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"Safety Analysis with Bypass Flow Holes Plugged"
June 9, 1975

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June 10, 1975

Mr. B. C. Rusche, Director
Office of Nuclear Reactor Regulation
Nuclear Regulatory Commission
Washington, D.C. 20555

Docket No. 50-331

Dear Mr. Rusche:

Mr. Charles W. Sandford's letter of June 2, 1975, indicated that further data would be submitted shortly regarding Iowa Electric's planned program for inspection of the Duane Arnold Energy Center core internals and any necessary repairs. Transmitted herewith are forty copies of a document entitled "Safety Analysis with Bypass Flow Holes Plugged." Additional information will be provided when available.

Sincerely,

Lowenstein, Newman, Reis & Axelrad
Attorneys for
Iowa Electric Light and Power
Company

Kathleen H. Shea

By Kathleen H. Shea

KHS/jcl
Enclosures

6319

June 9, 1975

DUANE ARNOLD ENERGY CENTER

SAFETY ANALYSIS WITH BYPASS

FLOW HOLES PLUGGED

BOILING WATER REACTOR PROJECTS DEPARTMENT • GENERAL ELECTRIC COMPANY
SAN JOSE, CALIFORNIA 95125

GENERAL ELECTRIC

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1. INTRODUCTION AND SUMMARY

During a recent outage at a foreign plant, it was determined that some fuel assembly channels exhibit severe corner wear adjacent to in-core neutron monitor and startup source locations. It was postulated, and subsequently confirmed, that the severe wear was caused by vibration of the in-core tubes due primarily to a high-velocity jet of water flowing through the bypass flow holes in the lower core plate. This caused the tubes to wear against the channel corner. In some cases, the wear was observed to penetrate the channel wall.

Duane Arnold Energy Center (DAEC) is similar to the foreign plant in that both are General Electric Boiling Water Reactors (BWR's) which incorporate bypass flow holes in the lower core plate. Reference 1 describes the actions that are being taken at DAEC to check for the occurrence of and to eliminate the possible recurrence of significant channel wear during the remainder of this fuel cycle. Reference 1 also describes a plug which has been demonstrated to effectively eliminate the high-velocity jet of water through the bypass flow holes. This plug will be installed in the DAEC lower core plate should the results of the diagnostic inspection program, also described in Reference 1, indicate the need.

Elimination of flow through the bypass flow holes significantly reduces the total bypass flow. This results in an increased bypass void fraction which affects the thermal-hydraulic analyses previously performed in the FSAR (Reference 2). The following sections of this report describe the re-analyses of normal operating conditions, stability, abnormal operational transients and accidents that have been performed to demonstrate the ability of DAEC to operate with the bypass flow holes plugged.

The results of these analyses lead to the identification of minimum critical power ratios (MCPR's) and limiting average planar linear heat generation rates (LAPLHGR's) which are applicable to plant operation with the bypass flow holes plugged. Further, the ability of the plant to operate safely within these constraints is demonstrated.

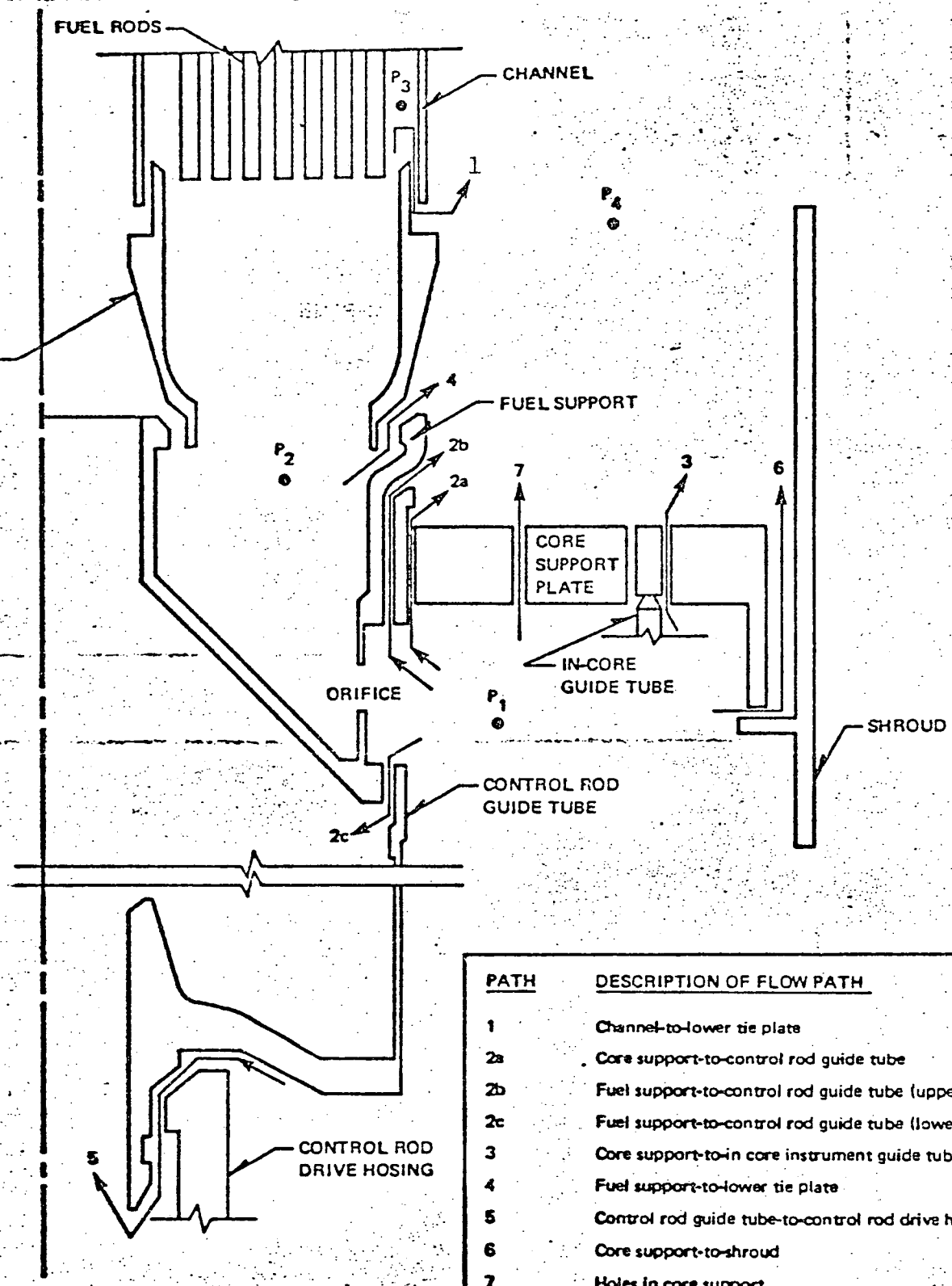
2. NORMAL OPERATION

2.1 STEADY-STATE

The DAEC fuel assemblies are equipped with "finger springs" on the lower tie plate to provide a controlled flow leakage path between the lower tie plate and the fuel channel. Since these finger springs reduce the flow in the core bypass region, 49 one-inch-diameter holes were drilled in the core plate to maintain the total bypass flow at about 10% of the total core flow. This value of bypass flow is desirable to reduce out-of-channel voiding which reduces the local power measurement uncertainty.

The steady-state thermal-hydraulics are only slightly affected by plugging the bypass flow holes. With the holes plugged, the in-channel flow increases by more than 5% which results in increased thermal margins. The increased in-channel flow comes at the expense of the bypass flow which is reduced by the same amount, assuming that the total core flow remains unchanged. Figure 2-1 shows the bypass flow paths which are considered in the determination of bypass flow. Prior to plugging the core plate at DAEC, Flow Paths 1 and 7 comprised about 80% of the total bypass flow, with about two-thirds of this value due to the holes in the core support plate (Path 7). Installation of plugs in the bypass flow holes reduces this contribution to essentially zero (tests[3] have shown some minor leakage is possible around the plugs).

The reduced bypass flow results in an increased core average bypass void fraction at rated power and flow. This increased void fraction in the bypass region reduces the maximum local peaking factor within the bundle (thereby increasing thermal margins), and also increases the nodal power calculation uncertainty. Disregarding the voiding would result in overprediction of the segmental bundle power in the bottom regions of the core where bypass voiding does not exist, and an underprediction of bundle segment power in the upper region of the core where bypass voiding does exist. The method for correcting these prediction errors is given in Subsection 2.2.



PATH	DESCRIPTION OF FLOW PATH
1	Channel-to-lower tie plate
2a	Core support-to-control rod guide tube
2b	Fuel support-to-control rod guide tube (upper)
2c	Fuel support-to-control rod guide tube (lower)
3	Core support-to-in core instrument guide tube
4	Fuel support-to-lower tie plate
5	Control rod guide tube-to-control rod drive housing
6	Core support-to-shroud
7	Holes in core support

Figure 2-1. Duane Arnold Energy Center Bypass Flow Path Identification

Voids in the bypass region add an additional uncertainty in the traversing in-core probe (TIP) measurements. This additional uncertainty affects the statistical analysis used to determine the MCPR safety limit. Further discussion of the impact of bypass voiding on the fuel cladding integrity safety limit is given in Subsection 2.3.

The difference in void distribution caused by plugging the bypass flow holes changes the void coefficient of reactivity. The method used to account for this change is described in Subsection 2.4. Also, an updated void reactivity coefficient calculational model is used to determine the value to be used in the thermal-hydraulic analysis.

Reference 1 describes the available data which indicate that plugging the bypass flow holes will significantly reduce in-core vibrations during normal operation. As an added precaution, measurements will be made during the return to power of DAEC to assure that no unexpected in-core vibrations are occurring. This program is outlined in Subsection 2.5.

2.2 TIP BYPASS VOID CORRECTION

The presence of voids in the bypass region perturbs the relation between the neutron flux detector signal and the local bundle segment power. Neglect of this effect in the process computer could result in an underprediction of the peak heat flux. To account for the perturbed signal-power relationship due to bypass voiding, a TIP correction, based on a detailed thermal-hydraulic model of the bypass flow region in the 7x7 D lattice, has been constructed. The relationship between the TIP detector response and the neighboring four bundle segments (P4B) as a function of bypass voids has been determined by standard two-dimensional 3-group fine mesh diffusion theory lattice model calculations. The sensitivity of P4B/TIP as a function of bypass voids is shown in Fig. 2-2. These calculations demonstrate that, for the expected range of in-channel voids, the overall correction factor due to bypass voids from 0 to 15 GWd/T is:

$$C = 1 + \lambda \cdot BV$$

where C is the overall correction factor, BV is the bypass void fraction and λ is the fractional change in bundle segment power/segment bypass void fraction = 0.45.

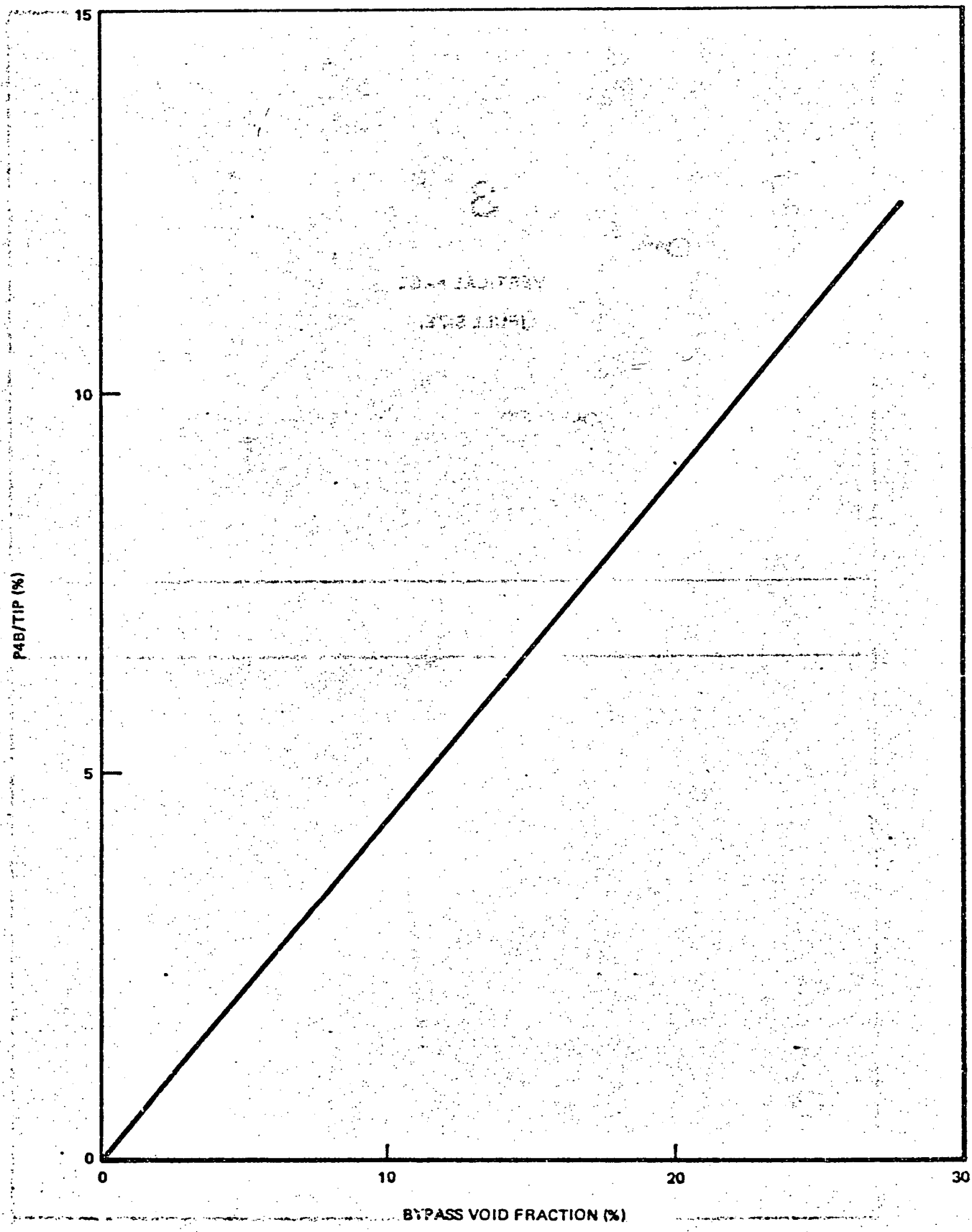


Figure 2-2. Duane Arnold Energy Center Bypass Void Sensitivity of P4B/TIP

This λ correction factor is used to adjust the core limits evaluation to account for voiding in the bypass region. The use of 0.45 for λ is consistent with the statistical analysis philosophy of GETAB.[4]

In the process computer, the void content in the bypass region associated with each segment TIP reading is determined from the bypass energy deposition due to the neutron slowing down, gamma and alpha heating, and heat conduction through the channel wall. Assuming uniform heating in the bypass channel, the bypass segment enthalpy and void fraction content are determined by integrating the energy deposition to the segment of interest and using the core inlet temperature, the fluid pressure level, and the bypass flow rate. By programming the bypass void correction factor into the process computer the entire range of operating conditions can be accounted for, and no external correction of the core limit calculations is required.

2.3 FUEL CLADDING INTEGRITY SAFETY LIMIT

The uncertainty in the determination of local power due to voiding in the by-pass regions is taken into account in the statistical analysis used to determine the fuel cladding integrity safety limit.

A new statistical analysis of the DAEC core was performed using the uncertainty inputs described in Table 2-1. The reactor core selected for this statistical analysis is a typical 251/764 BWR/4 core. The use of a large core analysis results in a conservative safety limit when applied to the Duane Arnold core. The histogram of relative bundle power used in the statistical analysis is shown in Figure 2-3. For comparison purposes, a typical operating power distribution of Duane Arnold is shown in Figure 2-4. It can be seen that the power distribution used in the statistical analysis is clearly skewed more to the high power side than the actual operating power distribution, thus yielding a conservative value of the 99.9% statistical limit MCPR. The nominal values of parameters used in the statistical analysis are listed in Table 2-2.

The results of this analysis show that at least 99.9% of the fuel rods in the core are expected to avoid boiling transition if the MCPR is 1.06 or greater. Based on these results, an appropriate fuel cladding integrity safety limit for DAEC with plugged bypass flow holes is a MCPR of 1.06.

Table 2-1

UNCERTAINTIES USED IN THE DETERMINATION OF THE FUEL CLADDING SAFETY LIMIT

Quantity	Standard Deviation (% of Point)	Comment
Feedwater flow	1.76	This is the largest component of total reactor power uncertainty.
Feedwater temperature	0.76	These are the other significant parameters in core power determination
Reactor pressure	0.5	
Core inlet temperature	0.2	Affect quality and boiling length. Flow is not measured directly, but is calculated from Jet Pump P. The listed uncertainty in total core flow corresponds to 11.2% standard deviation in each individual jet pump flow.
Core total flow	2.5	
Channel flow area	3.0	This accounts for manufacturing and service-induced variations in the free flow area within the channel.
Friction factor multiplier	10.0	Accounts for uncertainty in the correlation representing two-phase pressure losses.
Channel friction factor multiplier	5.0	Represents variation in the pressure loss characteristics of individual channels. Flow area and pressure loss variations affect the core flow distribution, influencing the quality and boiling length in individual channels.
TIP readings	6.3	These sets of data are the base from which gross power distribution is determined. The assigned uncertaintites include all electrical and geometrical components plus a contribution from the analytical extrapolation from the chamber location to the adjacent fuel assembly segment. Also included are uncertainties contributed by the LPRM system. LPRM readings are used to correct the power distribution calculations for changes which have occurred since the last TIP survey. The assigned uncertainty affects power distribution in the same manner as the base TIP reading uncertainty.

Table 2-1 (Continued)

Quantity	Standard Deviation (% of Point)	Comment
Bypass void effect on TIP	4.14 - 4.96	This accounts for additional uncertainty due to the bypass void content resulting from plugging of the core support plate leakage augmentation holes. The TIP uncertainty introduced by the bypass voids is zero in the bottom half of the core (no boiling in bypass region), and increases from 4.14% at the core midplane to 4.96% at the core exit. The TIP variations due to the bypass void in a given Monte Carlo trial are assumed (conservatively) to be perfectly correlated axially, so that each node receives an increment of the same sign, proportional to the corresponding nodal uncertainty.
R factor	1.6	This is the last of the three power distribution related uncertainties. It is a function of the uncertainty in local fuel rod power and is discussed in detail in Reference 4.
Critical power	3.6	Uncertainty in the GEXL correlation in terms of critical power.

Table 2-2

NOMINAL VALUES OF PARAMETERS USED IN THE STATISTICAL ANALYSIS OF FUEL CLADDING INTEGRITY SAFETY LIMIT

Core thermal power	3293 MW
Core flow	102.5 Mlb/hr
Dome pressure	1010.4 psig
Channel flow area	0.1078 ft^2
R factor	1.098 (High Enriched Bundle) 1.154 (Low Enriched Bundle)

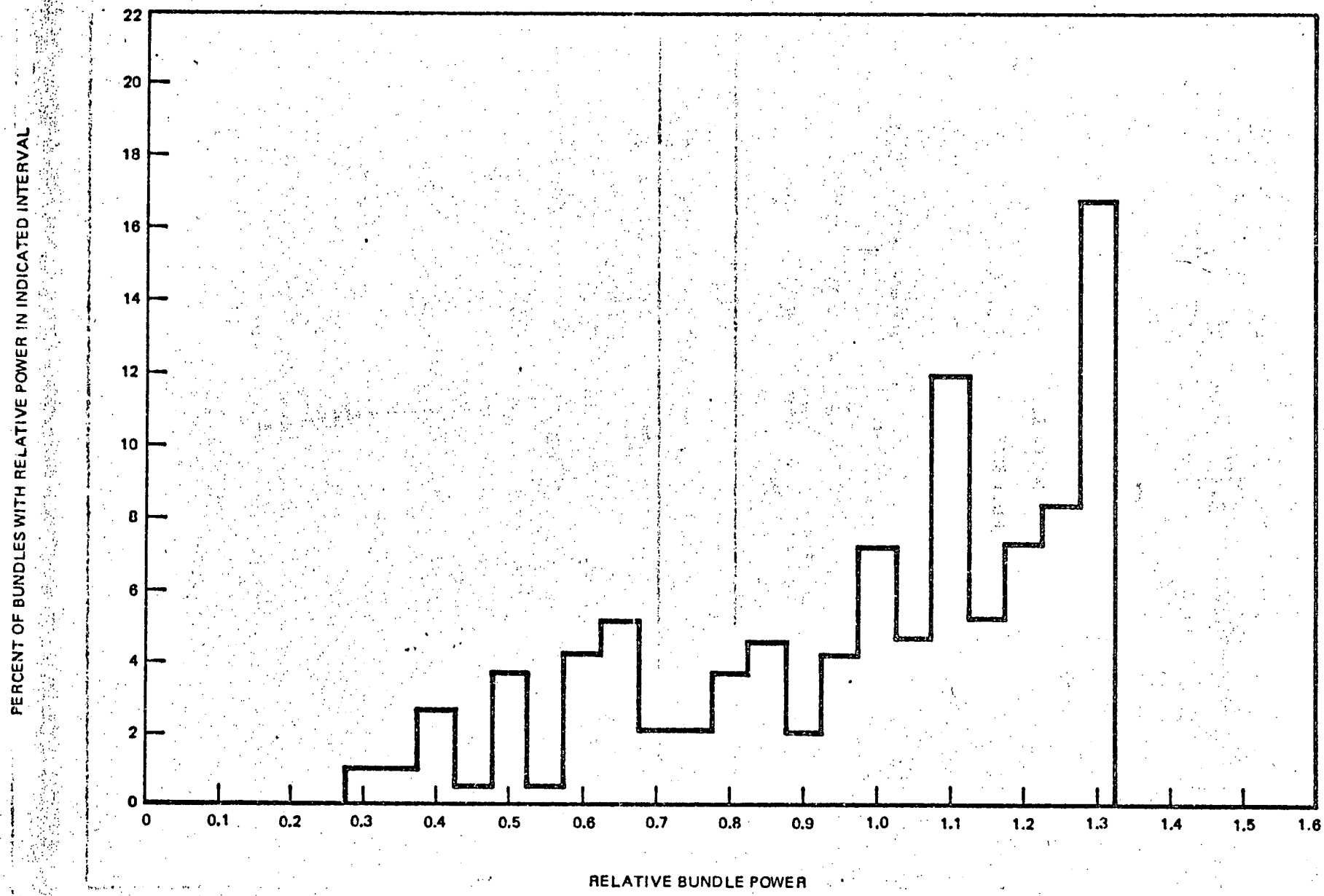


Figure 2-3. Relative Bundle Power Histogram for Power Distribution Used in Statistical Analysis to Determine Safety Limit

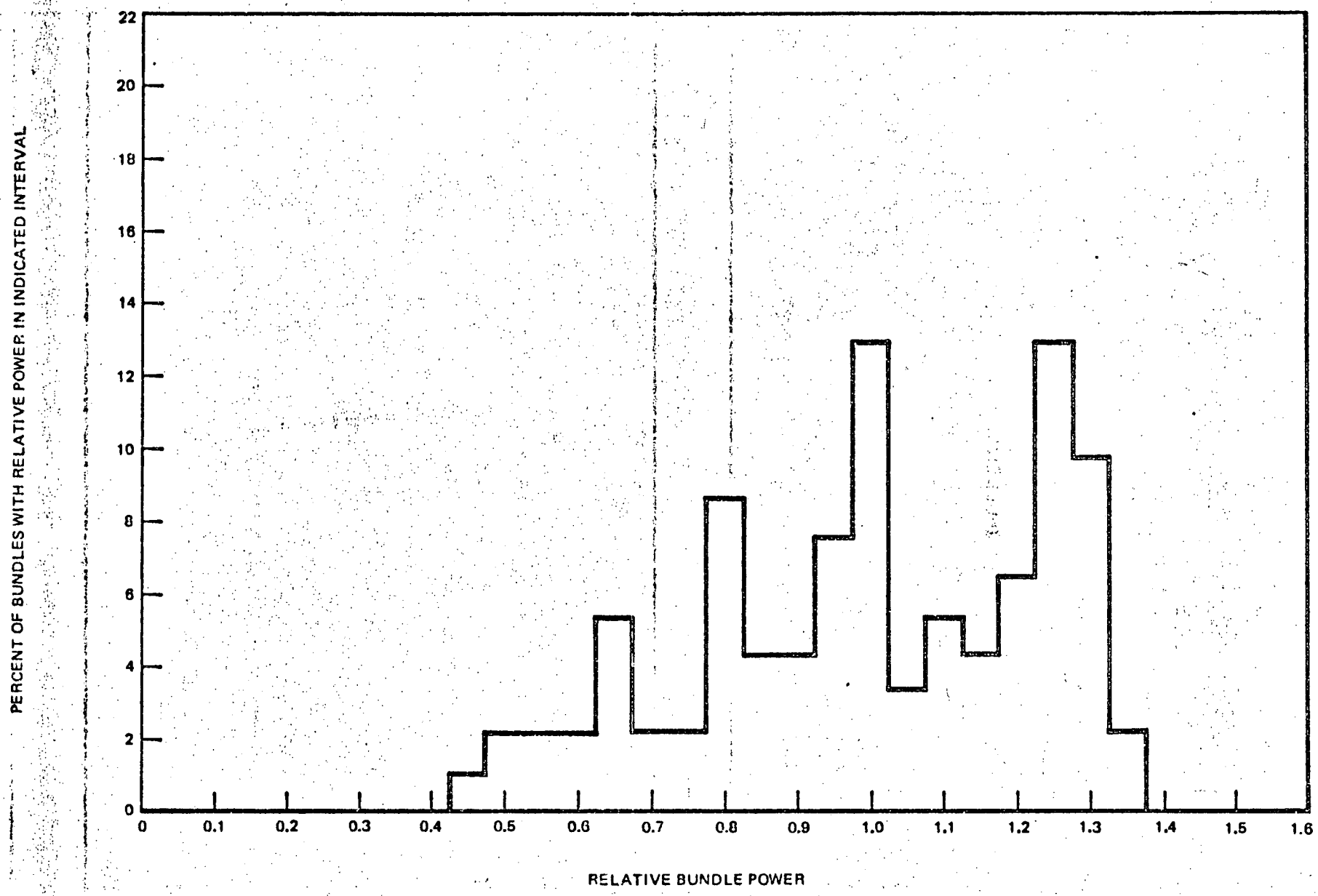


Figure 2-4. Relative Bundle Power Histogram for Actual Operating Power Distribution Duane Arnold Energy Center

2.4 MODERATOR VOID COEFFICIENT

Compared to the moderator void coefficient used in the original Cycle 1 analyses, the present value calculated assuming the bypass flow holes plugged is more negative. This increase in the void coefficient is due to:

a. An empirical adjustment in the void-quality relationship; and

b. The generation of voids in the bypass region due to the plugging of the flow holes in the lower core plate.

2.4.1 Void Quality Adjustment

During the latter part of 1973 and early part of 1974, comparison of operating and predicted axial power distribution data indicated a bias in the data. Predicted power in the bottom of the core was lower than that measured by the in-core instrumentation. After an examination of the significant input parameters that influence the axial power distribution, an adjustment in the void-quality relationship was made. The result of this adjustment, besides reducing the observed bias between measured and predicted axial power distributions, was to increase the core average voids. This increase in the core average void causes the moderator void coefficient to be more negative. This effect will be discussed in detail in the topical report scheduled to be submitted to the NRC in late summer 1975.

2.4.2 Bypass Voids

The definition of the moderator void coefficient is in terms of the change in in-channel voids alone. In the present situation where voids are generated in the bypass region, the change in k_{eff} is due to void collapse in both the in-channel and bypass regions. However, since we continue to define the moderator void coefficient only in terms of in-channel void changes, the magnitude of the void coefficient will increase (compared to no bypass voids).

2.4.3 Results

The void coefficient at EOC1, full power is calculated to be -0.00151 $\Delta k/\Delta V$. This is higher than -0.00136 $\Delta k/\Delta V$, the value first estimated for EOC1 with plugged holes. The more negative coefficient reflects an updated calculation in which the bypass void fraction used in the nuclear simulation is consistent with thermal hydraulic calculations.

2.5 STARTUP MONITORING

As added confirmation of the effectiveness of plugging the bypass flow holes to reduce in-core vibrations, a startup monitoring program will be performed. The program as presently conceived will consist of taking 16 LPRM power spectral density (PSD) measurements starting at 50% power and for each step of 10% power up to and including the maximum achievable power level consistent with this safety analysis. In addition, LPRM time traces will be taken at each of these conditions to form a basis for continuing plant operation. This program will be finalized and procedures will be developed prior to restart of the reactor.

To develop a standard for comparison, extensive measurements, TIP traces, PSD's and LPRM time traces, were taken at various power levels and for two different power shapes prior to the plant shutdown. These data and the data from the channel inspection program[1] will provide the bases for evaluation of the probability of significant channel wear when operating with plugged bypass flow holes.

The use of plugs is expected to have a significant impact on the noise content of the LPRM signals. This impact is expected to be dependent on frequency content. In the 0.1 to 1 Hz range, there should be little or no change in noise content. In the 1.4 to 3 Hz range, the noise content should be reduced. This is the range that has been associated with the mechanical vibrations of the LPRM's. In the 5 to 50 Hz range, an increase in noise due to boiling in the out-of-channel region is expected.

Evaluation of the pre-shutdown data is currently under way. The results of this evaluation are expected to form the basis for the acceptance criteria to be applied during restart. It is anticipated that a particular value for the ratio of 1.4 to 3 Hz noise to either 0.2 to 0.8 Hz or the dc level (RMS signal) will be established for the PSD technique. A second measure could be provided to monitor plant operation by LPRM time traces. A RMS level could be identified to be used as a trigger for further investigation using PSD measurements as described above.

3. STABILITY ANALYSES

3.1 INTRODUCTION

A BWR, such as DAEC, has many interacting dynamic processes and associated control systems. A dynamic process may be defined as one in which the interrelated variables are time varying, e.g., the boiling of water in the reactor core. The process may be self-regulating in that it exhibits a negative feedback effect. In a BWR, when a control rod is withdrawn, core power increases due to the reactivity insertion. This causes increased boiling. The increased boiling increases the steam volume in the core resulting in decreased neutron moderation. This is equivalent to removing reactivity to counteract the reactivity addition of the withdrawn control rod. Thus, a rise in core power is limited by the negative feedback effect of the increased steam volume. This inherent negative feedback effect present in boiling water reactors serves as a self-regulating mechanism upon core dynamics. A secondary inherent negative feedback effect, Doppler reactivity, also occurs as the fuel temperature varies with power. Whenever there is a negative feedback in a system, whether it be inherently self-regulated in the process or added to the process by a control system, the stability characteristics must be considered. There are many definitions of stability, but for feedback processes and control systems, the following definitions may be used: a system is stable if, following a disturbance, the transient settles to a steady, noncyclic state. A system may also be acceptably safe even if oscillatory, provided that any limit cycle of the oscillations is less than a prescribed magnitude. Instability then, is either a continual departure from a final steady-state value or a greater-than-prescribed limit cycle about the final steady-state value.

The mechanism for instability can be explained in terms of frequency response. Consider a sinusoidal input to a feedback control system which for the moment has the feedback disconnected. If there were no time lags or delays between input and output, the output would be in phase with the input. Connecting the output so as to subtract from the input (negative feedback or 180° out-of-phase connection) would result in stable closed loop operation. However, natural laws can cause phase shift between output and input and should the phase shift

reach 180°, the feedback signal would be reinforcing the input signal rather than subtracting from it. If the feedback signal were equal to or larger than the input signal (loop gain equal to one or greater), the input signal could be disconnected and the system would continue to oscillate. If the feedback signal were less than one (loop gains less than one), the oscillations would die out.

The objective of the stability evaluation is to analytically demonstrate that in the event of small disturbances, the reactor will always return to its normal operating state without compromising the integrity of the fuel or nuclear process barriers. It is possible for an unstable process to be stabilized by the addition of a control system. In general, however, it is preferable that a process with inherent feedback be designed to be stable by itself before it is combined with other processes and control systems. The design of the BWR is based on individual system components being stable.

Channel hydrodynamic and reactor core (reactivity) stability analyses have been performed for plugs installed in the lower core plate. These stability analyses are important since channel hydrodynamic instability could impede heat transfer to the moderator and drive the reactor into power oscillations, and reactivity feedback instability of the reactor core could also drive the reactor into power oscillations.

3.2 PERFORMANCE CRITERIA

The criteria to be considered are stated in terms of two compatible parameters. First is the decay ratio x_2/x_0, designated as the ratio of the magnitude of the second overshoot to the first overshoot resulting from a step perturbation. A plot of the decay ratio is a graphic representation of the physical responsiveness of the system, which is readily evaluated in a time-domain analysis. Second is the damping coefficient, ζ_n, the definition of which corresponds to the pole pair closest to the $j\omega$ axis in the s-plane for the system closed loop transfer function. This parameter also applies to the frequency-domain interpretation. The damping coefficient is directly related to the decay ratio as shown in Figure 3-1.

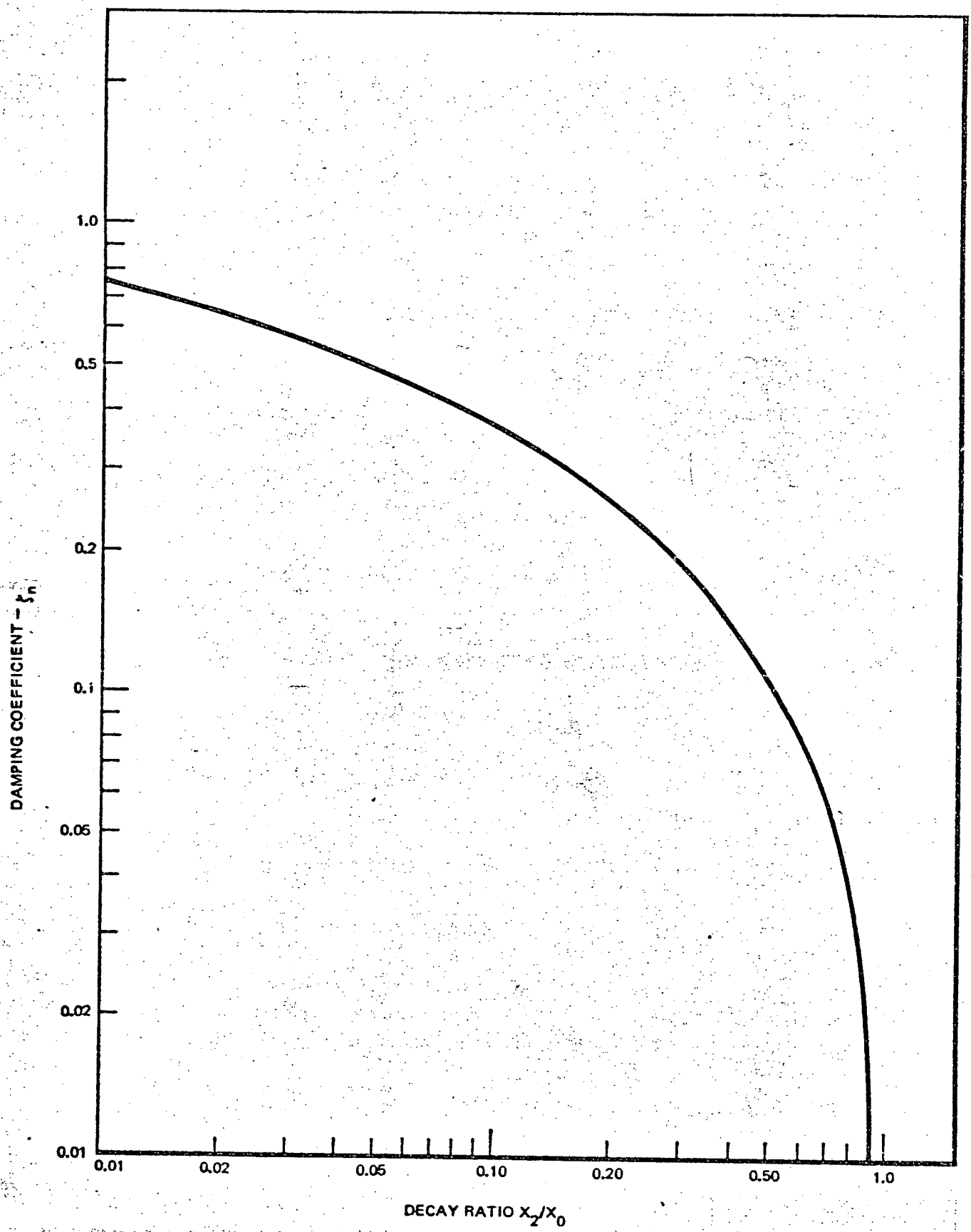


Figure 3-1. Damping Coefficient Versus Decay Ratio (Second Order Systems)

The assurance that the total plant is stable and, therefore, has significant safety margin is demonstrated analytically when the decay ratio, x_2/x_0, is less than 1.0 or, equivalently, when the damping coefficient, ζ_n, is greater than zero for each type of stability discussed. Special attention is given to differentiate between inherent system limit cycles and small, acceptable limit cycles that are always present, even in the most stable reactors. The latter are caused by physical nonlinearities (deadband, stiction, etc.) in real control systems and are not representative of inherent hydrodynamic or reactivity instabilities in the reactor. The safety performance limit criteria for the two types of dynamic performance are summarized below in terms of decay ratio and damping coefficient:

Channel hydrodynamic stability	$x_2/x_0 < 1$	$\zeta_n > 0$
Reactor core (reactivity) stability	$x_2/x_0 < 1$	$\zeta_n > 0$

These criteria must be satisfied for all attainable operating conditions. For stability purposes, the most severe conditions to which these criteria will be applied normally correspond to natural circulation flow at a power corresponding to the rod block power limit condition.

Although the safety performance limit criteria assure absolute reactor stability, an operational design guide based on acceptable performance standards of the control industry for most process systems is observed. The operational analysis assures good controllability over the normal operating range of the reactor.

The assurance that the plant has the desirable operational dynamic characteristics within the limits specified for the decay ratio, x_2/x_0, or the damping coefficient, ζ_n, is demonstrated analytically as follows:

Channel hydrodynamic performance	$x_2/x_0 \leq 0.5$	$\zeta_n \geq 0.11$
Reactor core (reactivity) performance	$x_2/x_0 \leq 0.25$	$\zeta_n \geq 0.22$

These limits are satisfied for all expected power and flow conditions to be encountered in normal operation. The most limiting condition corresponds to that

attained starting from rated power and flow and reducing flow, potentially to natural circulation, with a corresponding power reduction. The power and flow condition at which the above limits are analytically attained is recognized as the operational boundary for normal manual or automatic control.

The channel hydrodynamic operational design guide limit given above ($x_2/x_0 \leq 0.5$, $\zeta_n \geq 0.11$) allows locally more responsive operation than is allowed for the complete core or the total system. This is justified for a stable channel by the fact that the response of an individual component can be less damped than the total system as long as total performance is uncompromised and local transients are not harmful. These can both be satisfied in the presence of a highly responsive, but stable, channel. Because of the short period of natural resonance relative to the slow response of heat transfer, the local channel transients will not be manifest as significant local heat flux transients.

3.3 CHANNEL AND CORE MODEL

The mathematical model representing the core examines the linearized reactivity response of a reactor system with density-dependent reactivity feedback caused by boiling. In addition, the hydrodynamics of various hydraulically coupled reactor channels or regions are examined separately on an axially multinoded basis by grouping various channels that are thermodynamically and hydraulically similar. Interchannel hydrodynamic interaction or coupling exists through pressure variations in the inlet plenum, such as can be caused by disturbances in the flow distribution between regions or channels. This approach provides a reasonably accurate, three-dimensional representation of the reactor's hydrodynamics.

The model, shown in block diagram form on Figure 3-2, solves the dynamic equations that represent the reactor core in the frequency domain. From the solution of these dynamic equations, the reactivity and individual channel hydrodynamic stability of the boiling water reactor are determined for a given reactor flow rate, power distribution, and total power. This gives the most basic understanding of the inherent core behavior (and hence the system behavior) and

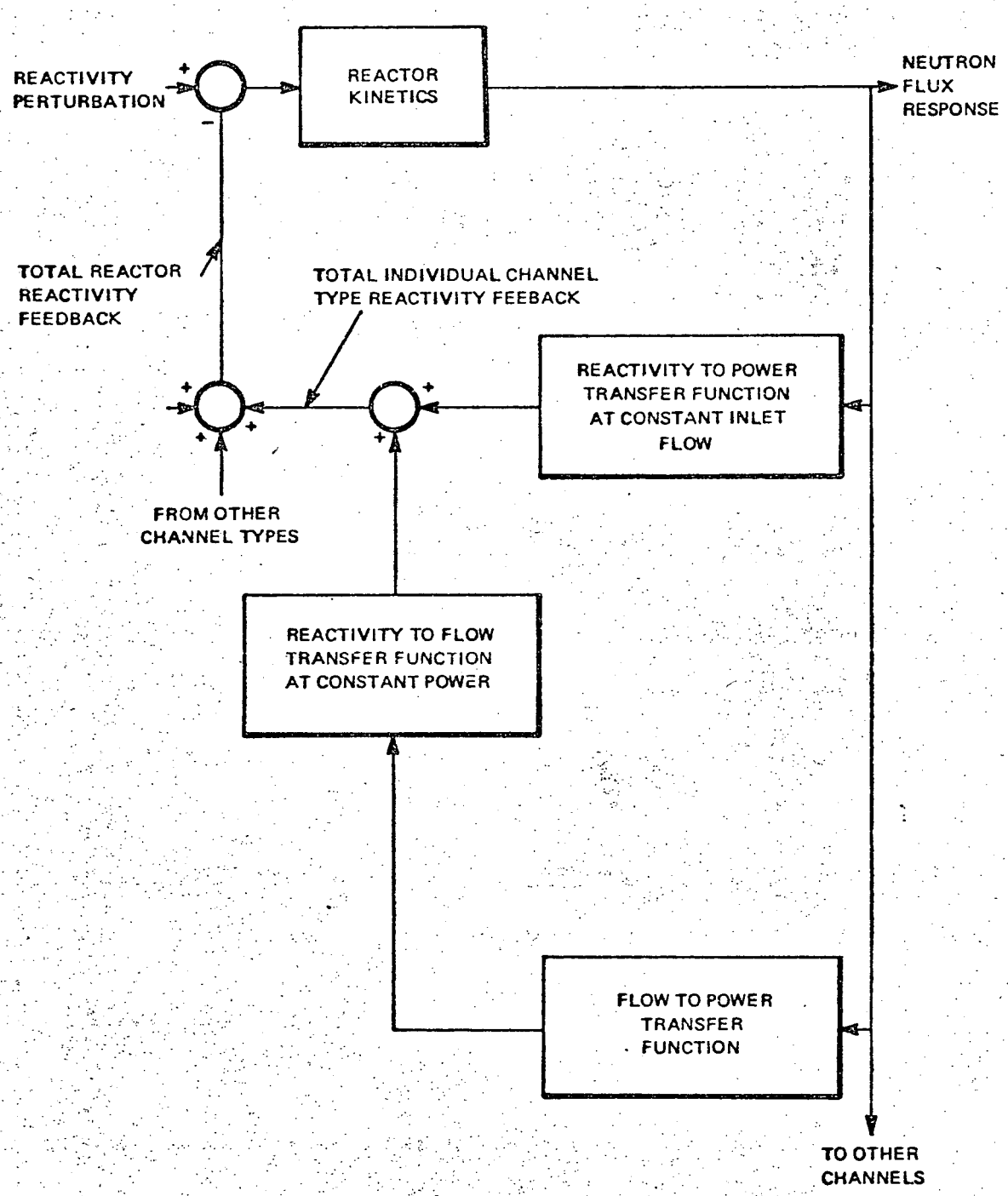


Figure 3-2. Hydrodynamic and Core Stability Model

is the principal consideration in evaluating the stable performance of the reactor. As new experimental or reactor operating data are obtained, the model is refined to improve its capability and accuracy.

Figure 3-3 demonstrates the competence and inherent conservatism of the core stability model. The relationship of the calculated damping coefficient from the reactor core dynamic analytical code is related to measured results from control rod oscillator tests performed at large operating BWR plants by the General Electric Company. The correlated Most Probable Values are presented, based on a least squares determination, and the line below which there is 97.5% confidence (2σ) that the actual values will fall is also shown.

The results show the analytical model to be an effective and useful design tool, with significant conservatism in its application to boiling water reactor core evaluation. Neal and Zivi[5] further confirm the effective application of essentially the same model to channel and core analyses.

3.4 PERFORMANCE EVALUATIONS

3.4.1 Channel Stability

The channel hydrodynamic performance is evaluated at the most limiting condition that occurs at the end of core life, with power peaked to the bottom of the core because the control rods are fully withdrawn. The calculations yield decay ratios as presented below:

DAEC Channel Hydrodynamic Performance	Natural Circulation 51.5% Power
Decay ratio, x_2/x_0	0.40
Resonant frequency, Hz	0.37

At this most responsive attainable mode, rod block power at natural circulation with end-of-life power peaking, the most responsive channel conforms with ultimate performance criteria of 1.0 decay ratio. At natural circulation, the

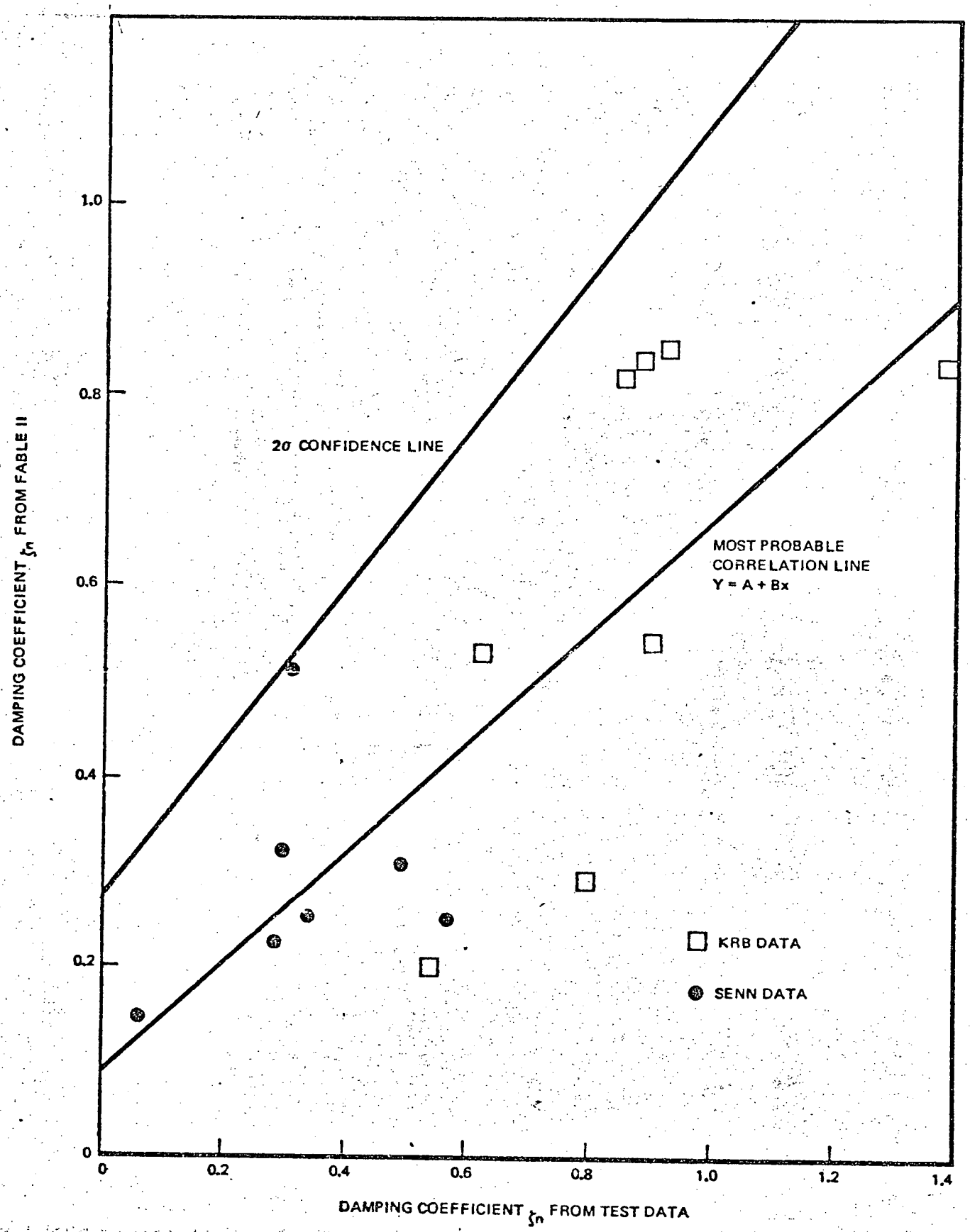


Figure 3-3. Comparison of Test Results with Reactor Core Analyses

100% rod pattern corresponds to a rod block condition. In this reactor, the channel performance over the entire range of attainable operation is below the threshold of instability.

At the limits of the normal operating range of the reactor, the following decay ratios were calculated.

DAEC Channel Hydrodynamic Performance	Rated Conditions	Low End of Flow Control Range
Decay ratio, x_2/x_0	$<$ 0.01	0.18
Frequency, Hz	0.64	0.55

The most responsive channel, therefore, conforms to the operational design guide of 0.5 decay ratio.

3.4.2 Core Stability

The most limiting condition occurs at the end of core life, with power peaking toward the bottom of the core. Because of the decrease in delayed neutron fraction, the value of the density reactivity coefficient, $\Delta k/k_{eff}/\Delta\rho$ increases. The most sensitive reactor operating condition is that corresponding to natural circulation flow and power level corresponding to the rod block set point. Values for reactor core stability are as follows:

DAEC Reactor Core Stability	Natural Circulation 51.5% Power
Decay ratio, x_2/x_0	0.72
Resonant frequency, Hz	0.40

The calculated values show the reactor to be in compliance with the ultimate performance criteria to the most responsive attainable mode as cited for the reactor core stability evaluation.

The calculated value of the decay ratio of the reactor power dynamic response for the high and low ends of the automatic flow control range (100% and 75% of power) are presented as follows:

DAEC Reactor Core Performance	Rated Conditions	Low End of Flow Control Range
Decay ratio, x_2/x_0	0.02	0.25
Resonant frequency, Hz	0.69	0.59

Figure 3-4 describes the calculated variation of the decay ratio over the normal power-flow range at end-of-life core conditions. The flow-control range to be covered during normal operation is bounded by the operating condition along the power-flow range at which the decay ratio of the plant becomes 0.25. This point corresponds to 75% power and 65% flow, along the 100% flow control line.

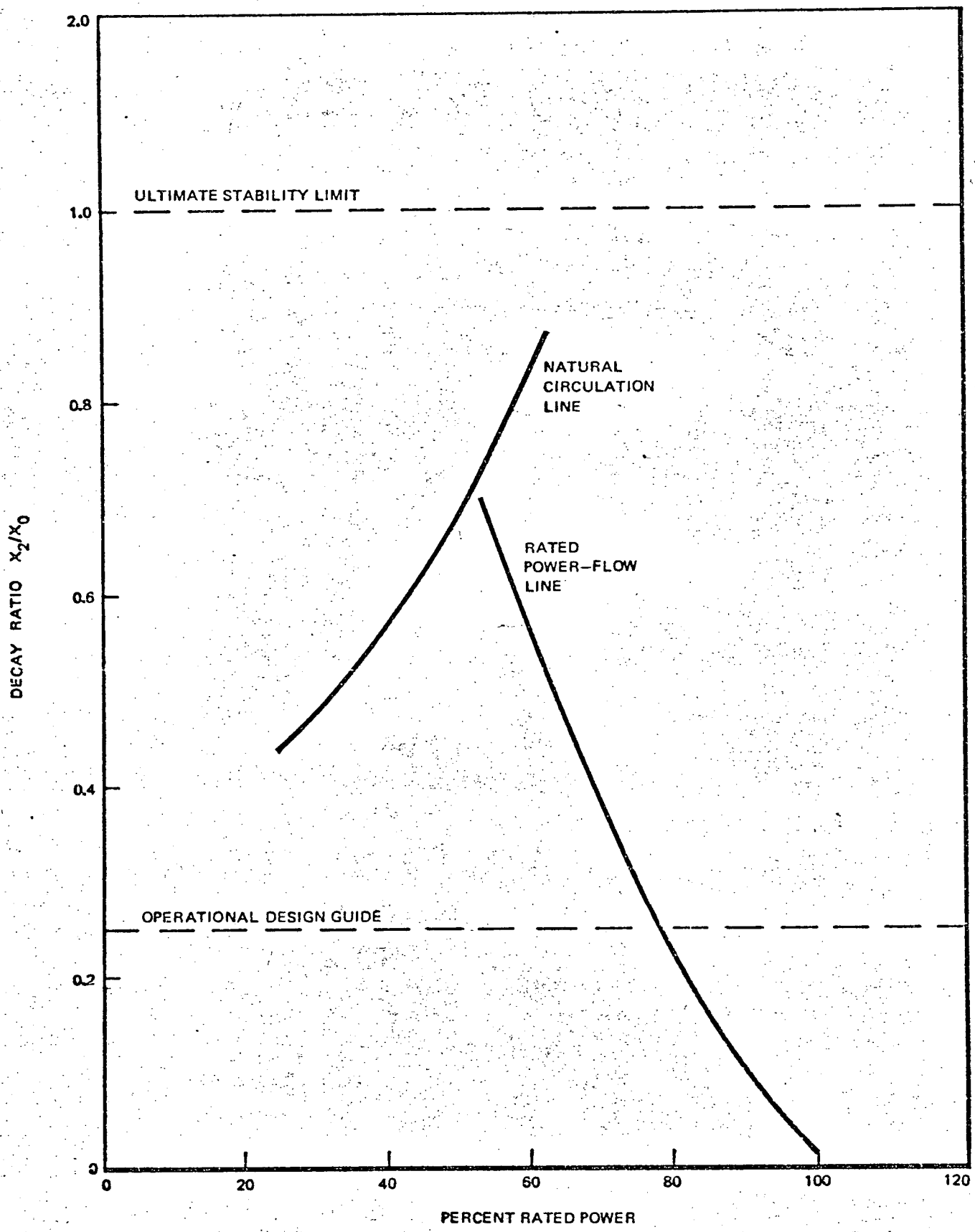


Figure 3.4. Duane Arnold Energy Center Core Reactivity Stability

4. ABNORMAL OPERATIONAL TRANSIENT AND CODE OVERPRESSURE PROTECTION ANALYSES

4.1 INTRODUCTION AND DEFINITIONS

To demonstrate protection from abnormal events, the Code overpressure protection analysis and limiting abnormal operation transients events were reanalyzed assuming the holes in the lower core plate were plugged. This reanalysis was performed because of the significant change in the void coefficient of reactivity described in Subsection 2.4.

The following definitions are used in this section.

4.1.1 Abnormal Operating Transients

Abnormal operational transients are the results of single equipment failures or single operator errors that can be reasonably expected during any normal or planned mode of plant operation. The following types of operational single failures and operator errors are identified:

a. The undesired opening or closing of any single valve.

b. The undesired or improper starting or stopping of any single component or system.

c. The malfunction or misoperation of any single control device.

d. Any single electrical failure.

e. Any single operator error.

These failures and errors are applied to the various plant systems for a variety of plant conditions to discover events that directly result in undesired parameter variations. There are two basic categories of abnormal operational transients: total core dynamic events and local power excursions.

The following are considered to be unacceptable safety results for abnormal operational transients:

a. A violation of the fuel design basis limits.

b. Nuclear system stress in excess of that allowed for transients by GE design practices and ASME vessel code limits.

4.1.2 Core Dynamic Transients

There are a number of transients evaluated in the FSAR[2] which can be classified as core dynamic transients. From a review of these events, it was concluded that the turbine trip without bypass and the loss of feedwater heater were the limiting events in this category.

4.1.3 Code Overpressure Protection

The ASME Boiler and Pressure Vessel Code requires overpressure protection for each vessel designed to meet Code Section III. For DAEC, the transient produced by the closure of all main steam line isolation valves represents the most severe abnormal operational transient resulting in a nuclear system pressure rise when direct scrams are ignored. The Code overpressure protection analysis hypothetically assumes the failure of the direct isolation valve position scram. The reactor is shut down by the backup, indirect, high neutron flux scram. This event can be categorized as a core dynamic event for analysis purposes.

4.1.4 Local Events

From a review of the FSAR, it has been determined that the rod withdrawal error is the limiting event in this category.

4.1.5 Significant Parameters

4.1.5.1 Delayed Neutron Fraction

The core average delayed neutron fraction, β, is a factor in the dynamic void and Doppler coefficients and also in the reactor period. In the FSAR analysis, a value of 0.0056 is used for β. In the re-analysis a new end of cycle (EOC) 1, β of 0.00546 was used in the analysis.

4.1.5.2 Void Reactivity Coefficient

The void coefficient is an important parameter, not only in transient analysis, but also in core stability. The core average void coefficient must be negative; however, it must not be so negative as to yield such a strong positive reactivity feedback during void collapse events that core and vessel limits are threatened. Conversely, events with void increase must produce sufficient negative feedback to maintain operation within safety limits. In evaluating transient analyses, the core physics void coefficient is multiplied by the average full power void fraction and divided by the delayed neutron fraction to derive the dynamic void reactivity coefficient, Kv. The void reactivity curve used in the re-analysis is shown in Figure 4-1.

4.1.5.3 Scram Reactivity Function

Scram reactivity is the worth of control rods as a function of time or position following the scram signal. The scram reactivity insertion is normally lowest at the end of cycle because there are no stubbed rods to insert negative reactivity more quickly than the remaining blades of the control rod bank. The scram reactivity function is related to dynamic performance when expressed (as plotted) in the form of $\Delta k/k\beta$. The scram reactivity curve used in the re-analysis is shown in Figure 4-2.

4.1.5.4 Doppler Reactivity Coefficient

The presence of U-238 and, ultimately, Pu-240 contributes to yield a strong negative Doppler coefficient which limits peak power during a transient. This

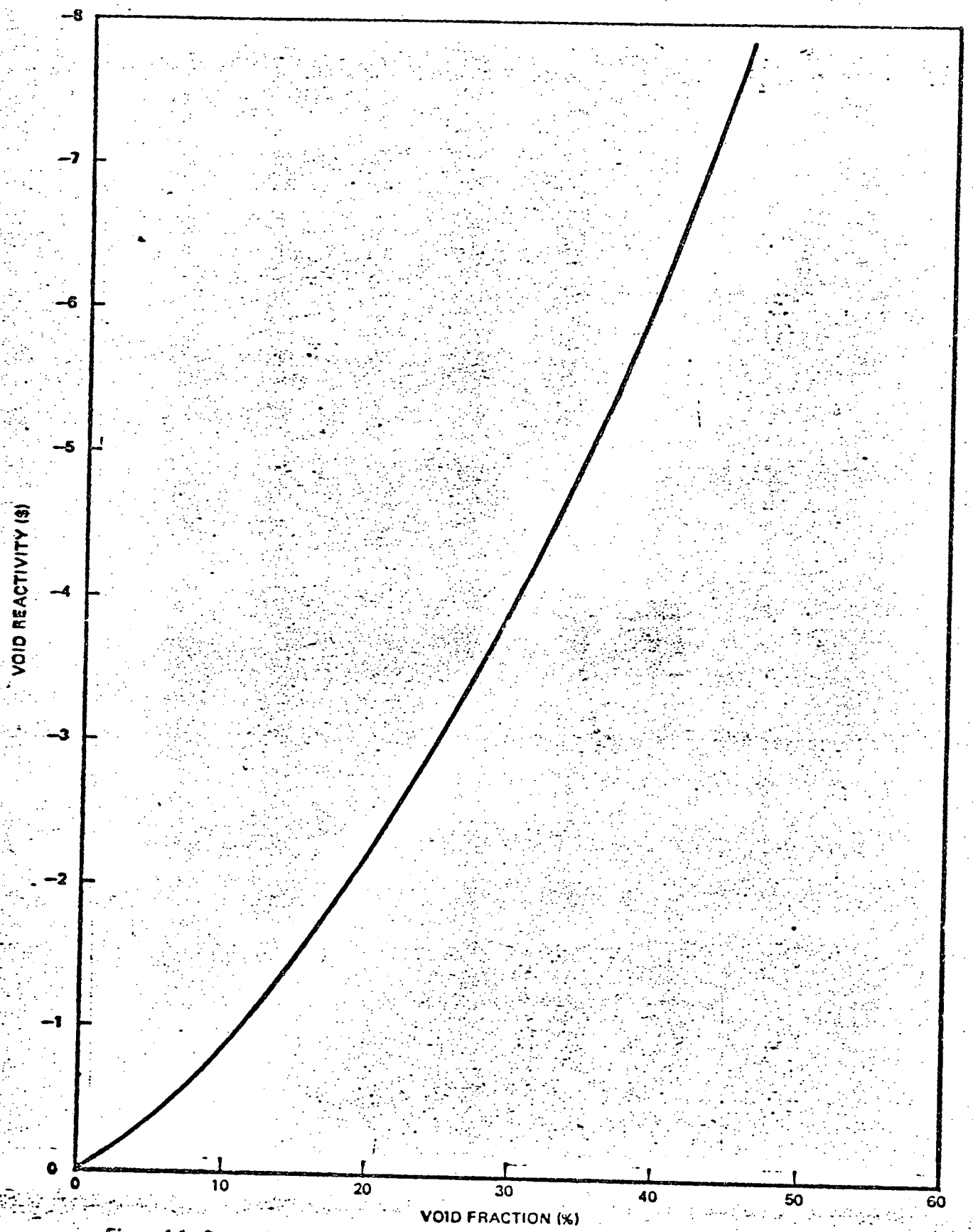


Figure 4-1. Duane Arnold Energy Center Void Reactivity Curve Cycle 1 Bypass Flow Holes Plugged

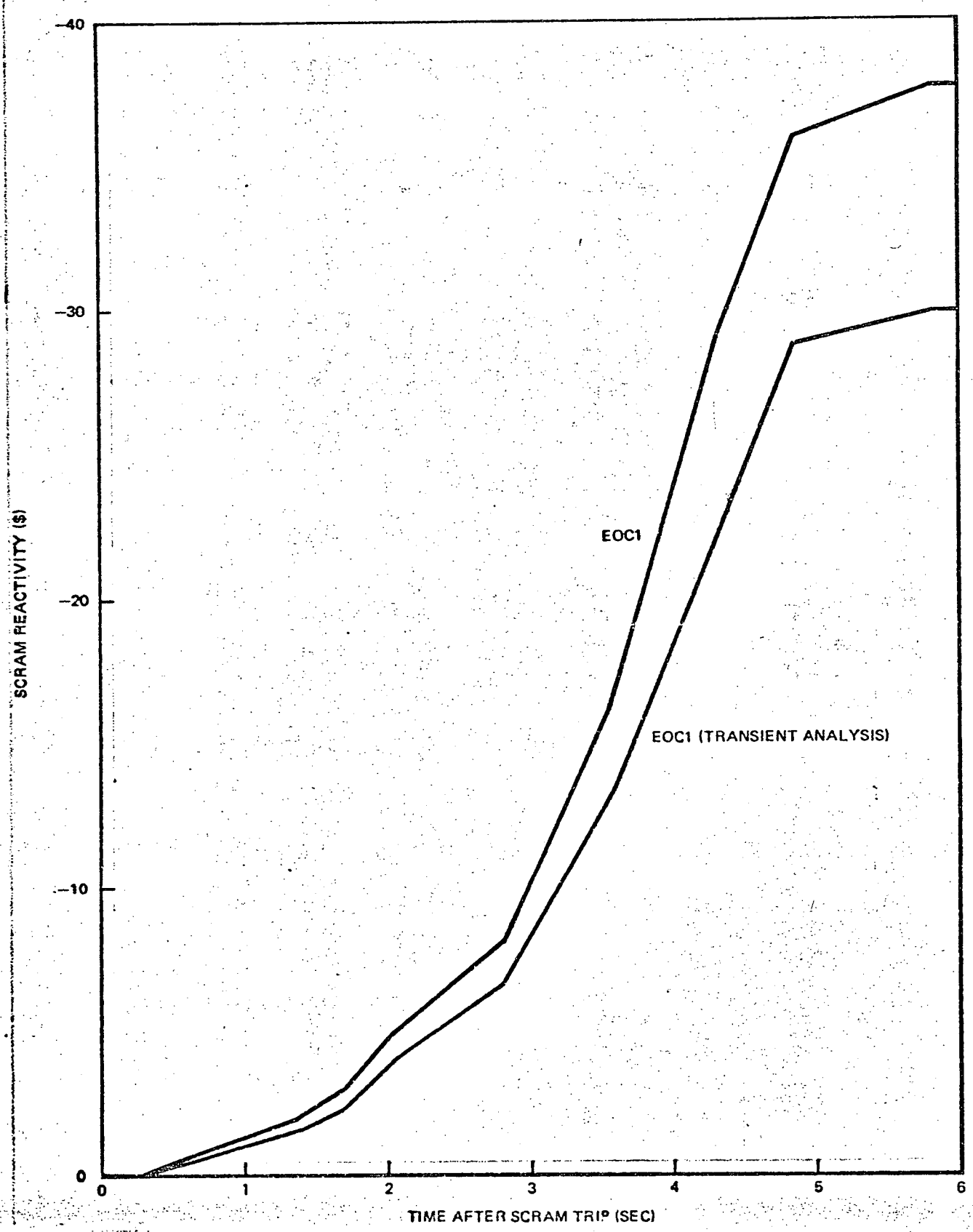


Figure 4-2. Duane Arnold Energy Center Scram Reactivity Curve End of Cycle 1 Bypass Flow Holes Plugged

coefficient provides instantaneous negative reactivity feedback to any fuel temperature rise, either gross or local. The magnitude of the Doppler coefficient is not dependent on gadolinium position or concentration in any bundle, because gadolinium has very little effect on the resonance group flux or on U-238 content of the core. The core physics Doppler coefficient is divided by the delayed neutron fraction to define a dynamic Doppler coefficient, K_D, which most effectively correlates the dynamic response of the plant to the Doppler reactivity feedback. The Doppler coefficient used in the re-analysis is the same as that used in the FSAR.

4.2 CORE DYNAMIC EVENTS

A summary of the input parameters is given in Table 4-1, and the results of the re-analyses are given in Table 4-2. The digital computer model described in Reference 6 is used in these analyses.

4.2.1 Turbine Trip from High Power without Bypass

This case is the most severe abnormal operational transient resulting directly in a nuclear system pressure increase. It also represents the events that would follow an assumed instantaneous loss of condenser vacuum, which automatically initiates closure of the turbine stop valves and turbine bypass valves. It is assumed that the plant is initially operating at the turbine-generator design condition (approximately 104% of reactor warranted power of 1593 MWt, or 1658 MWt).

Figure 4-3 shows the plant response to the trip. The scram is initiated by the position switches on the turbine stop valves. Peak neutron flux reaches 473% of rated, but peak surface heat flux peaks at 119.9% of its initial value. The nuclear system relief valves open fully to limit the pressure rise, then sequentially reclose as pressure decreases and the stored energy is dissipated. The peak pressure at the valves is 1204 psig, and the peak nuclear system pressure is 1240 psig at the bottom of the vessel. There is a margin of 36 psi to the lowest safety valve set point.

Table 4-1

DUANE ARNOLD ENERGY CENTER TRANSIENT ANALYSES

INPUT PARAMETERS

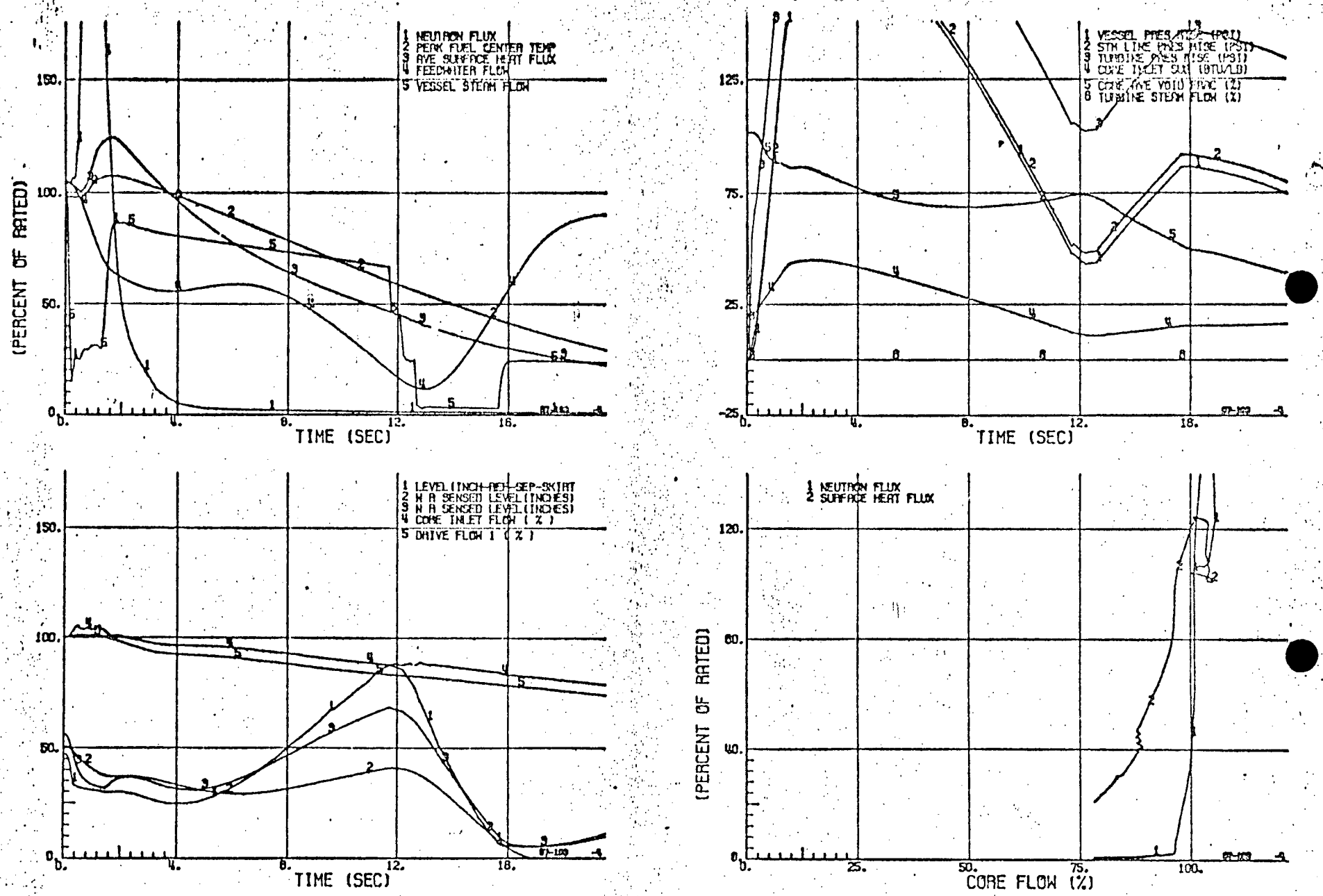
Parameter	Units	Value
Thermal Power (reference)	MWt/%	1658/104
Steam Flow	10^6 lb/hr/%	7.164/105
Core Flow	10^6 lb/hr/%	49.0/100
Dome Pressure	psig	1020
Turbine Pressure	psig	960
Leakage Flow	%	5.3
Gap Conductance	Btu/sec-ft-°F	1000
BPV Capacity	%	25
RV/Capacity	#/%	6/74.7
RV Set Point	psig	1090 + 1%
RV Time Delay/Stroke	sec/sec	0.4/0.1
SV/Capacity	#/%	2/18.9
SV Set Point	psig	1240 + 1%
Void Coefficient N/A	¢/% Rg	-11.9/-14.9
Void Fraction, Rg	%	43.39
Doppler Coefficient N/A	¢/% T_{avg}	-1.78/-1.6
Avg Fuel Temperature	°F	1334
CRD Specification	-	67A
Scram Curve	-	EOC-1
Scram Worth N/A	$	-37.4/-29.92
Operating MCPR Limit	-	1.34
Transient MCPR Limit	-	1.06

N = nuclear core data
A = analysis input data

Table 4-2

DUANE ARNOLD ENERGY CENTER EVENT DATA SUMMARY

Event	Power %	Core Flow %	Peak Neutron Flux % of Ref	Peak Surface Heat Flux % of Ref	Steam Line Pressure psig	Vessel Pressure psig
TT w/o BP-TScram	104	100	473	119.9	1204	1240
Loss of FW Htr (no scram)	104	100	128.6	126.8	1033	1079
(MCPR Calculation)			121	117.5		
MSIV-PScram RV/SV=6/2	104	100	-	-	1281	1324
MSIV-FScram RV/SV=6/2	104	100	-	-	1251	1292
MSIV-FScram RV/SV=5/2	104	100	-	-	1269	1311



DUANE ARNOLD EOC-1 KE1HRF01TT022
TURB TRIP W/O BP, TSCRAM, 67A-.8EOC1 SCRAM, 104% POWER, 100% FLOW

Figure 4-3. Duane Arnold Energy Center Turbine Trip w/o Bypass

4.2.2 Loss of a Feedwater Heater

A feedwater heater can be lost in at least two ways: (a) if the steam extraction line to the heater is shut, the heat supply to the heater is removed, producing a gradual cooling of the feedwater; (b) a bypass line is usually provided so that the feedwater flow can be passed around rather than through the heater. In either case, the reactor vessel receives cooler feedwater which produces an increase in core inlet subcooling. Due to the negative void reactivity coefficient, an increase in core power results. Figure 4-4 shows the response of the plant to the loss of 100°F of the feedwater heating capability of the plant. This represents the maximum expected single heater (or group of heaters) which can be tripped or bypassed by a single event. The reactor is assumed to be at turbine-generator design conditions on automatic recirculation flow control when the heater is lost. The feedwater flow delay time of approximately 25 seconds between the heaters and the feedwater sparger is neglected. The plant would continue at steady-state conditions during this delay period. The recirculation flow control system responds to the power increase by reducing core flow so that steam flow from the reactor vessel to the turbine remains essentially constant throughout the transient. Neutron flux increases above the initial value to produce turbine design steam flow with the higher inlet subcooling. The transient was analyzed without a neutron flux scram; however, the MCPR was calculated assuming a scram at 120% neutron flux. As shown in Table 4-2, this event is less severe than the turbine trip without bypass.

4.2.3 Code Overpressure Protection Analysis

Analysis of the event described in Subsection 4.1.3 demonstrates that the installed safety valve capacity of 18.7% of rated flow, in conjunction with relief valve capacity of 68.4% of rated flow, limits the peak nuclear system pressure at the bottom of the vessel to 1292 psig. The resulting 83 psi margin to the ASME Code limit assures adequate protection against excessive overpressurization of the nuclear system process barrier even for this hypothetical reactor isolation event. Figure 4-5 graphically shows the results produced by this simulated analysis.

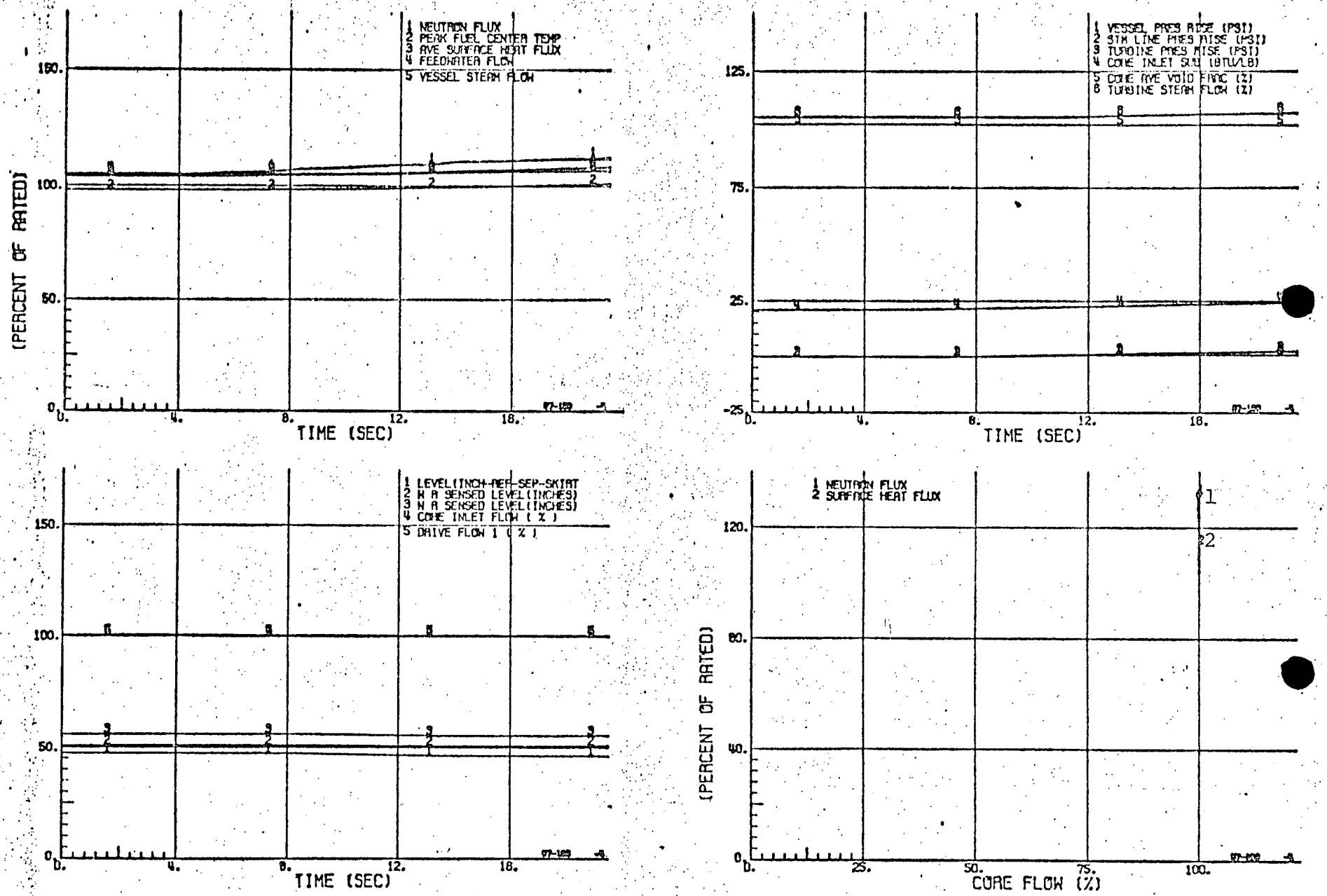
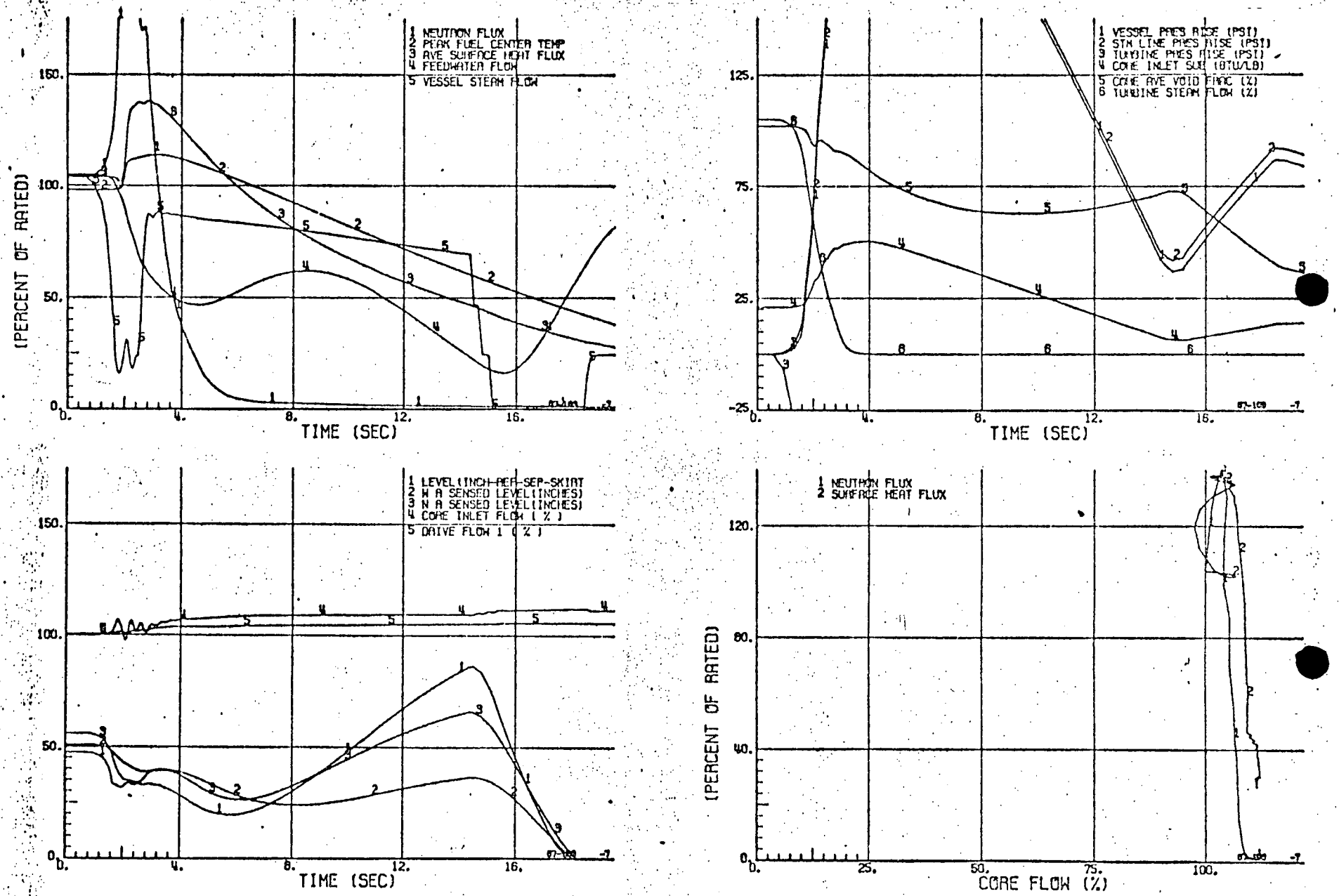


Figure 4-4. Duane Arnold Energy Center Transient Analysis Loss of Feedwater Heater

DUANE ARNOLD EOC-1 KE1HRF01HM032
LOSS OF 100 DEG FEEDWATER HEATER. NO SCRAM. 104% POWER. 100% FLOW



DUANE ARNOLD EOC-1
MSIV FLUX SCRAM. 67A-.8EOC.

KE1HAF01MF031
104% POWER. 100% FLOW

Figure 4-5. Duane Arnold Energy Center Code Over Pressure Protection Analysis Closure of All MSIV Isolation Value w/Flux Scram

The sequence of events assumed in the analysis was investigated to show conformance to code requirements and to evaluate the pressure relief system exclusively. In addition, an analysis was performed for a flux scram case with five safety/relief valves and two safety valves operable, and another analysis assuming a pressure scram with all valves operable was performed. The results of these analyses are given in Table 4-2.

4.2.4 Determination of Operational MCPR Limit

The results of the most limiting increase in pressure and power and decrease in coolant temperature transients were evaluated to determine the largest decrease in MCPR. Other types of transients have an insignificant effect upon critical power and are, therefore, not reviewed in depth.

The most severe of the above types of transients and the associated maximum decrease in MCPR are given below.

Limiting Transient	Maximum ΔMCPR
Turbine Trip without Bypass	0.28
Loss of Feedwater Heater	0.15

As can be determined from above, with a ΔMCPR of 0.28, the turbine trip without bypass is the most severe abnormal operational transient for DAEC. Addition of this ΔMCPR to the Safety Limit MCPR gives the minimum operating MCPR required to avoid violating the safety limit, should this limiting transient occur.

Based on the fuel cladding integrity safety limit of 1.06 and the results of the abnormal operational transient analyses, the operating limit MCPR is 1.34.

4.3 LOCAL EVENTS - ROD WITHDRAWAL ERROR

In the analysis of the rod withdrawal error (RWE), it is assumed that while operating in the power range in a normal mode of operation, the reactor operator

makes a procedural error and withdraws the maximum worth control rod to its fully withdrawn position. Due to the positive reactivity insertion, the core average power will increase. More importantly, the local power in the vicinity of the withdrawn control rod will increase and could potentially cause cladding damage due to either overheating which may accompany the occurrence of boiling transition or by exceeding the 1% plastic strain limit imposed on the cladding, which are as the assumed transient failure thresholds. The following list depicts the sequence of events for this transient.

Event	Approximate Elapsed Time
a. Event begins, operator selects and withdraws at maximum rod speed the maximum worth control rod	0
b. Core average and local power increases	
c. LPRM's alarm	$\leq$ 5 sec
d. Event ends - rod block by RBM	$\leq$ 30 sec

Under most normal operating conditions, no operator action will be required since the transient which will occur will be very mild. If the peak linear power design limits are exceeded, the nearest LPRM's will detect this phenomenon and sound an alarm. The operator must acknowledge this alarm and take appropriate action to rectify the situation.

If the RWE is severe enough, the rod block monitor (RBM) system will sound alarms, at which time the operator must acknowledge the alarms and take corrective action. Even for extremely severe conditions (i.e., for highly abnormal control rod patterns, operating conditions, and assuming that the operator ignores all alarms and warnings and continues to withdraw the control rod), the RBM system will block further withdrawal of the control rod before the fuel cladding Integrity Safety Limit MCPR or 1% plastic strain limits are exceeded.

The RWE was analyzed for the plugged lower core plate configuration from the limiting control rod pattern shown in Figure 4-6. The results of this analysis are given in Table 4-3. Assuming the RBM set point is at 107% of initial and the "worst case" failure of a LPRM, a rod block at approximately 6 feet would occur. The MCPR at this point is approximately 1.12 which is above the Safety Limit of 1.06, and the cladding strain is less than 1%.

	02	06	10	14	18	22	26	30	34	38	42
43					34		34				
39				46				46			
35			30		10		10		30		
31		46		34		42		34		46	
27	34		10		0		0		10		34
23				42		46		42			
19	34		10		0		0		10		34
15		46		34		42		34		46	
11			30		10		10		30		
07				46				46			
03					34		34				

NOTES:

1) Numbers indicate number of notches withdrawn out of 48. Blank is a fully withdrawn rod
2) Error rod is 18-27

Figure 4-6. Duane Arnold Energy Center Limiting Rod Pattern Used in Analysis of Rod Withdrawal Error

Table 4-3

ROD WITHDRAWAL ERROR SUMMARY

Rod Position Feet Withdrawn	Reactor Power MWt	MLHGR kW/ft	ΔMTM*
0	1593	18.50	0
2	1606	18.50	-0.024
4	1641.8	18.52	-0.113
6	1678.8	18.67	-0.210
8	1699.9	23.49	-0.288
12	1708.3	24.42	-0.394

*ΔMTM = MTM at the indicated rod position minus the MTM at rod position 0 feet withdrawn

5. ECCS ANALYSES - APPENDIX K REQUIREMENTS

5.1 INTRODUCTION

This section describes the results of the loss of coolant accident analysis for the Duane Arnold Energy Center.

The analysis was performed using General Electric calculational models which are consistent with the requirements of Appendix K of 10CFR Part 50 in effect on December 28, 1974. A complete discussion of each code employed in the analysis is described in References 7 and 8.

In December 1973 and January 1974, the NRC Staff finalized and published Appendix K to 10CFR50. Between August and December 1974, additional revisions to the evaluation models were made to resolve differences in interpretation of Appendix K and to consider additional phenomena. As a result, the models used in the present analysis differ from those used in the August 1974 submittal.

Also, during this time period, there were three major revisions to the GE model. These are summarized below:

a. In line with the announced NRC goal to make the calculations more "realistic," GE originally made full core power calculations assuming the peak bundle at the technical specification limit at the time of the hypothetical LOCA. The NRC has since interpreted Appendix K to mean that calculations should be done assuming the peak bundle operating at 102% of nominal power to account for possible instrument error. This change was incorporated in the model.

b. In the August 5 calculations, GE used the recent (1973) proposed ANS decay heat standard for the calculation of fission product decay heat using, in particular, an energy release per fission which is appropriate for the BWR fuel lattice instead of the more conservative value in the 1971 proposed standard referenced in Appendix K. Use of the 1971 edition was required by the NRC and was incorporated in the model.

c. In the August 5 calculations, GE assumed nucleate boiling heat transfer below the core midplane during the calculated flow stagnation period prior to lower plenum swell. The NRC concluded that there was not sufficient experimental evidence to justify that assumption and required that GE switch to a film boiling heat transfer mode for the calculation. This was incorporated in the model.

In addition to the above, in early November, the NRC raised a concern that the upward steam movement through the core region from lower plenum steaming and from steam generation within the bundle itself might result in core spray entrainment and a reduction in the rate of vessel flooding due to a reduced rate of core spray flow to the bottom region of the vessel.

In the core spray entrainment effect, it is postulated that high steam velocities could carry core spray fluid upward and out of the reactor core. A second concern involves the possibility that high upward steam velocites in geometrically restricted areas (e.g., the upper tie plate of a BWR fuel assembly) can impede the downward flow of liquid through that same area. This effect could delay the time at which core reflooding occurs. For jet pump plants, the reflooding of the core is the mechanism which terminates the LOCA transient, so the reflooding time is a very significant parameter in determining calculated peak cladding temperature.

In response to these expressed concerns, GE began testing on November 18, 1974, in San Jose to investigate these phenomena using a full-scale, full power simulated 8x8 fuel assembly. The test results verified, as predicted, that entrainment was a relatively insignificant factor, but did demonstrate that the effect on core spray-related flooding should be considered in the LOCA analysis. Many additional tests were run to quantify the critical parameters for such incorporation. Model modification was subsequently made to account for this phenomenon.

The cumulative effect of these changes is to reduce the amount of power that any fuel bundle can generate on an operating plant.

5.2 EMERGENCY CORE COOLING SYSTEM ANALYSES - BYPASS FLOW HOLES PLUGGED

The analyses of the design basis loss of coolant accident (LOCA) using the December 28, 1974 evaluation model have been redone to consider the effect of the plugging of the bypass flow holes. The principal effects of this modification are in the blowdown thermal-hydraulics and heat transfer, rod-to-rod power distribution and reflood time.

5.2.1 Blowdown Thermal-Hydraulics and Heat Transfer

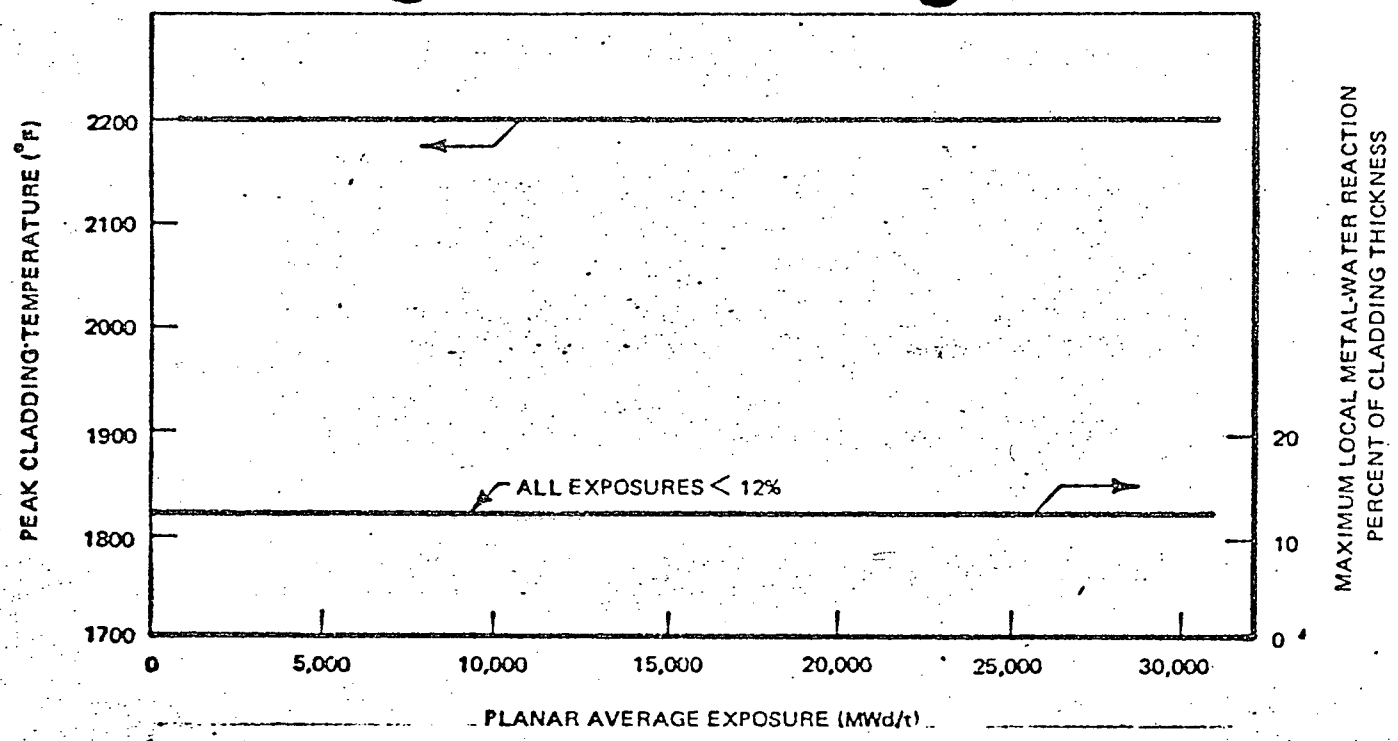
A review of these phenomena has been performed, and it was concluded that only second-order effects exist. Plugging the bypass flow holes has a minor effect on the heat transfer coefficients during blowdown due to changes in the blowdown flow rate. Sensitivity analyses have been performed which demonstrate that the change in peak clad temperature (PCT) will be less than 5°F. Therefore, changes in these phenomena due to plugging of the bypass flow holes result in no significant changes to the LAPLHGR.

5.2.2 Rod-to-Rod Power Distribution

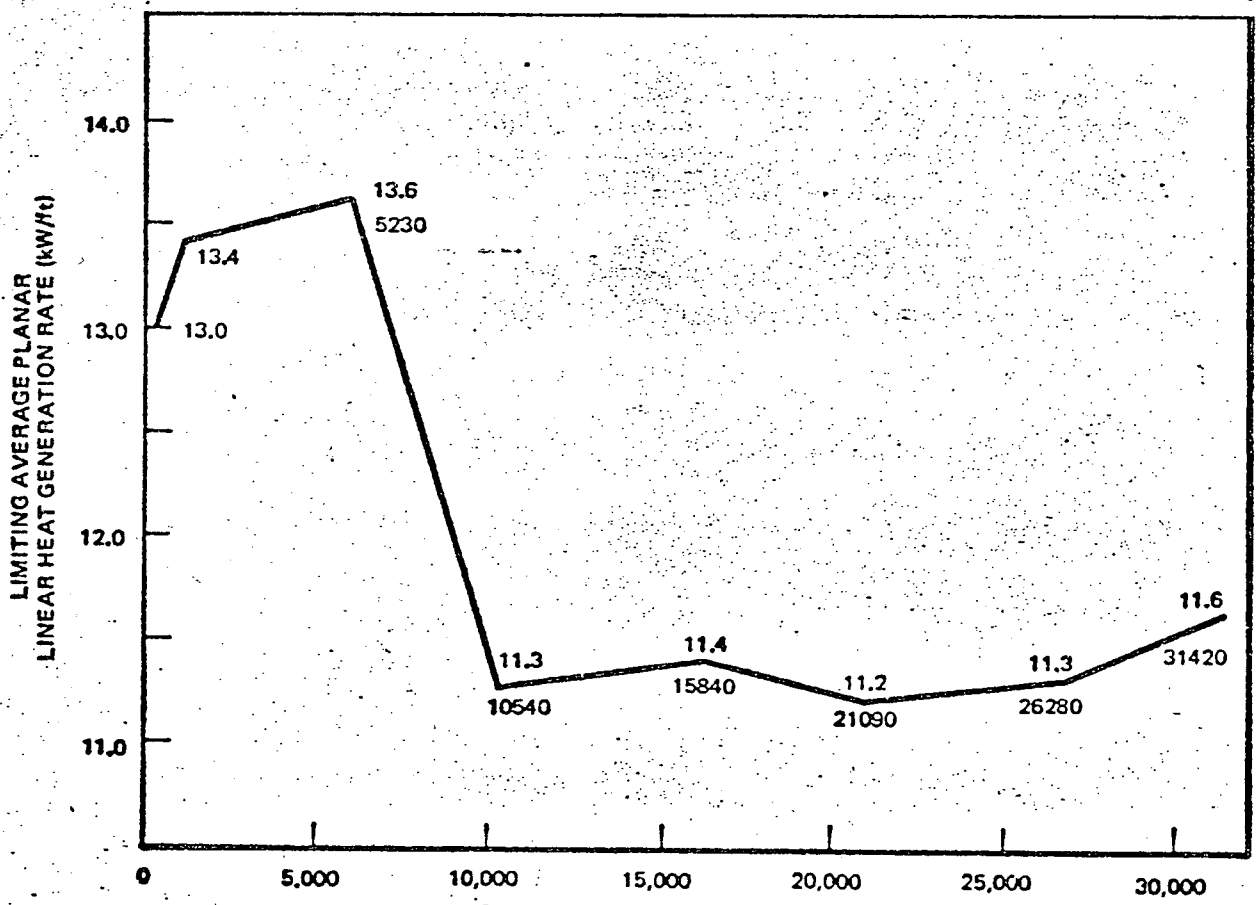
Voiding in the bypass region tends to flatten the rod-to-rod power distribution with a resultant increase in peak cladding temperature (PCT) at a given elevation. However, the plugging of the flow bypass holes does not result in any voids occurring in the bypass region at the elevation of interest (Core midplane). Therefore, no power flattening occurs with plugging which could affect the value of the LAPLHGR since the power distribution previously used assumed no bypass region boiling at the elevation of PCT.

5.2.3 Reflood Time

The plugging of the bypass flow holes eliminates the main flow path from the bypass region to the lower plenum. Reflooding subsequent to a LOCA must rely almost exclusively on the core spray flow through the fuel assemblies. The two reflood times are 233 seconds without plugged holes and 388 seconds with plugged holes. The effect of this slower reflooding has been considered in the recalculation of the LAPLHGR's as a function of fuel type and exposure. These recalculated LAPLHGR's are shown in Figures 5-1 and 5-2.

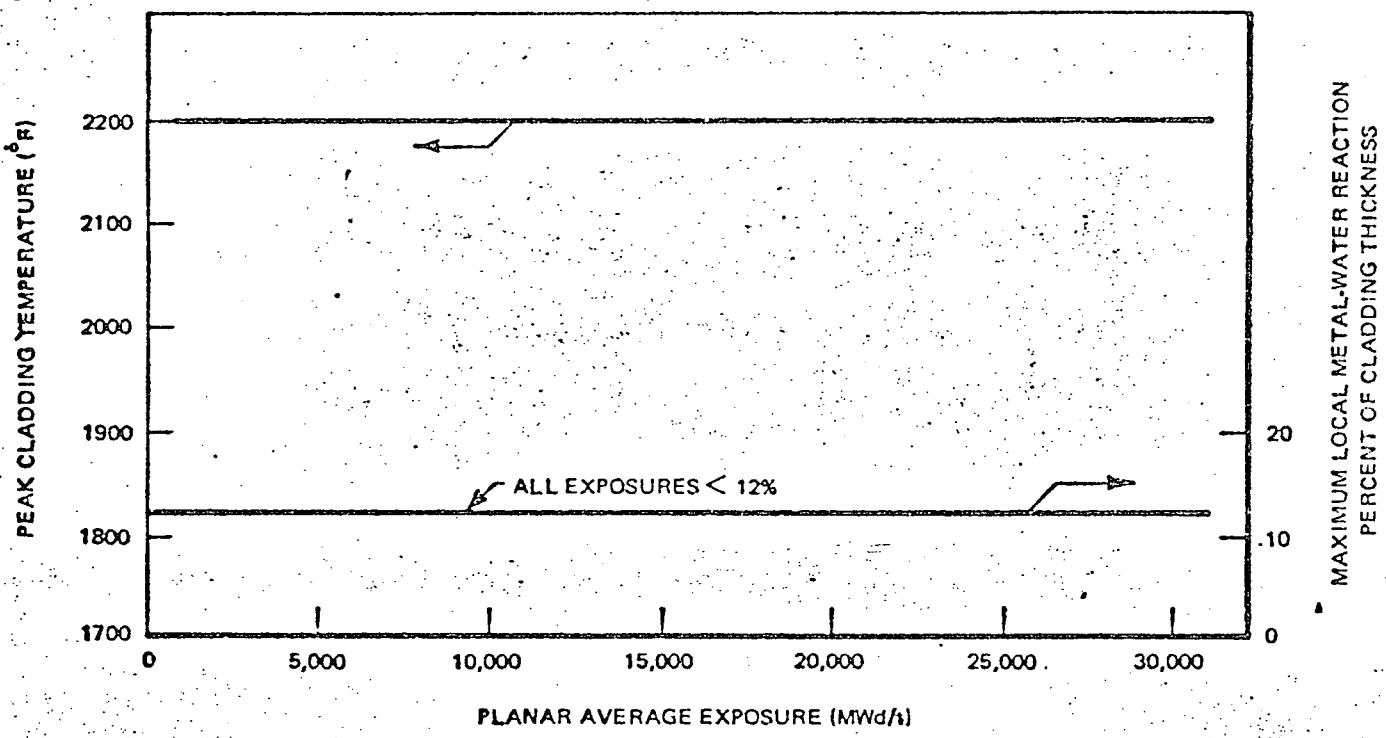


PEAK CLADDING TEMPERATURE VERSUS PLANAR AVERAGE EXPOSURE

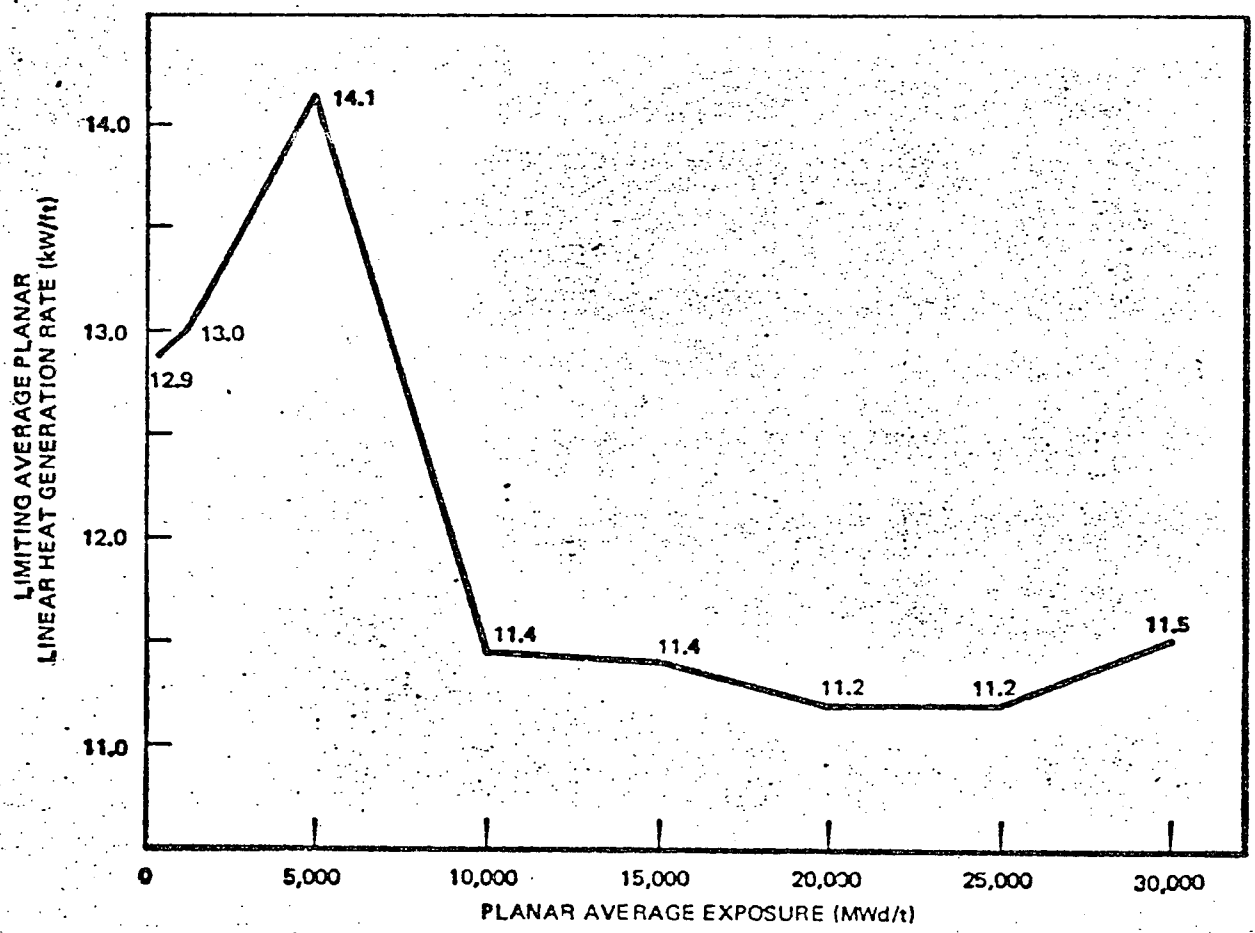


LIMITING AVERAGE PLANAR LINEAR HEAT GENERATION RATE (LAPLHGR)
VERSUS PLANAR AVERAGE EXPOSURE

Figure 5-1. Duane Arnold Energy Center ECCS Analysis Using December 28, 1974 Methods Initial Core Fuel Types 1 and 3



PEAK CLADDING TEMPERATURE VERSUS PLANAR AVERAGE EXPOSURE



LIMITING AVERAGE PLANAR LINEAR HEAT GENERATION RATE (LAPLHGR)
VERSUS PLANAR AVERAGE EXPOSURE

Figure 5-2. Duane Arnold Energy Center ECCS Analysis Using December 28, 1974 Methods Initial Core Fuel Type 2

6. REFERENCES

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5. L. G. Neal and S. M. Zivi, "The Stability of Boiling Water Reactors and Loops," Nuclear Science and Engineering, Vol. 30, Pg. 25, 1967.

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7. *General Electric Company Analytical Model for Loss-of-Coolant Analysis in Accordance with 10CFR50 Appendix K,* (Draft) submitted August 1974 (NEDO-20566).

8. *General Electric Refill Reflood Calculation (Supplement to SAFE Code Description),* transmitted to the USAEC by letter, G. L. Gyorey to Victor Stello, Jr., dated December 20, 1974.