Westinghouse
Electric Corporation

Water Reactor
Divisions

Nuclear Fuel Division

Box 3912
Pittsburgh Pennsylvania 15230

CAW-84-78
August 7, 1984

Mr. Harold R. Denton Director
Office of Nuclear Reactor Regulation
U. S. Nuclear Regulatory Commission
Phillips Building
7920 Norfolk Avenue
Bethesda, Maryland 20014

Dear Mr. Denton:

APPLICATION FOR WITHHOLDING PROPRIETARY
INFORMATION FROM PUBLIC DISCLOSURE

Reference: Wisconsin Electric Power Company letters, Fay to Denton, dated March 14, 1983 and September 6, 1983

The proprietary material for which withholding is being requested by the Wisconsin Electric Power Company is proprietary to Westinghouse and withholding is requested pursuant to the provisions of Paragraph (b)(1) of Section 2.790 of the Commission's regulations. Withholding from public disclosure is requested with respect to the subject information which is further identified in the affidavit accompanying this application.

The proprietary material transmitted by the referenced letter supplements the proprietary material previously submitted. Further, the affidavit submitted to justify the previous material was approved by the Commission on April 17, 1978, and is equally applicable to the subject material.

Accordingly, withholding the subject information from public disclosure is requested in accordance with the previously submitted affidavit, AW-76-60, a copy of which is attached.

Accordingly, this letter authorized the use of the proprietary information and affidavit AW-76-60 by the Wisconsin Electric Power Company for the Point Beach Nuclear Plant.

Correspondence with respect to this application for withholding or the accompanying affidavit should reference CAW-84-78 and be addressed to the undersigned.

Very truly yours,

Robert A. Wiesemann

Robert A. Wiesemann, Manager
Regulatory and Legislative Affairs

Enclosures(s)
cc: E. C. Shomaker, Esq.
Office of the Executive Legal Director, NRC

ENCLOSURE 4

AW-76-60

AFFIDAVIT

COMMONWEALTH OF PENNSYLVANIA:

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COUNTY OF ALLEGHENY:

Before me, the undersigned authority, personally appeared Robert A. Wiesemann, who, being by me duly sworn according to law, deposes and says that he is authorized to execute this Affidavit on behalf of Westinghouse Electric Corporation ("Westinghouse") and that the averments of fact set forth in this Affidavit are true and correct to the best of his knowledge, information, and belief:

Robert A. Wiesemann

Robert A. Wiesemann, Manager
Licensing Programs

Sworn to and subscribed
before me this 2 day
of December 1976.

Notary Public

(1) I am Manager, Licensing Programs, in the Pressurized Water Reactor Systems Division, of Westinghouse Electric Corporation and as such, I have been specifically delegated the function of reviewing the proprietary information sought to be withheld from public disclosure in connection with nuclear power plant licensing or rule-making proceedings, and am authorized to apply for its withholding on behalf of the Westinghouse Water Reactor Divisions.

(2) I am making this Affidavit in conformance with the provisions of 10 CFR Section 2.790 of the Commission's regulations and in conjunction with the Westinghouse application for withholding accompanying this Affidavit.

(3) I have personal knowledge of the criteria and procedures utilized by Westinghouse Nuclear Energy Systems in designating information as a trade secret, privileged or as confidential commercial or financial information.

(4) Pursuant to the provisions of paragraph (b)(4) of Section 2.790 of the Commission's regulations, the following is furnished for consideration by the Commission in determining whether the information sought to be withheld from public disclosure should be withheld.

(i) The information sought to be withheld from public disclosure is owned and has been held in confidence by Westinghouse.

(ii) The information is of a type customarily held in confidence by Westinghouse and not customarily disclosed to the public. Westinghouse has a rational basis for determining the types of information customarily held in confidence by it and, in that connection, utilizes a system to determine when and whether to hold certain types of information in confidence. The application of that system and the substance of that system constitutes Westinghouse policy and provides the rational basis required.

Under that system, information is held in confidence if it falls in one or more of several types, the release of which might result in the loss of an existing or potential competitive advantage, as follows:

(a) The information reveals the distinguishing aspects of a process (or component, structure, tool, method, etc.) where prevention of its use by any of Westinghouse's competitors without license from Westinghouse constitutes a competitive economic advantage over other companies.

(b) It consists of supporting data, including test data, relative to a process (or component, structure, tool, method, etc.), the application of which data secures a competitive economic advantage, e.g., by optimization or improved marketability.

(c) Its use by a competitor would reduce his expenditure of resources or improve his competitive position in the design, manufacture, shipment, installation, assurance of quality, or licensing a similar product.

(d) It reveals cost or price information, production capacities, budget levels, or commercial strategies of Westinghouse, its customers or suppliers.

(e) It reveals aspects of past, present, or future Westinghouse or customer funded development plans and programs of potential commercial value to Westinghouse.

(f) It contains patentable ideas, for which patent protection may be desirable.

(g) It is not the property of Westinghouse, but must be treated as proprietary by Westinghouse according to agreements with the owner.

There are sound policy reasons behind the Westinghouse system which include the following:

(a) The use of such information by Westinghouse gives Westinghouse a competitive advantage over its competitors. It is, therefore, withheld from disclosure to protect the Westinghouse competitive position.

(b) It is information which is marketable in many ways. The extent to which such information is available to competitors diminishes the Westinghouse ability to sell products and services involving the use of the information.

(c) Use by our competitor would put Westinghouse at a competitive disadvantage by reducing his expenditure of resources at our expense.

(d) Each component of proprietary information pertinent to a particular competitive advantage is potentially as valuable as the total competitive advantage. If competitors acquire components of proprietary information, any one component may be the key to the entire puzzle, thereby depriving Westinghouse of a competitive advantage.

(e) Unrestricted disclosure would jeopardize the position of prominence of Westinghouse in the world market, and thereby give a market advantage to the competition in those countries.

(f) The Westinghouse capacity to invest corporate assets in research and development depends upon the success in obtaining and maintaining a competitive advantage.

(iii) The information is being transmitted to the Commission in confidence and, under the provisions of 10 CFR Section 2.790, it is to be received in confidence by the Commission.

(iv) The information is not available in public sources to the best of our knowledge and belief.

(v) The proprietary information sought to be withheld in this submittal is that which is appropriately marked in the attachment to Westinghouse letter number NS-CE-1298, Eicheldinger to Stolz, dated December 1, 1976, concerning information relating to NRC review of WCAP-8567-P and WCAP-8568 entitled, "Improved Thermal Design Procedure," defining the sensitivity of DNB ratio to various core parameters. The letter and attachment are being submitted in response to the NRC request at the October 29, 1976 NRC/Westinghouse meeting.

This information enables Westinghouse to:

(a) Justify the Westinghouse design.

(b) Assist its customers to obtain licenses.

(c) Meet warranties.

(d) Provide greater operational flexibility to customers assuring them of safe and reliable operation.

(e) Justify increased power capability or operating margin for plants while assuring safe and reliable operation.

(f) Optimize reactor design and performance while maintaining a high level of fuel integrity.

Further, the information gained from the improved thermal design procedure is of significant commercial value as follows:

(a) Westinghouse uses the information to perform and justify analyses which are sold to customers.

(b) Westinghouse sells analysis services based upon the experience gained and the methods developed.

Public disclosure of this information concerning design procedures is likely to cause substantial harm to the competitive position of Westinghouse because competitors could utilize this information to assess and justify their own designs without commensurate expense.

The parametric analyses performed and their evaluation represent a considerable amount of highly qualified development effort. This work was contingent upon a design method development program which has been underway during the past two years. Altogether, a substantial amount of money and effort has been expended by Westinghouse which could only be duplicated by a competitor if he were to invest similar sums of money and provided he had the appropriate talent available.

Further the deponent sayeth not.

ENCLOSURE 2

Question 1:

The safety analysis for Point Beach references WCAP-9500-A which describes a 17x17 optimized fuel assembly (OFA). Provide justification for its application to the 14x14 OFA used in Point Beach.

Response:

The fuel design bases and criteria for Westinghouse 14x14 OFAs are the same as those discussed in Sections 4.2 and 4.4.1.2 of WCAP-9500 for the Westinghouse 17x17 OFA design. Verification that these criteria are met for Westinghouse fuel in the Point Beach Units is performed using the design methodology and models discussed in WCAP-9272, "Westinghouse Reload Safety Evaluation Methodology". These methods and core models used in the reload transition analysis are the same which have been used in the past Point Beach reload cycle designs. No changes in the nuclear design philosophy, methods or models are necessary due to the transition to OFA. Based on prototype hydraulic testing of the standard and the OFA assemblies, it was concluded that they are hydraulically compatible, and all of the current thermal and hydraulic design criteria are satisfied.

Question 2:

In our Safety Evaluation Report on WCAP-9500, "Reference Core Report 17x17 Optimized Fuel Assembly," the staff required that those plants using the Westinghouse Improved Thermal Design Procedure (ITDP) supply additional information on the plant specific application of the ITDP. Since the licensee is using the ITDP to perform their thermal-hydraulic analyses, we will require the following:

1) Provide the sensitivity factors (S_i) and their range of applicability;

2) If the S_i values used in the Point Beach analyses are different from those used in WCAP-9500, then the applicant should re-evaluate the use of an uncertainty allowance for application of equation 3-2 of WCAP-8567, "Improved Thermal Design Procedure," and they should validate the linearity assumption;

3) If there are any changes to the THINC-IV correlations, or parameter values outside of previously demonstrated acceptable ranges, the staff will require a re-evaluation of the sensitivity factors and/or the use of equation 3-2 of WCAP-8567.

4) Provide and justify the variances and distributions for the input parameters;

5) Justify that the nominal conditions used in the analyses bound all permitted modes of plant operation (including future operating cycles);

6) Provide a discussion of what code uncertainties, including their values, are included in the DNBR analyses; and

7) Provide a block diagram depicting sensors, processing equipment, computer and readout devices for each parameter channel used in the

uncertainty analysis. Within each element of the block diagram identify the accuracy, drift, range, span, operating limits, and setpoints. Identify the overall accuracy of each channel transmitter to final output and specify the minimum acceptable accuracy for use with the new procedure. Also identify the overall accuracy of the final output value and maximum accuracy requirements for each input channel for this final output device.

<u>Response</u>:

1) The sensitivity factors and their range of applicability are given in Table 2-1 for 14x14 OFA fuel. These sensitivities have been determined using the WRB-1 DNB correlation.

2) The S_i values used in the Point Beach OFA analyses are different from those used in WCAP-9500 because the WCAP-9500 sensitivity values are not applicable to the 14x14 OFA fuel geometry. The uncertainty allowance calculation is shown in Tables 2-2 and 2-3 for typical and thimble cells, respectively.

3) For the Point Beach units, the THINC IV code and the WRB-1 DNB correlation are the same as that used in WCAP-9500 for Westinghouse OFA fuel. All parameter values are within the ranges of codes and correlations used, and sensitivity factors have been determined specific to the fuel type over the range of Point Beach plant parameters.

(4), (5), and (7) - The responses to these questions are given in Appendix 1 and Attachments A and B to Appendix 1. The uncertainties conservatively bound those associated with Point Beach instrumentation.

(6) Code uncertainty values that have been used in the DNB calculations are shown in Tables 2-2 and 2-3.

TABLE 2-1

[

]+(a,c)

1565L:6/080684

TABLE 2-2

CALCULATION OF DESIGN DNBR LIMIT FOR TYPICAL CELL

[

]+(a,c)

TABLE 2-3

CALCULATION OF DESIGN DNBR LIMIT FOR THIMBLE CELL

[

]$^{+(a,c)}$

ATTACHMENT TO QUESTION 2

APPENDIX 1

INSTRUMENT UNCERTAINTIES FOR THE POINT BEACH ITDP CALCULATIONS

Use of the Improved Thermal Design Procedure (ITDP) requires the calculation of the standard deviation (σ) for RCS pressure, temperature, power, and flow. For plants using Sostman or Rosemount RTDs, Westinghouse has determined on a generic basis, the uncertainty for the T_{avg} portion of the Rod Control System and a precision RCS flow calorimetric. In addition generic calculations have been made to determine the uncertainty for the Pressurizer Pressure control system and a daily power calorimetric. These generic calculations are outlined in Attachment A. The calculations for Point Beach were performed using input supplied by the plant (Attachment B) and the methodology and equations supplied in Attachment A. Plant specific variations from the generic calculations will be identified in each of the sections covering the four parameters. The first parameter to be discussed is Pressurizer Pressure.

Pressurizer Pressure

The uncertainty in pressure is based on the accuracy of the Pressurizer Pressure control system. Using Equation 3 from Attachment A and the individual uncertainties noted on Attachment B, it was determined that the total uncertainty for the control system is [$]^{+a,c}$. Allowing for the interaction of the Pressurizer spray and heaters results in an uncertainty of [$]^{+a,c}$. Assuming a normal, two sided probability distribution results in σ = [$]^{+a,c}$, which is the value used in the ITDP analysis.

T_{avg}

The uncertainty in T_{avg} is determined by looking at the T_{avg} input to the Rod Control System. As noted in Attachment A an auctioneered value is compared with a reference as a function of power. The two inputs to the control system are T_{avg} ($(T_H + T_C)/2$) and First Stage Turbine Impulse Chamber Pressure (the reference signal). Using Equation 3 of Attachment A and the instrument uncertainties for Point Beach from Attachment B, it was determined that the accuracy of the control system is [ ]$^{+a,c}$. However, this does not include the uncertainty for the control system deadband. The deadband uncertainty is noted on page 7b of Attachment A and when combined with the control system uncertainty, results in a total uncertainty of [ ]$^{+a,c}$. Assuming a normal two sided probability distribution results in σ = [ ]$^{+a,c}$, the value used in the analysis.

Reactor Power

To determine the uncertainty in the daily power calorimetric is somewhat more complicated than determining the uncertainties in pressure and T_{avg}. However section III.3.b of Attachment A lists the generic assumptions and equations used by Westinghouse. A similar calculation was performed for Point Beach using Equation 2 of Attachment A and those uncertainties from Attachment B noted as "Power Calorimetric". Using these and plant specific sensitivity values an equivalent of Table 2b of Attachment A can be constructed. For Point Beach the equivalent is as follows:

Component	Instrument Error	Uncertainty
		$^{+a,c}$
Feedwater Flow		
Venturi		
Thermal Expansion Coefficient		
Temperature		
Material		
Density		
Temperature		
Pressure		
Δp		
Feedwater Enthalpy		
Temperature		
Pressure		
Steam Enthalpy		
Pressure		
Moisture		
Net Pump Heat Addition		

* Dependent Parameters [ ]$^{+a,c}$

** Dependent Parameters [ ]$^{+a,c}$

As noted above some of the parameters are statistically dependent. In Attachment A this was ignored based on the conservatism of the assumed values. However for Point Beach actual plant values were used, therefore the degree of conservatism was less. The Point Beach calculation was performed treating dependent parameters correctly, as noted above. Carrying through the calculations for a single loop, the uncertainty in power is [ ]$^{+a,c}$. For a two loop plant the uncertainty is [ ]$^{+a,c}$. The standard deviation for this parameter, as used in the ITDP calculations is σ = [ ]$^{+a,c}$.

RCS Flow

The uncertainty in RCS Flow is the combination of two uncertainties, 1) a precision flow calorimetric (performed at the beginning of each cycle), and 2) the Cold Leg Elbow Taps (which are normalized to the flow calorimetric). The first uncertainty to be discussed is the flow calorimetric. A flow calorimetric is essentially a power calorimetric with the additional measurement of T_H, T_c and Pressurizer Pressure. Unlike the daily power calorimetric which assumed that the measurement values were from the plant process computer, the precision flow measurement assumes that the most accurate means reasonably available is used. This implies the use of recently calibrated special test instrumentation and DVMs. It also assumes that multiple measurements of each loop's parameters are made over an appreciable period of time (typically once every five minutes over a one hour period). These two assumptions eliminate drift effects and small parameter variations due to power or temperature oscillations. In addition, the measurement should be performed at the beginning of the cycle (or use an LEFM) to eliminate possible venturi fouling after startup.

The basic methodology and equations used are noted in Section III.4.b of Attachment A. For the Point Beach specific calculations, Equation 1 of Attachment A and those uncertainties from Attachment B noted as "Flow Calorimetric" were used. In addition Point Beach has noted that multiple channels will be measured for a given parameter on a loop. An example of this is Steamline Pressure. There are three channels for measuring Steamline Pressure on each of the two steamlines. All three of the channels will be measured and averaged to calculate the average steamline pressure for that loop for the period of the measurement. Those parameters for which multiple channels will be measured on each loop are:

Steamline Pressure - 3 channels/loop,
T_H - 2 channels/loop and
T_c - 2 channels/loop.

In addition all four Pressurizer Pressure channels will be averaged, thus requiring this uncertainty to be treated as a system error, not a loop error. It should also be noted that Point Beach has installed a Leading Edge Flow Meter

(LEFM) in the common feedwater header. Use of the LEFM would then require treatment of the feedwater flow uncertainty as a system error.

Based on use of the uncertainties of Attachment B and plant specific sensitivities, it is possible to construct a table equivalent to Table 3b of Attachment A. For Point Beach the equivalent table assuming use of the feedwater venturi's for flow measurement is:

Component	Instrument Error	Uncertainty
		+a,c
Feedwater Flow		
Venturi		
Thermal Expansion Coefficient		
Temperature		
Material		
Density		
Temperature		
Pressure		
Δp		
Feedwater Enthalpy		
Temperature		
Pressure		
Steam Enthalpy		
Pressure		
Moisture		
Net Pump Heat Addition		
Hot Leg Enthalpy		
Temperature		
Streaming		
Pressure		
Cold Leg Enthalpy		
Temperature		
Pressure		

* Dependent Parameters [ ]+a,c

Δ Dependent Parameters [ ]+a,c

□ Dependent Parameters [ ]+a,c

For a single loop the uncertainty (after treating dependent parameters in a rigorous manner) is [ ]$^{+a,c}$. For two loops the system uncertainty is [ ]$^{+a,c}$. In this instance the standard deviation (assuming a normal, two sided probability distribution) is [ ]$^{+a,c}$.

If the LEFM is used to measure feedwater flow (with a corresponding more accurate measurement of feedwater temperature) the secondary side uncertainties are revised to as follows:

<u>Component</u>	<u>Instrument Error</u>	<u>Uncertainty</u>
	[	]$^{+a,c}$
Feedwater Flow		
Feedwater Enthalpy		
Temperature		
Pressure		
Steam Enthalpy		
Pressure		
Moisture		
Net Pump Heat Addition		

For a single loop the uncertainty is [ ]$^{+a,c}$. For two loops the uncertainty is (after correct treatment of system uncertainties) [ ]$^{+a,c}$. In this instance the standard deviation is [ ]$^{+a,c}$.

However, this is only half of the flow measurement uncertainty. The second half is the uncertainty of the Cold Leg Elbow Tap. Assuming that only one elbow tap per loop is ready by the plant process computer, Attachment B notes the uncertainty for one loop. Combining uncertainties results in the following total uncertainties for RCS Flow:

[ ]$^{+a,c}$ assuming use of the feedwater venturi,
[ ]$^{+a,c}$ assuming use of the LEFM for feedwater flow measurement. To be conservative, the ITDP calculations assumed the use of the feedwater venturi for the flow calorimetric. This

would then result in a standard deviation of [ ]$^{+a,c}$.

In summary, the following uncertainties and standard deviations were calculated for use in the ITDP analysis.

	Uncertainty	σ
Pressurizer Pressure Control		
Rod Control (Temperature)		
Power Calorimetric		
RCS Flow		

Question 3:

Please identify the limiting DNBR transient and provide the results of the analysis of the event, including the calculated value of minimum DNBR.

Response:

The limiting DNB transient is Uncontrolled RCCA Withdrawal at Power. Results of this postulated event, including figures of the minimum calculated values of DNBR, are attached.

Question 3 Attachment

14.1.2 Uncontrolled RCCA Withdrawal at Power

An uncontrolled RCCA withdrawal at power results in an increase in core heat flux. Since the heat extraction from the steam generator remains constant, 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 prevent the possibility of damage to the cladding, the Reactor Protection System is designed to terminate any such transient with an adequate margin to DNB.

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

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

2. Reactor trip is actuated if any two out of four ΔT channels exceed an overtemperature ΔT setpoint. This setpoint is automatically varied with axial power imbalance, coolant temperature and pressure to protect against DNB.

3. Reactor trip is actuated if any two out of four ΔT channels exceed an overpower ΔT setpoint. This setpoint is automatically varied with axial power imbalance and coolant temperature to ensure that the allowable heat generation rate (kw/ft) is not exceeded.

4. 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.

5. A high pressurizer water level reactor trip, actuated from any two out of three level channels, is actuated at a fixed setpoint. This affords additional protection for RCCA withdrawal accidents.

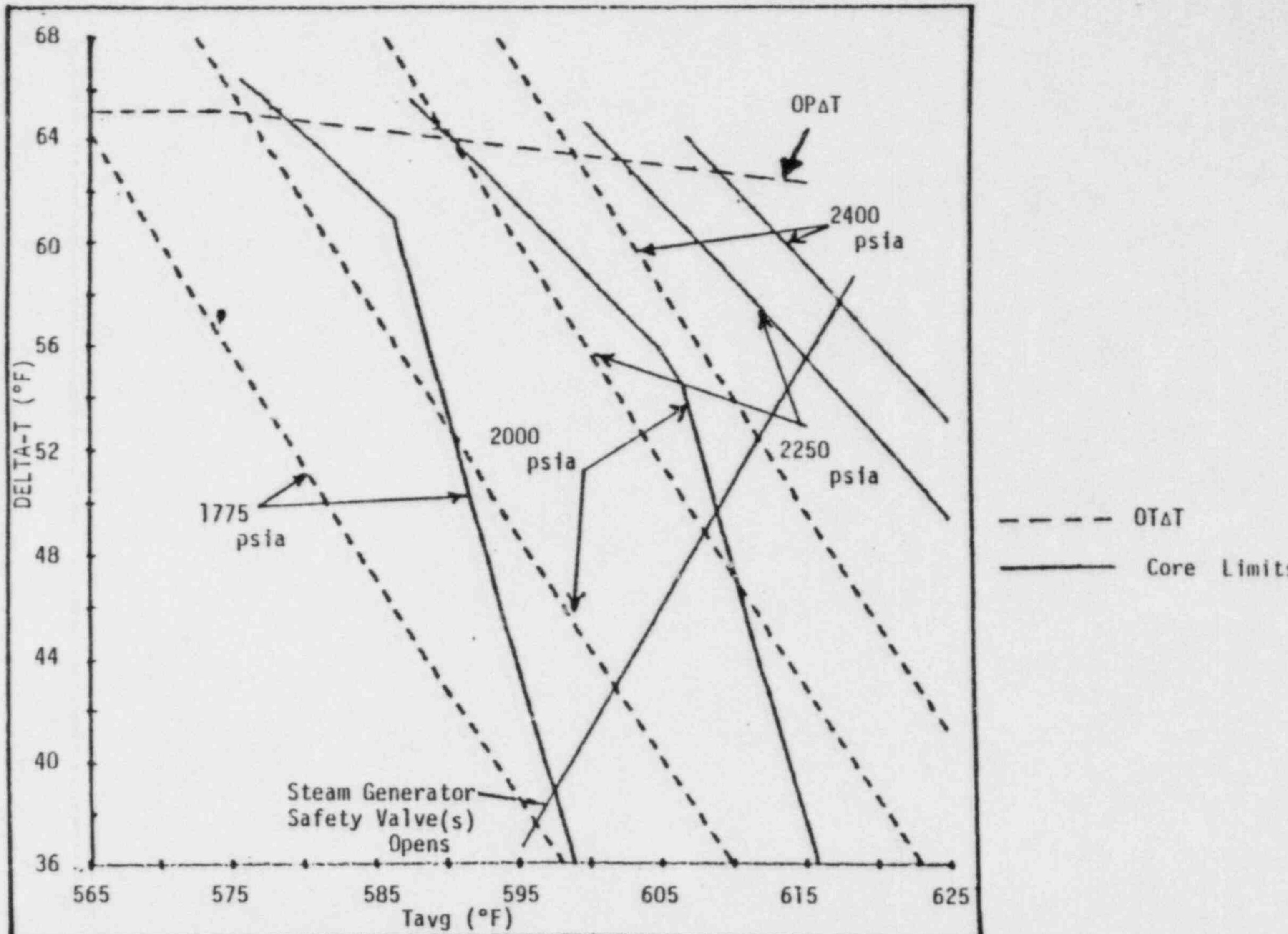
The manner in which the combination of overpower and overtemperature ΔT trips provides protection over the full range of reactor coolant system conditions is illustrated in Figure 14-1. Figure 14-1 presents allowable reactor loop average temperature and ΔT for the design power distribution and flow as a function of primary coolant pressure. The boundaries of operation defined by the overpower ΔT trip and the overtemperature ΔT trip are represented as "protection lines" on this diagram. These protection lines are drawn to include all adverse instrumentation and setpoint errors, so that under nominal conditions trip would occur well within the area bounded by these lines. A maximum steady state operating condition for the reactor is also shown on the Figure.

The utility of the diagram just described is in the fact that the operating limit imposed by any given DNB ratio can be represented as a line on this coordinate system. The DNB lines represent the locus of conditions for which the DNBR equals the limit value (1.65 for the thimble cell and 1.66 for the typical cell). All points below and to the left of this line have a DNB ratio greater than this value. The diagram shows that DNB is prevented for all cases if the area enclosed within the maximum protection lines is not traversed by the applicable DNB ratio line at any point.

The region of permissible operation (power, pressure, and temperature) is completely bounded by the combination of reactor trips: nuclear overpower (fixed setpoint); high pressure (fixed setpoint); low pressure

FIGURE 14-1

ILLUSTRATION OF OVERTEMPERATURE AND OVERPOWER ΔT PROTECTION



(fixed setpoint); overpower and overtemperature ΔT (variable setpoints). These trips are designed to prevent overpower and a DNB ratio of less than the limit value.

Method of Analysis

Uncontrolled rod cluster control assembly bank withdrawal is analyzed by the LOFTRAN code. This code simulates the neutron kinetics, reactor coolant system, pressurizer, pressurizer relief and safety valves, pressurizer spray, steam generator, and steam generator safety valves. The code computes pertinent plant variables, including temperatures, pressures, and power level. The core limits, as illustrated in Figure 14-1, are used as input to LOFTRAN to determine the minimum departure from nucleate boiling ratio during the transient. This accident is analyzed with the Improved Thermal Design Procedure as described in WCAP-8567.

In order to obtain conservative values of departure from nucleate boiling ratio, the following assumptions are made:

1. Initial Conditions - Initial reactor power, reactor coolant average temperatures, and reduced reactor coolant pressure (2000 psia) are assumed to be at their nominal values. Uncertainties in initial conditions are included in the limit DNBR as described in WCAP-8567.

2. Reactivity Coefficients - Two cases are analyzed.

a. Minimum Reactivity Feedback - A positive (5 pcm/°F) moderator coefficient of reactivity is assumed, corresponding to the beginning of core life. A variable Doppler power coefficient with core power is used in the analysis. A conservatively small (in absolute magnitude) value is assumed.

b. Maximum Reactivity Feedback - A conservatively large positive moderator density coefficient and a large (in absolute magnitude) negative Doppler power coefficient are assumed.

3. The rod cluster control assembly trip insertion characteristic is based on the assumption that the highest worth assembly is stuck in its fully withdrawn position.

4. The reactor trip on high neutron flux is assumed to be actuated at a conservative value of 118% of nominal full power. The overtemperature ΔT trip includes all adverse instrumentation and setpoint errors; the delays for trip actuation are assumed to be the maximum values. No credit was taken for the other expected trip functions.

5. The maximum positive reactivity insertion rate is greater than that for the simultaneous withdrawal of the combination of the two control banks having the maximum combined worth at maximum speed.

The effect of rod cluster control assembly movement on the axial core power distribution is accounted for by causing a decrease in overtemperature and overpower ΔT trip setpoints proportional to a decrease in margin to DNB.

Results

Figures 14.1.2-1 and 14.1.2-2 show the response of neutron flux, pressure, average coolant temperature, and departure from nucleate boiling ratio to a rapid rod cluster control assembly withdrawal incident starting from full power. Reactor trip on high neutron flux occurs shortly after the start of the accident. Since this is rapid with respect to the thermal time constants of the plant, small changes in T_{avg} and pressure result, and a large margin to DNB is maintained.

The response of neutron flux, pressure, average coolant temperature, and DNBR for a slow control rod assembly withdrawal from 10% power is shown in Figures 14.1.2-3 and 14.1.2-4. Reactor trip on overtemperature ΔT occurs after a longer period, and the rise in temperature and pressure is consequently larger than for rapid rod cluster control assembly withdrawal. Again, the minimum DNBR is greater than the limit value.

Figure 14.1.2-5 shows the minimum departure from nucleate boiling ratio as a function of reactivity insertion rate from initial full-power operation for the minimum and maximum reactivity feedback cases. It can be seen that two reactor trip channels provide protection over the whole range of reactivity insertion rates. These are the high neutron flux and overtemperature ΔT trip channels. The minimum DNBR is never less than the limit value.

Figures 14.1.2-6 and 14.1.2-7 show the minimum departure from nucleate boiling ratio as a function of reactivity insertion rate for rod cluster control assembly withdrawal incidents starting at 60% and 10% power respectively. The results are similar to the 100% power case, except that as the initial power is decreased, the range over which the overtemperature ΔT trip is effective is increased. In neither case does the departure from nucleate boiling ratio fall below the DNBR limit value.

In the referenced figures, the shape of the curves of minimum departure from nucleate boiling ratio versus reactivity insertion rate is due both to reactor core and coolant system transient response and to protection system action in initiating a reactor trip.

Referring to Figure 14.1.2-6, for example, it is noted that:

1. For high reactivity insertion rates (i.e., between ~100 pcm/second and ~5 pcm/second), reactor trip is initiated by the high neutron flux trip for the minimum reactivity feedback cases. The neutron flux level in the core rises rapidly for these insertion rates, while core heat flux and coolant system temperature lag behind due to the thermal capacity of the fuel and coolant system fluid. Thus, the reactor is tripped prior to significant increase in heat flux or water temperature with resultant high minimum departure from nucleate boiling ratios during the transient. Within this range, as the reactivity insertion rate decreases, core heat flux and coolant temperatures can remain more nearly in equilibrium with the neutron flux; minimum DNBR during the transient thus decreases with decreasing insertion rate.

2. With further decrease in reactivity insertion rate, the overtemperature ΔT and high neutron flux trips become equally effective in terminating the transient (e.g., at ~4 pcm/second reactivity insertion rate).

   The overtemperature ΔT reactor trip circuit initiates a reactor trip when measured coolant loop ΔT exceeds a setpoint based on measured reactor coolant system average temperature and pressure. It is important in this context to note, however, that the average temperature contribution to the circuit is lead-lag compensated in order to decrease the effect of the thermal capacity of the reactor coolant system in response to power increases.

   For reactivity insertion rates between ~4 pcm/second and ~.6 pcm/second, the effectiveness of the overtemperature ΔT trip increases (in terms of increased minimum departure from nucleate boiling ratio) due to the fact that, with lower insertion rates, the power increase rate is slower, the rate of rise of average coolant temperature is slower, and the lead-lag compensation provided can increasingly account for the coolant system thermal capacity lag.

3. For maximum reactivity feedback cases reactivity insertion rates less than ~60 pcm/second, the rise in reactor coolant temperature is sufficiently high so that the steam generator safety valve setpoint is reached prior to trip. Opening these valves, which act as an additional heat load on the reactor coolant system, sharply decreases the rate of rise of reactor coolant system average temperature. This decrease in rate of rise of the average coolant system temperature during the transient is accentuated by the lead-lag compensation, causing the overtemperature ΔT trip setpoint to be reached later with resulting lower minimum departure from nucleate boiling ratios.

Figures 14.1.2-5, 14.1.2-6, and 14.1.2-7 illustrate minimum departure from nucleate boiling ratio calculated for minimum and maximum reactivity feedback. The calculated sequence of events for this accident is shown in Table 14.1.2-1.

<u>Conclusions</u>

In the unlikely event of an at power (either from full power or lower power levels) control rod bank withdrawal incident, the core and reactor coolant system are not adversely affected since the minimum value of DNB ratio reached is in excess of the DNB limit value for all rod reactivity rates. Protection is provided by nuclear flux overpower and overtemperature ΔT. Additional protection would be provided by the high pressurizer level, overpower ΔT, and the high pressure reactor trip. The preceding sections have described the effectiveness of these protection channels.

TABLE 14.1.2-1

TIME SEQUENCE OF EVENTS FOR
UNCONTROLLED RCCA WITHDRAWAL AT POWER

Event	Time of Each Event (Seconds)
Case A:	
Initiation of uncontrolled rod cluster control assembly withdrawal at full power and maximum reactivity insertion rate (100 pcm/sec)	0
Power range high neutron flux high trip point reached	1.061
Rods begin to fall into core	1.561
Minimum departure from nucleate boiling ratio occurs	2.5
Case B:	
Initiation of uncontrolled rod cluster control assembly withdrawal at 10% power and at a small reactivity insertion rate (4 pcm/sec)	0
Overtemperature ΔT reactor trip signal initiated	125.80
Rods begin to fall into core	127.80
Minimum departure from nucleate boiling ratio occurs	128.2

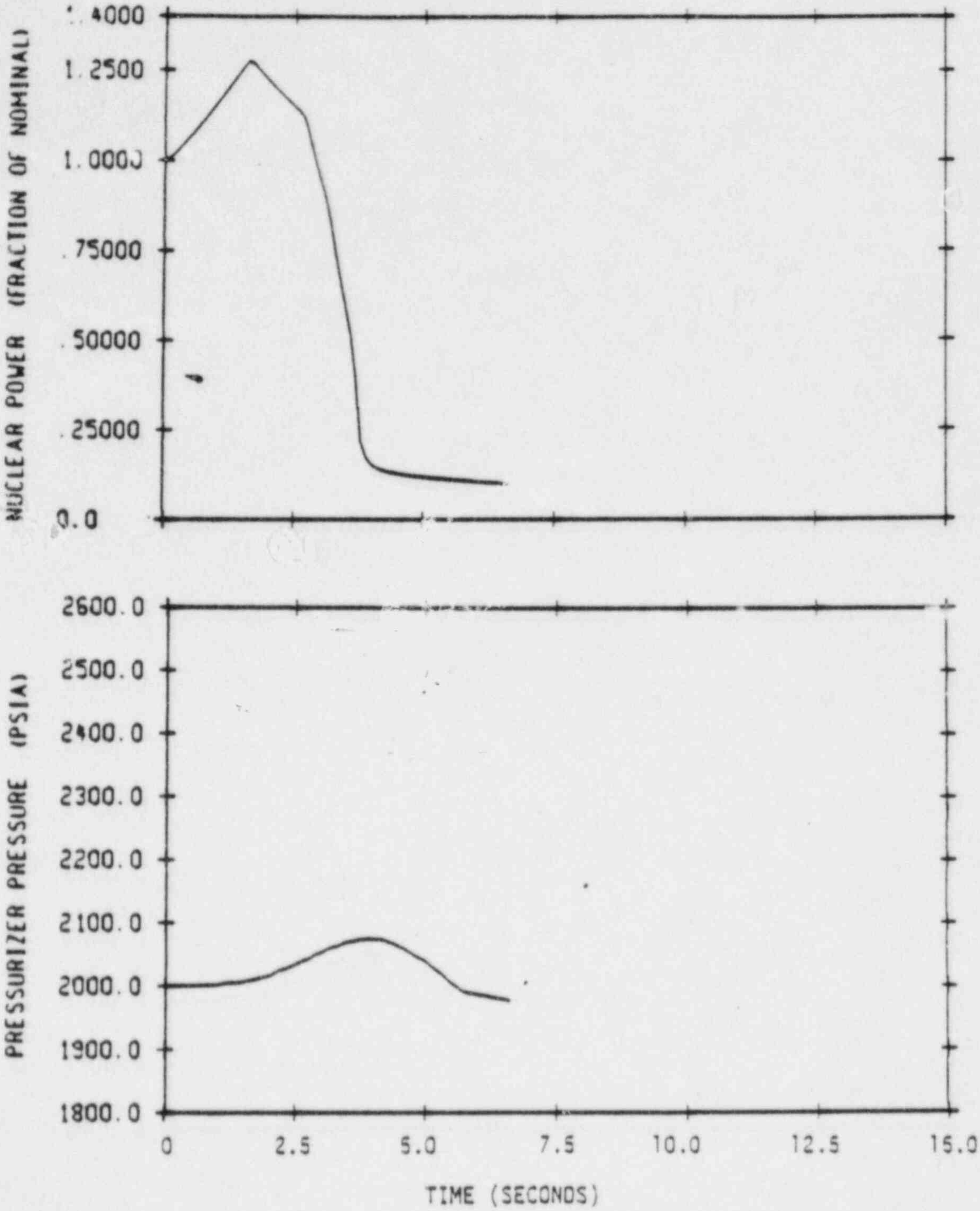


FIGURE 14.1.2-1

ROD WITHDRAWAL AT POWER, FULL POWER, MINIMUM FEEDBACK, 100 pcm/sec WITHDRAWAL RATE
NUCLEAR POWER & PRESSURIZER PRESSURE VS. TIME

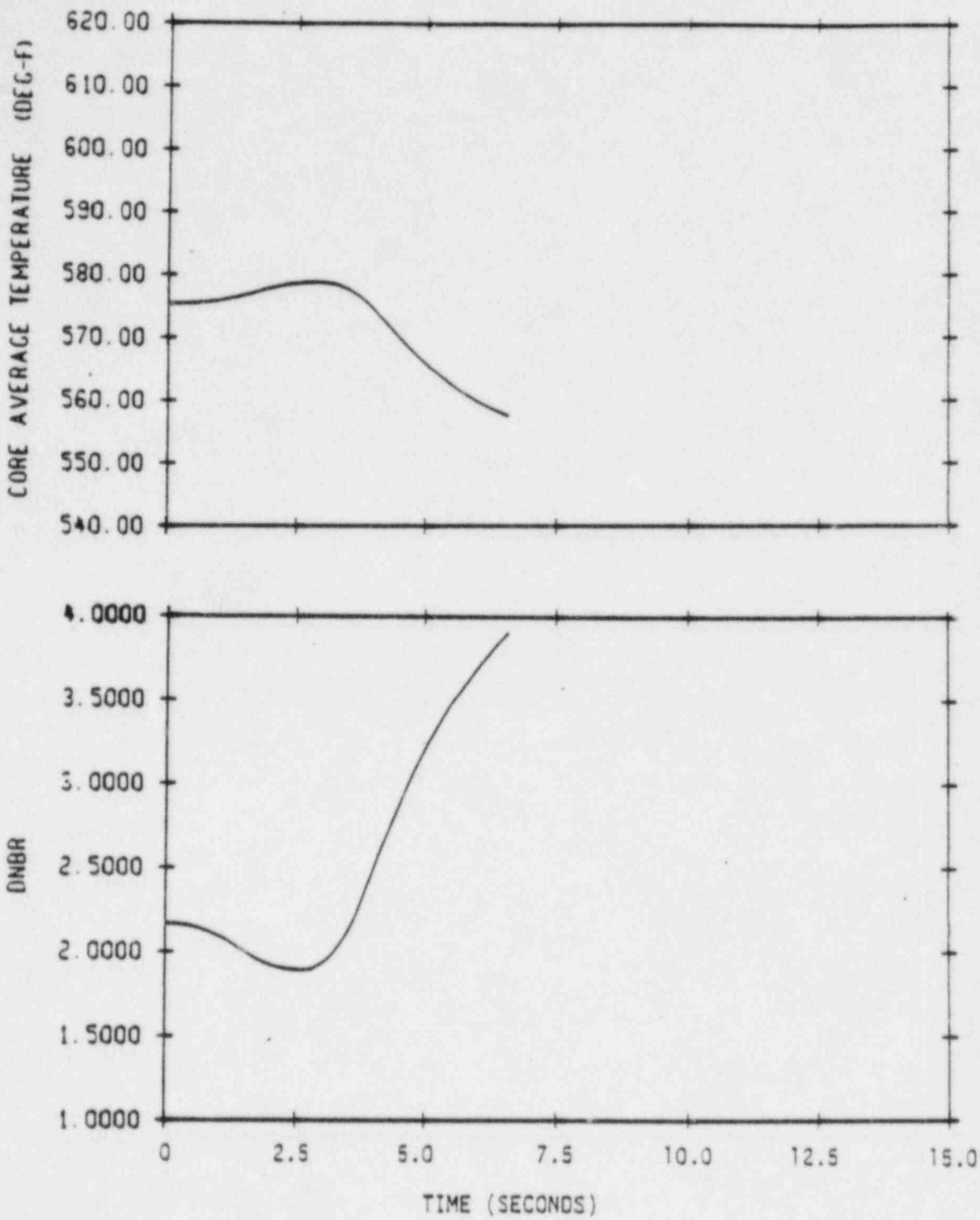


FIGURE 14.1.2-2

ROD WITHDRAWAL AT POWER, FULL POWER, MINIMUM FEEDBACK, 100 pcm/sec WITHDRAWAL RATE
CORE AVERAGE TEMPERATURE & DNBR VS. TIME

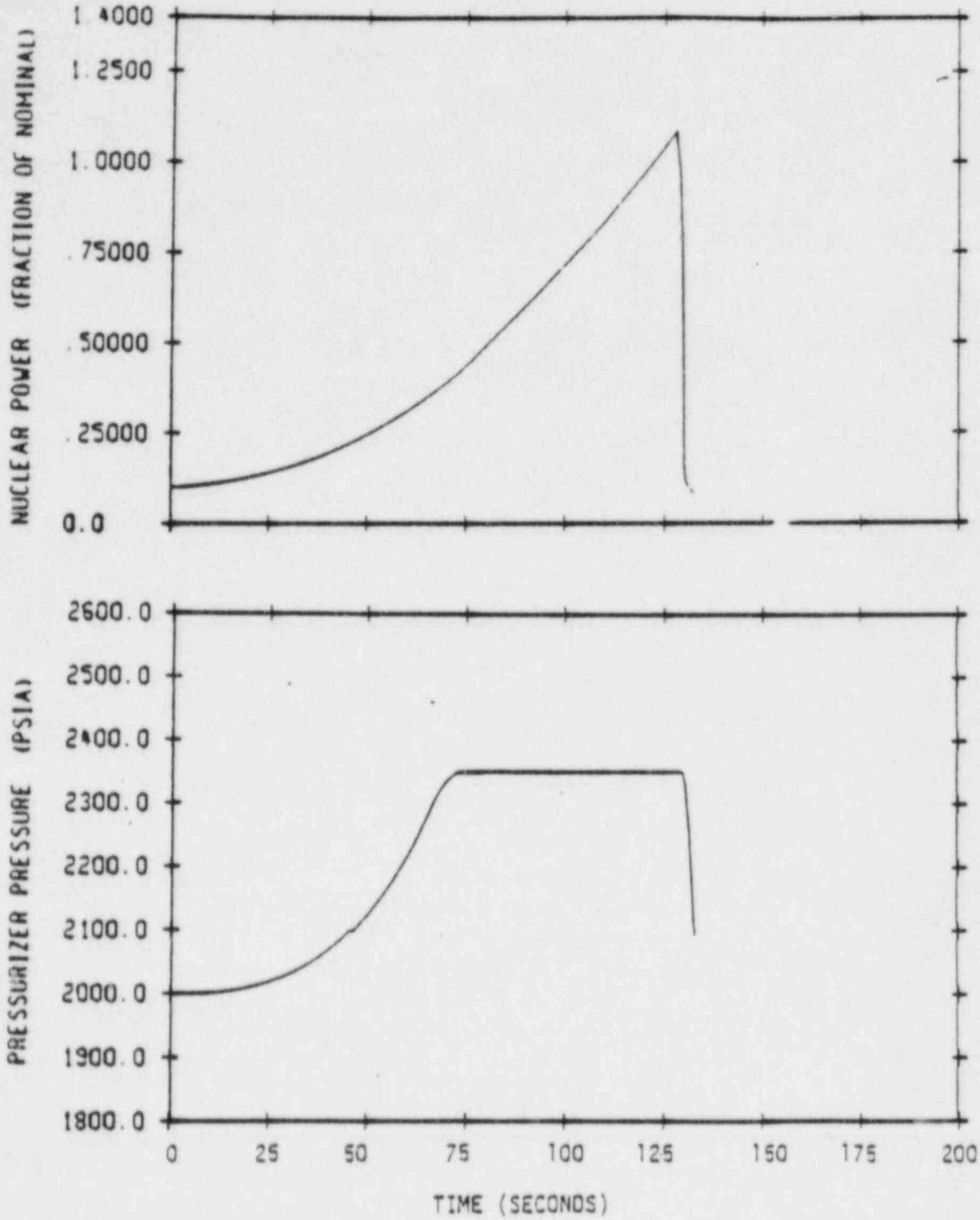


FIGURE 14.1.2-3

ROD WITHDRAWAL AT POWER, 10% POWER, MINIMUM FEEDBACK, 4 pcm/sec WITHDRAWAL RATE
NUCLEAR POWER & PRESSURIZER PRESSURE VS. TIME

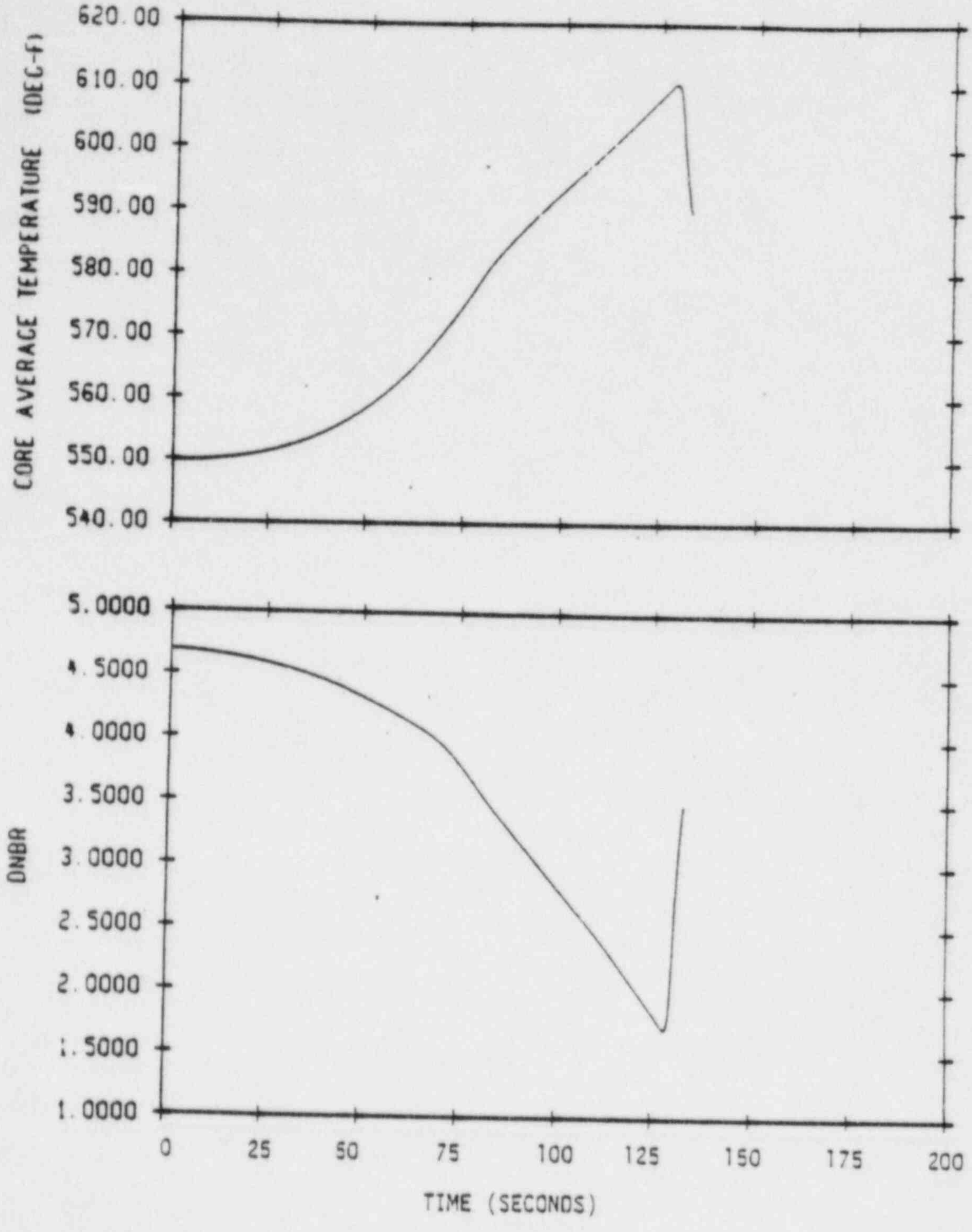
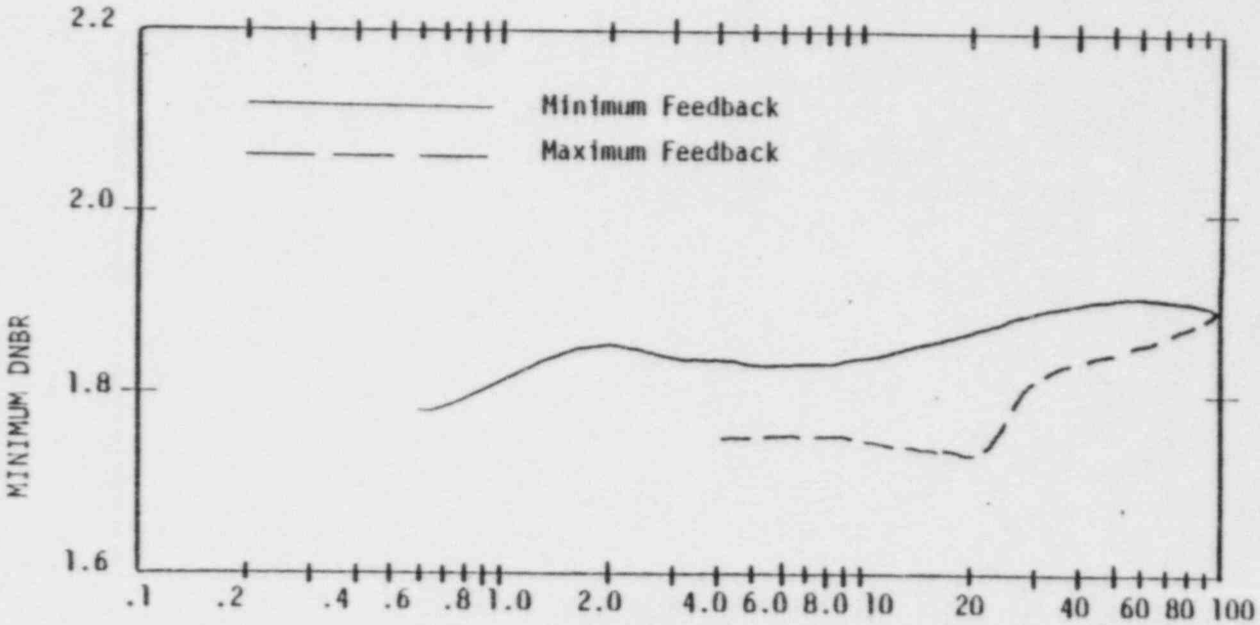


FIGURE 14.1.2-4

ROD WITHDRAWAL AT POWER, 10% POWER, MINIMUM FEEDBACK, 4 pcm/sec WITHDRAWAL RATE
CORE AVERAGE TEMPERATURE & DNBR VS. TIME



REACTIVITY INSERTION RATE (PCM/SEC)

FIGURE 14.1.2-5

ROD WITHDRAWAL AT POWER FULL POWER
MINIMUM DNBR VS REACTIVITY INSERTION RATE

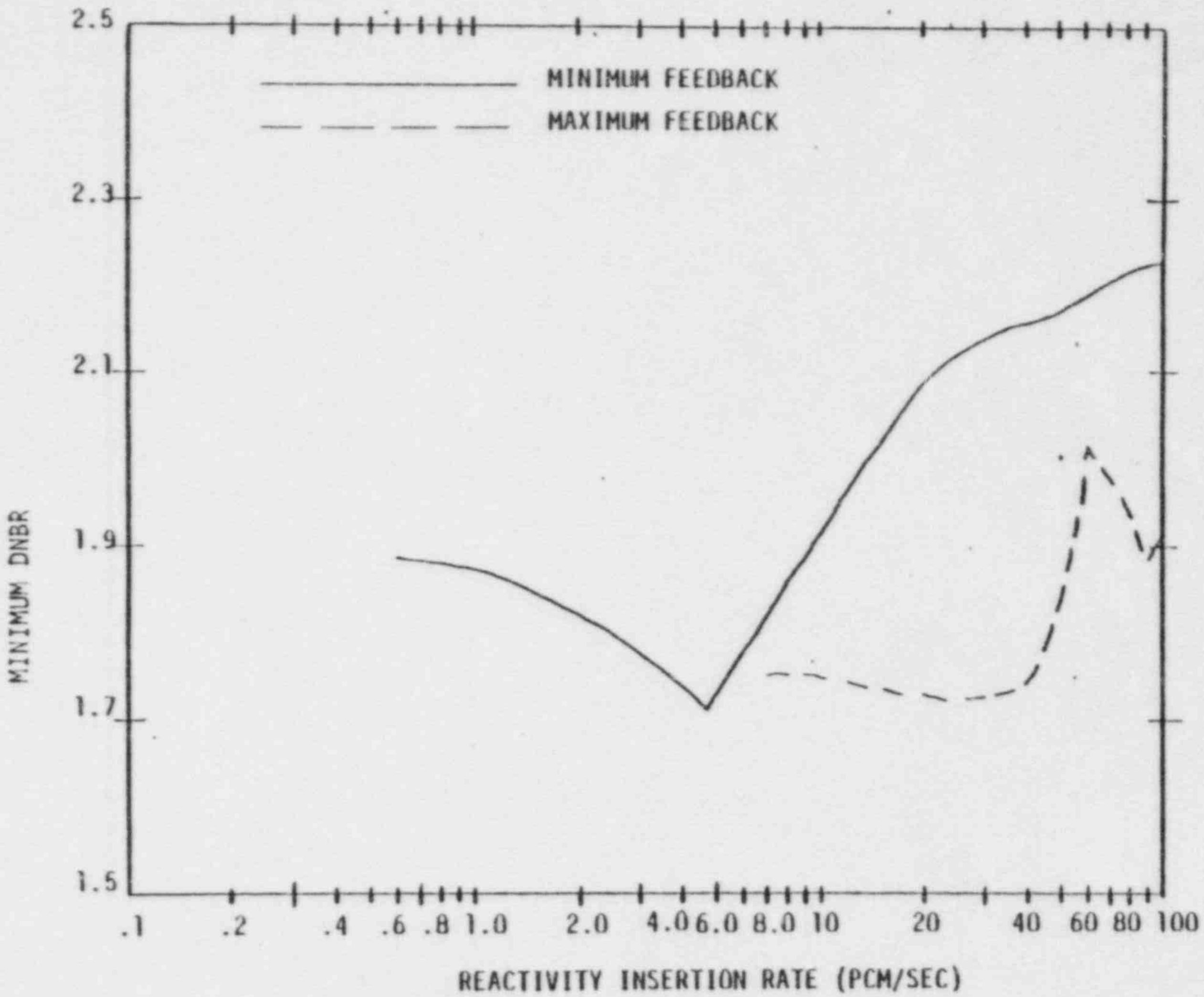


FIGURE 14.1.2-6

ROD WITHDRAWAL AT POWER 60 PERCENT POWER
MINIMUM DNBR VS REACTIVITY INSERTION RATE

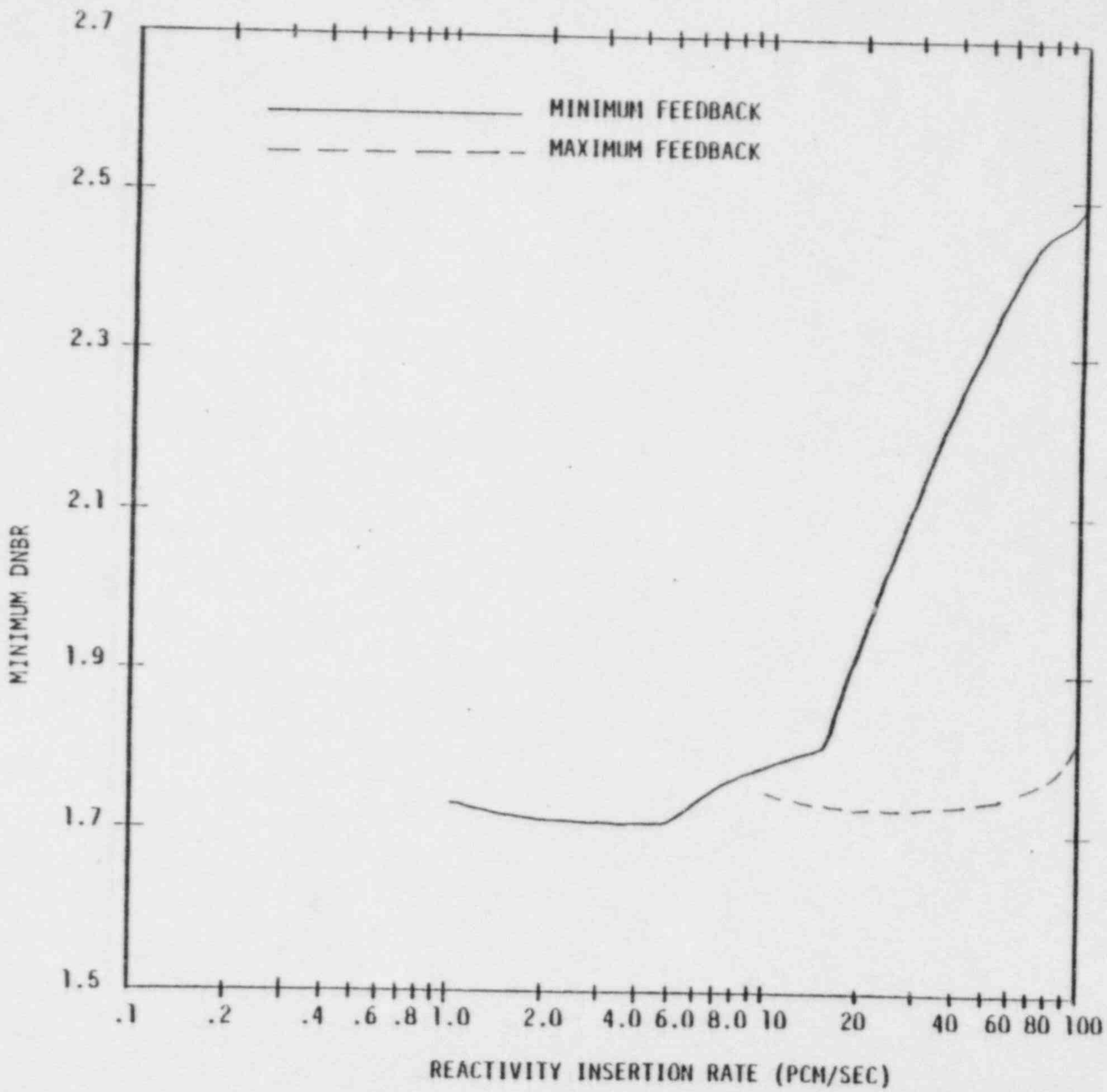


FIGURE 14.1.2-7

ROD WITHDRAWAL AT POWER 10 PERCENT POWER
MINIMUM DNBR VS REACTIVITY INSERTION RATE

Question 4:

Please provide a table of kinetics parameter ranges for the standard core, mixed cores and OFA core.

Response:

Table 4-1 provides the required kinetic parameters.

TABLE 4-1

LIMITS USED IN THE TRANSIENT ANALYSIS — TYPICAL CYCLE

Parameter	Standard Fuel Core	OFA Core*	Typical Value	Limiting Fuel Type
Most Positive MTC (pcm/°F)	0.0	+5.0	+3.0	OFA
Most Negative MTC (pcm/°F)	-35.0	-35.0	-31.0	STD
Most Negative DTC (pcm/°F)	-1.6	-2.9	-2.4	STD
Least Negative DTC (pcm/°F)	-1.0	-0.91	-1.4	OFA
Most Negative DPC (pcm/°F)	--	--	-13.1	STD
Least Negative DPC (pcm/% power)	--	--	-8.0	OFA
Maximum Boron Worth (pcm/ppm)	--	--	-13.3	OFA
Minimum Boron Worth (pcm/ppm)	--	--	-8.5	STD
Maximum Beta eff	0.00700	0.0072	0.00584	OFA
Minimum Beta eff	0.00453	0.0043	0.00505	STD
Maximum Promp Neutron Lifetime (10^{-6} sec)	18	26	21	OFA

*The limits for a transition core are the same as for an all OFA core.

1540L:6/081084

Question 5:

Provide more information on the results of the reanalysis for the Uncontrolled Rod Withdrawal at Power. In the updated FSAR this event is apparently the limiting DNBR transient. Provide quantitative discussion of the effect of OFA and positive MTC on this event.

Response:

The safety evaluation for Pt. Beach OFA transition core has demonstrated that the DNB design basis has been met for this transient for both optimized and standard fuel consistent with the Thermal/Hydraulic methodology used to evaluate each fuel type as specified in the Safety Evaluation for Point Beach Units 1 and 2 Transition to Westinghouse 14x14 Optimized Fuel Assemblies [Reference Attachment B of September 6, 1983 letter from Fay (WEPCO) to Denton (NRC)]. The moderator temperature coefficient has been assumed at its most conservative value within the bounds of Technical Specification 15.3.1 regarding MTC.

Question 6:

Provide a qualitative discussion of the effect of the various changes (OFA, positive MTC, RAOC, etc.) on all other events that were reanalyzed.

Response:

General discussions of the impacts due to OFA, positive MTC, $F_{\Delta H}$ multiplier, RAOC and other changes are discussed in Section 6.1 of Attachment B to letter of September 6, 1983 from Fay (WEPCO) to Denton (NRC); discussions specific to the various transients are included in Section 6.2 of that document.

Question 7:

Provide a discussion of the Primary System Pipe Rupture (small break LOCA) event.

Response:

The small break LOCA analysis for Point Beach applicable to transition and full OFA core cycles was reanalyzed due to the differences between Westinghouse standard and OFA designs. The currently approved October 1975 small break ECCS evaluation model was utilized for a spectrum of cold-leg breaks.

When assessing the transition core impact on small break LOCA, the only mechanism available to cause a transition core to have a greater calculated PCT than a full core of either fuel is the possibility of flow redistribution due to fuel assembly hydraulic resistance mismatch.

The W-FLASH computer code was used to model the core hydraulics during a small break LOCA event. Only one core flow channel was modeled in W-FLASH, since the core flowrate during a small break LOCA is relatively low, and this provides enough time to maintain flow equilibrium between fuel assemblies (i.e., crossflow). Therefore, hydraulic resistance mismatch is not a factor for small break LOCA. Thus it was not necessary to perform a small break evaluation for transition cores and it was sufficient to reference the small break LOCA for the full core of the OFA design.

The small break OFA LOCA analysis for Point Beach utilizing the currently approved 1975 Small Break Evaluation model resulted in a PCT of 992°F for the 6-inch diameter cold-leg break. The analysis assumed the worst small break power shape consistent with a LOCA F_Q envelope of 2.32 at core midplane elevation and 1.5 at the top of the core.

Analyses showed that the high and low head portions of the ECCS, together with the accumulators, provided sufficient core flooding to keep the calculated PCT well below the required limits of 10 CFR 50.46. Adequate protection is therefore afforded by the ECCS in the event of a small break LOCA in the Point Beach Units.

Question 8:

Westinghouse has recognized that, under certain circumstances, the FSAR analysis of the dropped rod event for turbine runback plants may not be complete. Specifically dropping a very low worth rod would lead to a turbine runback to 78 percent power but would not reduce core power by this amount. Thus a core turbine mismatch would be created with possible violation of DNBR limits. Please confirm that an analysis of this scenario has been performed, describe the analysis procedure, and provide the results for the limiting core configuration.

Response:

A reanalysis of the dropped rod event for Point Beach has been performed to address this issue. A description, including methods, assumptions, results and conclusions is attached. The DNB design basis has been confirmed to be met for this event.

Question 8 Attachment

Rod Cluster Control Assembly (RCCA) Drop

Dropping of a full-length RCCA occurs when the drive mechanism is deenergized. This would cause a power reduction and an increase in the hot channel factor. If no protective action occurred, the Reactor Control System would restore the power to the level which existed before the incident. This would lead to a reduced safety margin or possibly DNB, depending upon the magnitude of the resultant hot channel factor.

If an RCCA drops into the core during power operation, it would be detected by either a rod bottom signal, by an out-of-core chamber, 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 out-of-core power range channel signals. This rod drop detection circuit is actuated upon sensing a rapid decrease in local flux and is designed such that normal load variations do not cause it to be actuated.

A rod drop signal from any rod position indication channel, or from one or more of the four power range channels, initiates the following protective action: reduction of the turbine load by a preset adjustable amount and blocking of further automatic rod withdrawal. The turbine runback is achieved by acting upon the turbine load limit and/or on the turbine load reference. The rod withdrawal block is redundantly achieved.

Method of Analysis

The transient following a dropped RCCA accident is determined by a detailed digital simulation of the plant. The dropped rod causes a step decrease in reactivity and the core power generation is determined using the LOFTRAN code. The overall response is calculated by simulating the turbine load runback and preventing rod withdrawal. The analysis is presented for the case in which the load cutback is greater than that required to match the worth of the dropped rod (75pcm). The load is assumed to be cut back from 100 to 76% of full load at a conservatively slow rate of approximately 1% per second.

The least negative values of moderator and Doppler temperature coefficients of reactivity are used in this analysis resulting in the highest heat flux during the transient. These are moderator density coefficient of reactivity of 0.0 $\Delta\rho$/gm/cc and a Doppler temperature coefficient of reactivity of -1 pcm/°F.

This accident is analyzed with the Improved Thermal Design Procedure as described in WCAP-8567.

Results

Figures 14.1.3-4 through 14.1.3-6 illustrate the transient response following a dropped rod of worth 75 pcm. The reactor coolant average temperature decreases initially, due to the decrease in reactor core power. Since the drop in power is less than the drop in load, with no reactivity feedback, coolant temperature then increases. The higher primary power level (as opposed to secondary) is eventually matched by opening of the steam generator safety valves. Steady-state conditions are then achieved.

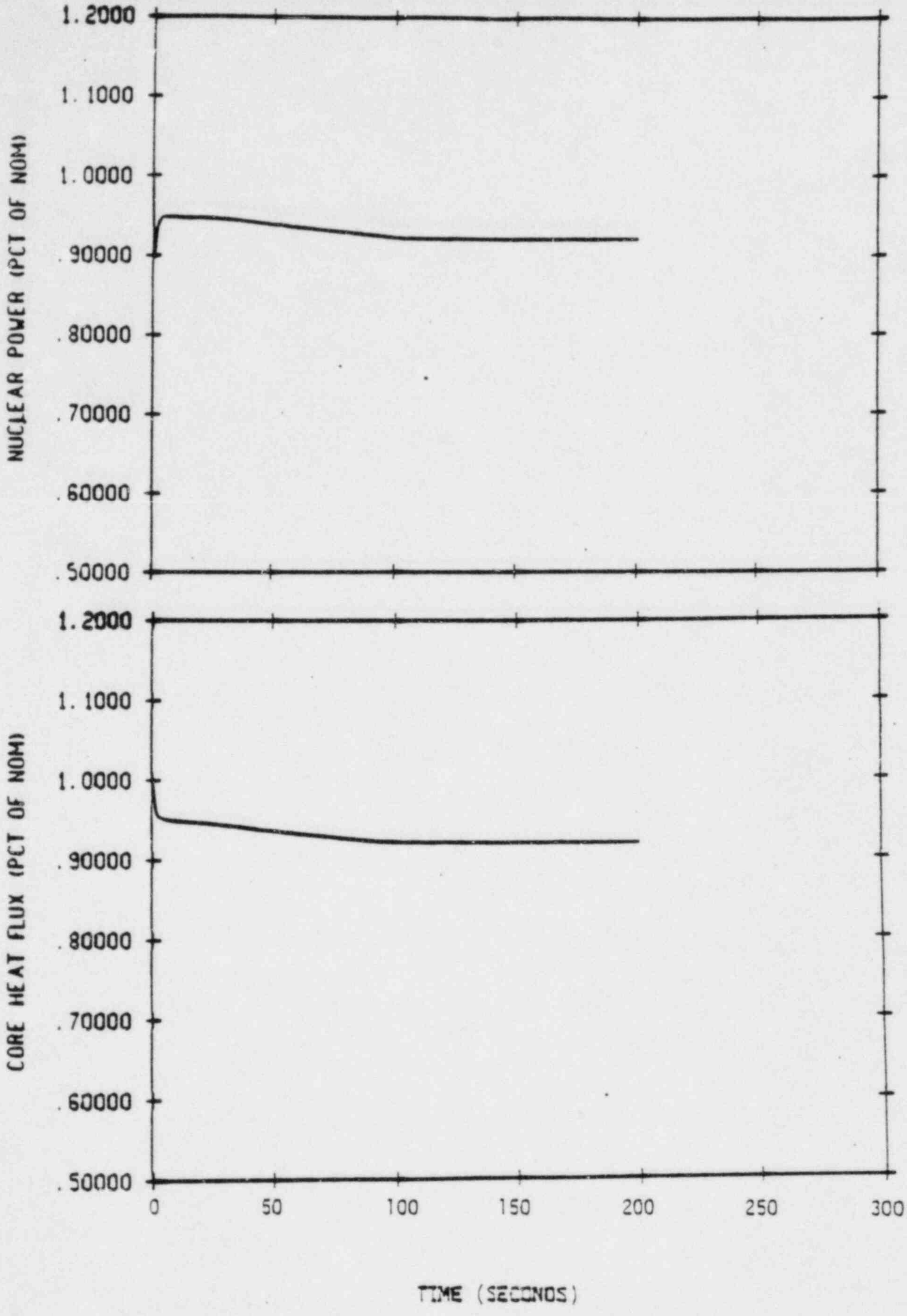


FIGURE 14.1.3-4
RESPONSE TO A DROPPED RCCA OF WORTH - 75 pcm
NUCLEAR POWER & CORE HEAT FLUX VS. TIME

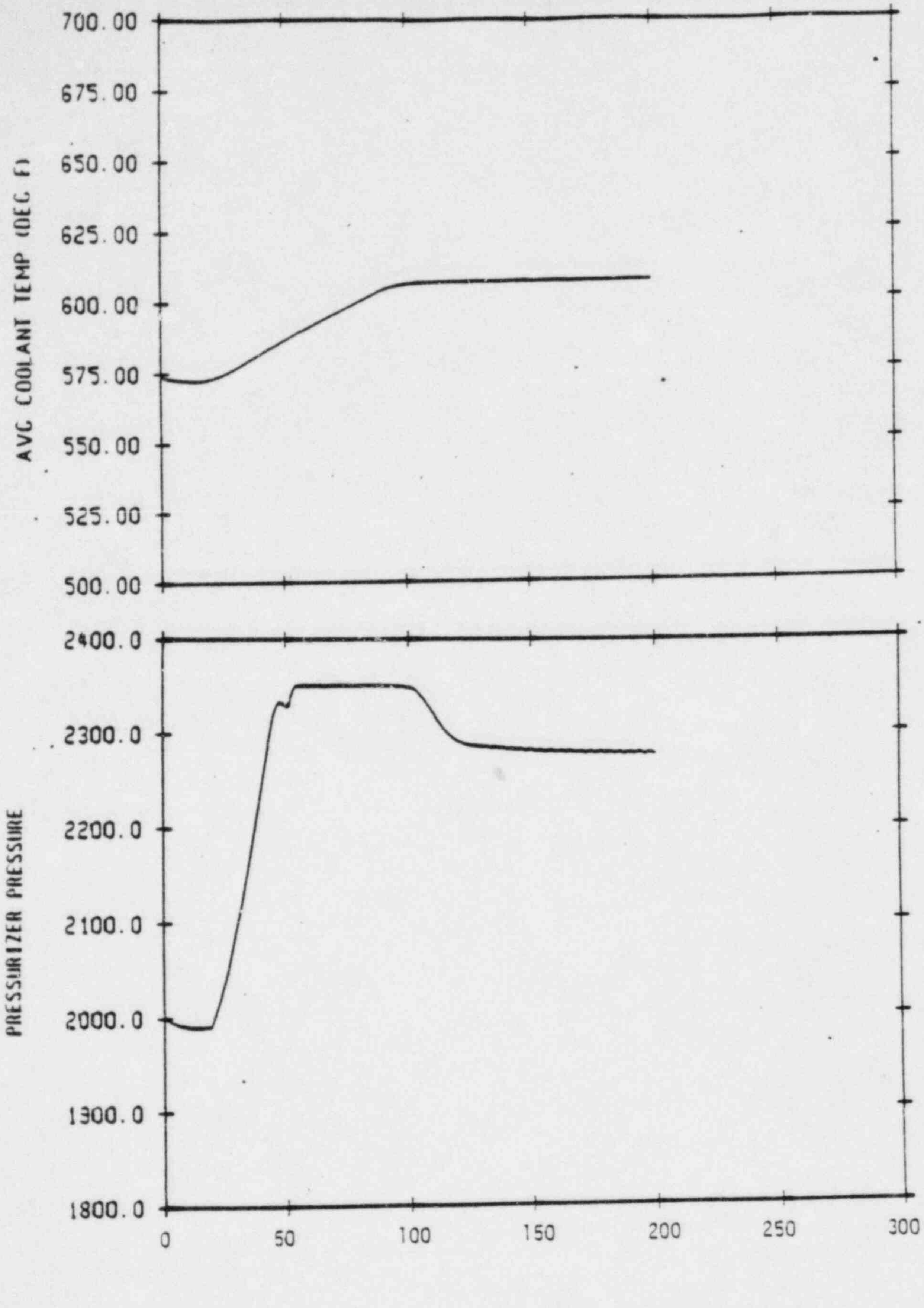


FIGURE 14.1.3-5
RESPONSE TO A DROPPED RCCA OF WORTH - 75 pcm
PRESSURIZER PRESSURE & AVG. COOLANT TEMP. VS. TIME

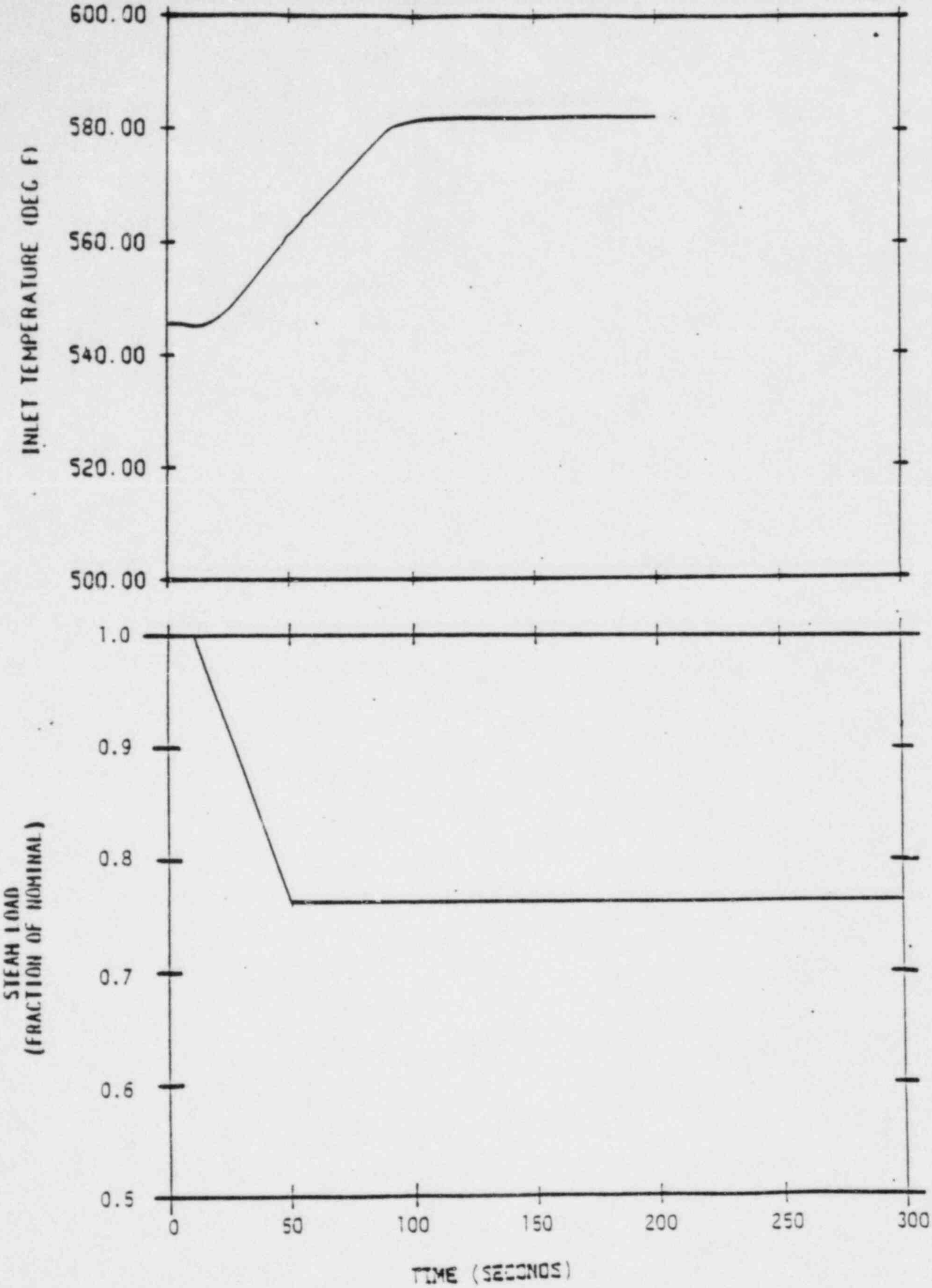


FIGURE 14.T.3-6

RESPONSE TO A DROPPED RCCA OF WORTH - 75 pcm
INLET TEMP. & STEAM LOAD VS. TIME

Question 9:

Technical Specification 15.3.10 is not complete. The measured F_Q value must be multiplied by a factor which accounts for xenon transient effects before it is compared to the $F_Q(z)$ limit. This Specification should be altered to be consistent with the sample Specification given in Appendix A to Part B of NS-EPR-2649, "The F_Q Surveillance Technical Specification." The W(z) report required by that Specification should be submitted in a timely manner.

Response:

The revision to Technical Specification 15.3.10 is complete without the W(z) factor. Although the Westinghouse submittal (WCAP-10216-P-A) to the NRC deals with both RAOC and F_Q Surveillance Technical Specifications, the implementation of RAOC does not require the use of F_Q Surveillance. The revised Point Beach Technical Specification (15.3.10) is consistent with the present Point Beach specification which requires a measurement of F_Q every 30 days. Adding the W(z) factor results in an F_Q surveillance Technical Specification which is not related to RAOC operation.

ATTACHMENT A - GENERIC CALCULATIONS (TO APPENDIX 1)

Questions:

4) Provide and justify the variances and distributions for input parameters.

5) Justify that the nominal conditions used in the analyses bound all permitted modes of plant operation.

7) Provide a block diagram depicting sensor, processing equipment, computer, and readout devices for each parameter channel used in the uncertainty analysis. Within each element of the block diagram identify the accuracy, drift, range, span, operating limits, and setpoints. Identify the overall accuracy of each channel transmitter to final output and specify the minimum acceptable accuracy for use with the new procedure. Also identify the overall accuracy of the final output value and maximum accuracy requirements for each input channel for this final output device.

Response: Rosemount RTDs

I. INTRODUCTION

Four operating parameter uncertainties are used in the uncertainty analysis of the Improved Thermal Design Procedure (ITDP). These operating parameters are pressurizer pressure, primary coolant temperature (T_{avg}), reactor power, and reactor coolant system flow. These parameters are monitored on a regular basis and several are used for control purposes. The reactor power is monitored by the performance of a secondary side heat balance (power calorimetric measurement) at least once every 24 hours. The RCS flow is monitored by the performance of a precision flow calorimetric measurement at the beginning of each cycle. The RCS loop elbow taps can then be normalized against the precision calorimetric and used for monthly surveillance (with a small increase in total uncertainty) or a precision flow calorimetric can be performed on the same surveillance schedule. Pressurizer pressure is a controlled parameter and the uncertainty for the Improved Thermal Design Procedure reflects the use of the control system. T_{avg} is a controlled parameter through the use of the temperature input to the Control Rod control system; the uncertainty presented here reflects the use of this control system.

Since 1978 Westinghouse has been deeply involved with the development of several techniques to treat instrumentation uncertainties, errors, and allowances. The earlier versions of these techniques have been documented for several plants; one approach uses the methodology outlined in WCAP-8567 "Improved Thermal Design Procedure"[1,2,3] which is based on the conservative assumption that the uncertainties can be described with uniform probability distributions. The other approach is based on the more realistic assumption that the uncertainties can be described with normal probability distributions. This assumption is also conservative in that the "tails" of the normal distribution are in reality "chopped" at the extremes of the range, i.e., the ranges or uncertainties are finite and thus, allowing for some probability in excess of the range limits is a conservative assumption. This approach has been used to substantiate the acceptability of the protection system setpoints for several plants with a Westinghouse NSSS, e.g., D. C. Cook II[4], North Anna Unit 1, Salem Unit 2, Sequoyah Unit 1, V. C. Summer, and McGuire Unit 1. Westinghouse now believes that the latter approach can be used for the determination of the instrumentation errors and allowances for the ITDP parameters. The total instrumentation errors presented in this response are based on this approach.

II. METHODOLOGY

The methodology used to combine the error components for a channel is basically the appropriate statistical combination of those groups of components which are statistically independent, i.e., not interactive. Those errors which are not independent are combined arithmetically to form independent groups, which can then be systematically combined. The statistical combination technique used by Westinghouse is the [

$]^{+a,c,e}$ of the instrumentation uncertainties. The instrumentation uncertainties are two sided distributions. The sum of both sides is equal to the range for that parameter, e.g., Rack Drift is typically [$]^{+a,c}$, the range for this parameter is [$]^{+a,c}$. This technique has been utilized before as noted above and has been endorsed by the staff[5,6,7] and various industry standards[8,9].

The relationship between the error components and the statistical instrumentation error allowance for a channel is defined as follows:

1. For parameter indication in the racks using a DVM;

[$]^{+a,c}$

Eq. 1

2. For parameter indication utilizing the plant process computer;

[$]^{+a,c}$

Eq. 2

3. For parameters which have control systems;

[$]^{+a,c}$

Eq. 3

where:

CSA = Channel Statistical Allowance
PMA = Process Measurement Accuracy
PEA = Primary Element Accuracy
SCA = Sensor Calibration Accuracy
SD = Sensor Drift

STE = Sensor Temperature Effects
SPE = Sensor Pressure Effects
RCA = Rack Calibration Accuracy
RD = Rack Drift
RTE = Rack Temperature Effects
DVM = Digital Voltmeter Accuracy
ID = Computer Isolator Drift
A/D = Analog to Digital Conversion Accuracy
CA = Controller Accuracy

The parameters above are as defined in reference 4 and are based on SAMA standard PMC-20-1973[10]. However, for ease in understanding they are paraphrased below:

PMA - non-instrument related measurement errors, e.g., temperature stratification of a fluid in a pipe,
PEA - errors due to metering devices, e.g., elbows, venturis, orifices,
SCA - reference (calibration) accuracy for a sensor/transmitter,
SD - change in input-output relationship over a period of time at reference conditions for a sensor/transmitter,
STE - change in input-output relationship due to a change in ambient temperature for a sensor/transmitter,
SPE - change in input-output relationship due to a change in static pressure for a Δp cell,
RCA - reference (calibration) accuracy for all rack modules in loop or channel assuming the loop or channel is tuned to this accuracy. This assumption eliminates any bias that could be set up through calibration of individual modules in the loop or channel.
RD - change in input-output relationship over a period of time at reference conditions for the rack modules,
RTE - change in input-output relationship due to a change in ambient temperature for the rack modules,
DVM - the measurement accuracy of a digital voltmeter or multimeter on it's most accurate applicable range for the parameter measured,

ID - change in input-output relationship over a period of time at reference conditions for a control/protection signal isolating device,

A/D - allowance for conversion accuracy of an analog signal to a digital signal for process computer use,

CA - allowance for the accuracy of a controller, not including deadband.

A more detailed explanation of the Westinghouse methodology noting the interaction of several parameters is provided in reference 4.

III. Instrumentation Uncertainties

The instrumentation uncertainties will be discussed first for the two parameters which are controlled by automatic systems, Pressurizer pressure, and T_{avg} (through Rod Control). The uncertainties for both of these parameters are listed on Table 1b, Typical Instrumentation Uncertainties.

1.b. <u>Pressurizer Pressure</u>

Pressurizer pressure is controlled by a system that compares the measured pressure against a reference value. The pressure is measured by a pressure cell connected to the vapor space of the pressurizer. Allowances are made as indicated on Table 1b for the sensor/transmitter and the process racks/controller. As noted, the CSA for this function is [ ]$^{+a,c}$ which corresponds to a control accuracy of [ ]$^{+a,c}$. The accuracy assumed in the ITDP analysis is [ ]$^{+a,c}$, thus, margin exists between analysis and the plant. Being a controlled parameter, the nominal value of 2235 psig is reasonable and bounded by ITDP error analysis assumptions, i.e., assuming a normal, two sided distribution for CSA and a 95+% probability distribution (which will be documented later in this response), σ for the noted CSA equals [ ]$^{+a,c}$. Assuming a normal, two sided distribution for the ITDP assumption of [ ]$^{+a,c}$ and a 95+% probability distribution results in a σ = [ ]$^{+a,c}$. Thus,

TABLE 1b

TYPICAL INSTRUMENTATION UNCERTAINTIES
(Using Rosemount RTDs)

	Pressurizer Pressure Control (1)	Rod Control (Temperature) Tavg (1)	Rod Control (Temperature) 1st Stage Turbine Impulse Pressure (1)	Steamline Pressure Indication (Computer (1)	Feedwater Temperature Indication (Computer) (1)	Feedwater Pressure Indication (Computer) (1)	Feedwater Δp Indication (Computer) (1)	Pressurizer Pressure Indication (DVM (1)	Feedwater Temperature Indication (DVM) (1)	Steamline Pressure Indication (DVM) (1)	T_H Indication (DVM) (1)	T_C Indication (DVM) (1)
PMA												
PEA												
SCA												
SD												
STE												
SPE												
RCA												
RD												
RTE												
DVM												
ID												
A/D												
CA												
CSA												
Span	800 psi	100°F	100°F	1200 psi	400°F	1500 psi	100%Δp	800 psi	400°F	1200 psi	100°F	100°F

+a,c

(1) % instrument span
(2) Corresponds to an accuracy of [ ]+a,c
(3) Determined using Eq. 3
(4) Determined using Eq. 1
(5) Determined using Eq. 2
(6) Corresponds to an accuracy of [ ]+a,c

Cb

margin exists between the expected and assumed standard deviations for Pressurizer pressure.

2.b. $\underline{T_{AVG}}$

T_{avg} is controlled by a system that compares the auctioneered high T_{avg} from the loops with a reference derived from the First Stage Turbine Impulse Pressure. T_{avg} is derived from the average of the narrow range T_H and T_C from the bypass manifolds. The highest loop T_{avg} is then used in the controller. Allowances are made as noted on Table 1b for the sensor/transmitter and the process racks/controller. As noted, the CSA for this function is [ ]$^{+a,c}$ which corresponds to an instrumentation accuracy of [ ]$^{+a,c}$. Assuming a normal, two sided distribution for CSA and a 95+% probability distribution results in a standard deviation, σ = [ ]$^{+a,c}$.

However, this does not include the controller deadband of ± 1.5°F. To determine the controller accuracy the instrumentation accuracy must be combined with the deadband. Westinghouse has determined that the probability distribution for the deadband is [

]$^{+a,c}$ The variance for the deadband uncertainty is then:

[ ]$^{+a,c}$

and the standard deviation, $\sigma \approx$ [ ]$^{+a,c}$.

Combining statistically the standard deviations for instrumentation and deadband results in a controller standard deviation of:

$\sigma_T = \sqrt{\sigma_1^2 + \sigma_2^2} =$ [ ]$^{+a,c}$.

Therefore, the controller uncertainty for a 95+% normal probability distribution is ∿ [ ].$^{+a,c}$ This is the uncertainty assumed for the ITDP error analysis and reasonably bounds the nominal value corresponding to the full power T_{avg}.

3.b. Reactor Power

Generally a plant performs a primary/secondary side heat balance once every 24 hours when power is above 15% Rated Thermal Power. This heat balance is used to verify that the plant is operating within the limits of the Operating License and to adjust the Power Range Neutron Flux channels when the difference between the NIS and the heat balance is greater than that allowed by the plant Technical Specifications.

Assuming that the primary and secondary sides are in equilibrium; the core power is determined by summing the thermal output of the steam generators, correcting the total secondary power for steam generator blowdown (if not secured), subtracting the RCP heat addition, adding the primary side system losses, and dividing by the core rated Btu/hr at full power. The equation for this calculation is:

$$RP = \left(\frac{\sum^{N} [Q_{SG} - Q_p] + Q_L}{H} \right) 100 \qquad \text{Eq. 4}$$

where;

RP	=	Core power (% RTP)
N	=	Number of primary side loops
Q_{SG}	=	Steam Generator thermal output (Btu/hr)
Q_p	=	RCP heat adder (Btu/hr)
Q_L	=	Primary system net heat losses (Btu/hr)
H	=	Core rated Btu/hr at full power.

For the purposes of this uncertainty analysis (and based on H noted above) it is assumed that the plant is at 100% RTP when the measurement is taken. Measurements performed at lower power levels will result in different uncertainty values. However, operation at lower power levels results in increased margin to DNB far in excess of any margin losses due to increased measurement uncertainty.

The thermal output of the steam generator is determined by a calorimetric measurement defined as:

$$Q_{SG} = (h_s - h_f)\, W_f \qquad \text{Eq. 5}$$

where;

h_s = Steam enthalpy (Btu/lb)
h_f = Feedwater enthalpy (Btu/lb)
W_f = Feedwater flow (lb/hr).

The steam enthalpy is based on the measurement of steam generator outlet steam pressure, assuming saturated conditions. The feedwater enthalpy is based on the measurement of feedwater temperature and an assumed feedwater pressure based on steamline pressure, plus 100 psi. The feedwater flow is determined by multiple measurements and a calculation based on the following:

$$W_f = (K)(F_a)(\sqrt{\rho_f \Delta p}) \qquad \text{Eq. 6}$$

where:

K = Feedwater venturi flow coefficient
F_a = Feedwater venturi correction for thermal expansion
ρ_f = Feedwater density (lb/ft^3)
Δp = Feedwater venturi pressure drop (inches H_2O).

The feedwater venturi flow coefficient is the product of a number of constants including as-built dimensions of the venturi and calibration tests performed by the vendor. The thermal expansion correction is based on the coefficient of expansion of the venturi material and the difference between feedwater temperature and calibration temperature. Feedwater density is based on the measurement of feedwater temperature and feedwater pressure. The venturi pressure drop is obtained from the output of the differential pressure cell connected to the venturi.

The RCP heat adder is determined by calculation, based on the best estimates of coolant flow, pump head, and pump hydraulic efficiency.

The primary system net heat losses are determined by calculation, considering the following system heat inputs and heat losses:

Charging flow
Letdown flow
Seal injection flow
RCP thermal barrier cooler heat removal
Pressurizer spray flow
Pressurizer surge line flow
Component insulation heat losses
Component support heat losses
CRDM heat losses

A single calcuated sum for full power operation is used for these losses/heat inputs.

The core power measurement is based on the following plant measurements:

Steamline pressure (P_s)
Feedwater temperature (T_f)
Feedwater pressure (P_f)
Feedwater venturi differential pressure (Δp)
Steam generator blowdown (if not secured)

and on the following calculated values:

Feedwater venturi flow coefficient (K)
Feedwater venturi thermal expansion correction (F_a)
Feedwater density (ρ_f)

Feedwater enthalpy (h_f)
Steam enthalpy (h_s)
Moisture carryover (impacts h_s)
Primary system net heat losses (Q_L)
RCP heat adder (Q_p)

These measurements and calculations are presented schematically on Figure 1.

Starting off with the Equation 6 parameters, the detailed derivation of the measurement errors is noted below.

Feedwater Flow

Each of the feedwater venturis is calibrated by the vendor in a hydraulic laboratory under controlled conditions to an accuracy of [ ]$^{+a,b,c}$ % of span. The calibration data which substantiates this accuracy is provided for all of the plant venturis by the respective vendors. An additional uncertainty factor of [ ]$^{+a,c}$ % is included for installation effects, resulting in an overall flow coefficient (K) uncertainty of [ ]$^{+a,c}$ %. Since steam generator thermal output is proportional to feedwater flow, the flow coefficient uncertainty is expressed as [ ]$^{+a,c}$ % power.

The uncertainty applied to the feedwater venturi thermal expansion correction (F_a) is based on the uncertainties of the measured feedwater temperature and the coefficient of thermal expansion for the venturi material, usually 304 stainless steel. For this material, a change of ± 2°F in the feedwater temperature range changes F_a by [ ]^{a,b,c} % and the steam generator thermal output by the same amount. For this derivation, an uncertainty of [ ]$^{+a,c}$ in feedwater temperature was assumed (detailed breakdown for this assumption is provided in the feedwater enthalpy section). This results in a total uncertainty in F_a and steam generator output of [ ]$^{+a,c}$ %.

Based on data introduced into the ASME code, the uncertainty in F_a for 304 stainless steel is ± 5 percent. This results in an additional uncertainty of [$]^{+a,c}$ % in feedwater flow. A conservative value of [$]^{a,c}$ % is used in this analysis.

Using the ASME Steam Tables (1967) for compressed water, the effect of a [$]^{+a,c}$ error in feedwater temperature on the $\sqrt{\rho_f}$ is [$]^{+a,c}$ % in steam generator thermal output. An error of [$]^{+a,c}$ in feedwater pressure is assumed in the analysis (detailed breakdown of this value is provided in the steam enthalphy section). This results in an uncertainty in $\sqrt{\rho_f}$ of [$]^{+a,c}$ % in steam generator thermal output. The combined effect of the two results in a total $\sqrt{\rho_f}$ uncertainty of [$]^{+a,c}$ % in steam generator thermal output.

Table 1b provides a listing of the instrumentation errors for feedwater Δp (including an allowance for the venturi as defined above) assuming display on the process computer. With the exception of the computer readout error, the electronics errors are in percent Δp span and must be translated into percent feedwater flow at full power conditions. This is accomplished by multiplying the error in percent Δp span by the conversion factor noted below:

$$\left(\frac{1}{2}\right)\left(\frac{\text{span of feedwater flow transmitter in \% of nominal flow}}{100}\right)^2 .$$

For a feedwater flow transmitter span of [$]^{+a,c}$ % nominal flow, the conversion factor is [$]^{+a,c}$ (which is the value used for this analysis).

As noted in Table 2b, the statistical sum of the errors for feedwater flow is [$]^{+a,c}$ % of steam generator thermal output.

Feedwater Enthalpy

The next major error component is the feedwater enthalpy used in Equation 5. For this parameter the major contributor to the error is the uncertainty in the feedwater temperature. Table 1b provides the detailed error breakdown for this temperature measurement assuming indication on the process computer. Statistically summing these errors (utilizing Eq. 2) results in a total temperature error of [ ]$^{+a,c}$ % span. Assuming a span of [ ]$^{+a,c}$ results in a temperature error of [ ].$^{+a,c}$ A conservative, bounding value of [ ]$^{+a,c}$ was assumed for this analysis. Assuming smaller spans results in smaller temperature errors.

Using the ASME steam tables (1967) for compressed water, the effect of a [ ]$^{+a,c}$ error in feedwater temperature on the feedwater enthalpy (h_f) is [ ]$^{+a,c}$ % in steam generator thermal output. Assuming a [ ]$^{+a,c}$ error in feedwater pressure (detailed breakdown provided in the steam enthalpy section) results in a [ ]$^{+a,c}$ % effect in h_f and steam generator thermal output. The combined effect of the two results in a total h_f uncertainty of [ ]$^{+a,c}$ %. A conservative value (based on round-off effects of individual instrumentation errors) of [ ]$^{+a,c}$ % for h_f uncertainty is used in this analysis (as noted on Table 2b).

Steam Enthalpy

The steam enthalpy has two contributors to the calorimetric error, steamline pressure and the moisture content. For steamline pressure the errors are as noted on Table 1b, assuming display on the process computer. This results in a total instrumentation error (utilizing Eq. 2) of [ ]$^{+a,c}$ % span. Based on a 1200 psig span this equals [ ].$^{+a,c}$ A conservative value of [ ]$^{+a,c}$ is assumed in this analysis. The feedwater pressure is assumed to be 100 psi higher than the steamline pressure with a conservatively high measurement error of [ ].$^{+a,c}$ Table 1b provides a breakdown of expected errors if feedwater pressure is measured directly and displayed on the process computer. The results indicate an expected error of [ ]$^{+a,c}$, well within the assumed value.

Using the ASME Steam Tables (1967) for saturated water and steam, the effect of a [ ]$^{+a,c}$ ([ ]$^{+a,c}$) error in steamline pressure on the steam enthalpy (h_s) is [ ]$^{+a,c}$ % in steam generator thermal output. Thus a total instrumentation error of [ ]$^{+a,c}$ in steamline pressure results in an uncertainty of [ ]$^{+a,c}$ % in steam generator thermal output.

The major contributor to h_s uncertainty is moisture content. The nominal or best estimate performance level is assumed to be [ ]$^{+a,c}$ %, which is the design limit to protect the high pressure turbine. The most conservative assumption that can be made in regards to maximizing steam generator thermal output is a steam moisture content of zero. This conservatism is introduced by assigning an uncertainty of [ ]$^{+a,c}$ % to the moisture content, which is equivalent through enthalpy change to [ ]$^{+a,c}$ % of thermal output. The combined effect of the steamline pressure and moisture content on the total h_s uncertainty is [ ]$^{+a,c}$ % in steam generator thermal output.

Loop Power

The loop power uncertainty is obtained by statistically combining all of the error components noted for the steam generator thermal output (Q_{SG}) in terms of loop power. Within each loop these components are independent effects (or formed into independent quantities) since they are independent measurements. Technically, the feedwater temperature and pressure uncertainties are common to several of the error components. However, they are treated as independent quantities because of the conservatism assumed and the arithmetic summation of their uncertainties before squaring them has no significant effect on the final result.

The only effect which tends to be dependent, affecting all loops, is the accumulation of crud on the feedwater venturis, which can effect the Δp for a specified flow. Although it is conceivable that the crud accumulation could affect the static pressure distribution at the venturi throat pressure tap in a manner that would result in a higher flow for a specified Δp, the reduction in throat area resulting in a lower flow at the specified Δp is the stronger effect. All reported cases of venturi fouling have been associated with a significant loss in electrical output, indicating that the actual thermal power has been below the measured power rather than above it. Losses in net power generation which have been correlated with venturi fouling have occurred in about half of the more than 20 Westinghouse pressurized water reactors operating in the United States. These power losses have been generally in the range of two to three percent. Power losses have also occurred in at least three, and possibly five plants out of the more than ten Westinghouse plants operating abroad. In no case has venturi fouling been reported which resulted in a non-conservative feedwater flow measurement. Because the venturi crud formations have resulted in a conservative, reduced power condition, no uncertainty has been included in the analysis of power measurement error for this phenomenon.

The net pump heat uncertainty is derived in the following manner. The primary system net heat losses and pump heat adder for a four loop plant are summarized as follows:

Systems heat losses	- 2.0 MWt
Component conduction and convection losses	- 1.4
Pump heat adder	+18.0
Net Heat input to RCS	+14.6 MWt

The uncertainties for these quantities are as follows: The uncertainty on system heat losses, which are essentially all due to charging and letdown flows, has been estimated to be [ ]$^{+a,c}$ % of the calculated value. Since direct measurements are not possible, the uncertainty on component conduction and convection losses has been assumed to be [ ]$^{+a,c}$ % of the calculated value. Reactor coolant pump hydraulics are known to a relatively high confidence level, supported by the system hydraulics tests performed at Prairie Island II and by input power measurements from several plants, so the uncertainty for the pump heat adder is estimated to be [ ]$^{+a,c}$ % of the best estimate value. Considering these parameters as one quantity which is designated the net pump heat uncertainty, the combined uncertainties are less than [ ]$^{+a,c}$ % of the total, which is equivalent to [ ]$^{+a,c}$ % of core power.

The Total Loop Power uncertainty (noted in Table 2 as [ ]$^{+a,c}$ %) is the statistical sum of the Loop Power uncertainty (Q_{SG}), [ ]$^{+a,c}$ %, and the Net Pump Heat Addition, [ ]$^{+a,c}$ %. The Total Secondary Power uncertainty is the statistical combination of the Loop Power uncertainty and the number of primary side loops in the plant. As noted in Table 2b, the Secondary Power uncertainty for N loops is as follows:

N = 4	uncertainty =	$\pm$ 1.2 % power
3		$\pm$ 1.4 % power
2		$\pm$ 1.7 % power

In all cases the total Secondary Power uncertainty is less than or equal to the historically used value of $\pm$ 2 % power. For ITDP, credit is taken for the increased knowledge of reactor power and the values noted above are used in the ITDP error analysis, i.e., the standard deviation for reactor power, at the 95+% probability level is:

FIGURE 1
POWER CALORIMETRIC SCHEMATIC

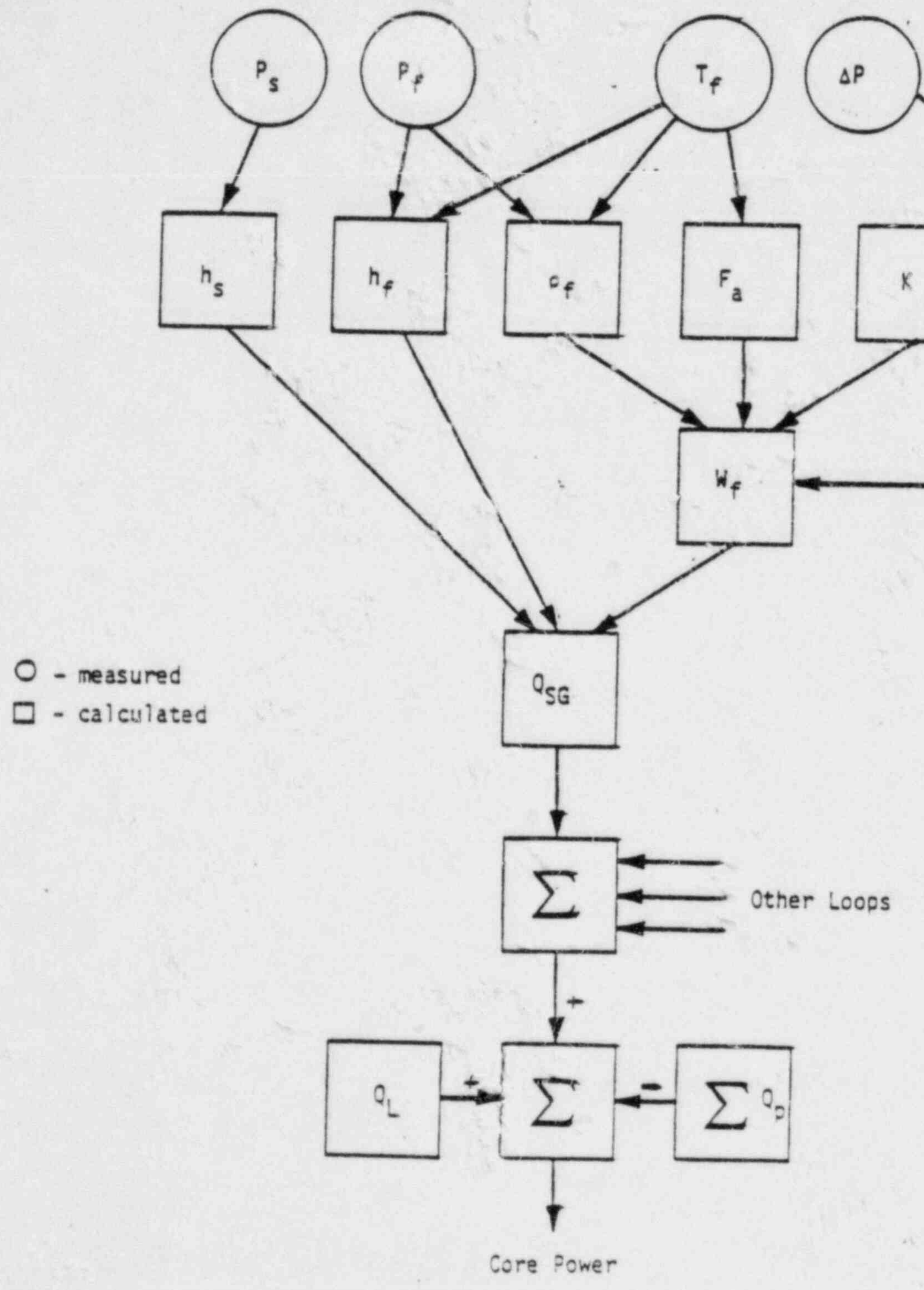


TABLE 2 b

SECONDARY POWER CALORIMETRIC MEASUREMENT UNCERTAINTIES

Component	Instrument Error	Power Uncertainty
Feedwater Flow		+a,c
Venturi, K		
Thermal Expansion Coefficient		
Temperature		
Material		
Density		
Temperature		
Pressure		
Electronics		
ΔP Cell Calibration		
Sensor Pressure Effects		
Sensor Temperature Effects		
Sensor Drift		
Rack Calibration		
Rack Temperature Effects		
Rack Drift		
Computer Isolator Drift		
Computer Readout		
Total Electronics Error $\sqrt{\Sigma(e)^2}$		
Total Feedwater Flow Error $\sqrt{\Sigma(e)^2}$		

TABLE 2b (Cont)

SECONDARY POWER CALORIMETRIC MEASUREMENT UNCERTAINTIES

+a,c

Component	Instrument Error	Power Uncertainty
Feedwater Enthalpy		
Temperature (Electronics)		
RTD Calibration		
R/I Converter		
Rack Accuracy		
Rack Temperature Effects		
Rack Drift		
Computer Isolator Drift		
Computer Readout		
Total Electronics Error $\sqrt{\Sigma(e)^2}$		
Feedwater Temperature Error Assumed		
Pressure		
Total Feedwater Enthalpy Error $\sqrt{\Sigma(e)^2}$		
Steam Enthalpy		
Steamline Pressure (Electronics)		
Pressure Cell Calibration		
Sensor Temperature Effects		
Sensor Drift		
Rack Calibration		
Rack Temperature Effects		

TABLE 2 b(Cont)

SECONDARY POWER CALORIMETRIC MEASUREMENT UNCERTAINTIES

Component	Instrument Error	Power Uncertainty
Steam Enthalpy (Cont)		
Rack Drift	+a,c	+a,c
Computer Isolator Drift		
Computer Readout		
Total Electronics Error $\sqrt{\Sigma(e)^2}$		
Steamline Pressure Error Assumed		
Moisture Carryover		
Total Steam Enthalpy Error $\sqrt{\Sigma(e)^2}$		
Loop Power Uncertainty $\sqrt{\Sigma(e)^2}$		
Net Pump Heat Addition Uncertainty		
Total Loop Power Uncertainty (8)		
Total Secondary Power Uncertainty $\sqrt{[\Sigma(e)^2]/N}$		
where N = 4 loops		$\pm$ 1.2%
3 loops		$\pm$ 1.4%
2 loops		$\pm$ 1.7%

NOTES FOR TABLE 2 b

1. Temperature effect on Thermal Expansion Coefficient is assumed to be linear with an uncertainty of [ ]$^{+a,b,c}$ per 2°F change.

2. Conservative assumption for value, particularly if steamline pressure + 100 psi is assumed value. Uncertainty for steamline pressure noted in Steam Enthalpy.

3. To transform error in percent Δp span to percent of feedwater flow at 100% of nominal feedwater flow; multiply the instrument error by:

$$\left(1/2\right)\left(\frac{\text{Span of feedwater flow transmitter in percent of nominal flow}}{100}\right)^2$$

In this analysis the feedwater flow transmitter span is assumed to be [ ]$^{+a,c}$ % of nominal flow.

4. In this analysis assumed an error of [ ]$^{+a,c}$ and a maximum swing in feedwater pressure from no load to full power of [ ].$^{+a,c}$

5. [ ]$^{+a,c}$

6. [ ]$^{+a,c}$ span of [ ]$^{+a,c}$ equals [ ]$^{+a,c}$ which equals [ ]$^{+a,c}$ power.

7. Conservative assumption for instrumentation error for this analysis.

8. Statistical sum of Loop Power Uncertainty and Net Pump Heat Addition Uncertainty.

N = 4	σ =	$\left[\quad\right]^{+a,c}$	power
3			power
2			power

4.b. <u>RCS FLOW</u>

The Improved Thermal Design Procedure (ITDP) and some plant Technical Specifications require an RCS flow measurement with a high degree of accuracy. It is assumed for this error analysis, that this flow measurement is performed within seven days of calibrating the measurement instrumentation therefore, drift effects are not included (except where necessary due to sensor location). It is also assumed that the calorimetric flow measurement is performed at the beginning of a cycle, so no allowances have been made for feedwater venturi crud buildup.

The flow measurement is performed by determining the steam generator thermal output, corrected for the RCP heat input and the loop's share of primary system heat losses, and the enthalpy rise (Δh) of the primary coolant. Assuming that the primary and secondary sides are in equilibrium; the RCS total vessel flow is the sum of the individual primary loop flows, i.e.,

$$W_{RCS} = \sum W_L \quad . \qquad \text{(Eq. 7)}$$

The individual primary loop flows are determined by correcting the thermal output of the steam generator for steam generator blowdown (if not secured), subtracting the RCP heat addition, adding the loop's share of the primary side system losses, dividing by the primary side enthalpy rise, and multiplying by the specific volume of the RCS cold leg. The equation for this calculation is:

$$W_L = \frac{(\gamma)\left\{Q_{SG} - Q_P + \left(\frac{Q_L}{N}\right)\right\}(V_C)}{\left[h_H - h_C\right]} \qquad \text{(Eq. 8)}$$

where; W_L = Loop flow (gpm)

γ = 0.1247 gpm/(ft^3/hr)

Q_{SG} = Steam Generator thermal output (Btu/hr)

Q_p = RCP heat adder (Btu/hr)

Q_L = Primary system net heat losses (Btu/hr)

V_c = Specific volume of the cold leg at T_C (ft^3/lb)

N = Number of primary side loops

h_H = Hot leg enthalpy (Btu/lb)

h_c = Cold leg enthalpy (Btu/lb).

The thermal output of the steam generator is determined by the same calorimetric measurement as for reactor power, which is defined as:

$$Q_{SG} = (h_s - h_f)\, W_f \qquad \text{(Eq. 5)}$$

where; h_s = Steam enthalpy (Btu/lb)

h_f = Feedwater enthalpy (Btu/lb)

W_f = Feedwater flow (lb/hr).

The steam enthalpy is based on measurement of steam generator outlet steam pressure, assuming saturated conditions. The feedwater enthalpy is based on the measurement of feedwater temperature and an assumed feedwater pressure based on steamline pressure plus 100 psi. The feedwater flow is determined by multiple measurements and the same calculation as used for reactor power measurements, which is based on the following:

$$W_f = (K)\,(F_a)\left\{\sqrt{\rho_f\, \Delta p}\right\} \qquad \text{(Eq. 6)}$$

where; K = Feedwater venturi flow factor

F_a = Feedwater venturi correction for thermal expansion

ρ_f = Feedwater density (lb/ft^3)

Δp = Feedwater venturi pressure drop (inches H_2O).

The feedwater venturi flow coefficient is the product of a number of constants including as-built dimensions of the venturi and calibration tests performed by the vendor. The thermal expansion correction is based on the coefficient of expansion of the venturi material and the difference between feedwater temperature and calibration temperature. Feedwater density is based on the measurement of feedwater temperature and feedwater pressure. The venturi pressure drop is obtained from the output of the differential pressure cell connected to the venturi.

The RCP heat adder is determined by calculation, based on the best estimates of coolant flow, pump head, and pump hydraulic efficiency.

The primary system net heat losses are determined by calculation, considering the following system heat inputs and heat losses:

Charging flow
Letdown flow
Seal injection flow
RCP thermal barrier cooler heat removal
Pressurizer spray flow
Pressurizer surge line flow
Component insulation heat losses
Component support heat losses
CRDM heat losses.

A single calculated sum for full power operation is used for these losses/heat inputs.

The hot leg and cold leg enthalpies are based on the measurement of the hot leg temperature, cold leg temperature and the pressurizer pressure. The cold leg specific volume is based on measurement of the cold leg temperature and pressurizer pressure.

The RCS flow measurement is thus based on the following plant measurements:

Steamline pressure (P_s)
Feedwater temperature (T_f)
Feedwater pressure (P_f)
Feedwater venturi differential pressure (Δp)
Hot leg temperature (T_H)
Cold leg temperature (T_C)
Pressurizer pressure (P_p)
Steam generator blowdown (if not secured)

and on the following calculated values:

Feedwater venturi flow coefficients (K)
Feedwater venturi thermal expansion correction (F_a)
Feedwater density (ρ_f)
Feedwater enthalpy (h_f)
Steam enthalpy (h_s)
Moisture carryover (impacts h_s)
Primary system net heat losses (Q_L)
RCP heat adder (Q_p)
Hot leg enthalpy (h_H)
Cold leg enthalpy (h_C).

These measurements and calculations are presented schematically on Figure 2.

Starting off with the Equation 6 parameters, the detailed derivation of the measurement errors is noted below.

Feedwater Flow

Each of the feedwater venturis is calibrated by the vendor in a hydraulics laboratory under controlled conditions to an accuracy of [$]^{+a,b,c}$ % of span. The calibration data which substantiates this accuracy is provided for all of the plant venturis by the respective vendors. An additional uncertainty factor of [$]^{+a,c}$ % is included for installation effects, resulting in an overall flow coefficient (K) uncertainty of [$]^{+a,c}$ %. Since RCS loop flow is proportional to steam generator thermal output which is proportional to feedwater flow, the flow coefficient uncertainty is expressed as [$]^{+a,c}$ % flow.

The uncertainty applied to the feedwater venturi thermal expansion correction (F_a) is based on the uncertainties of the measured feedwater temperature and the coefficient of thermal expansion for the venturi material, usually 304 stainless steel. For this material, a change of $\pm$ 2°F in the feedwater temperature range changes F_a by [$]^{+a,b,c}$ % and the steam generator thermal output by the same amount. For this derivation, an uncertainty of [$]^{+a,c}$ in feedwater temperature was assumed (detailed breakdown for this assumption is provided in the feedwater enthalpy section). This results in a negligible impact in F_a and steam generator output.

Based on data introduced into the ASME Code, the uncertainty in F_a for 304 stainless steel is $\pm$ 5 %. This results in an additional uncertainty of [$]^{+a,c}$ % in feedwater flow. A conservative value of [$]^{+a,c}$ % is used in this analysis.

Using the ASME Steam Tables (1967) for compressed water, the effect of a [$]^{+a,c}$ error in feedwater temperature on the $\sqrt{\rho_f}$ is [$]^{+a,c}$ % in steam generator thermal output. An error of [$]^{+a,c}$ in feedwater pressure is assumed in this analysis (detailed breakdown of this value is provided in the steam enthalpy section). This results in an uncertainty in $\sqrt{\rho_f}$ of [$]^{+a,c}$ % in steam generator thermal output. The combined effect of the two results in a total $\sqrt{\rho_f}$ uncertainty of [$]^{+a,c}$ % in steam generator thermal output.

It is assumed that the Δp cell (usually a Barton or Rosemount) is read locally and soon after the Δp cell and local meter are calibrated (within 7 days of calibration). This allows the elimination of process rack and sensor drift errors from consideration. Therefore, the Δp cell errors noted in this analysis are [ ]$^{+a,c}$ % for calibration and [ ]$^{+a,c}$ % for reading error of the special high accuracy, local gauge. These two errors are in % Δp span. In order to be useable in this analysis they must be translated into % feedwater flow at full power conditions. This is accomplished by multiplying the error in % Δp span by the conversion factor noted below:

$$\left(\frac{1}{2}\right)\left(\frac{\text{span of feedwater flow transmitter in percent of nominal flow}}{100}\right)^2$$

For a feedwater flow transmitter span of [ ]$^{+a,c}$ % nominal flow, the conversion factor is [ ]$^{+a,c}$ (which is the value used in this analysis).

As noted in Table 3b, the statistical sum of the errors for feedwater flow is [ ]$^{+a,c}$ % of steam generator thermal output.

Feedwater Enthalpy

The next major error component is the feedwater enthalpy used in Equation 5. For this parameter the major contributor to the error is the uncertainty in the feedwater temperature. It is assumed that the feedwater temperature is determined through the use of an RTD or thermocouple whose output is read by a digital voltmeter (DVM) or digital multimeter (DMM) (at the output of the RTD or by a Wheatstone Bridge for RTD's, or at the reference junction for thermocouples). It is also assumed that the process components of the above are calibrated within 7 days prior to the measurement allowing the elimination of drift effects. Therefore, the error breakdown for feedwater temperature is as noted on Table 1b. The statistical combination of these errors results in a total feedwater temperature error of [ ]$^{+a,c}$.

Using the ASME Steam Table (1967) for compressed water, the effect of a [ ]$^{+a,c}$ error in feedwater temperature on the feedwater enthalpy (h_f) is [ ]$^{+a,c}$ % in steam generator thermal output. Assuming a [ ]$^{+a,c}$ error in feedwater pressure (detailed breakdown provided in the steam enthalpy section) results in a [ ]$^{+a,c}$ % effect in h_f and steam generator thermal output. The combined effect of the two results in a total h_f uncertainty of [ ]$^{+a,c}$ % steam generator thermal output, as noted on Table 3b.

Steam Enthalpy

The steam enthalpy has two contributors to the calorimetric error, steamline pressure and the moisture content. For steamline pressure the error breakdown is as noted on Table 1b. This results in a total instrumentation error of [ ]$^{+a,c}$ %, which equals [ ]$^{+a,c}$ for a 1200 psi span. For this analysis a conservative value of [ ]$^{+a,c}$ is assumed for the steamline pressure. The feedwater pressure is assumed to be 100 psi higher than the steamline pressure with a conservatively high measurement error of [ ]$^{+a,c}$. If feedwater pressure is measuredon the same basis as the steamline pressure (with a DVM) the error is [ ]$^{+a,c}$ % span, which equals [ ]$^{+a,c}$ for a 1500 psi span. Thus, an assumption of an error of [ ]$^{+a,c}$ is very conservative.

Using the ASME Steam Tables (1967) for saturated water and steam, the effect of a [ ]$^{+a,c}$ ([ ]$^{+a,c}$) error in steamline pressure on the steam enthalpy is [ ]$^{+a,c}$ % in steam generator thermal output. Thus, a total instrumentation error of [ ]$^{+a,c}$ results in an uncertainty of [ ]$^{+a,c}$ % in steam generator thermal output, as noted on Table 3b.

The major contributor to h_s uncertainty is moisture content. The nominal or best estimate performance level is assumed to be [ ]$^{+a,c}$ % which is the design limit to protect the high pressure turbine. The most conservative assumption that can be made in regards to maximizing steam generator thermal output is a steam moisture content of zero. This conservatism is introduced by assigning an uncertainty of [ ]$^{+a,c}$ % to the moisture content, which is equivalent through enthalpy change to [ ]$^{+a,c}$ % of thermal output. The combined effect of the steamline pressure and moisture content on the total h_s uncertainty is [ ]$^{+a,c}$ % in steam generator thermal output.

Secondary Side Loop Power

The loop power uncertainty is obtained by statistically combining all of the error components noted for the steam generator thermal output (Q_{SG}) in terms of Btu/hr. Within each loop these components are independent effects since they are independent measurements. Technically, the feedwater temperature and pressure uncertainties are common to several of the error components. However, they are treated as independent quantities because of the conservatism assumed and the arithmetic summation of their uncertainties before squaring them has no significant effect on the final result.

The only effect which tends to be dependent, affecting all loops, would be the accumulation of crud on the feedwater venturis, which can affect the Δp for a specified flow. Although it is conceivable that the crud accumulation could affect the static pressure distribution at the venturi throat pressure tap in a manner that would result in a higher flow for a specified Δp, the reduction in throat area resulting in a lower flow at the specified Δp is the stronger effect. No uncertainty has been included in the analysis for this effect. If venturi fouling is detected by the plant, the venturi should be cleaned, prior to performance of the measurement. If the venturi is not cleaned, the effect of the fouling on the determination of the feedwater flow, and thus, the steam generator power and RCS flow, should be measured and treated as a bias, i.e., the error due to venturi fouling should be added to the statistical summation of the rest of the measurement errors.

The net pump heat uncertainty is derived in the following manner. The primary system net heat losses and pump heat adder for a four loop plant are summarized as follows:

System heat losses	-2.0 MWt
Component conduction and convection losses	-1.4
Pump heat adder	+18.0
Net Heat input to RCS	+14.6 MWt

The uncertainties for these quantities are as follows: The uncertainty on systems heat losses, which is essentially all due to charging and letdown flows, has been estimated to be [ ]+a,c % of the calculated value. Since direct measurements are not possible, the uncertainty on component conduction and convection losses has been assumed to be [ ]+a,c % of the calculated value. Reactor coolant pump hydraulics are known to a relatively high confidence level, supported by the system hydraulics tests performed at Prairie Island II and by input power measurements from several plants, so the uncertainty for the pump heat adder is estimated to be [ ]+a,c % of the best estimate value. Considering these parameters as one quantity which is designated the net pump heat uncertainty, the combined uncertainties are less than [ ]+a,c % of the total, which is [ ]+a,c % of core power.

The Total Secondary Side Loop Power Uncertainty (noted in Table 3b as [ ]+a,c %) is the statistical sum of the secondary side loop power uncertainty (Q_{SG}), [ ]+a,c %, and the net pump heat addition, [ ]+a,c %.

Primary Side Enthalpy

The primary side enthalpy error contributors are T_H and T_C measurement errors and the uncertainty in pressurizer pressure. The instrumentation errors for T_H are as noted on Table 1b. These errors are based on the assumption that the DVM has been recently calibrated (within 7 days prior to the measurement) and the DVM is used to read the output of the RTD, or a bridge, thus allowing the elimination of drift effects in the racks. The statistical combination of the above errors results in a total T_H uncertainty of [$]^{+a,c}$.

Table 1b also provides the instrumentation error breakdown for T_C. The errors are based on the same assumptions as for T_H, resulting in a total T_C uncertainty of [$]^{+a,c}$.

Pressurizer pressure instrumentation errors are noted on Table 1b. A sensor drift allowance of [$]^{+a,c}$ % is included due to the difficulty in calibrating while at power. It is assumed calibration is performed only as required by plant Technical Specifications.

Statistically combining these errors results in the total pressurizer pressure uncertainty equaling [$]^{+a,c}$ % of span, which equals [$]^{+a,c}$ for an [$]^{+a,c}$ span. In this analysis a conservative value of [$]^{+a,c}$ is used for the instrumentation error for pressurizer pressure.

The effect of an uncertainty of [$]^{+a,c}$ in T_H on h_H is [$]^{+a,c}$ % of loop flow. Thus, an error of [$]^{+a,c}$ in T_H introduces an uncertainty of [$]^{+a,c}$ percent in h_H. An error of [$]^{+a,c}$ in T_C is worth [$]^{+a,c}$ % in h_C. Therefore, an error of [$]^{+a,c}$ in T_C results in an uncertainty of [$]^{+a,c}$ % in h_C and loop flow. An uncertainty of [$]^{+a,c}$ in pressurizer pressure introduces an error of [$]^{+a,c}$ % in h_H and [$]^{+a,c}$ % in h_C. Statistically combining the hot leg and cold leg temperature and pressure uncertainties results in an h_H uncertainty of [$]^{+a,c}$ %, an h_C uncertainty of [$]^{+a,c}$%, and a total uncertainty in Δh of [$]^{+a,c}$ % in loop flow.

Statistically combining the Total Secondary Side Loop Power Uncertainty (in Btu/hr) with the primary side enthalpy uncertainty (in Btu/lb),

FIGURE 2

RCS FLOW CALORIMETRIC SCHEMATIC

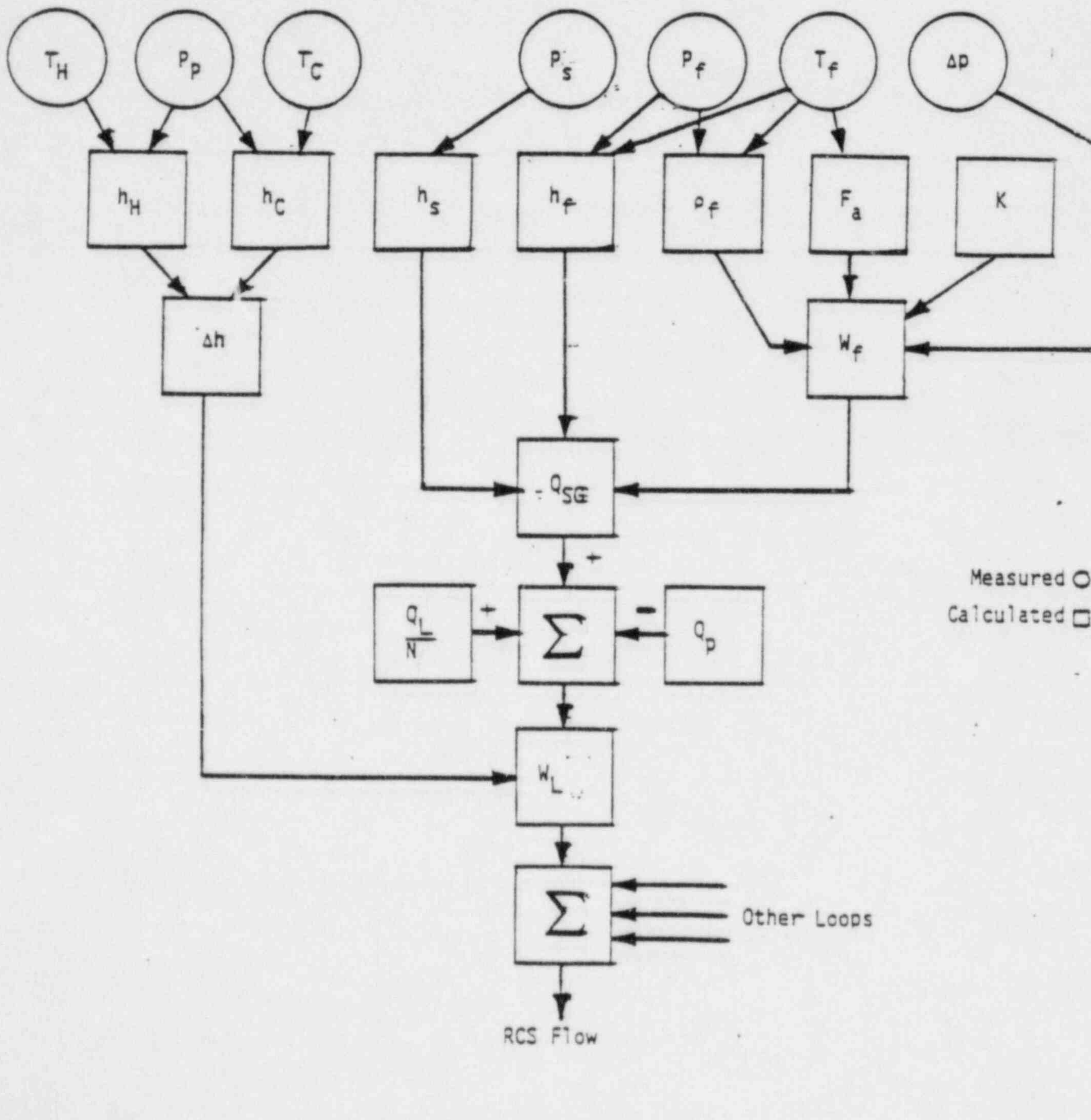


TABLE 3b

CALORIMETRIC RCS FLOW MEASUREMENT UNCERTAINTIES

Component	Instrument Error(1)	Flow Uncertainty
Feedwater Flow		
Venturi, K		
Thermal Expansion Coefficient		
Temperature		
Material		
Density		
Temperature		
Pressure		
Instrumentation		
Δp Cell Calibration		
Δp Cell Gauge Readout		
Total Instrumentation Error $\sqrt{\Sigma(e)^2}$		
Total Feedwater Flow Error $\sqrt{\Sigma(e)^2}$		
Feedwater Enthalpy		
Temperature (Electronics)		
RTD Calibration		
DVM Accuracy		
Total Temperature Error $\sqrt{\Sigma(e)^2}$		
Pressure		
Total Feedwater Enthalpy Error $\sqrt{\Sigma(e)^2}$		

+a,c

TABLE 3b (Cont)

CALORIMETRIC RCS FLOW MEASUREMENT UNCERTAINTIES

Component	Instrument Error(1)	Flow Uncertainty
		+a,c
Steam Enthalpy		
Steamline Pressure (Electronics)		
Pressure Cell Calibration		
Sensor Temperature Effects		
Rack Calibration		
Rack Temperature Effects		
DVM Accuracy		
Total Electronics Error $\sqrt{\Sigma(e)^2}$		
Steamline Pressure Error Assumed		
Moisture Carryover		
Total Steam Enthalpy Error $\sqrt{\Sigma(e)^2}$		
Secondary Side Loop Power Uncertainty $\sqrt{\Sigma(e)^2}$		
Net Pump Heat Addition Uncertainty	$\pm$ 20%	
Total Secondary Side Loop Power Uncertainty $\sqrt{\Sigma(e)^2}$		
Primary Side Enthalpy		
T_H (Electronics)		
RTD Calibration		
DVM Accuracy		
T_H Instrumentation Error $\sqrt{\Sigma(e)^2}$		
T_H Temperature Streaming Error		
T_H Temperature Error $\sqrt{\Sigma(e)^2}$		

TABLE 3b (Cont)

CALORIMETRIC RCS FLOW MEASUREMENT UNCERTAINTIES

Component	Instrument Error(1)	Flow Uncertainty
		+a,c
T_C (Electronics)		
RTD Calibration		
DVM Accuracy		
T_C Instrumentation Error $\sqrt{\Sigma(e)^2}$		
Pressurizer Pressure (Electronics)		
Pressure Cell Calibration		
Sensor Temperature Effects		
Sensor Drift		
Rack Calibration		
Rack Temperature Effects		
DVM Accuracy		
Total Pressurizer Pressure Error $\sqrt{\Sigma(e)^2}$		
Pressurizer Pressure Error Assumed		
T_H Pressure Effect		
T_H Total Error $\sqrt{\Sigma(e)^2}$		
T_C Pressure Effect		
T_C Total Error $\sqrt{\Sigma(e)^2}$		
Total Δh Uncertainty $\sqrt{\Sigma(e)^2}$		
Primary Side Loop Flow Uncertainty $\sqrt{\Sigma(e)^2}$		
Total RCS Flow Uncertainty $\sqrt{[\Sigma(e)^2]/N}$		
where N = 4 loops		$\pm$ 1.5%
3 loops		$\pm$ 1.75%
2 loops		$\pm$ 2.1%

NOTES FOR TABLE 3b

1. Measurements performed within 7 days after calibration thus Rack Drift, and where possible Sensor Drift, effects are not included in this analysis.

2. Conservative assumption for value, particularly if steamline pressure + 100 psi is assumed value. Uncertainty for steamline pressure noted in steam enthalpy.

3. To transform error in percent Δp span to percent of feedwater flow at 100% of nominal feedwater flow; multiply the instrument error by:

$$\left(1/2\right)\left(\frac{\text{Span of feedwater flow transmitter in percent of nominal flow}}{100}\right)^2$$

   In this analysis the feedwater flow transmitter span is assumed to be $[125]^{+a,c}$ % of nominal flow.

4. Reading error for multiple readings of a Barton gauge.

5. Conservative assumption for instrumentation error for this analysis.

6. Maximum allowed moisture carryover to protect HP turbine.

7. Calibration accuracy of $[\quad]^{+a,c}$ span of $[\quad]^{+a,c}$ which equals $[\quad]^{+a,c}$.

8. Credit taken for the 3 tap scoop RTD bypass loop in reducing uncertainties due to temperature streaming.

9. Convoluted sum of T_H Temperature Error and T_H Pressure Effect.

10. Convoluted sum of T_C Instrumentation Error and T_C Pressure Effect.

11. Convoluted sum of T_H Total Error and T_C Total Error.

results in a Primary Side Loop Flow Uncertainty of [ ]$^{+a,c}$ % loop flow. The RCS flow uncertainty is the statistical combination of the primary side loop flow error and the number of primary side loops in the plant. As noted in Table 3b, the RCS Flow uncertainty for N loops is:

N=4	uncertainty	=	$\pm$ 1.5 % flow
3		=	$\pm$ 1.75 % flow
2		=	$\pm$ 2.1 % flow.

For ITDP, credit is taken for the increased knowledge of RCS flow and the values noted above are used in the ITDP error analysis, i.e., the standard deviation for RCS flow, at the 95+% probability level is:

+a,c

N=4	σ	=	[ ] % flow
3		=	[ ] % flow
2		=	[ ] % flow

5. <u>USE OF AN LEFM</u>

If a plant uses a Leading Edge Flow Meter (LEFM), from the Oceanics Division of Westinghouse, for the measurement of feedwater flow, several changes are made in the calorimetric power and flow uncertainty analyses. The following are typical LEFM uncertainties in mass flow (lbs/hr):

a. A nominal accuracy of [ ]$^{+a,c}$ flow. This is based on a feedwater temperature uncertainty of [ ]$^{+a,c}$ and a feedwater pressure uncertainty of [ ]$^{+a,c}$.

b. For each [ ]$^{+a,c}$ increase in Feedwater temperature uncertainty, the mass flow uncertainty increases by [ ]$^{+a,c}$.

c. For a feedwater pressure uncertainty greater than [ ]$^{+a,c}$ but less than [ ]$^{+a,c}$, the mass flow uncertainty increases by [ ]$^{+a,c}$.

Thus, for a typical LEFM installation with a feedwater temperature uncertainty of [ ]$^{+a,c}$ and a pressure uncertainty less than [ ]$^{+a,c}$, the mass flow uncertainty is [ ]$^{+a,c}$ flow.

The effect of the use of an LEFM is seen primarily in the measurement of Reactor Power. The following table provides a comparison of the uncertainties for a power calorimetric using a feedwater venturi and an LEFM. It is assumed for these calculations that a measurement device (either a venturi or an LEFM) is in the feedwater line to each steam generator.

TABLE 4b

COMPARISON OF VENTURI VS. LEFM POWER CALORIMETRIC UNCERTAINTIES

	Venturi*	LEFM	
Reactor Power			+a,c
Feedwater Temperature			
Feedwater Flow			
Feedwater Enthalpy			
Steam Enthalpy			
Loop Power Uncertainty			
Total Loop Power Uncertainty			
Total Secondary Power Uncertainty			
4 loops	± 1.2% RTP	± 0.4% RTP	
3 loops	± 1.4% RTP	± 0.4% RTP	
2 loops	± 1.7% RTP	± 0.5% RTP	

* from Table 2

** due to [ ]+a,c assumption

The impact of the LEFM on RCS Flow measurement is considerably less (primarily due to the [ ]+a,c feedwater temperature error already being assumed and the prime error contributor being T_H and T_C for primary side Δh). However, the following table notes the differences between the two measurements for an RCS Flow calorimetric measurement. For these calculations it is assumed that a measurement device (either a venturi or an LEFM) is in the feedwater line to each steam generator.

TABLE 5b

COMPARISON OF VENTURI VS. LEFM FLOW CALORIMETRIC UNCERTAINTIES

	Venturi*	LEFM
RCS Flow		
Feedwater Flow		
Feedwater Enthalpy		
Steam Enthalpy		
Secondary Loop Power Uncertainty		
Total Secondary Power Uncertainty		
Primary Enthalpy		
Primary Loop Flow Uncertainty		
Total RCS Flow Uncertainty		
4 loops	± 1.5% flow	± 1.45% flow
3 loops	± 1.75% flow	± 1.7% flow
2 loops	± 2.1% flow	± 2.05% flow

]+a,c

* from Table 3b

** due to [ ]+a,c assumption

Therefore, if a plant has installed an LEFM to measure feedwater flow credit would be taken in the ITDP error analysis for the lower uncertainty in Reactor Power, but no credit would be taken in RCS flow.

6.b NORMALIZED ELBOW TAPS FOR RCS FLOW MEASUREMENT

Based on the results of Table 3b, in order for a plant to assure operation within the ITDP assumptions an RCS flow calorimetric would have to be performed once every 31 EFPD. However, this is an involved procedure which requires considerable staff and setup time. Therefore, many plants perform one flow calorimetric at the beginning of the cycle and normalize the loop elbow taps. This allows the operator to quickly determine if there has been a significant reduction in loop flow on a shift basis and to avoid a long monthly procedure. The elbow taps are forced to read 1.0 in the process racks after performance of the full power flow calorimetric, thus, the elbow tap and it's Δp cell are seeing normal operating conditions at the time of calibration/normalization and 1.0 corresponds to the measured loop flow at the time of the measurement.

For monthly surveillance to assure plant operation consistent with the ITDP assumptions, two means of determining the RCS flow are available. One, to read the loop flows from the process computer, and two, to measure the output of the elbow tap Δp cells in the process racks with a DVM. The uncertainties for both methods and their convolution with the calorimetric uncertainty are presented below.

Assuming that only one elbow tap per loop is available to the process computer results in the following elbow tap measurement uncertainty:

	%Δp span	% flow	
PMA	[	]	+a,c
PEA			
SCA			
SPE			
STE			
SD			

	%Δp span	% flow	
RCA	[	]	+a,c
RTE			
RD			
ID			
A/D			
Readout			

Δp span is converted to flow on the same basis as provided in Note 3 of Table 3L for an instrument span of [ ]$^{+a,c}$. Using Eq. 2 results in a loop uncertainty of [ ]$^{+a,c}$ flow per loop. The total uncertainty for N loops is:

N = 4	[ ]$^{+a,c}$	flow
3		
2		

The instrument/measurement uncertainties for normalized elbow taps and the flow calorimetric are statistically independent and are 95+% probability values. Therefore, the statistical combination of the standard deviations results in the following total flow uncertainty at a 95+% probability:

4 loops	$\pm$ 1.7% flow
3 loops	$\pm$ 2.0
2 loops	$\pm$ 2.3

Another method of using normalized elbow taps is to take DVM readings in the process racks of all three elbow taps for each loop. This results in average flows for each loop with a lower instrumentation uncertainty for the total RCS flow. The instrumentation uncertainties for this measurement are:

	%Δp span	% flow
PMA		
PEA		
SCA		
SPE		
STE		

$[\ \ ]^{+a,c}$

	%Δp span	% flow
SD		
RCA		
RTE		
RD		
DVM		
Readout		

$[\ \ ]^{+a,c}$

Δp span is converted to flow on the same basis as provided in Note 3 of Table 3b for an instrument span of $[\ \ ]^{+a,c}$. Using Eq. 1 results in a channel uncertainty of $[\ \ ]^{+a,c}$ flow. Utilizing three elbow taps (which are independent) results in a loop uncertainty of $[\ \ ]^{+a,c}$ flow per loop. The total uncertainty for N loops is:

N =	
4	
3	
2	

$[\ \ ]^{+a,c}$ flow

The calorimetric and the above noted elbow tap uncertainties can be statistically combined as noted earlier. The 95+% probability total flow uncertainties, using three elbow taps per loop are:

4 loops	$\pm$ 1.6% flow
3 loops	$\pm$ 1.8
2 loops	$\pm$ 2.2

The following table summarizes RCS flow measurement uncertainties.

TABLE 6b

TOTAL FLOW MEASUREMENT UNCERTAINTIES

Loops	4	3	2
Calorimetric uncertainty*	$\pm$ 1.5	$\pm$ 1.75	$\pm$ 2.1
Total uncertainty 3 elbow taps/loop	$\pm$ 1.6	$\pm$ 1.8	$\pm$ 2.2
Total uncertainty 1 elbow tap/loop	$\pm$ 1.7	$\pm$ 2.0	$\pm$ 2.3

* Calorimetric uncertainty noted assumes feedwater measurement with a venturi, however, use of an LEFM for feedwater measurement results in essentially the same value.

IV. PROBABILITY JUSTIFICATION

As noted in Section III, it is Westinghouse's belief that the total uncertainty for Pressurizer Pressure, T_{avg}, Reactor Power, and RCS Flow are normal, two sided, 95+% probability distributions. This section will substantiate that position with a comparison between three approaches, the first being that noted in Section II, the second involves determination of the variance assuming a uniform probability distribution for each uncertainty and then determination of the 95% probability value assuming a one sided normal distribution, and the third involves determination of the variance assuming a normal, two sided probability distribution for each uncertainty and then determination of the 95% probability value assuming a two sided normal distribution.

Table 7b lists the results of the three approaches. Column 1 lists the values noted for CSA on Table 1b which are determined through the use of equations 1, 2, or 3, whichever is applicable to that particular function. Column 2 lists the variance for each function assuming the uncertainty for each of the parameters listed in Section 2 is a uniform probability distribution. For this assumption,

$$\sigma^2 = \frac{R^2}{12} \qquad \text{Eq. 9}$$

where R equals the range of the parameter. The variance for the function equals the arithmetic sum of the parameter variances. From a safety point of view deviation in the direction of non-conservatism is important. Therefore, Column 3 lists the one sided 95% probability values based on the variances provided in Column 2, i.e., the one sided 95% probability value for a near normal distribution can be reasonably approximated by: $1.645\sqrt{\sigma^2}$.

Column 4 lists the variance for each function assuming the uncertainty for each of the parameters listed in Section 2 is a near normal, two sided probability distribution. Efforts have been made to conservatively determine the probability value for each of the parameters, see Table 8. For example, [

]$^{+a,c}$ The corresponding Z value listed on Table 8 is from the standard normal curve where:

$$Z = (x - \mu)/\sigma \qquad \text{Eq. 10}$$

The variance for a parameter is then the square of the uncertainty divided by its Z value:

$$\sigma^2 = \left(\frac{\text{uncertainty}}{Z}\right)^2 \qquad \text{Eq. 11}$$

The variance for the function equals the arithmetic sum of the parameter variances. From the variance the two sided 95% probability value for a normal distribution can be calculated: $1.96\sqrt{\sigma^2}$.

To summarize; Column 1 is the results of Equations 1, 2, and 3. Column 2 is the total variance assuming uniform probabilty distributions, i.e.,

$$\sigma^2 = \frac{R_1^{\,2} + R_2^{\,2} + \ldots}{12} = \frac{(2\ unc_1)^2 + (2\ unc_2)^2 + \ldots}{12} \qquad \text{Eq. 12}$$

Column 3 is $1.645\sqrt{\sigma^2}$.

Column 4 is the total variance assuming near normal probability distributions, i.e.,

$$\sigma^2 = \left(\frac{unc_1}{Z_1}\right)^2 + \left(\frac{unc_2}{Z_2}\right)^2 + \ldots \qquad \text{Eq. 13}$$

Column 5 is $1.96\sqrt{\sigma^2}$.

A comparison of Columns 1, 3, and 5 will show that the approach used in Section 2 results in values more conservative than those of Columns 3 and 5. Thus, it can be concluded that the results presented in Section 3 are total uncertainties with probabilities in excess of 95%.

Confidence limits are applicable only to a particular data set, which in this case not available. Therefore, based on the relatively small number of reports indicating large values of deviation, i.e., the number of instances where a channel fails a functional test is very small as compared to the many thousands of functional tests performed, Westinghouse believes that the total uncertainties presented on Table 1b are 95% probability values at a high confidence level.

V. CONCLUSIONS

The preceding sections provide what is believed to be a reasonable means of accounting for instrument and measurement errors for four parameters used in the ITDP analysis. The assumptions used in this response are generic and conservative. It is the intent of this response to generically resolve any concerns with the measurement and control of Reactor Power, RCS Flow, Pressurizer Pressure and T_{avg} as they are applied to ITDP. As such, plant specific responses will provide only that information which indicates that, 1) the instrument and measurement uncertainties for that plant are consistent with or conservative with respect to those presented here, or 2) specific instrument and/or measurement uncertainties for that plant are not consistent with those presented. In the second case the impact of the inconsistency on the four parameters will be provided with corresponding new total uncertainties if the impact is sufficiently large.

TABLE 7b
COMPARISON OF STATISTICAL METHODS

	1 Method 1	2 Variance Method 2	3 95% Probability Method 2	4 Variance Method 3	5 95% Probability Method 3
Pressurizer Pressure - Control					+a,c
T_{avg} - Control					
Steamline Pressure - Computer					
Feedwater Temperature - Computer					
Feedwater Pressure - Computer					
Feedwater Δp - Computer					
Pressurizer Pressure - DVM					
Steamline Pressure - DVM					
Feedwater Temperature - DVM					
T_H - DVM					
T_C - DVM					

Notes for Table 7b

1. Uncertainties presented in columns 1, 3, and 5 are in % span.

2. While values noted are listed to the second decimal place, values are accurate only to the first decimal place. Second place is noted for round-off purposes only.

TABLE 8

UNCERTAINTY PROBABILITIES

	Two Sided Normal Probability (%)	Two Sided Normal, Z Value
PMA		
PEA		
SCA		
SD		
STE		
SPE		
RCA		
RD		
RTE		
DVM		
ID		
A/D		
CA		

+a,c

REFERENCES

1. Westinghouse letter NS-CE-1583, C. Eicheldinger to J. F. Stolz, NRC, dated 10/25/77.

2. Westinghouse letter NS-PLC-5111, T. M. Anderson to E. Case, NRC, dated 5/30/78.

3. Westinghouse letter NS-TMA-1837, T. M. Anderson to S. Varga, NRC, dated 6/23/78.

4. Westinghouse letter NS-TMA-1835, T. M. Anderson to E. Case, NRC, dated 6/22/78.

5. NRC letter, S. A. Varga to J. Dolan, Indiana and Michigan Electric Company, dated 2/12/81.

6. NUREG-0717 Supplement No. 4, Safety Evaluation Report related to the operation of Virgil C. Summer Nuclear Station, Unit No. 1 Docket 50-395, August, 1982.

7. NRC proposed Regulatory Guide 1.105 Rev. 2, "Instrument Setpoints", dated 12/81 for implementation 6/82.

8. ANSI/ANS Standard 58.4-1979, "Criteria for Technical Specifications for Nuclear Power Stations".

9. ANSI/N719 ISA Standard S67.04, Draft F, 5/22/79, "Setpoints for Nuclear Safety-Related Instrumentation Used in Nuclear Power Plants".

10. Scientific Apparatus Manufacturers Association, Standard PMC-20-1-1973, "Process Measurement and Control Terminology".

ATTACHMENT B

INSTRUMENTATION UNCERTAINTIES FOR POINT BEACH

	Controllers: Pressurizer Pressure Control (1)	Controllers: Rod Control (Temperature) T_{avg} (1)	Controllers: Rod Control (Temperature) 1st Stage Turbine Impulse Chamber Pressure (1)	Power Calorimetric: Steamline Pressure Computer (1)	Power Calorimetric: Feedwater Pressure Computer (1)	Power Calorimetric: Feedwater Temperature Computer (1)	Power Calorimetric: Feedwater ΔP Computer (1)	Flow Calorimetric: Steamline Pressure DVM (1)	Flow Calorimetric: Feedwater Pressure Computer (1)	Flow Calorimetric: Feedwater Temperature (RTD) DVM (1)	Flow Calorimetric: Feedwater Temperature (T/C) DVM (1)	Flow Calorimetric: Feedwater ΔP Gauge (1)	Flow Calorimetric: T_H DVM (1)	Flow Calorimetric: T_C DVM (1)	Flow Calorimetric: Pressurizer Pressure DVM (1)	Flow Calorimetric: RCS Flow Cold Leg Elbow Tap (2)
PMA		[ ]+a,c														[
PEA							[ ]+a,c					[ ]+a,c				
SCA	0.5	0.9	0.7	0.5	0.5	2.3	0.5	0.5	0.5	0.2	2.3		0.5 (6)	0.5 (6)	0.5	
SPE							0.2									
STE	0.5		0.7	0.5	0.5		0.5	0.5	0.5						0.5	
SD	1.0		1.3	1.0	1.0		1.0	1.0	1.0	1.0					1.0	
RCA	0.5	1.0 (3)	0.7	0.5	0.5	0.5	0.5	0.5	0.5	0.5						
RTE	0.5	0.7		0.5	0.5	0.7	0.5	0.5	0.5	0.8						
RD	2.0	1.3	1.3	1.3	1.0	1.0	1.0		1.0							
ID				0.5			0.5									
A/D				0.3	0.3	0.4	0.3		0.3							
CA	0.5	0.2(4)	0.2 (4)													
D/M								0.1		0.1	0.1 (5)		0.1 (7)	0.1 (7)	0.1	]+a,c
CSA	[															]+a,c
Span	800 psi	75°F	75°F	1400 psi	1600 psi	150°F	100% ΔP	1400 psi	1600 psi	100°F	150°F	100% ΔP	75°F	75°F	800 psi	120% flow

(1) : Instrument span
(2) : flow
(3) [ ]+a,c
(4) [ ]+a,c
(5) [ ]+a,c
(6) [ ]+a,c
(7) [ ]+a,c
(8) [ ]+a,c
(9) [ ]+a,c
(10) [ ]+a,c
(11) [ ]+a,c