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COMPUTER TECHNIQUES
FOR SENSOR VALIDATION
DURING EBR-II NATURAL CIRCULATION

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ABSTRACT

Methods have been developed for validating the flowrate through the EBR-II reactor during low flow, transient (natural circulation) operating conditions. The methods rely mainly on the operating history of the reactor prior to shutdown and thermocouple readings from the Data Acquisition System during natural circulation. Because of the large number of thermocouples in the reactor, the methods are inherently redundant and therefore relatively insensitive to the behavior of one or two individual thermcouples.

The first model developed is called the assembly heat balance model, and it is implemented in two steps. The first step is executed before the reactor is brought up to power, and the output from this step consists of tables of ratios of predicted temperature rise across one subassembly to the predicted temperature rise across another subassembly as a function of total flow through the reactor. Twenty-five tables are generated for as many pairs of subassemblies. The second step is executed during actual natural circulation conditions. Ratios of temperature rise across one subassembly to temperature rise across another subassembly are calculated from the Data Acquisition System signals, and the total flow through the reactor is predicted from the tables generated in the first step. The individual predicted flows from each pair are weighted differently, depending on the amount of variation in the individual prediction, to arrive at a single prediction of total flow through the reactor.

The second model developed is called the transient heat balance model and consists of a simple energy balance on the EBR-II reactor. The decay heat is determined as a function of time after shutdown from the reactor operating history using a modified form of the ANS Revised Standard, and an unweighted average of subassembly outlet thermocouples is used to approximate the time behavior of the bulk outlet temperature. This information is used in a simple transient heat balance equation to determine the total flow through the reactor.

Using a natural circulation data tape from Test 8A, these two models were evaluated. It was found that the assembly heat balance model predicted total flow through the reactor very well at very low flows (less than or equal to two percent of total flow), although some individual pairs were very poor indicators of total flow. At slightly higher flows (approximately five percent of full flow), the model followed the right trends but failed to predict the flow accurately. There was some evidence to suggest this problem occurred because of the relatively low bulk temperature rise across the reactor at higher flows. The transient heat balance model was found to perform in a very similar fashion; agreement was excellent at lower flows, but not very good at slightly higher ones. This was again partially attributed to the relatively low bulk temperature rise across the reactor at higher flows.

INTRODUCTION

The operator of a nuclear power plant is supplied with an extraordinary amount of information from plant sensors. In some instances he must choose between conflicting results from redundant or related sensors; this situation should obviously be avoided, however, since the wrong choice could result in serious consequences.

One way to avoid this situation is to intercept and validate the information provided by the plant Data Acquisition System (DAS) before the information is sent to the operator. This may be accomplished by constructing analytic measurements to supplement direct measurements, and then submitting all measurements to a fault detection and isolation (FDI) algorithm. After processing the information, the FDI sends a validated estimate to the operator. Techniques of signal validation were applied to the EBR-II reactor flowrate in the forced flow regime as a result of work performed by the Charles Stark Draper Laboratory (Ref. 1). The purpose of this study is to extend the range of signal validation into the EBR-II natural circulation regime.

Although the concept known as analytic redundancy is used in both studies, the analytic models used in the present study are much different from those used previously. The change is required since many of the sensors that provide accurate readings during forced flow conditions do not do so during natural circulation. In particular, all of the pressure instruments and some of the flowmeters yield meaningless information during natural circulation. The models developed in this study thus rely primarily on temperature readings to infer reactor flowrate.

ASSEMBLY HEAT BALANCE MODEL

The first analytic measurement developed for the EBR-II reactor flowrate in the natural circulation regime is called the assembly heat balance model. This method of inferring the flowrate is based on the flattening of the interassembly temperature profile at low flows. As the total flow within the reactor decreases, the elevation pressure drop becomes comparable to, and then greater than, the frictional pressure drop. Flows within hot channels increase, and those within cool channels decrease, so that the total pressure drop across each subassembly is the same. The degree to which the interassembly profile is flattened depends on the total flow and power levels in the reactor; hence, the flowrate can be inferred from measurements of relative temperature rise across different parts of the reactor.

EBR-II is equipped with thermocouples that allow such inferences to be made. Thermocouples used in the method are located in the lower plenum and in the outlet flow paths of several (typically ten) subassemblies. Ratios of temperature rise across pairs of subassemblies are

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then calculated; usually, each "hot" subassembly (typically five) is paired with each "cold" subassembly (typically five) to provide a large number of ratios of temperature rise. For each ratio, a value of reactor flow may be obtained from tables prepared before on-line operation. A single analytical flow measurement may then be obtained from a weighted average of those individual analytic measurements. Because the on-line procedure involves only a simple calculation of temperature rise ratios and then a flow prediction based on the use of these ratios in existing tables, it is quite useful for real-time applications.

Tabulation of Temperature Rise Ratios. The tables of temperature rise ratios are prepared from a computer program which models the thermal hydraulic behavior in the first six rows of the EBR-II reactor. Although the EBR-II reactor contains sixteen rows, the hydraulic resistance of the inlet piping and the row-by-row orificing of the subassemblies are such that about 70% of the total reactor flow moves through the first six rows. The method outlined below actually provides an analytic measurement of the percent of full flow within these first six rows. Because the flow within this region constitutes a large fraction of the total reactor flow, it is felt the analytic measurement also provides a reasonable estimate of the percent of full flow in the entire reactor.

There are two major reasons for restricting the analysis to the first six rows. The first is the length of time needed for the computation. It takes a few hours for the current version of the table generating program to run, and, as will be shown below, the computer time scales with the number of subassemblies squared. A seven row analysis (127 subassemblies) would take nearly twice as long to run as a six row (91 subassemblies) analysis. The run time increases more dramatically as additional rows are included. The second reason for restricting the analysis to the first six rows is that the subassembly outlet thermocouples used in the second step of the procedure are all located within this boundary. Thus, little additional information can be gained by including additional rows outside the sixth row.

Basis for Governing Equations. Modeling the thermal hydraulic behavior in the first six rows of the reactor (91 subassemblies) would still be an extremely difficult task if all the details of the behavior were included in the analysis. Results from data compilations of several natural circulation tests (Ref. 2) indicate that the flow within a subassembly can be treated as one-dimensional in the natural circulation regime. This is due to the flattening of the intra-assembly temperature profile; there is very little difference between the temperature in an interior subchannel and that in an edge subchannel at a given axial location during natural circulation. For this reason, the details of the intra-assembly behavior are ignored, and the governing equations are based on the bulk temperature of the fluid in each subassembly. This assumption breaks down when the flow becomes appreciably

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larger than typical natural circulation flows (e.g., for flows greater than 10% of full flow values).

In addition, because natural circulation is a quasi-steady-state phenomena, the temperature rise ratios are calculated from a set of steady-state equations. The model will therefore fail to predict accurately the flow during more severe transients; it performs poorly during the first minute after shutdown as the flow slows down and it is expected to perform poorly during sharp changes from one flow level to another.

<u>Governing Equations.</u> Despite these limitations, the model is valid over a large portion of the natural circulation regime. The calculation of the temperature rise ratios is based on a set of momentum and energy equations for each subassembly, a conservation of mass equation involving all subassemblies, and a simple, single phase equation-of-state for sodium. The governing momentum equations for the ith subassembly are:

$$\Delta p_i = \Delta p_{fi} + \Delta p_{ei} \tag{1}$$

$$\Delta p_{fi} = f(m_i) \tag{2}$$

$$\Delta p_{ei} = (c_i \rho_{in,i} + (1.-c_i)\rho_{out,i})Lg \tag{3}$$

where

Δp_i = total pressure drop,

Δp_{fi} = frictional pressure drop,

Δp_{ei} = elevation pressure drop,

m_i = flowrate,

c_i = thermal center (fractional position),

$\rho_{in,i}, \rho_{out,i}$ = inlet and outlet sodium densities,

Lg = product of subassembly axial length and gravitational constant.

The energy equation for the ith subassembly is:

$$m_i c_p(dT_i(z)/dz) = q_i(z) - \Sigma U_{ij}(z)P_{ij}(z) \ (T_i(z) - T_j(z)) \tag{4}$$

where

m_i = flowrate,

c_p = heat capacity of sodium,

$dT_i(z)/dz$ = axial rate of change of bulk temperature,

q_i = linear heat generation rate,

U_{ij} = overall heat transfer coefficient between subassembly i and adjacent subassembly j,

P_{ij} = perimeter between subassembly i and adjacent subassembly j.

Thus the energy equation states that the increase in bulk temperature of a subassembly is due to internal decay heat generation less the heat lost to its adjacent subassemblies through transverse conduction. It is again emphasized that the temperatures which appear in the transverse conduction term of Eq'n. (4) are ordinarily edge subchannel temperatures and that these usually differ from the bulk subassembly temperature; however, it has been observed (Ref. 2) that under natural circulation conditions, there is little difference between the two, and bulk temperatures may be used in the conduction term.

The conservation of mass equation for the set of subassemblies is:

$$M_T = \sum m_i \tag{5}$$

where

M_T = total mass flowrate,

m_i = flowrate in subassembly i.

Finally, the equation of state for sodium is approximated by:

$$\rho = \rho_0 - \alpha(T-T_0) \tag{6}$$

where

ρ_0 = reference density at some specified temperature T_0

α = temperature expansion coefficient

For a specified level of total flow and total power, the unknowns in this set of equations are the individual subassembly mass flowrates and the pressure drop across the reactor. The unknowns are found by imposing the constraint that the pressure drop across all subassemblies is

equal. The temperature rise across each subassembly is determined as part of the solution. Temperature rise ratios may therefore be calculated as functions of total flow and total power by solving this single problem over many flow/power combinations.

Solution Procedure. The exact solution procedure for a single combination of total flow and total power is as follows:

(a) Assume initially that the percent flow through each subassembly is the same as the percent total flow, and that the pressure drop across the reactor is the elevation pressure drop evaluated using the density of the inlet plenum sodium.

(b) Solve the coupled set of energy equations (4) to get the axial temperature profiles within each subassembly. Calculate the thermal centers of each subassembly based on these profiles.

(c) Find the sodium density at the inlet and outlet of each subassembly using (6), then evaluate the pressure drop across each subassembly using (1), (2) and (3).

(d) If the pressure drop across each subassembly is not the same, formulate new guesses for the flowrates using a form of the Newton-Raphson method and return to step (b). A more complete description of the Newton-Raphson method is given in Appendix A; it is this portion of the program which is the most time-consuming and is responsible for the computer time scaling with the number of subassemblies squared.

(e) Once the calculation is finished, store the temperature rise ratios in a table as a function of flow and power.

Repeating this procedure for several flow/power combinations results in a series of tables whose graphical form is shown in Fig. 1. These results can be compared to a similar set of tables illustrated in Fig. 2. The Fig. 2 results are obtained assuming no transverse conduction (Ref. 3). The conduction effects explain the relatively weak dependence on power level shown in Fig. 1.

The characteristic shape of both sets of curves is most easily explained by referring to Fig. 2. At 100% of full flow, the temperature rise across the channel is greater than the average temperature rise; this is a "hot" channel. As the percent total flow decreases, the elevation pressure drop becomes dominant, and the flow within the hot chan-
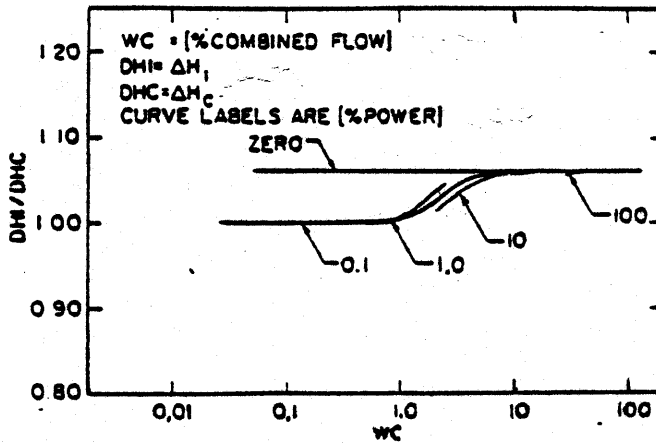
Figure 1 - Temperature Rise Ratios Including Transverse Conduction

nel increases. The interassembly profile flattens as the temperature rise across the "hot" channels approach the average temperture rise. As the percent total flow decreases even further, the elevation pressure drop becomes orders of magnitude larger than the friction pressure drop, and the temperature rise across all channels is the same.

The strong dependence on power level illustrated in Fig. 2, the case without transverse conduction, can be explained by considering the effect of power level on elevation pressure drop. Since the elevation
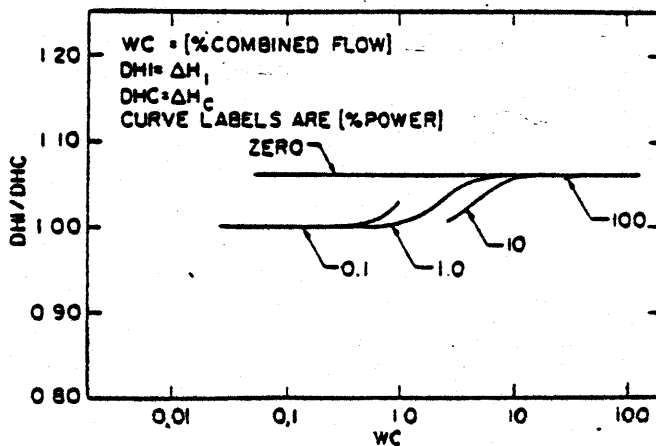

Figure 2 - Temperature Rise Ratios Without Transverse Conduction

pressure drop is dependent on the axial density profile which is in turn dependent on the axial temperature profile, significant differences in elevation pressure drops between channels do not occur until the temperature rise across the channels is significant. For low power levels, this does not occur until the percent total flow is very low. Thus the curves shift to the left with decreasing power level.

When transverse conduction is present, as in Fig. 1, the temperature rise in a particular channel is dependent on the axial temperature distributions in adjacent subassemblies as well as the flowrate within the subassembly. The temperature rise in a particular subassembly can change relative to the temperature rise in other subassemblies independent of the flowrate through that subassembly. Considering a low power, high total flow case, the amount of transverse conduction between subassemblies has a small absolute value, but it is comparable to the bulk temperature rise across the reactor. Thus some flattening of the interassembly temperature profile takes place before the elevation pressure drop becomes dominant. The result is that the low power curves are pushed into the higher power curves, and the relative temperature rise across various parts of the reactor becomes a weak function of total power.

Validation of the Method. To determine the validity of this technique, a data tape from natural circulation test 8A (Ref. 2) was obtained and the second step of the analytic measurement was performed. The tables of relative temperature rise were generated using a simpler, much faster solution procedure than the one described above; the tables were generated at a single power level on the basis of the initial values of the flowrates (no Newton-Raphson method was used). Figure 3 compares the analytic measurement with a direct flow measurement made during natural circulation test 8A. The reactor was allowed to operate in the natural circulation regime for about fifty minutes after shutdown, at which time the auxiliary pump was turned on (provides 5.5% of full flow). The analytic measurement was based on a weighted average of the individual analytic measurements obtained from the tables. The figure shows the analytic measurement is in excellent agreement with the direct measurement at lower flows, but not in very good agreement at slightly higher ones. The disagreement is partially attributed to the lower temperature rise across the core with the auxiliary pump on (10-15 F), which makes accurate, consistent calculation of temperature rise ratios more difficult. Room for improvement also exists in the model. A better characterization of the EBR-II core, more accurate modeling of the frictional pressure drop within subassemblies and a more complete set of input data will help establish the validity of the technique over a wider range of flow conditions.

% OF FULL FLOW / TIME (SECS)
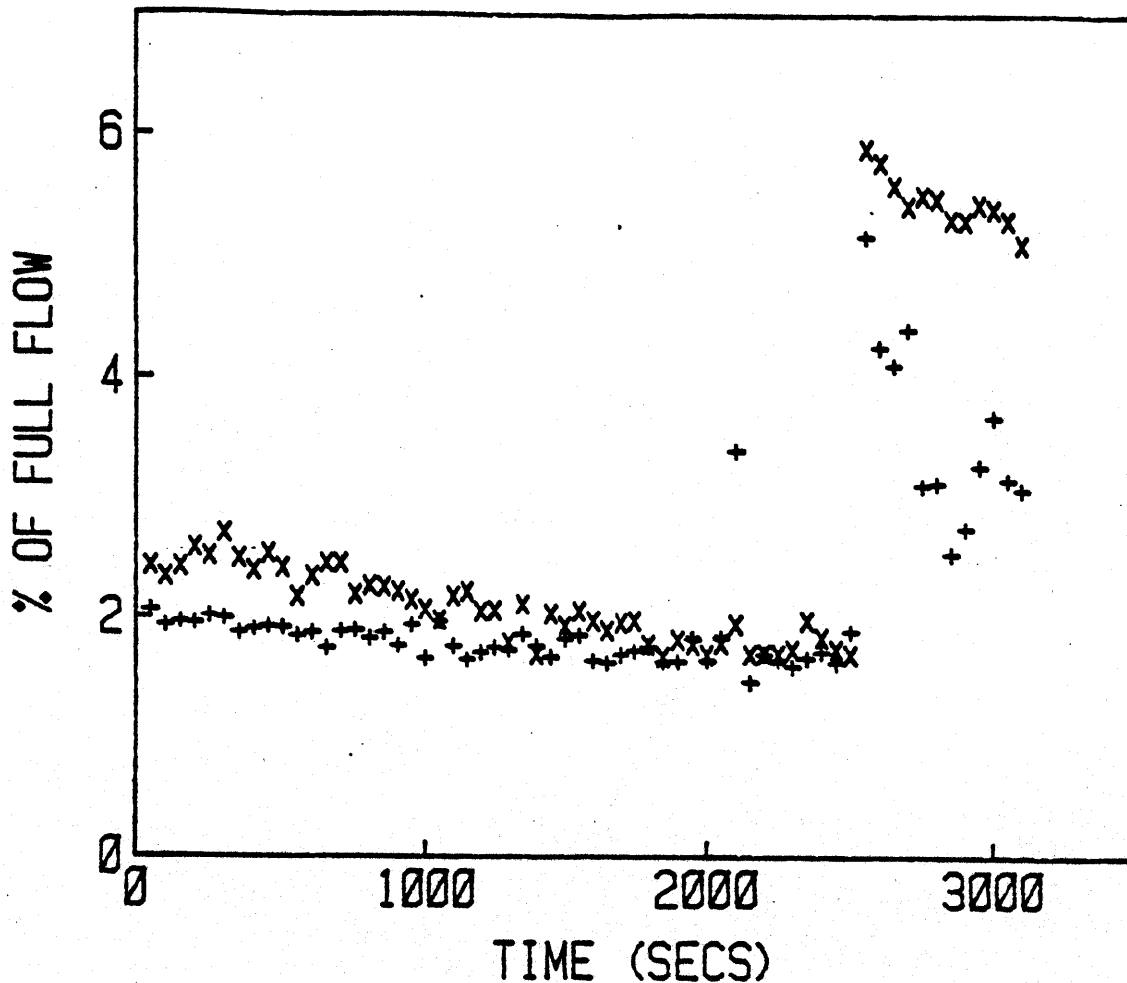
X -CORRECTED FLOW MEASURMENT FROM HPP2 FLOWMETER
+ -AVERAGE OF PREDICTED FLOWS FROM
ASSEMBLY HEAT BALANCE MODEL

Figure 3 - Direct and Analytic Measurements from AHB Model

TRANSIENT HEAT BALANCE MODEL

A second way of using thermocouples to predict total flow through the reactor is through a transient heat balance equation. This can be accomplished if the decay power level in the reactor is known and an estimate of the bulk temperature rise across the reactor can be obtained.

Expressions for Decay Heat. The decay power level is found from the reactor operating history prior to shutdown using a form of the ANS Revised Standard (Ref. 4). The standard describes a variety of methods

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for estimating the decay heat in the reactor for any arbitrary operating history based on 23 different decay groups for each of three isotopes: U-235, U-238 and Pu-239. The form of the decay power from a specific isotope for operation at a fixed level is

$$F(t,T) = \sum (a_i/\lambda_i) \exp(-\lambda_i t) \{1 - \exp(-\lambda_i T)\} \qquad (7)$$
(Mev/fission)

where

a_i, λ_i are decay group constants

t=time after shutdown

T=length of operation at fixed level

For some arbitrary operating history, the standard recommends that the decay power be calculated from:

$$P = \sum R_{jn} \sum (a_i/\lambda_i) \exp(-\lambda_i t_n) \{1 - \exp(-\lambda_i T_n)\} \text{ Mev/s} \qquad (8)$$

where

a_i, λ_i are the same decay group constants

R_{jn}=fission rate of nuclide j during operating period T_n (fissions/sec)

T_n= operating period of length n seconds

t_M= t + $\sum T_n$ = time after operating period n ended

This algorithm may predict the decay power as a function of time after shutdown, but it is in a computationally inefficient form. The problem is that the operating history must first be tabulated, then broken into discrete operating levels before the algorithm can actually be used. Tabulating the operating history may require large amounts of storage space, and some normal operational procedures, such as a ramp up to full power, may not be easily represented by a set of discrete operating levels. An alternative approach is to continuously update the contributions from each of the 23 decay groups while the reactor is running. This eliminates both the need for large amounts of storage space and the problem of partitioning the operating history. Let

X_i=normalized contribution from decay group i

(a_i/λ_i)=normalization constant for decay group i

N=normalized neutron power

P=normalization constant for neutron power (=62.5 MW_{th})

Then the contribution from decay group i may be updated using

$$\partial X_i/\partial t = \lambda_i(N-X_i) \tag{9}$$

During operation, the contribution from each X_i is calculated from

$$(X_i)_{new} = (X_i)_{old}\lambda_i\Delta t(N-(X_i)_{old}) \tag{10}$$

When the reactor shuts down, the initial contribution from each decay group is

$$X_{io} = (a_i/\lambda_i)(X_i)_{shutdown} \tag{11}$$

and the decay power is calculated as percent of full power from:

$$P=R_j\Sigma X_{io}\exp(-\lambda_i t) \quad Mev/sec. \tag{12}$$

Transient Heat Balance Equation. The decay power is used as an input to the transient heat balance equation, which is used to predict the flow through the reactor. The heat balance equation is:

$$\rho V c_p(\partial T_c/\partial t) + M_T c_p \Delta T_c = Q \tag{13}$$

where

   ρ=average sodium density in the reactor

   V=volume of sodium in the reactor

   c_p=heat capacity of sodium

   $\partial T_c/\partial t$ = rate of change of bulk temperature in the reactor

   M_T=total mass flowrate through the reactor

   ΔT_c=bulk temperature rise across the reactor

   Q=thermal power

The quantities $\partial T_c/\partial t$ and T_c can be ascertained from temperature measurements, and the decay heat is used as an approximation for Q, which is a very good approximation tens of seconds after shutdown. The equation can then be solved for mass flowrate, M_T.

Comparison of Predicted Flowrate with Measured Flowrate. The transient heat balance model, like the assembly heat balance model, was tested using the natural circulation data tape from Test 8A. Prior to this run, a decay heat level of .16 MW_{th} existed in the reactor. The rise to power of Test 8A occurred over a two and three-quarter hour period, and the reactor was left at a steady-state power of 21.8 MW_{th} for an additional three and a quarter hours. At this time electrical power to the primary system pumps was cut, precipitating a scram, and the transient began.

The predicted flowrate is plotted against the measured flowrate in Fig. 4. In one calculation, the reactor outlet temperature was used to find ΔT_c and $\partial T_c/\partial t$ while the other used an unweighted average of the subassembly outlet temperatures. Note first that the flowrate predicted on the basis of the reactor outlet temperature behaves differently than that based on the subassembly outlet temperatures. First, there is a great deal less scatter in the prediction based on the subassembly outlet temperatures; although each individual thermocouple reading contains a small amount of noise, the averaging process seems to smooth out the errors. The smoother signal results in a consistent and reasonable evaluation of the temperature derivative in Eqn. (13), resulting in a more consistent flow prediction. The prediction based on reactor outlet temperatures also appears to be consistently higher than that based on the subassembly outlet temperatures at long times after shutdown. This occurs because heat transfer from the upper plenum to the bulk pool at very low flows decreases the reactor outlet temperature relative to the average subassembly outlet temperature. The apparently smaller temperature rise across the core results in a higher flow prediction. Both predictions, however, fall short of the measured flow once the auxiliary pump is turned on. This again appears to be due to the relatively small bulk temperature rise across the core at higher flows.

SUMMARY

The purpose of any sensor validation routine is to provide reactor operators with information that is worthy of a high degree of confidence, enabling them to perform their jobs better. A sensor validation routine based on analytic redundancy requires development of analytical models which *predict* the value of a particular sensor of interest based on readings from other (back-up) sensors in the plant. These analytical models must be either numerous or highly reliable so that failure of one or two individual back-up sensors does not result in false information being sent to the reactor operator. In addition, it is desirable to construct the validation algorithm in such a way that the influence of failed back-up sensors may be eliminated as they fail.

Two models have been presented which provide analytic measurements of the total flowrate in the EBR-II reactor during natural circulation.
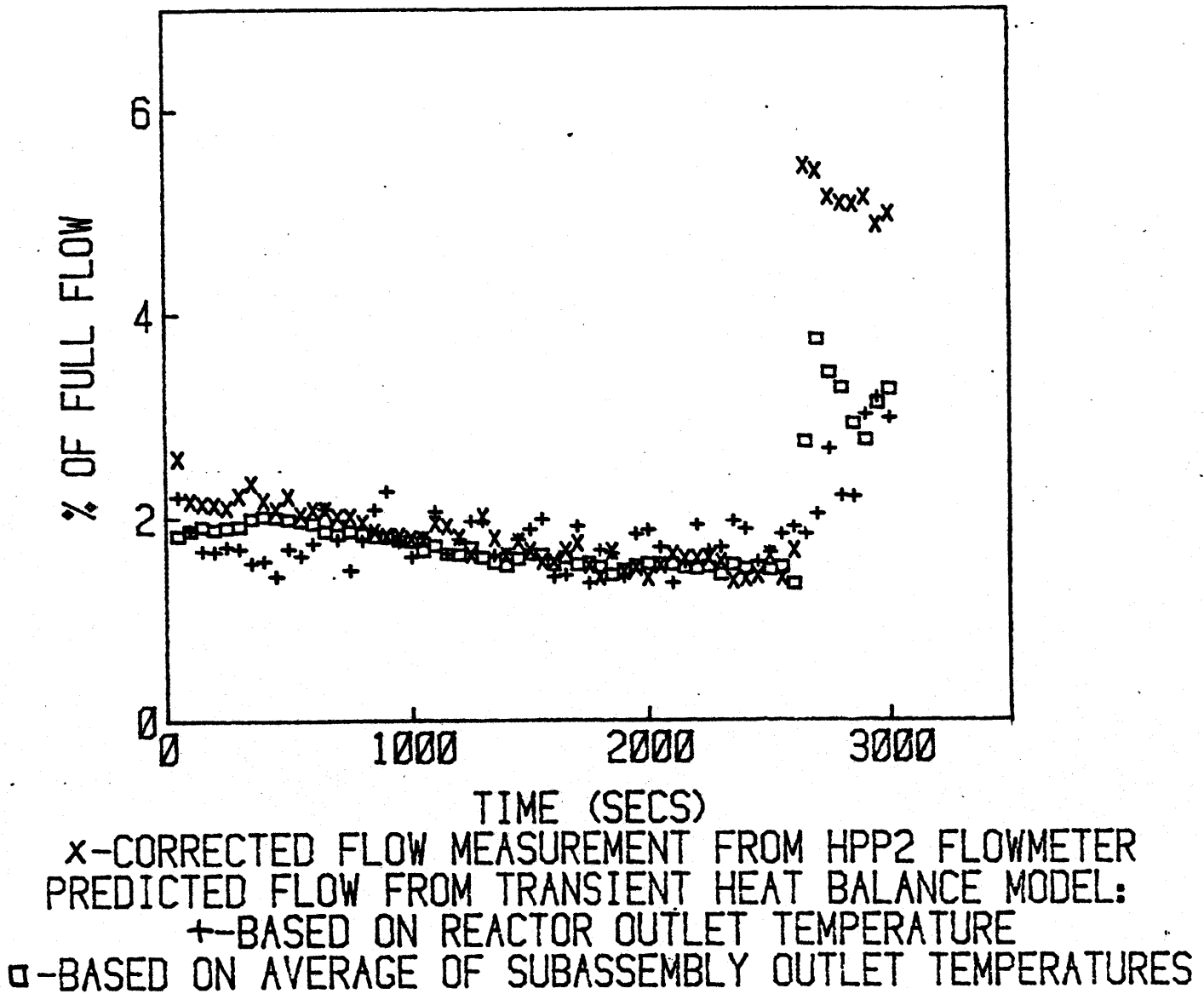
Figure 4 - Direct and Analytic Measurements from THB Model

The first model presented was the *assembly heat balance model*. This model predicted the total flow through the reactor on the basis of tabulated ratios of temperature rise across one subassembly to the temperature rise across another subassembly. Because so many subassembly outlet thermocouples exist in the EBR-II reactor, this model was inherently redundant and insensitive to individual thermocouple readings.

Previous work related to this model (Ref. 2) indicated that these ratios would be functions of both total flowrate *and* the power level in the reactor; however, after solving the problem exactly for the specific case of EBR-II, the dependence on power level was found to be extremely

weak. The reason for this was attributed to the excellent transverse conduction between subassemblies at low flowrates. A comparison of the flowrates predicted by this model and the actual flowrates taken from the Data Acquistion System during natural circulation Test 8A was very promising; agreement was excellent over the lowest flow range, but significant disagreement occurred in a slightly higher range. This discrepancy was felt to be partially due to the lower bulk temperature rise across the reactor at the higher flowrate, but room for improvement also exists within the model.

A second model was presented called the *transient heat balance model*. This model used the decay heat in the reactor and the time-dependent behavior of the unweighted average of the subassembly outlet thermocouples to determine the total flow through the reactor. The decay heat was calculated on the basis of reactor operating history using a modified form of the ANS Revised Standard, and the time-dependent behavior of the bulk outlet temperature was calculated from the unweighted average of the subassembly outlet thermocouples. This model was also tested against the data tape in natural circulation Test 8A. The agreement was also found to be very good at lower flows, but not very good at slightly higher ones; this error was again attributed to the low bulk temperature rise across the reactor.

These two models have been developed and verified separately using the natural circulation data tape from Test 8A. For future work, it is recommended that these models be integrated into sensor validation routines and actually be put to use on the EBR-II plant computer. In particular, this future work should include making the models compatible with a fault detection and isolation algorithm as discussed in the introduction. Only after a successful integration of the thermal hydraulic models with this algorithm will the models provide a useful tool for EBR-II personnel.

APPENDIX A

The exact form of the Newton-Raphson procedure used in the solution procedure of the assembly heat balance model is described here. First define a normalized pressure drop difference:

$$\psi_i = (\Delta p_i - \Delta p)/\Delta p_a \; ; \; i=1,91 \tag{A1}$$

where

Δp_i = pressure drop across ith subassembly

from (1) in step (c) of the solution procedure,

Δp = actual pressure drop across the reactor (unknown),
initially set equal to Δp_a,

Δp_a = approximate pressure drop across the reactor
calculated in step (a) of the solution procedure.

Also define a normalized mass flowrate difference:

$$\psi_{92} = (M_T - \Sigma m_i)/M_T \tag{A2}$$

These quantities are functions of the normalized mass flowrates and
pressure drop, defined as:

$$x_i = m_i/m_{i,full} \; ; \; i=1,91 \tag{A3}$$

where

m_i = mass flowrate in the ith subassembly

$m_{i,full}$ = mass flowrate in the ith subassembly during full flow.

$x_{92} = \Delta p/\Delta p_a$

Considering the vectors ψ and X,

$$(\psi_1 - \psi_0) = (\partial \psi/\partial X)(X_1 - X_0) \tag{A4}$$

where

X_0 = initial guesses of normalized mass flowrates

$\psi_0 = \psi(X_0)$ = results from initial guesses of X

X_1 = next set of guesses of X

$\psi_1 = \psi(X_1)$ = results from next set of guesses of X

$\partial \psi/\partial X$ = Jacobian of ψ with respect to X

A next set of mass flowrates can be found by first solving for the
Jacobian, $\partial \psi/\partial X$. This can be done by perturbing the flow in one subassembly, executing steps (a)-(c) in the solution procedure, and repeating
the calculation for all subassemblies. This is in fact the reason the
run time scales with the number of subassemblies squared; the solution
of Eq'ns. (4) scales linearly with the number of subassemblies, but so
does the number of perturbations required to fill the Jacobian. Solving

for the value of X_1 that will yield $\psi_1 = 0$ gives the next set of guesses for the flowrates:

$$X_1 = X_0 - (\partial\psi/\partial X)^{-1}\psi_0 \qquad\qquad (A5)$$

ACKNOWLEDGEMENTS

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