.6	5144.5
12.0	4984.3	3991.5	8572.2	4706.4
13.0	4505.5	3481.2	7592.7	4159.8
13.5	4308.2	3286.2	7254.1	3868.3
14.0	4130.7	3143.7	7069.4	3634.3
15.5	3483.0	2879.5	6172.5	2960.7

DOUBLE-ENDED PUMP SUCTION BREAK
BLOWDOWN MASS AND ENERGY RELEASES

TIME (SECONDS)	BREAK PATH NO.1 FLOW [1] (LBM/SEC)	BREAK PATH NO.1 FLOW [1] ($BTUx10^3$/SEC)	BREAK PATH NO.2 FLOW [2] (LBM/SEC)	BREAK PATH NO.2 FLOW [2] ($BTUx10^3$/SEC)
16.0	3244.1	2847.0	5802.9	2742.3
16.5	2955.0	2840.7	5382.6	2521.1
17.0	2435.4	2707.5	4617.4	2081.5
17.5	1964.7	2397.6	3983.0	1687.8
18.0	1598.3	1975.9	3410.5	1362.5
18.5	1319.7	1640.2	3020.0	1145.1
19.0	1093.7	1365.0	2709.2	982.7
19.5	870.4	1089.5	2797.1	954.2
20.0	682.7	856.5	3050.1	977.2
20.5	525.7	660.8	2420.2	754.5
21.5	233.1	294.0	724.0	215.2
22.0	100.8	127.6	0.0	0.0
22.5	0.0	0.0	0.0	0.0

NOTES:

1. Mass and energy exiting from the steam generator side of the break.

2. Mass and energy exiting from the pump side of the break.

TABLE 14.3.4.1-7

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
REFLOOD MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW (1)		BREAK PATH NO.2 FLOW (2)	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
22.5	0.0	0.0	0.0	0.0
24.0	0.2	0.2	0.0	0.0
24.3	5.4	6.4	0.0	0.0
24.6	20.8	24.5	0.0	0.0
25.4	47.0	55.5	0.0	0.0
26.6	72.6	85.7	0.0	0.0
27.6	89.8	106.0	0.0	0.0
30.6	129.0	152.4	0.0	0.0
31.6	139.7	165.0	0.0	0.0
32.6	151.0	178.4	1160.4	214.9
33.6	153.9	181.8	1858.3	347.7
34.6	153.5	181.3	1869.6	352.6
35.6	153.8	181.8	2212.4	381.9
37.6	152.4	180.1	2136.5	372.8
39.6	151.1	178.5	2062.6	363.8
41.6	149.9	177.1	1991.4	354.9
42.6	149.3	176.4	1956.9	350.6
44.6	148.2	175.0	1890.0	342.2
46.6	147.1	173.8	1825.9	334.1
48.6	146.1	172.6	1764.4	326.2
50.6	145.1	171.4	1705.2	318.5
52.6	144.2	170.3	1648.4	311.1
53.6	143.7	169.8	1620.7	307.5
55.6	142.9	168.8	1566.9	300.4
57.6	142.0	167.8	1515.0	293.5
59.6	141.3	166.9	1464.7	286.7
61.6	140.5	166.0	1416.1	280.1
65.6	139.0	164.2	1323.1	267.3
69.6	137.6	162.6	1235.2	255.0
73.6	136.3	161.0	1151.8	243.0
77.6	135.0	159.5	1072.2	231.3
78.6	134.2	158.6	781.4	187.6
80.6	135.1	159.6	754.0	184.4
81.7	135.5	160.1	739.3	182.7
85.6	136.6	161.4	689.2	176.9
89.6	137.2	162.1	640.6	171.3
91.6	136.0	160.7	247.5	150.2
93.6	134.0	158.3	245.2	147.4
101.6	126.4	149.3	236.2	136.9

TABLE 14.3.4.1-7

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
REFLOOD MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW [1]		BREAK PATH NO.2 FLOW [2]	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
102.1	125.9	148.7	235.7	136.2
109.6	119.6	141.3	228.2	127.5
115.6	115.2	136.1	223.0	121.3
123.6	110.0	129.9	216.9	114.1
125.6	108.9	128.6	215.5	112.5
133.6	104.7	123.6	210.5	106.5
141.6	101.2	119.5	206.3	101.5
163.6	94.6	111.7	198.2	92.0
189.6	91.0	107.5	193.5	86.5
201.6	90.4	106.7	192.5	85.2
210.8	90.5	106.8	194.6	85.7

NOTES:

1. Mass and energy exiting from the steam generator side of the break.

2. Mass and energy exiting from the pump side of the break.

TABLE 14.3.4.1-8

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
PRINCIPLE PARAMETERS DURING REFLOOD

TIME	FLOODING		CARRYOVER	CORE	DOWNCOMER	FLOW	INJECTION			
	TEMP	RATE	FRACTION	HEIGHT	HEIGHT	FRACTION	TOTAL	ACCUMULATOR	SPILL	ENTHALPY
Seconds	Degree°F	In/Sec		ft	ft		(Pounds Mass per Second)			Btu/Lbm
22.5	156.0	0.000	0.000	0.00	0.00	0.333	0.0	0.0	0.0	0.00
23.3	155.5	16.138	0.000	0.52	0.73	0.000	2895.7	2895.7	0.0	99.50
23.8	155.2	8.217	0.000	1.08	0.73	0.000	2857.6	2857.6	0.0	99.50
24.2	155.4	2.602	0.035	1.23	1.31	0.197	2827.9	2827.9	0.0	99.50
24.5	155.6	3.115	0.073	1.29	1.82	0.303	2806.0	2806.0	0.0	99.50
25.7	156.3	2.309	0.285	1.50	3.98	0.396	2713.2	2713.2	0.0	99.50
26.6	156.8	2.227	0.380	1.61	5.51	0.409	2656.7	2656.7	0.0	99.50
30.7	159.7	2.497	0.588	2.00	12.64	0.427	2412.1	2412.1	0.0	99.50
32.6	161.4	2.650	0.629	2.16	15.36	0.432	2306.1	2306.1	0.0	99.50
35.6	164.1	2.545	0.659	2.39	15.57	0.437	2547.8	2163.7	0.0	95.51
37.2	165.5	2.494	0.668	2.51	15.57	0.437	2476.8	2092.7	0.0	95.40
45.0	172.8	2.351	0.690	3.01	15.57	0.435	2174.7	1790.5	0.0	94.82
53.5	181.0	2.264	0.699	3.50	15.57	0.433	1905.0	1520.7	0.0	94.16
62.5	189.8	2.197	0.704	4.00	15.57	0.432	1663.7	1279.4	0.0	93.38
72.6	199.6	2.136	0.708	4.54	15.57	0.431	1429.2	1044.8	0.0	92.38
78.6	205.4	2.100	0.709	4.85	15.57	0.430	1023.3	638.8	0.0	89.55
80.6	207.4	2.101	0.710	4.95	15.57	0.432	996.4	612.0	0.0	89.29
81.7	208.5	2.101	0.711	5.00	15.57	0.433	981.9	597.6	0.0	89.14
89.6	215.8	2.092	0.714	5.40	15.57	0.438	883.1	499.1	0.0	87.99
91.6	217.5	2.079	0.714	5.50	15.43	0.437	384.2	0.0	0.0	73.03
93.6	219.2	2.050	0.714	5.60	15.27	0.436	384.2	0.0	0.0	73.03
102.1	225.9	1.934	0.714	6.00	14.66	0.435	384.3	0.0	0.0	73.03
113.6	233.8	1.801	0.713	6.52	14.08	0.433	384.3	0.0	0.0	73.03
125.3	240.7	1.691	0.713	7.00	13.73	0.431	384.4	0.0	0.0	73.03

TABLE 14.3.4.1-8

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
PRINCIPLE PARAMETERS DURING REFLOOD

TIME	FLOODING		CARRYOVER	CORE	DOWNCOMER	FLOW	INJECTION			ENTHALPY
	TEMP	RATE	FRACTION	HEIGHT	HEIGHT	FRACTION	TOTAL	ACCUMULATOR	SPILL	
Seconds	Degree°F	In/Sec		ft	ft		(Pounds Mass per Second)			Btu/Lbm
139.6	247.9	1.587	0.713	7.56	13.56	0.430	384.4	0.0	0.0	73.03
151.4	253.1	1.522	0.714	8.00	13.58	0.429	384.4	0.0	0.0	73.03
165.6	258.6	1.466	0.715	8.50	13.74	0.428	384.4	0.0	0.0	73.03
180.2	263.6	1.427	0.718	9.00	14.03	0.428	384.5	0.0	0.0	73.03
195.6	268.4	1.400	0.721	9.51	14.41	0.428	384.5	0.0	0.0	73.03
210.8	272.5	1.388	0.725	10.00	14.83	0.429	384.5	0.0	0.0	73.03

TABLE 14.3.4.1-9

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
POST-REFLOOD MASS AND ENERGY RELEASES

TIME (SECONDS)	BREAK PATH NO.1 FLOW (1) (LBM/SEC)	BREAK PATH NO.1 FLOW (1) (BTUx10^3/SEC)	BREAK PATH NO.2 FLOW (2) (LBM/SEC)	BREAK PATH NO.2 FLOW (2) (BTUx10^3/SEC)
210.9	100.7	127.5	283.9	95.7
225.9	99.9	126.5	284.7	95.4
230.9	100.6	127.4	283.9	95.0
260.9	98.9	125.3	285.6	94.4
265.9	99.6	126.2	284.9	94.0
290.9	98.2	124.4	286.3	93.5
295.9	98.9	125.3	285.6	93.2
325.9	97.2	123.2	287.3	92.5
330.9	97.9	124.0	286.6	92.2
355.9	96.5	122.2	288.0	91.7
360.9	97.1	123.0	287.4	91.3
385.9	95.7	121.2	288.8	90.8
390.9	96.3	122.0	288.2	90.4
420.9	95.0	120.3	289.6	91.9
425.9	95.7	121.2	288.8	91.5
455.9	94.5	119.6	290.1	90.7
460.9	95.2	120.5	289.4	90.3
490.9	93.9	118.9	290.6	89.4
495.9	94.6	119.8	289.9	89.0
525.9	93.3	118.2	291.2	88.2
530.9	94.0	119.0	290.6	87.8
555.9	92.9	117.6	291.7	89.1
560.9	93.5	118.4	291.0	88.7
585.9	92.3	117.0	292.2	87.9
590.9	93.0	117.7	291.6	87.5
615.9	91.9	116.4	292.6	86.7
645.9	92.3	116.9	292.2	87.2
670.9	91.2	115.5	293.3	86.3
695.9	91.7	116.1	292.9	86.8
715.9	90.7	114.9	293.8	86.0
740.9	91.0	115.3	293.5	86.5
810.9	89.5	113.3	295.1	84.5
825.9	90.0	114.0	294.5	85.1
850.9	89.2	112.9	295.3	83.6
865.9	89.5	113.4	295.0	84.2
915.9	88.4	112.0	296.1	83.8
925.9	88.8	112.5	295.7	82.9
1055.9	87.1	110.3	297.5	81.8
1060.9	51.0	64.6	333.6	92.2
1172.8	51.0	64.6	333.6	92.2

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
POST-REFLOOD MASS AND ENERGY RELEASES

TIME	BREAK PATH NO.1 FLOW (1)		BREAK PATH NO.2 FLOW (2)	
(SECONDS)	(LBM/SEC)	(BTUx10^3/SEC)	(LBM/SEC)	(BTUx10^3/SEC)
1172.9	59.5	72.7	325.0	89.0
1289.1	59.5	74.2	325.0	88.8
1289.2	57.5	66.2	327.0	29.3
1680.0	54.0	62.2	330.5	30.0
1680.1	54.0	62.2	26.5	7.8
3600.0	45.2	52.0	35.4	9.4
3600.1	32.0	36.8	48.6	3.6
3780.0	31.3	36.0	52.4	3.8
3780.1	34.3	39.5	49.4	8.3
10000.0	23.2	26.7	60.5	10.1
64800.0	14.1	16.2	69.6	11.6
64800.1	15.5	17.8	68.2	11.5
100000.0	13.6	15.7	70.1	11.8
1000000.0	5.8	6.7	77.9	13.1

NOTES:

1. Mass and energy exiting from the steam generator side of the break.

2. Mass and energy exiting from the pump side of the break.

TABLE 14.3.4.1-10

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
MASS BALANCE

	Time (seconds)	0.00	22.50 (1)	22.50 (2)	210.83	1172.93	1289.05	3600.00
		Mass (Thousand Lbm)						
Initial	In RCS and Acc.	579.16	579.16	579.16	579.16	579.16	579.16	579.16
Added Mass	Pumped Injection	0.00	0.00	0.00	67.57	437.50	482.15	1370.77
	TOTAL ADDED	0.00	0.00	0.00	67.57	437.50	482.15	1370.77
	TOTAL AVAILABLE ----->	579.16	579.16	579.16	646.73	1016.66	1061.31	1949.93
Distribution	Reactor Coolant	403.94	26.46	70.06	111.82	111.82	111.82	111.82
	Accumulator	175.22	145.27	101.66	0.00	0.00	0.00	0.00
	TOTAL CONTENTS	579.16	171.73	171.73	111.82	111.82	111.82	111.82
Effluent	Break Flow	0.00	407.43	407.43	534.90	904.83	949.48	1838.10
	ECCS Spill	0.00	0.00	0.00	0.00	0.00	0.00	0.00
	TOTAL EFFLUENT	0.00	407.43	407.43	534.90	904.83	949.48	1838.10
	TOTAL ACCOUNTABLE --->	579.16	579.15	579.15	646.72	1016.65	1061.30	1949.92

NOTES:

1. Mass balance at the end of the blowdown phase.
2. Mass balance at the end of the refill phase.

TABLE 14.3.4.1-11

DOUBLE-ENDED PUMP SUCTION BREAK WITH DIESEL FAILURE
ENERGY BALANCE

	Time (seconds)	0.00	22.50 [(1)]	22.50 [(2)]	210.83	1172.93	1289.05	3600.00
				Energy (Million Btu)				
Initial Energy	In RCS,Acc,S Gen	624.22	624.22	624.22	624.22	624.22	624.22	624.22
Added Energy	Pumped Injection	0.00	0.00	0.00	4.93	31.95	35.21	100.10
	Decay Heat	0.00	4.60	4.60	19.55	72.63	78.09	168.40
	Heat From Secondary	0.00	-5.17	-5.17	-5.17	-3.23	-3.22	-3.22
	TOTAL ADDED	0.00	-0.57	-0.57	19.32	101.35	110.07	265.28
TOTAL AVAILABLE ----->		624.22	623.66	623.66	643.54	725.57	734.29	889.50
Distribution	Reactor Coolant	237.49	7.14	11.48	29.50	29.50	29.50	29.50
	Accumulator	17.43	14.45	10.12	0.00	0.00	0.00	0.00
	Core Stored	23.83	14.14	14.14	4.03	3.87	3.82	2.68
	Primary Metal	118.73	112.88	112.88	97.99	58.99	56.38	40.49
	Secondary Metal	58.66	58.24	58.24	54.45	32.97	31.04	22.54
	Steam Generator	168.07	166.30	166.30	153.12	89.28	84.17	61.01
	TOTAL CONTENTS	624.22	373.15	373.15	339.08	214.61	204.90	156.21
Effluent	Break Flow	0.00	250.03	250.03	303.87	510.38	514.54	721.54
	ECCS Spill	0.00	0.00	0.00	0.00	0.00	0.00	0.00
	TOTAL EFFLUENT	0.00	250.03	250.03	303.87	510.38	514.54	721.54
TOTAL ACCOUNTABLE --->		624.22	623.18	623.18	642.95	724.98	719.44	877.75

NOTES:

1. Energy balance at the end of the blowdown phase.
2. Energy balance at the end of the refill phase.

TABLE 14.3.4.2-1

NOMINAL PLANT PARAMETERS AND
INITIAL CONDITION ASSUMPTIONS FOR THERMAL UPRATE (1)

MAIN STEAM LINE BREAK - MASS AND ENERGY RELEASES

NSSS Power, Mwt	2311.4
Core Power, Mwt	2300
Reactor Coolant Pump Heat, Mwt	11.4
Reactor Coolant Flow (total), gpm	255,000
Pressurizer Pressure, psia	2250
Core Bypass, %	6.0
Reactor Coolant Temperatures, °F	
Core Outlet	611.3
Vessel Outlet	607.8
Core Average	580.5
Vessel Average	577.2
Vessel/Core Inlet	546.6
Steam Generator	
Steam Temperature, °F	522.8
Steam Pressure, psia	832
Steam Flow (total), 10^6 lbm/hr	10.17
Feedwater Temperature, °F	443
Zero-Load Temperature, °F	547

INITIAL CONDITIONS POWER LEVEL (%)

PARAMETER	102	0
RCS Average Temperature (°F)	583.2 (1)	547.0
RCS Flowrate (gpm)	255,000	255,000
RCS Pressure (psia)	2250	2250
Pressurizer Water Volume (ft^3)	688.6	321.9
Feedwater Enthalpy (Btu/lbm)	424.9	70.68
SG Water Level, faulted/intact (% span)	66/54	56/44

NOTE:

1. Noted values correspond to plant conditions defined by 0% steam generator tube plugging and the high end of the RCS T-avg window.

TABLE 14.3.4.3-1

CONTAINMENT ANALYSIS PARAMETERS

ICW temperature (◦F)[Containment Integrity]	100
Refueling water temperature (◦F)	105
RWST minimum water deliverable volume (gal)	2.399×10^5
Initial containment temperature (◦F)	130
Initial containment pressure (psia)	15.0
Initial relative humidity (%)	20
Net free volume (ft^3)	1.55×10^6

Emergency Containment Coolers

Total	3
Analysis maximum	2
Analysis minimum	1
Setpoint (psig)	6.0
Delay time (sec)	
Without Offsite Power	50.0
With Offsite Power	35.0

Containment Spray Pumps

Total	2
Analysis maximum	2
Analysis minimum	1
Setpoint (psig)	25.0
Delay time (sec)	
Without Offsite Power	60.0
With Offsite Power	45.0

TABLE 14.3.4.3-2

CONTAINMENT HEAT SINK DATA

Wall Description	Heat Transfer Area (ft^2)	Material	Thickness (ft)
1	360.9	Paint	0.000833
		Carbon Steel	0.617473
2	2725.6	Paint	0.000833
		Carbon Steel	0.232245
3	6368.1	Paint	0.000833
		Carbon Steel	0.109355
4	5426.0	Paint	0.000833
		Carbon Steel	0.066368
5	17366.0	Paint	0.000833
		Carbon Steel	0.038986
6	137461.3	Paint	0.000833
		Carbon Steel	0.021498
7	84988.4	Paint	0.000833
		Carbon Steel	0.011212
8	105344.0	Paint	0.000833
		Carbon Steel	0.005121
9	89906.9	Paint	0.000833
		Carbon Steel	0.001918
10	1378.0	Stainless Steel	0.08398
11	2335.8	Stainless Steel	0.043972
12	2684.9	Stainless Steel	0.015155
13	27329.0	Stainless Steel	0.002537
14	1207.0	Stainless Steel	0.0091
15	2150.0	Aluminum	0.020833

TABLE 14.3.4.3-2

CONTAINMENT HEAT SINK DATA

Wall Description	Heat Transfer Area (ft^2)	Material	Thickness (ft)
16	106200.1	Aluminum	0.000603
17	50132.0	Paint	0.00325
		Concrete	1.5
18	67240.0	Paint	0.000833
		Carbon Steel Liner	0.020833
		Concrete	1.5
19	775.0	Stainless Steel Liner	0.01
		Concrete	1.5
20	5825.0	Stainless Steel Liner	0.005417
		Concrete	1.5

TABLE 14.3.4.3-3

THERMAL PROPERTIES OF CONTAINMENT HEAT SINKS

Material	Thermal Conductivity (Btu/hr-°F-ft)	Volumetric Heat Capacity (Btu/ft^3-°F)
Paint	0.138	11.105
Carbon Steel	28.88	54.66
Stainless Steel	14.48	57.37
Aluminum	91.25	38.59
Concrete	1.048	26.27

TABLE 14.3.4.3-4

CONTAINMENT SPRAY PUMP FLOW

Containment Pressure (psig)	1 Pump (gpm)	2 Pumps (gpm)
0.0	1548.0	3009.0
10.0	1509.0	2947.0
20.0	1469.0	2870.0
30.0	1429.0	2789.0
40.0	1386.0	2704.0
50.0	1340.0	2611.0

TABLE 14.3.4.3-5

EMERGENCY CONTAINMENT COOLER PERFORMANCE
CONTAINMENT INTEGRITY ANALYSES
(Btu/sec/ECC)

(Based on 2000 gpm CCW Flow/ECC and 25,000 CFM Steam-Air Flow)

Containment Temp. (◦F)

CCW Temp. (◦F)	120	140	160	180	200	220	240	260	280	300
95	319.7	898.	1726.	2852.	4504.	6652.	9599.	13505.	18320.	25209.
110	222.4	806.	1635.	2780.	4406.	6550.	9485.	13294.	18164.	24973.
120	0.0	589.	1421.	2585.	4181.	6311.	9168.	12921.	17900.	24450.
130	0.0	325.	1162.	2302.	3917.	6030.	8860.	12577.	17253.	23705.
135	0.0	170.	1012.	2171.	3767.	5871.	8704.	12368.	17036.	23402.
140	0.0	0.0	848.	2016.	3603.	5702.	8518.	12196.	16797.	23107.
145	0.0	0.0	664.	1840.	3422.	5516.	8251.	11865.	16541.	22740.
150	0.0	0.0	464.	1649.	3230.	5310.	7954.	11618.	16082.	22357.
170	0.0	0.0	0.0	636.4	2188.4	4227.2	6762.	10291.	14652.	20622.
210	0.0	0.0	0.0	0.0	0.0	1022.3	3373.6	6597.6	10588.	16012.

TABLE 14.3.4.3-6

1.4 FT^2 MSLB HOT ZERO POWER WITH MSCV FAILURE
SEQUENCE OF EVENTS

Time (sec)	Event Description
0.0	Main Steamline break occurs.
1.4	HI-1 Containment pressure setpoint reached.
3.4	Rod motion occurs (HI-1 actuates SI which actuates Reactor Trip).
9.9	High steam flow coincident with low T_{avg} SI signal (539°F).
14.4	Safety injection initiated (actuated on HI-1); Feedwater isolation (actuated on HI-1).
14.5	HI-2 Containment pressure setpoint reached.
16.9	Steamline isolation occurs via a high steam flow coincident with low T_{avg} SI signal.
36.1	Emergency Containment Coolers (2) actuate.
76.1	Containment Sprays (2 trains) actuate.
238.3	Peak Containment pressure (48.1 psig) and temperature (269.4°F) occur.
606.0	Mass and energy releases terminate (SG Dryout).

TABLE 14.3.4.3-7

DOUBLE-ENDED PUMP SUCTION BREAK
CONTAINMENT AT +0.3 PSIG WITH DIESEL FAILURE
SEQUENCE OF EVENTS

Time (sec)	Event Description
0.0	Break occurs, Reactor Trip and Loss of Offsite Power are assumed.
0.8	Containment HI-1 pressure setpoint reached.
4.0	Low Pressurizer pressure SI setpoint = 1745.0 psia reached.
5.0	Containment HI-2 pressure setpoint reached.
12.7	Broken loop accumulator begins injecting water.
13.0	Intact loop accumulator begins injecting water.
19.7	Peak pressure and temperature occur.
22.5	End of blowdown phase.
50.8	Emergency Containment Coolers (2) actuate.
65.0	Containment Spray (RWST) begins (1 train).
77.8	Broken loop accumulator water injection ends.
89.9	Intact loop accumulator water injection ends.
210.8	End of reflood for minimum SI Case.
1680.0	RWST Low Level Reached - Recirc Sequence Begins.
3780.0	RWST Low-Low level reached - Cold Leg Recirc begins; Containment Spray (RWST) ends.
3780.1	Containment Spray (SUMP) begins.
64,800.0	Switchover to Hot Leg Recirculation begins; Containment Spray (SUMP) ends.
1.0E+06	Transient modelling terminated.

TABLE 14.3.4.3-8

DOUBLE-ENDED PUMP SUCTION BREAK
CONTAINMENT AT +0.3 PSIG WITH DIESEL FAILURE (ONLY 1 ECC)
SEQUENCE OF EVENTS

Time (sec)	Event Description
0.0	Break occurs, Reactor Trip and Loss of Offsite Power are assumed.
0.8	Containment HI-1 pressure setpoint reached.
4.0	Low Pressurizer pressure SI setpoint = 1745.0 psia reached.
5.0	Containment HI-2 pressure setpoint reached.
12.7	Broken loop accumulator begins injecting water.
13.0	Intact loop accumulator begins injecting water.
19.7	Blowdown peak pressure and temperature occur.
22.5	End of blowdown phase.
50.8	Emergency Containment Coolers (1) actuate.
65.0	Containment Spray (RWST) begins (1 train).
77.8	Broken loop accumulator water injection ends.
89.9	Intact loop accumulator water injection ends.
210.8	End of reflood for minimum SI case.
1059.5	Overall peak pressure and temperature occur.
1680.0	RWST low level reached - Recirc. sequence begins.
3780.0	RWST low-low level reached - Cold Leg Recirc. begins Containment Spray (RWST) ends.
3780.1	Containment Spray (SUMP) begins.
86,400.0	Second ECC manually actuated.
1.0E+07	Transient modelling terminated.

TABLE 14.3.4.3-9

DOUBLE-ENDED HOT LEG BREAK
SEQUENCE OF EVENTS

Time (sec)	Event Description
0.0	Break occurs, Reactor Trip and Loss of Offsite Power are assumed.
3.3	Low Pressurizer pressure SI setpoint = 1745.0 psia reached.
10.9	Broken loop accumulator begins injecting water.
11.1	Intact loop accumulator begins injecting water.
18.7	Peak pressure and temperature occur.
21.5	End of blowdown phase.
50.0	Transient modelling terminated.

TABLE 14.3.4.3-10

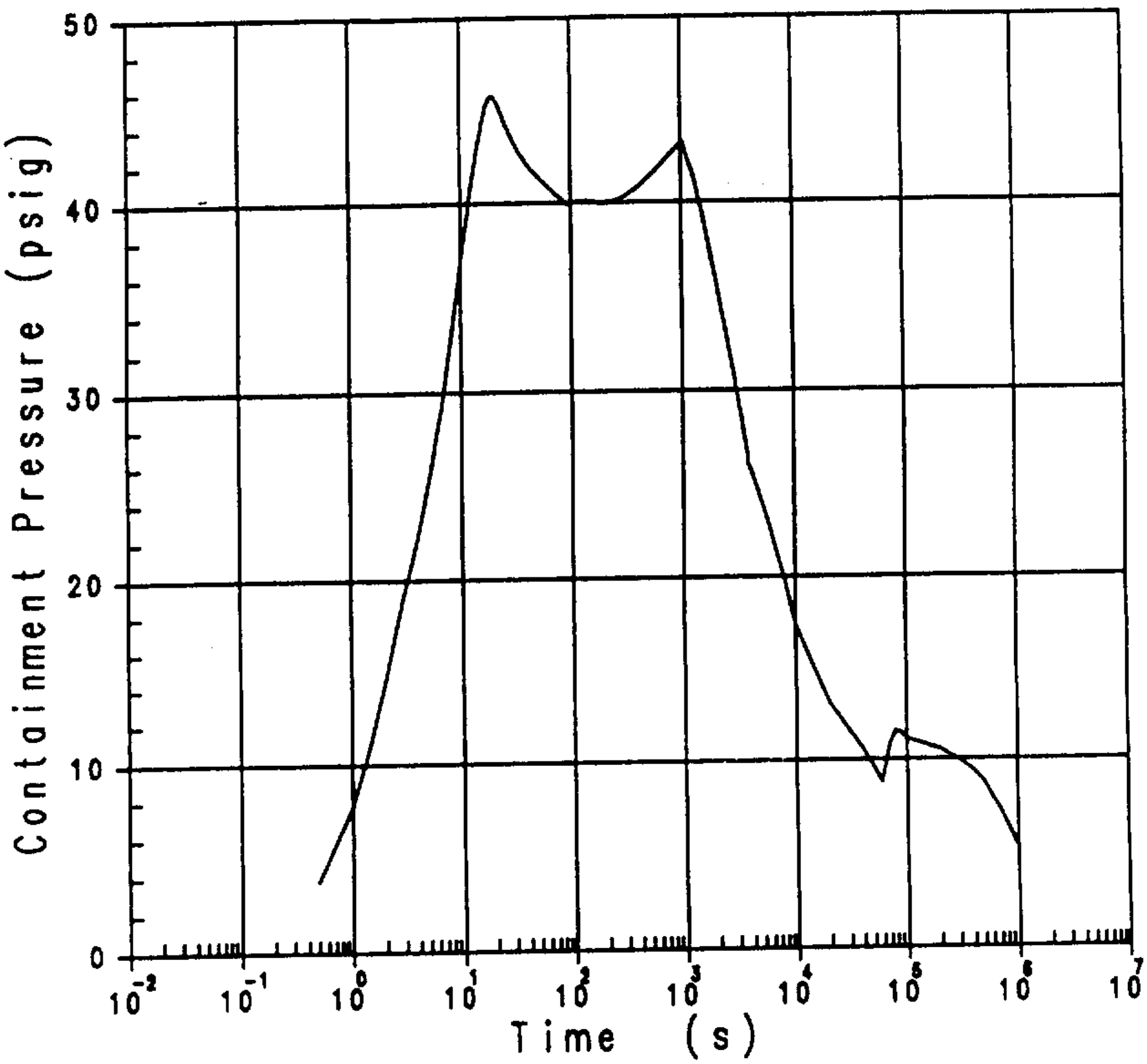
CONTAINMENT INTEGRITY RESULTS

LOCA
(Loss of Offsite Power Assumed)

FAILURE SCENARIO	INITIAL CONT. PRESS (psig)	PEAK PRESS (psig)	TIME OF PEAK (sec)	PEAK TEMP (∘F)	TIME OF PEAK (sec)	PRESS AT 24 HRS (psig)
DEPS w/Diesel, 2 ECCs and Recirc Spray Off at 18 hrs.	0.3	45.8	19.7	270.8	19.7	11.5
DEPS w/Diesel, 1 ECC, 2nd ECC at 24 hrs.; w/Continued Recirc Spray	0.3	46.2	1059.5	271.1	1059.5	7.6
DEHL	0.3	48.1	18.7	273.9	18.7	---

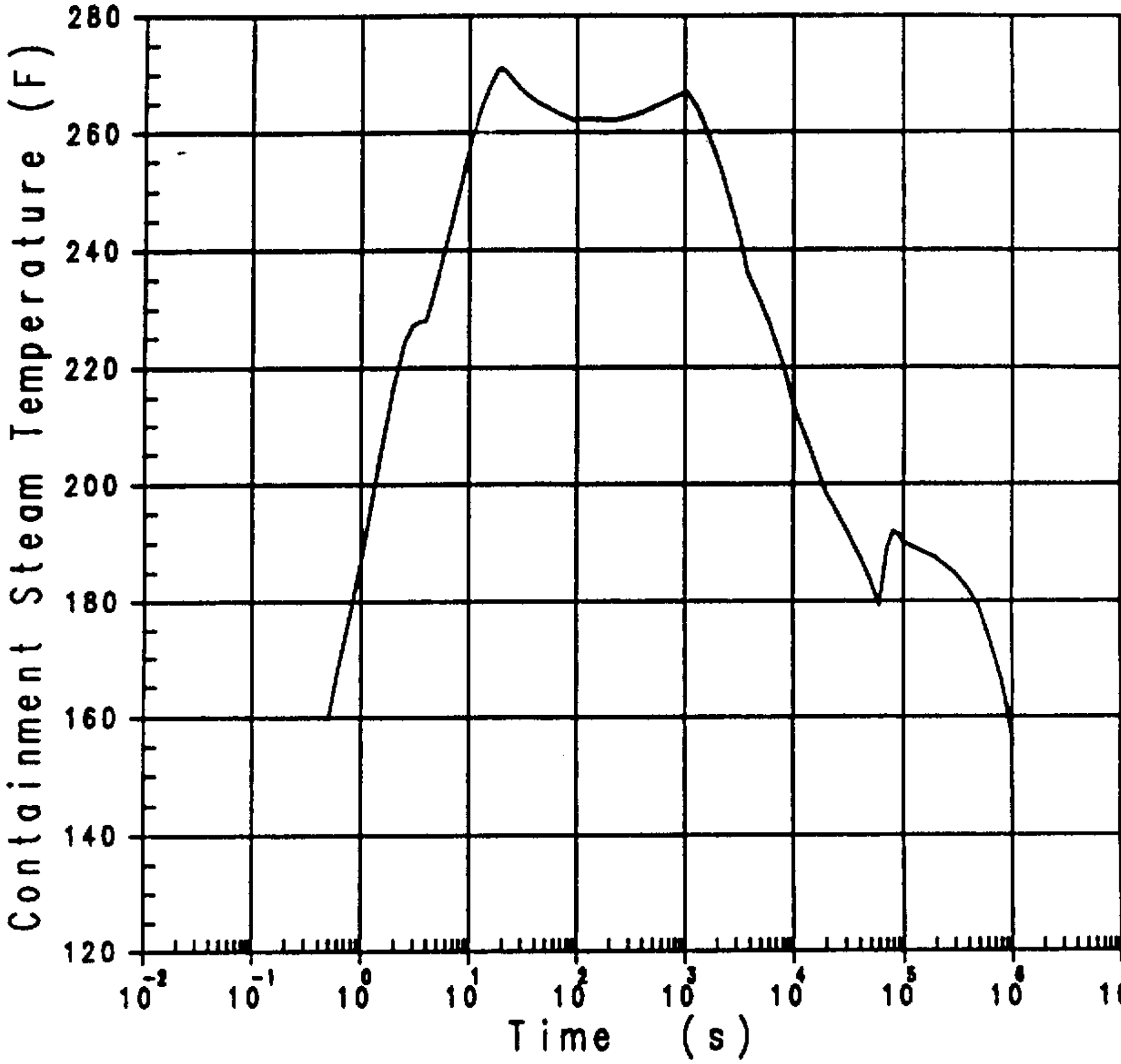
MSLB
(Offsite Power Available)

FAILURE SCENARIO	INITIAL CONT. PRESS (psig)	PEAK PRESS (psig)	TIME OF PEAK (sec)	PEAK TEMP (∘F)	TIME OF PEAK (sec)
1.4 ft^2 DER AT HZP	3.0	48.1	238.3	269.4	238.3



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY **TURKEY POINT PLANT UNITS 3 & 4**
CONTAINMENT PRESSURE DEPS : DIESEL FAILURE CASE WITH 1 CCS AND 2 ECCs AT Pcont = 0.3 PSIG **FIGURE 14.3.4.3-1**

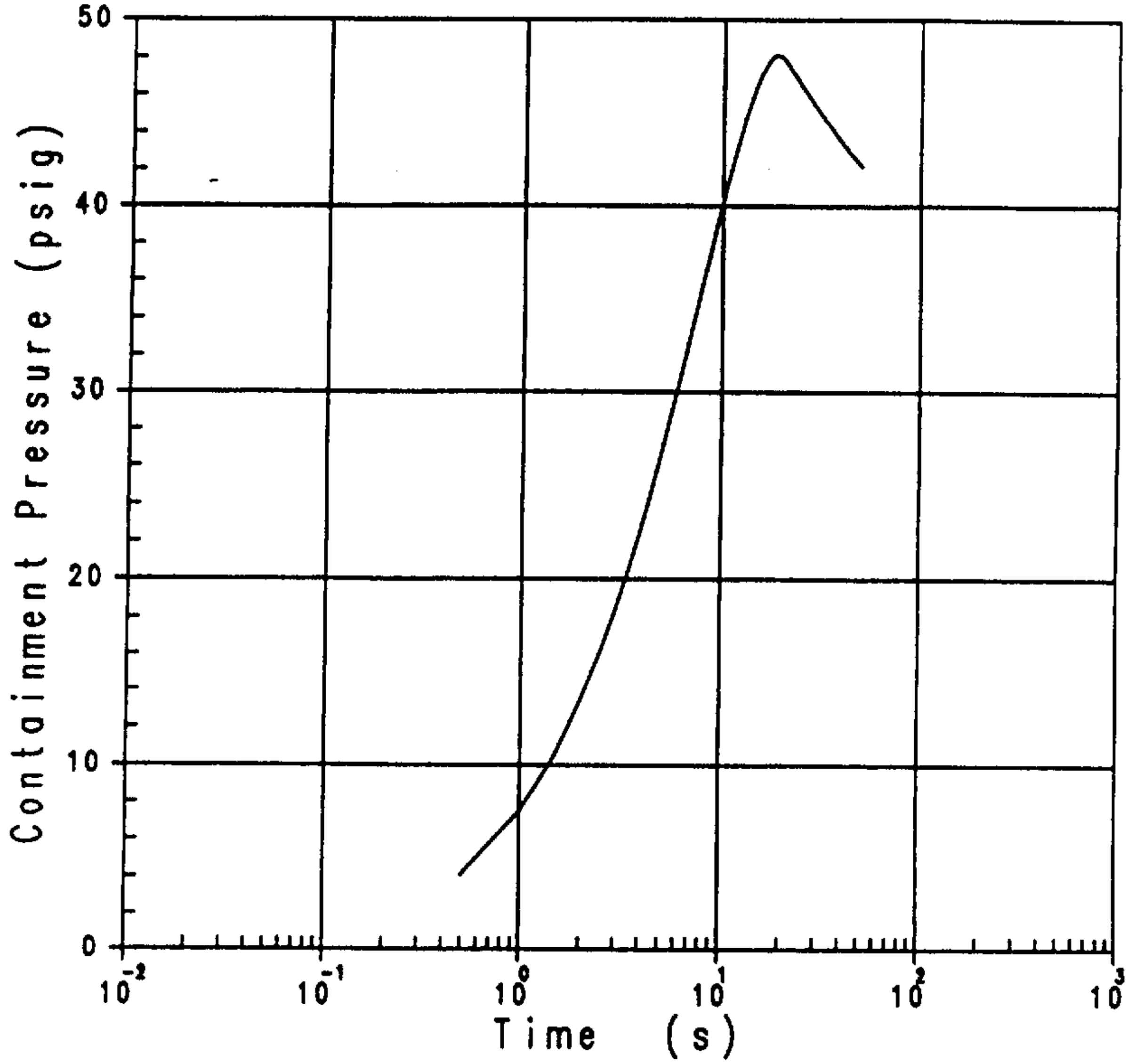


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT STEAM TEMPERATURE
DEPS : DIESEL FAILURE CASE WITH
1 CCS AND 2 ECCs AT Pcont = 0.3 PSIG

FIGURE 14.3.4.3-2

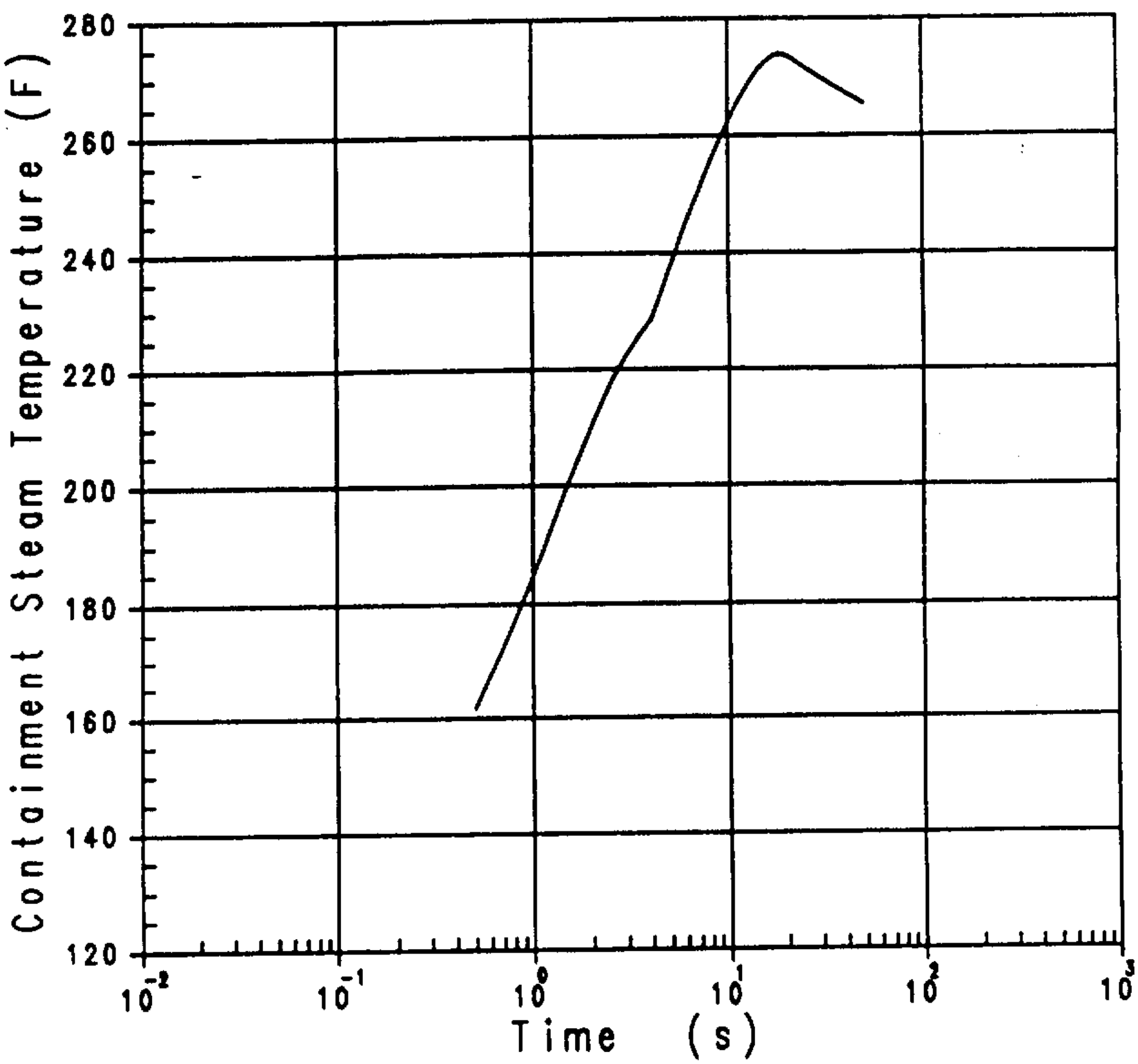


REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT PRESSURE
DEHL : CASE WITH Pcont = 0.3 PSIG

FIGURE 14.3.4.3-3



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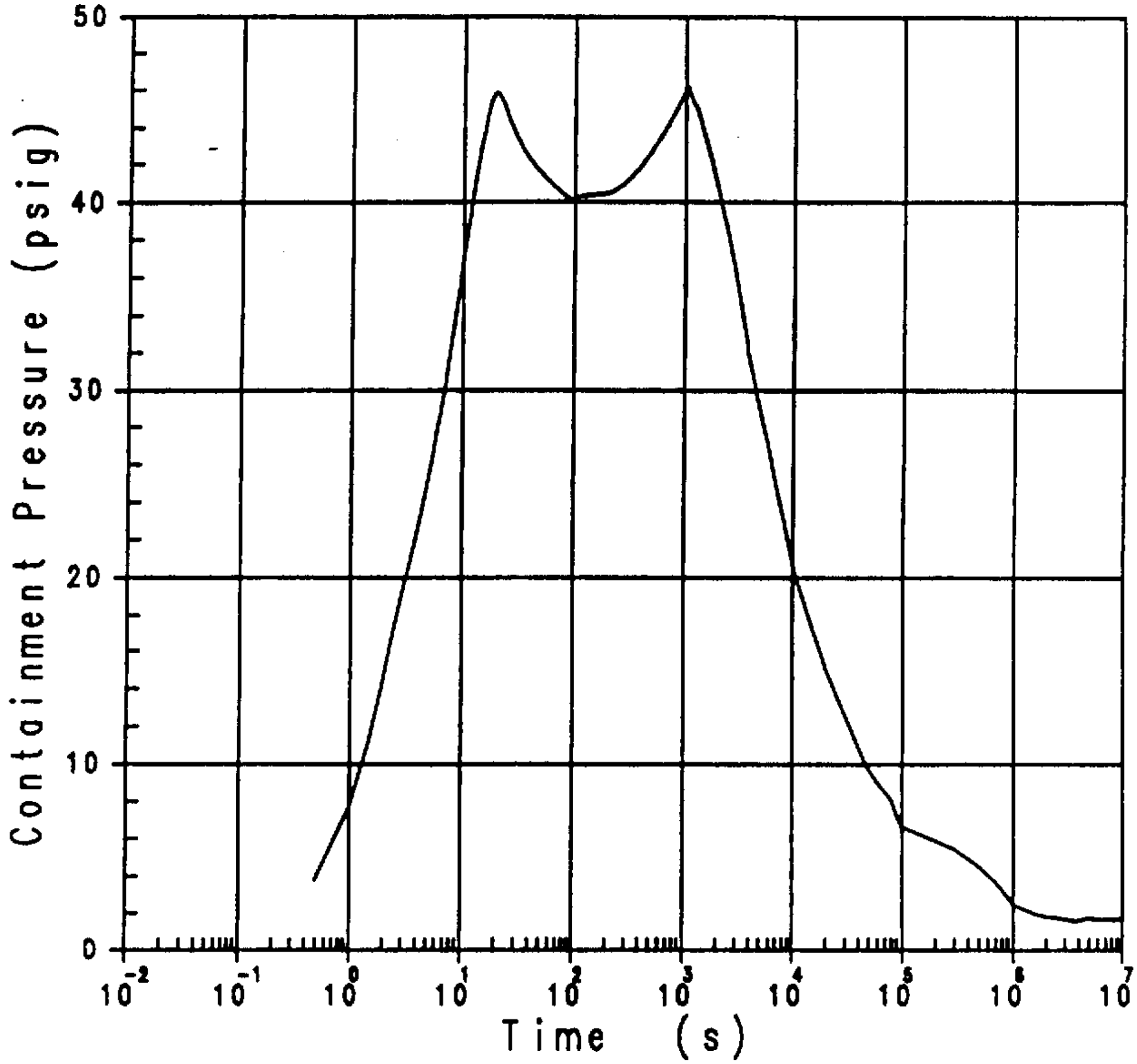
FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT STEAM TEMPERATURE
DEHL : CASE WITH Pcont = 0.3 PSIG

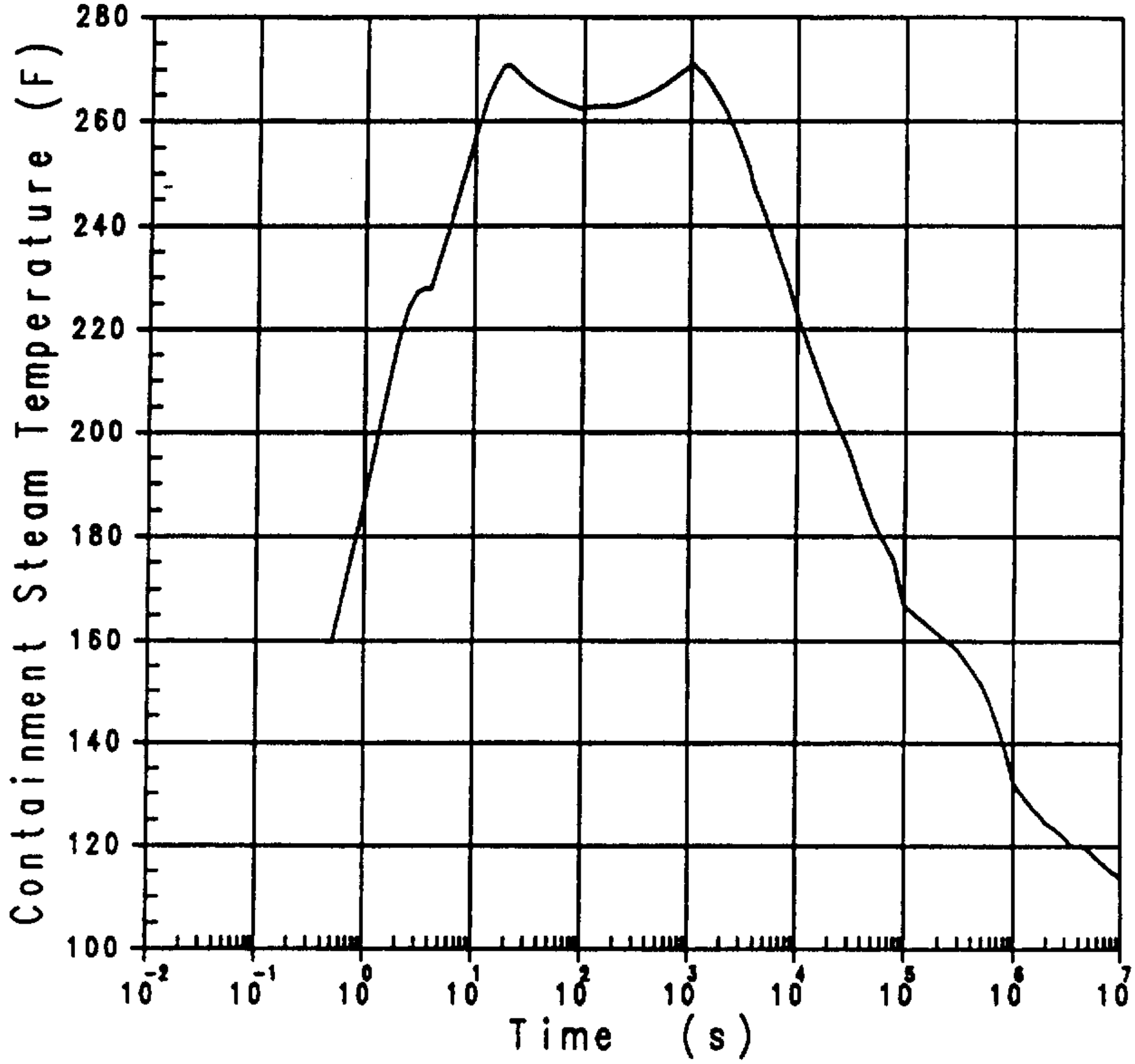
FIGURE 14.3.4.3-4



REV 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT PRESSURE
DEPS : DIESEL FAILURE CASE WITH
1 CCS AND 1 ECCs AT Pcont = 0.3 PSIG
FIGURE 14.3.4.3-5

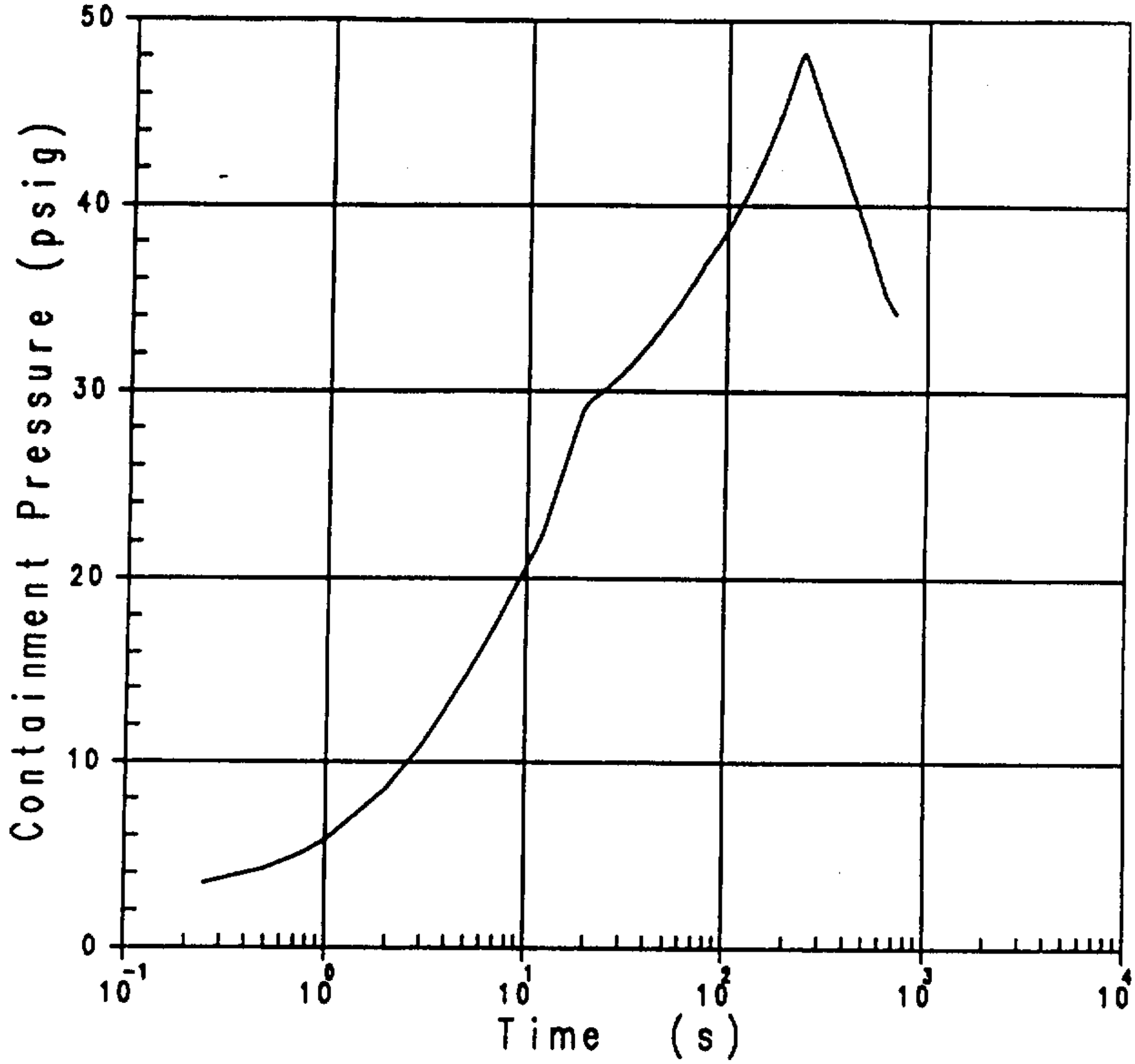


REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT STEAM TEMPERATURE
DEPS : DIESEL FAILURE CASE WITH
1 CCS AND 1 ECCs AT Pcont = 0.3 PSIG

FIGURE 14.3.4.3-6



REV. 14 (2/97)

FLORIDA POWER & LIGHT COMPANY
TURKEY POINT PLANT UNITS 3 & 4

CONTAINMENT PRESSURE
1.4 FT^3 HZP STEAMLINE BREAK,
MSCV FAILURE, 2 ECCs AND CCSs
FIGURE 14.3.4.3-7

14.3.5 ENVIRONMENTAL CONSEQUENCES OF A LOSS-OF-COOLANT ACCIDENT

14.3.5.1 ANALYSIS

The original licensing basis LOCA dose analysis can be found in its entirety in Appendix 14F. Also refer to Appendix 14F for the discussion of the atmospheric dispersion model, whole body dose computations, and the radiological assessment of containment purging. This section describes the LOCA dose analysis performed as part of the Power Uprating Project.

A large pipe rupture in the reactor coolant system (RCS) is assumed to occur. As a result of the accident, it is assumed that core damage occurs and iodine and noble gas activity is released to the containment atmosphere. A portion of this activity is released via the containment purge system, which is open when the accident occurs and activity is released to the atmosphere through this path until the containment purge system is isolated. Also, once Engineered Safety Features (ESF) recirculation is established, leakage from ESF equipment outside containment releases activity to the outside environment.

The uprated power level of 2346 MWt is used in the analysis. Both offsite and control room doses are determined. This includes not only determining doses due to containment leakage but doses due to an open containment purge system.

<u>Containment Leakage</u>

Following the large break LOCA 50% of the core iodine activity and 100% of the core noble gas activity are assumed to be immediately released to containment when determining doses due to containment leakage. Fifty percent of the iodine released to containment is assumed to instantaneously plate out on containment surfaces for this case. This leaves 25% of the core iodine activity and 100% of the core noble gas activity instantaneously available for leakage from the containment. This iodine is assumed to be 91% elemental, 4% methyl and 5% particulate.

The Technical Specification design basis containment leak rate of 0.25% by weight of containment air is used for the initial 24 hours. Thereafter, the containment leak rate is assumed to be one-half the design value, or 0.125% per day.

In addition to the immediate plateout on containment surfaces of 50% of the iodine activity released to containment, the time-dependent deposition on containment surfaces of the remaining elemental iodine is considered. An elemental iodine deposition coefficient of 5.94 hr^{-1} is determined. Credit is taken for this deposition until a decontamination factor of 100 in the containment inventory of elemental iodine is reached.

The SI signal following the large break LOCA starts the emergency containment filtration (ECF) system filter fans. To account for time to allow the fans to be loaded on the emergency diesel generators, to reach operating speed and to add conservatism, credit for the ECF system filters is not taken in the initial 90 seconds following the accident. After 2 hours following the accident, no further credit for iodine removal by the ECFS filters is taken. The filter efficiencies for the ECF system filters are 90% for elemental iodine, 30% for methyl iodide and 95% for particulate iodine.

Containment Purge

The containment purge system is assumed to be open at the time the accident occurs. However, the large break LOCA results in a containment isolation signal, which automatically closes the containment purge system isolation valve. Although the valve closure time is approximately 5 seconds, a closure time of 8 seconds is used in this analysis to account for time for signal generation.

The time at which fuel clad damage would be initiated (i.e., the hot rod burst time) following the accident is well after 8 seconds. Thus, the activity release to containment for this case is limited to the RCS activity prior to the large break LOCA which results in a pre-accident iodine spike. No credit for plateout or deposition on surfaces inside the containment is taken. Since the ECF system units will not be loaded on a diesel generator and running prior to the closure of the containment purge system valve, no credit for ECF system filtration of the iodine is taken.

The iodine release from the RCS to the containment is assumed to be 100% elemental iodine. Since only HEPA filters (which remove particulate iodine) exist in the containment purge system, it is assumed that the iodine release through the system is unfiltered.

The containment purge system flowrate is limited to 7000 cfm when the system is open during power operation of the plant.

The RCS noble gas activity prior to the LOCA is based on a 1.0% fuel defect level.

Control Room Parameters

The doses to personnel in the control room are determined for each of the activity release paths discussed above. The control room volume is 47,786 ft^3, the filtered makeup flow is 525 cfm, the filtered recirculation flow is 375 cfm, and the unfiltered inleakage flow is 10 cfm. The control room filter removal efficiency is 95% for all chemical forms of iodine.

The major assumptions and parameters used to determine the doses as the result of: (1) containment leakage are given in Table 14.3.5-1; and (2) containment purge are given in Table 14.3.5-2. The control room asssumptions and parameters are given in Table 14.3.5-3. The thyroid dose conversion factors, breathing rates and atmospheric dispersion factors used in the dose calculations are given in Table 14.3.5-4.

The offsite doses must meet the guidelines of 10 CFR 100, or 300 rem thyroid and 25 rem whole body for the initial 2 hour period following the accident at the exclusion boundary (EB) and for the duration of the accident at the low population zone (LPZ). The dose criteria for control room personnel following the accident are 5 rem whole body, 30 rem thyroid, and 30 rem β-skin (or 75 rem β-skin with protective clothing).

14.3.5.2 RESULTS

The offsite and control room doses due to containment leakage and containment purge are given in Table 14.3.5-5, along with the total doses due to the activity release from both release paths. The total offsite doses and the total control room doses due to the large break LOCA meet the acceptance criteria.

TABLE 14.3.5-1

ASSUMPTIONS USED
FOR
LARGE BREAK LOCA DOSE ANALYSIS CONTAINMENT LEAKAGE

Power	2346MWt
Iodine Chemical Species :	
Elemental	91%
Methyl	4%
Particulate	5%
Iodine Removal in Containment:	
Instantaneous Iodine Plateout	50%
Elemental Iodine Deposition	5.94 hr^{-1} for DF $\leq$100 0 hr^{-1} for DF >100
Emergency Containment Filters	
Start Delay Time	90 sec
Number of Units	2
Flow Rate per Unit	33,750 cfm
Filter Efficiency	
Elemental	90%
Methyl	30%
Particulate	95%
Operating Time	2 hr
Containment Free Volume	1.55 x 10^6 ft^3
Containment Leak Rate	
0-24 hr	0.25% per day
> 24 hr	0.125% per day

TABLE 14.3.5-2

ASSUMPTIONS USED
FOR
LARGE BREAK LOCA DOSE ANALYSIS CONTAINMENT PURGE

Power	2346 MWt
Reactor Coolant Noble Gas Activity Prior to Accident	1.0% Fuel Defect Level
Reactor Coolant Iodine Activity Prior to Accident	60μCi/gm of DE I-131
Iodine Chemical Form	100% Elemental
Containment Purge System Flow Rate	7000 cfm
Containment Purge System Isolation Time	8 sec
Containment Purge System Filtration	None
ECF System Filtration	None
Iodine Plateout/Deposition in Containment	None
Containment Free Volume	1.55×10^6 ft^3

TABLE 14.3.5-3

ASSUMPTIONS USED
FOR
LARGE BREAK LOCA DOSE ANALYSIS CONTROL ROOM

Volume	47,786 ft^3
Unfiltered Inleakage	10 cfm
Filtered Makeup	525 cfm
Filtered Recirculation	375 cfm
Filter Efficiency:	
Elemental	95%
Methyl	95%
Particulate	95%
Occupancy Factors:	
0-1 day	1.0
1-4 days	0.6
4-30 days	0.4

TABLE 14.3.5-4

DOSE CONVERSION FACTORS
BREATHING RATES AND
ATMOSPHERIC DISPERSION FACTORS

Isotope	Dose Conversion Factors [(1)] (rem/curie)
I-131	1.07E6
I-132	6.29E3
I-133	1.81E5
I-134	1.07E3
I-135	3.14E4

Time Period (hr)	Breathing Rate[(2)] (m^3/sec)
0-8	3.47E-4
8-24	1.75E-4
24-720	2.32E-4

	Atmospheric Dispersion Factors (sec/m^3)
Exclusion Boundary (0-2 hr)	1.54E-4
Low Population Zone	
0-2 hr	1.50E-5
2-12 hr	6.50E-6
12-720 hr	2.40E-7
Control Room	
0-8 hr	9.58E-4
8-24 hr	7.52E-4
24-96 hr	5.26E-4
96-720 hr	2.94E-4

NOTES:

1. ICRP Publication 30.
2. NRC Regulatory Guide 1.4

TABLE 14.3.5-5

LARGE BREAK LOCA OFFSITE AND CONTROL ROOM DOSES

	Thyroid Dose Dose (Rem)		
	EB (0-2 Hr)	LPZ (0-30 Day)	CR*(0-30 Day)
Containment Leakage	2.33 E+1	2.76 E+0	1.49 E+1
Containment Purge	2.91 E-1	2.83 E-2	7.28 E-2
Total	2.36 E+1	2.80 E+0	1.50 E+1

	Whole Body Dose Dose (Rem)		
	EB (0-2 Hr)	LPZ (0-30 Day)	CR*(0-30 Day)
Containment Leakage	1.04 E+0	1.61 E-1	4.36 E-1
Containment Purge	6.48 E-5	6.31 E-6	1.08 E-5
Total	1.04 E+0	1.61 E-1	4.36 E-1

	Control Room β-Skin Dose Dose (Rem)
	30 Day
Containment Leakage	2.01 E+1
Containment Purge	8.90 E-4
Total	2.01 E+1

NOTE:

1. CR = Control Room

14.3.6 HYDROGEN CONCENTRATION CONTROL

Sources and Characteristics of Hydrogen

For several months following a maximum hypothetical accident there would be gradual rise in hydrogen concentration in the reactor containment. Hydrogen is generated by radiolysis of the reactor coolant, by the zirconium-water reaction and by chemical reaction of materials in the post-accident containment environment. If corrective measures were not taken, a hydrogen concentration level might be reached where a flammable recombination reaction with oxygen would occur releasing additional energy within the containment. The resultant rise in temperature and pressure would not be expected to affect the containment vapor barrier integrity nor the health and safety of the public.

The lower flammability limit for hydrogen in saturated air at room temperature and atmospheric pressure is 4.1 volume percent (References 1, 2, and 3). The propagation characteristics in the flammability range up to about 18 v/o lie within subsonic velocities. Flame propagation occurs only in the upward direction up to 6 v/o concentration because the rate of convective rise is greater than its rate of propagation. Up to 9 v/o concentration both horizontal and upward propagation occurs. From 9 to 18 v/o the rate of flame propagation increases rapidly in all directions. Detonation occurs at concentrations above 18 v/o.

Not all of the hydrogen burns when ignition occurs in concentrations under 10 v/o. At about 5.6 v/o, only 50% of the hydrogen initially present recombines. Sparks from electrical equipment or hot surfaces can cause ignition. Hydrogen ignites also without a spark or other external energy supply when the temperature is sufficiently high (Reference 4). This spontaneous ignition temperature varies with emission velocity and steam content, and occurs conservatively, at 1256°F for low velocities and high vapor concentrations, down to as low as about 968°F where a hydrogen jet impinges on a solid object at high velocity.

A. Radiolysis of Water

Following the postulated accident, a potentially major source of hydrogen production would result from the decomposition of water by radiolysis. Such decomposition of water is caused by the complex interaction of ionizing radiation and water or dilute aqueous solutions. The initial products of radiolysis are generally believed to be the hydrated electron $e^{-}_{(aq)}$, the OH^{-} radical, and H_3O^{+} and are formed along the path of energy absorption. These initial products next either react with one another or other constituents of the solution. These subsequent reactions occur, with different rate constants, to form hydrogen, hydrogen peroxide, and oxygen in addition to other products. These subsequent reactions are also responsible for a certain amount of recombination which can occur. The essential net result is the generation of oxygen and hydrogen gases unless the solutions contain material which reacts with them.

In a closed system, the net rate of decomposition of water eventually becomes zero. The exact equilibrium concentrations, however, depend upon a number of factors such as water purity, the amount of hydrogen, hydrogen peroxide or oxygen in the solution. It is important to note, however, that the equilibrium concentration is strongly affected by the loss from the system of gaseous reaction products. Since the situation at Turkey Point limits this loss, the calculation of the equilibrium value is conservative.

Following the accident, it is not possible to determine the degree to which gaseous reaction products are lost from the water since for some period of time following the accident the emergency core cooling water may be at or near saturation enthalpy. Furthermore, the coolant is not pure water but contains boric acid, materials added for pH control and various corrosion and fission products.

The rate of hydrogen production is customarily expressed in terms of G values. Primary or direct yields of a species are indicated by a subscript, e.g., G_{H2}, and the net production considering secondary reactions is indicated by a parenthetical notation e.g., $G(H_2)$.

For pure water, there appears to be sufficient evidence that the maximum rate of production of the species, H_2, as a result of beta and gamma radiation is 0.44 molecules of hydrogen per 100 electron volts absorbed, or G_{H2} = 0.44. For pure water or dilute solutions which do not contain reactive solutes the maximum net yield of hydrogen is equal to the initial direct yield when no recombination occurs, hence $G_{H2} = G(H_2)$.

Westinghouse studies of radiolysis in dynamic systems (Reference 5) show 0.44 molecules per 100 ev to be a maximum yield for high solution flow rates through a gamma radiation field. Work by ORNL (References 6 and 7), Zittel (Reference 8), and Allen (Reference 9) confirm this value.

A value of $G(H_2)$ = 0.44 is a representative maximum value to describe the net hydrogen yield immediately following the loss-of-coolant accident. This value would be expected to decrease somewhat as coolant temperature decreases and, hence, gas solubility increases resulting in increasing recombination within the liquid.

The energy source of radiolysis derives from the decay of fission products originally located within the fuel rods. Following a severe loss-of-coolant accident, some cladding damage is expected and consequently a fraction of the more volatile fission products contained in the fuel rod gas gap would be released and be distributed throughout the water and atmosphere within the containment.

To be consistent with the general approach used to evaluate the offsite effects of a major accident with a nuclear reactor, the released fission products are grouped into three broad categories, viz, the halogens, the noble gases, and solids.

It is worth noting at this point that the hydrogen yield from a given amount of any fission product is greater if that fission product is dissolved or suspended in the coolant than if it remained within the fuel rod. This is because essentially all the beta energy and all but a few percent of the gamma energy is absorbed within the fuel rod. Therefore, to be conservative, the assumptions regarding fission product release are the same as is used for reactor siting purposes as described in TID-14844 (Reference 10). These assumptions are:

a. 100% release of noble gases.
b. 50% release of halogens.
c. 1% release of "solids".

The total radiolytic hydrogen produced is the sum of that produced by fission products retained in the core and that produced by fission products released from the core but which remain with the coolant. Since energy is produced from these two sources at different rates, the hydrogen production from these sources are determined separately.

1. In-Core Contribution

The in-core contribution is determined from the fission product decay energy, based on the assumption that 7.4% of the gamma energy is absorbed by the solution in the region of the core. It is assumed that the noble gases escape to the containment vapor space.

The $G(H_2)$ value described above, 0.44 molecules per 100 ev, is utilized in the analysis.

2. Out-of-Core Contribution

In the case of the out-of-core contribution to radiolysis, the total decay energy from the released fission products, both beta and gamma, is assumed to be fully absorbed in the solution.

The depth of the sump solution inhibits the ready diffusion of hydrogen from solution; this retention of hydrogen in solution will have a significant effect in reducing the hydrogen yields to the containment atmosphere. The buildup of hydrogen in solution will enhance the back reaction to form water and lower the net hydrogen yields in the same manner as a reduction in the gas to liquid volume ratio will reduce the yield. Based on the work of Bell

(Reference 11), a value of 0.30 molecules per 100 ev has been used for the net G value when computing the net production of hydrogen in the sump water.

3. Total Radiolytic Hydrogen

The amount of hydrogen produced in-core and out-of-core and the total radiolytic production are included in Figure 14.3.6-1. It was assumed that the reactor had been operating at 2346 Mw(t) and that just prior to the loss-of-coolant accident the containment temperature was 130°F at 1 atmosphere pressure. The containment atmosphere temperature at the time of the accident affects the initial amount of air with which the hydrogen will be mixed and decreases with increasing initial temperature; hence, the value selected is based on the highest expected normal containment temperature during operation.

B. Zirconium - Water Reaction

Zirconium will react with steam given the proper conditions according to the following reaction:

$$Zr + 2H_2O \rightarrow ZrO_2 + 2H_2$$

The reaction rate becomes significant at a temperature of 1800°F and increases rapidly with increasing temperature. Thus, the hydrogen would be formed in an environment at a temperature considerably higher than that required for ignition. However, the action of the emergency core cooling systems will limit temperatures attained by the reactor core following a loss-of-coolant such that only a small fraction of the Zirconium in the core will react. Calculations indicate that a small fraction of 1% of the Zirconium in the core will react. Because of the temperature distribution across the core, the highest local fraction reached will be less than 1% while some parts of the core will not experience any reaction.

The reactor core contains approximately 36,800 lb of Zircaloy; 36,300 lb is cladding which is potentially subject to the high temperature required for significant reaction.

For conservatism, the amount of Zirconium reacted is assumed to be 5% or 1840 lbs. This reaction is assumed to occur essentially instantaneously.

The hydrogen discharge issuing from a reactor coolant pipe rupture would be impinging on solid objects at high velocity which spontaneous ignition temperature was earlier stated to be approximately 968^{o}F. Thus, in order to prevent ignition as hydrogen flows from the break, it would be necessary to cool it by at least 544^{o}F, or more likely, by as much as 832^{o}F.

Calculations have shown that the heat loss from the hydrogen stream to the reactor coolant structure will not reduce the temperature below the spontaneous ignition temperature along the direct flow path to the rupture location. Cooling by mixing with saturated vapor does not appear likely considering that the zirconium - water reaction model assumes the availability and consumption of steam to sustain the reaction.

C. Corrosion of Metals

The problem of corrosion of metals has received a great deal of study and has been found to be a very complex subject. Although it is generally believed that corrosion is basically an electrochemical process, there are questions of protective films, polarization, oxidation, concentration cells and electrode potentials which confuse the issue so that practical solutions to corrosion problems are largely empirical. The fact that corrosion studies are slanted to the protection of the metal makes it difficult to apply the available information on corrosion to the problem which concerns us here, i.e., the generation of hydrogen within the containment after a loss-of-coolant accident.

To better understand the complexities of the corrosion problem, a brief review of the sequence of events following the postulated MHA is presented. On the initiation of the break, the reactor cooling system water will spurt out, partly flashing into steam, and impinge on any equipment in its path. The water will flow down all paths available to it and collect in the bottom of the containment. The composition of the solution collecting in the containment bottom initially will have the same composition that it had in the reactor coolant system when the reactor was operating at power except to be somewhat concentrated because of the flashing to steam. At the beginning of life, this composition could be as high as 1250 ppm of boron as boric acid with the pH adjusted by the addition of a chemical, such as lithium hydroxide. At the end of life, the boron concentration in the primary coolant will be essentially zero, and there may be a very small amount of lithium present for pH adjustment.

A few seconds after the break, boron will start to be injected into the reactor coolant system. The system contains 1950 to 2050 ppm boric acid. The solution from the accumulators and the refueling water storage tank will fill the reactor coolant system as far as possible with the remainder spilling and running into the containment bottom. Accordingly, the solution discharging onto the equipment within the containment at the break may start out as a neutral or slightly alkaline solution and then will become more acid as the blowdown proceeds.

Approximately 30-60 seconds after the break, the containment spray system will start to operate, spraying water from the refueling water storage tank into the top of the containment. Accordingly, all of the components and structures in the containment will be drenched by this boric acid spray. The water in the bottom of the containment will be a solution which probably will be somewhat alkaline and will become more acidic as the spray continues until a maximum of approximately 300,000 gallons of solution have been used.

The spray introduced into the containment will rapidly come to temperature equilibrium with the air-steam atmosphere. The temperature of the containment atmosphere will reach 270°F approximately ten seconds after the break and fall slowly (Reference 12). This figure is for the minimum safeguards operating. After a period of time, the pumps' suction will be switched from the refueling water storage tanks to the containment sump. The volume of solution in the sump will be approximately 300,000 gallons when the reactor coolant system has been refilled. Depending upon the location of the break, some portion of the approximately 65,000 gallons of reactor coolant volume will be added to the 300,000 gallons in the sump.

Once the recirculation mode is started, the composition of the solution sprayed in the containment and that in the sump will be the same, except that the spray liquid will contain appreciable amounts of dissolved oxygen due to the exposure to the containment atmosphere. Within the primary system and in portions of the sump the dissolved oxygen may be consumed in the corrosion reactions.

Within containment there are a variety of metals and zinc-rich coatings which can potentially be important sources of hydrogen generation during the post-accident period. The most important materials include galvanized coatings, non-coated and coated zinc primers, and exposed aluminum metal. The total inventory of these types of materials which have the potential to generate hydrogen by chemical reaction in the post-accident containment environment were calculated (Reference 15). Hydrogen production was then calculated for these inventories of zinc and aluminum within containment.

For zinc and aluminum, the reactions of concern are the following:

$$Zn + H_2O \rightarrow ZnO + H_2$$

and,

$$2Al + 3H_2O \rightarrow Al_2O_3 + 3H_2$$

Corrosion rates for galvanized metal, coated steel, and aluminum are based on industry data or national laboratory experiments to evaluate corrosion rates in a post-LOCA environment. Corrosion rates for aluminum metal are taken from Burchell and D.D. Whyte (Reference 13), based on an anticipated pH of 7.

In the case of the zinc material in containment, a distinction was made between zinc metal and zinc primer with epoxy topcoat. The latter was based on NUREG/CR-3803 (Reference 14). This approach acknowledges the fact that the qualified coating will remain intact, at least for some period of time following the accident. According to Reference 14, failure of the phenolic topcoat in the vapor/spray mode occurs "via a cracking of the phenolic, but with no delamination." The tests described in Reference 14 show that "the cracked phenolic in these cases did not become detached from the substrate, but remained bonded to the primer." It is reasonable to make use of a decreased corrosion rate in consideration of this fact.

D. Total Hydrogen Generation

The total hydrogen generated from the radiolysis of water, the zirconium-water reaction, and metal corrosion are given in Figure 14.3.6-1. The zirconium-water contribution assumes a 5% reaction takes place immediately following the MHA, while the contributions from radiolysis and corrosion are time dependent.

E. Dispersion of Local H_2 Concentrations

The results of the hydrogen calculation are presented in Figures 14.3.6-1 and 14.3.6-2. Figure 14.3.6-1 shows the total hydrogen gas accumulation associated with the calculation. No removal term has been assumed in this figure, and accumulation values are presented in standard cubic feet.

Two additional calculations were performed to demonstrate the effectiveness of a hydrogen recombiner with a limiting flow rate of at least 30 scfm. In these runs, a hydrogen recombiner was modeled as being placed in service at the beginning of the thirteeth day following a LOCA. The first of the two was based on a recombiner flow rate of 30 scfm, while the second run assumed a recombiner flow rate of 40 scfm. The results of both calculations are plotted in Figure 14.3.6-2.

As Figure 14.3.6-2 shows, both recombiner cases show an immediate reduction or reversal of containment hydrogen buildup following the twelfth day after a LOCA. As this plot demonstrates, the initiation of recombiner operation within twelve days (i.e., by the beginning of the thirteenth day) of a LOCA event will maintain hydrogen concentrations below four volume percent.

Control of Post-Accident Combustible Gases

The generation rate of hydrogen is sufficiently low that control of its concentration in the containment could be adequately handled on an administrative basis after the other more urgent considerations are resolved in placing the reactor at cold shutdown and in containing of radioactivity following an MHA. The H_2 concentration would be determined by periodic measurements made on containment gas samples. Residual containment leakage would certainly maintain the H_2 concentration sufficiently low that, were the latent energy released by ignition, the containment vapor barrier integrity would be assured. At the option of plant operations, a controlled, monitored bleed-off from the containment will be initiated for release through the plant vent.

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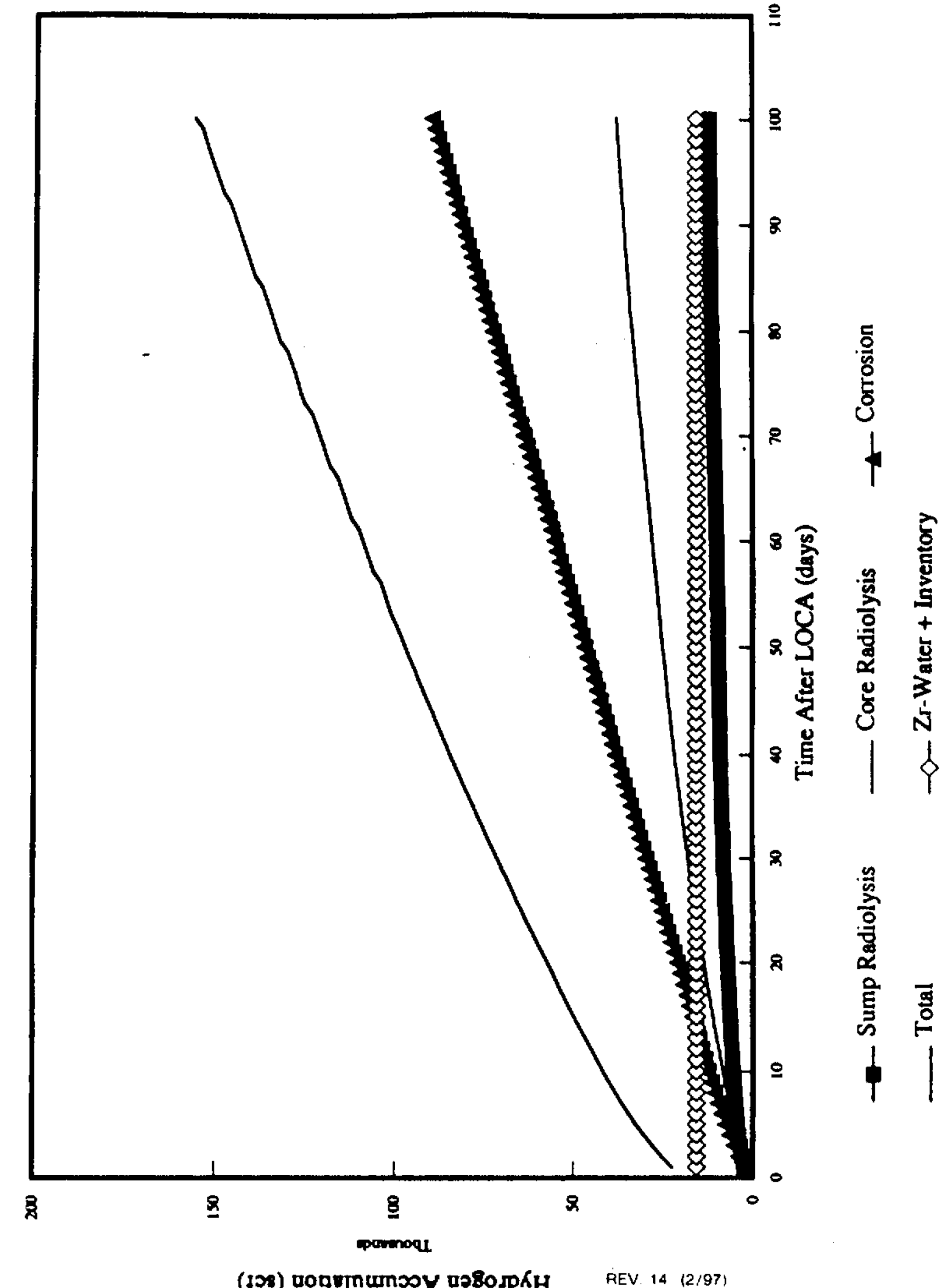

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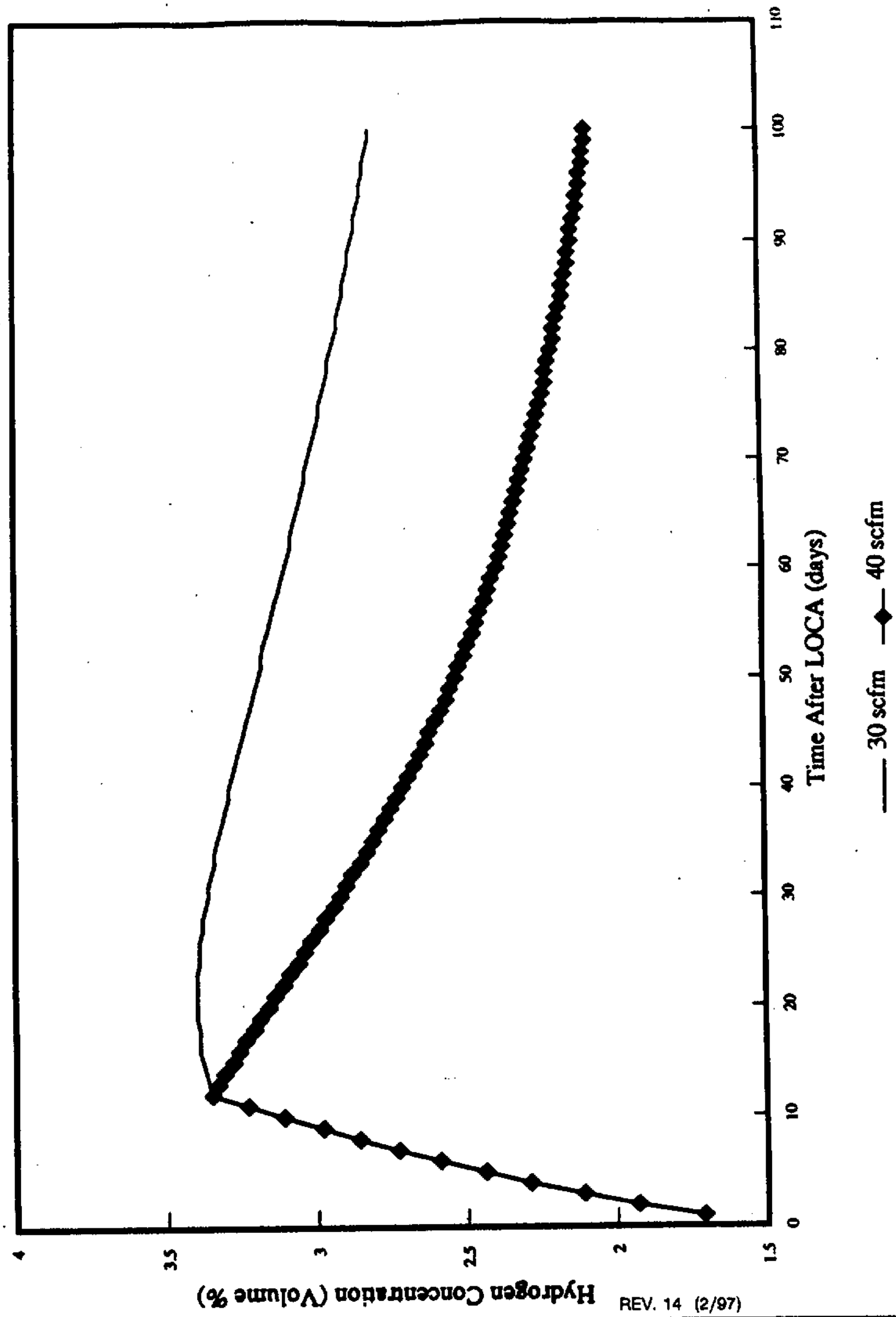
TURKEY POINT PLANT UNITS 3 & 4

HYDROGEN ACCUMULATION -
NO RECOMBINER
5% ZIRCONIUM-WATER REACTION
FIGURE 14.3.6-1



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CONTAINMENT HYDROGEN CONCENTRATION WITH INITIATION OF RECOMBINER AFTER 12 DAYS

FIGURE 14.3.6-2