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MULTIVARIABLE CONTROL FOR THE PRISM ALMR*

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INTRODUCTION

This paper describes the application of state variable control design methods in the conceptual design of two key system level controllers in the PRISM ALMR. The PRISM plant concept consists of three power blocks, each made up of three identical reactors and steam generator modules connected to one turbine. Operation of these physically distributed systems is directed by a hierarchy of controllers at the system, power block, and plant levels. The system level controllers in this paper are the reactivity controllers for movement of the six control rods in each of the nine reactor modules, and the feedwater control of the three parallel steam generators in each of the three power blocks.

1. REACTIVITY CONTROLLER

The PRISM reactivity controller is a system level controller, accepting direction from the module level controller and generating commands for insertion or withdrawal of the six control rods in each of the nine reactor modules.

1.1 Development of a Detailed PRISM Primary Loop Model

Reference 1 examined aspects of the potential application of a model-based reactivity controller for EBR-II. This approach was also used in the initial development of the PRISM reactivity controller. A state variable model of the PRISM primary system was developed in sufficient detail to closely approximate the reactor and primary system dynamics. A detailed model of the PRISM primary loop is used for two purposes. First, a detailed non-linear model which can be linearized provides a benchmark against which reduced order linear models can be compared to assess whether they adequately approximate the primary loop dynamics, and estimate the magnitude of the modeling error introduced by their use. Secondly, an accurate model of the plant is required for many diagnostic applications, as the magnitude of the differences between the actual plant and the diagnostic model may be required to be quite small to permit early fault detection. A fifty-one state variable nonlinear model of the primary system was developed using the graphical SYSTEM BUILD™ feature of the $MATRIX_x$™ controls design code.**

The primary loop system model includes models of the reactor core, upper plenum region, IHX, and a multi-node model of the return code leg sodium flow-path back to the inlet plenum region below the reactor core. Also included were models for reactivity feedbacks, reactor kinetics, reactor power, control rods, and reactor flux and sodium temperature instrumentation. The distribution of the fifty-one state variables among the models is shown in Figure 1.1.

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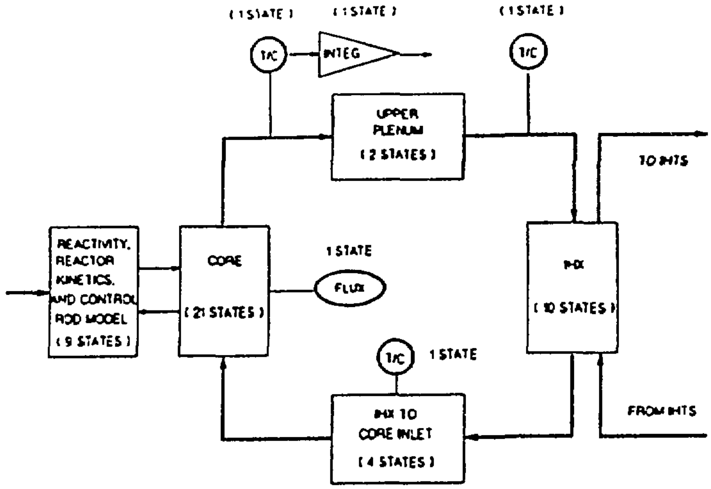

Figure 1.1 - DISTRIBUTION OF STATE VARIABLES IN THE PRISM PRIMARY SYSTEM MODEL

Reactor Core Model

The SYSTEM BUILD feature of MATRIX-x permits a hierarchical arrangement of modeling. Inside the Reactor Core model or "superblock" are "superblocks" for modeling an average fuel pin, an average internal blanket pin and an average radial blanket pin. Each pin model includes three axial sections to provide an approximation of the axial power and temperature profiles in the core. Each fuel pin axial section consists of two fuel nodes and one clad/sodium node. The internal blanket and radial blanket axial sections each consist of one fuel node and one clad/sodium node. Thermal resistances are initially adjusted to match node temperatures with the ARIES-P systems code node temperatures. Material properties are assumed constant.

Upper Plenum Model

A single sodium mixing time constant is assumed and a single node representing upper plenum metal mass in contact with the upper plenum sodium is used. The sodium mixing time constant varies inversely with sodium flow. A description of the individual models comprising the detailed primary loop model follows:

IHX Model

One of the two parallel IHXs is modeled. It is assumed for the events under consideration that the sodium flow distribution between the IHXs remains constant. Five axial nodes are used, with the tube wall sodium thermal capacity apportioned equally between the primary and intermediate sodium nodes. Heat flow between the shell side primary sodium nodes and the tube side intermediate nodes is calculated to be from the primary side node outlets to the intermediate side node outlets. This provides a very stable internal temperature profile even with widely unbalanced primary and intermediate flows.

Primary Cold Leg Model

This region is modeled as four series mixing nodes, representing the distinct flow regions between the IHX and the inlet to the reactor core. The first node represents the sodium in the cold pool region and the second represents the sodium in the pump inlet plenum. The sodium then flows through the four parallel sodium pumps and into the third node which represents the sodium volume in the pump outlet piping. The reactor inlet plenum is modeled by the fourth mixing node. The sodium mixing time constants again vary inversely with primary sodium flow rate.

Reactivity Calculational Models

Calculations to determine the reactivity effects caused by doppler, axial expansion, core gridplate radial expansion, sodium expansion, and control rod motion caused by both the control rod drive and by thermal expansion and contraction are included in the model. The bowing reactivity was not modeled in this initial study. The magnitude of the bowing reactivity is less than the other reactivity terms for changes between steady state power levels, but a first order bowing model will be added to the detailed primary system model for future analyses. The doppler reactivity feedback model includes the usual non-linear logarithmic relation of reactivity change with the variation of temperature in the six fuel pin fuel nodes and the three nodes of the inner blanket assembly model and the three nodes in the radial blanket assembly model. The core axial fuel expansion reactivity is computed as a linear change with fuel and blanket assembly fuel temperatures. The core axial clad and structure expansion reactivity varies linearly with the corresponding sodium node temperature changes. The core radial gridplate expansion reactivity is calculated as a linear function of the core inlet sodium temperature. The sodium expansion reactivity (a positive feedback) is calculated as a linear function of the change in the fuel and blanket assembly sodium temperatures. Reactivity changes initiated by Plant Control System insertion or withdrawal of the control rods is modeled as a sine squared function of the banked control rod insertion. Control rod insertion and withdrawal due to thermal expansion effects are modeled by approximating changes in the relative expansion of the control rod driveline and the vessel wall. Most of the driveline length is assumed to lag the upper plenum sodium temperature changes by a first order time lag. The vessel wall expansion and a portion of the driveline expansion is approximated by a first order time lag relative to the core inlet sodium temperature. The control rod thermal expansion reactivity effect is then computed as a non-linear (sine squared) function of the relative driveline and vessel wall expansion.

Reactor Kinetics Model and Reactor Power Calculations

The net excess reactivity calculated by the reactivity models is input to the reactor kinetics model which uses conventional point reactor kinetics with six delayed neutron precursor groups to determine the instantaneous fission rate and heat generation rate due to fissioning. An assumed constant decay heat is added to provide a calculation of the total heat generation.

Control Rod Model

The Plant Control System provides a velocity demand as input to the control rod model. The velocity demand is limited by the local rod controller to a maximum nine inches per minute insertion or withdrawal rate. The six control rods are moved together as a bank.

Reactor Instrumentation Models

The thermocouples measuring the core exit sodium temperature and the IHX primary sodium inlet temperature are modeled as a first order lag with a time constant of 10 seconds. The reactor flux instrumentation is assumed to have a 0.5 second measurement time constant.

1.2 MATRIX-χ Model Linearization and Regulator Design

The plant model described in Section 1.1 has four external inputs. These inputs are 1) the primary sodium flow rate, 2) the intermediate sodium flowrate, 3) the intermediate cold leg sodium temperature into the IHX, and 4) the demanded control rod velocity. The first three inputs were held constant for the control analysis performed. This was because PRISM operation maintains the primary and intermediate sodium flows constant during power range operation. Without varying sodium flows, the use of IHTS cold leg sodium temperature variation to control reactor power is an extremely sluggish method of control. Without changes in SGS equipment, its range of controllability is also limited for a saturated cycle plant. Therefore only the demanded control rod velocity was used for this study.

The fifty-one plant model state variables described in Section 1.1 are all assumed available for measurement in the best case assumption of full state availability. The states which are actually instrumented are 1) the core exit sodium temperature, 2) the IHX primary sodium inlet temperature, 3) the primary sodium pump discharge temperatures, 4) the reactor flux, and 5) the control rod positions.

The first step in linearization of the primary system model was to perturb the system equations about initial conditions. The plant model was linearized about the initial 100% plant power conditions using the linearization command LIN(DELTA) of the SYSTEM BUILD feature of MATRIX-χ. DELTA is the perturbation applied to each variable in the perturbation process. Values of 0.001 and 0.005 were used with no apparent change in the calculated coefficients.

The first step in the linear, quadratic optimal controller design is to select the weighting matrices which provide weights on the states (x) and controls (u). Quadratic cost functions were used. The selection of the weighting matrices is generally an iterative process, where controller responses to given weightings are examined before converging on the final weightings selected.

The MATRIX-χ command used in this design step was the REGULATOR command. This computes the optimal constant gain state feedback matrices for continuous time systems under the assumption of full state feedback. The usual procedure of specifying diagonal weighting matrices was used. The weighting terms selected were as follows:

STATE VARIABLE	WEIGHTING
State Weighting Matrix:	
Core Exit Temperature Error, Degrees F	1.E-13

STATE VARIABLE	WEIGHTING
Integrated Core Exit Temp. Error, Degrees F-Sec.	1.E-14
Reactor Flux, Normalized	1.E-7
Control Weighting Matrix (Scalar Value):	
Control Rod Velocity Demand, Pct. Stroke/Second	1.E-10

The resulting state variable feedback terms for the measurable variables (outputs), as calculated by the REGULATOR command were:

STATE VARIABLE	FEEDBACK GAIN	
Core Exit Temperature Error, Degrees F	0.0821	(K1)
Integrated Core Exit Temp. Error, Degrees F	0.010	(K2)
Reactor Flux, Normalized	-11.68	(K3)
Control Rod Position, Inches	7.42	(K4)

The implementation of these gains into the reactivity controller is shown in Figure 1.2. Since a one percent power perturbation changes core outlet temperatures approximately three degrees Fahrenheit, the effective reactivity controller weighting for temperature is greater than for power. This avoids a design which might tend to defeat the reactor module inherency effects. The MATRIX-X command SIM was used to iteratively simulate system response with the selected controller gains by using the nonlinear primary system model.

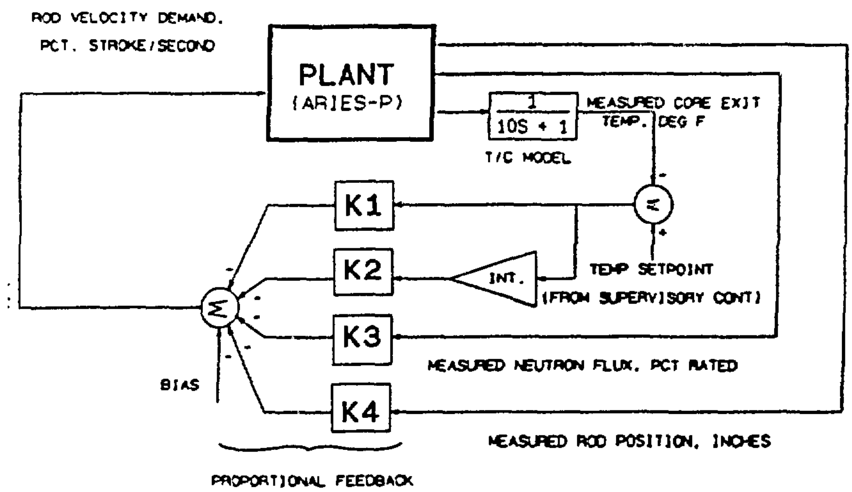

Figure 1.2 - PRISM MULTIVARIABLE REACTIVITY CONTROLLER

Feedback terms from the non-measured states were not used for this initial study. This permitted closed loop eigenvectors to shift from their full state feedback positions. An approach will be later tried which selects K1 through K4 to fix four key eigenvectors at their full state feedback positions. Later studies will include development of a reduced order plant model (while retaining the key system characteristics), use of a filter/observer to provide estimates of the unmeasured states, and incorporation of the estimator into the controller. The relative performance of reactivity controllers developed using these various methods will be compared.

1.3 Reactivity Controller Simulation Results

The response of the reactivity controller during normal controlled plant operation and during plant upset events was simulated using the non-linear full plant analysis code ARIES-P. Proportional-Integral (PI) controllers are used in the remainder of the plant. Eight plant transient events were simulated to test the reactivity controller, examining plant operation with unbalanced reactor module power levels.

These events included ramp changes in plant power demand during turbine leading operation, single module scram, single module fast runback following IHTS sodium pump trip and recirculation pump trip, trip of a main feedwater pump and turbine trip.

The initial multivariable design of the PRISM reactivity controller resulted in a controller which closely tracks the input primary core exit temperature demand from the supervisory controller. A slighcly underdamped response was chosen to provide enough flux overshoot on limiting cases to provide early compensation for the energy changes in the system during rapid step demands in steam flow during turbine leading operation. Figure 1.3 shows the controller response compared to an earlier PI design for an equivalent +10 percent change in plant load in 30 seconds. The controllers' responses are similar for the first 30 seconds The updated controller has a somewhat greater overshoot but approaches the desired final power level more quickly than the previous PI controller. The quadratic design criteria used in the updated controller (integral of error squared) accepts a larger overshoot for a short period of time in order to more quickly reach the final desired setpoint. By 200 seconds, the updated controller is closely tracking the controller setpoint, while the PI controller is approaching the final desired power level much more slowly.

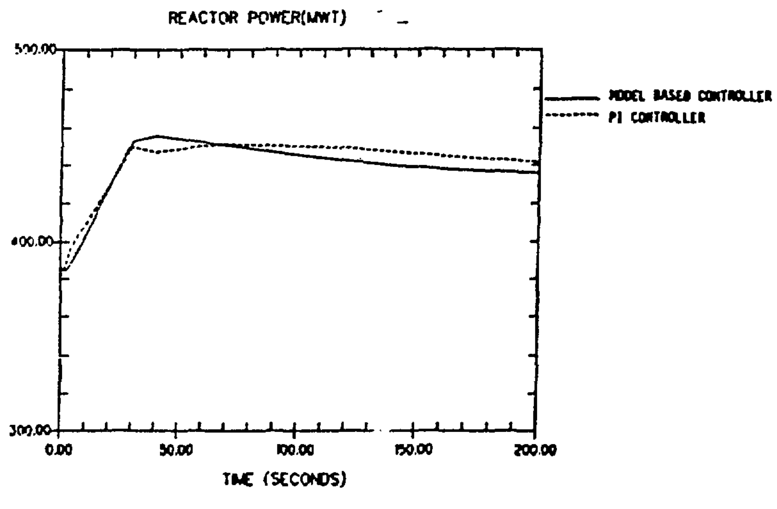

Figure 1.3 - TURBINE LEADING POWER INCREASE FROM 80% POWER TO 83.3% POWER WITH TWO MODULES REMAINING AT 80% POWER

2.0 Feedwater Controller

The feedwater controller maintains a constant drum level in the steam generator by throttling the feedwater control valve. Three steam generators in a power block are connected to a common feedwater system. The three steam generators affect one another through the connections at the steam and feedwater headers. The major challenge is to design a controller which can handle a trip or rapid runback in one module in a power block without affecting the remaining modules.

2.1 Steam Generator Model Description

The model for the PRISM steam generator and feedwater flow network is an arrangement of Modular Modeling System (MMS) modules (Reference 2). The MMS provides a simple means for setting up a model of the steam/water side of the steam generator and the feedwater network. The MMS model can be readily linearized for use with the $Matrix_X$ control analysis tools.

The steam generator and feedwater flow network in a power block are shown in Figure 2.1. The model begins at the feedwater pump. The PRISM design for a power block has two, constant-speed, feedwater pumps in parallel. In this study, the two pumps are simulated by a single equivalent pump module. The pressure losses in the high pressure feedwater heater and the heat addition are modeled by an MMS pipe model. The feedwater flow divides at a feedwater header going to the three steam generators in the power block each through its own control valve. The header is modeled by an MMS divider module and is connected to three feedwater valves.

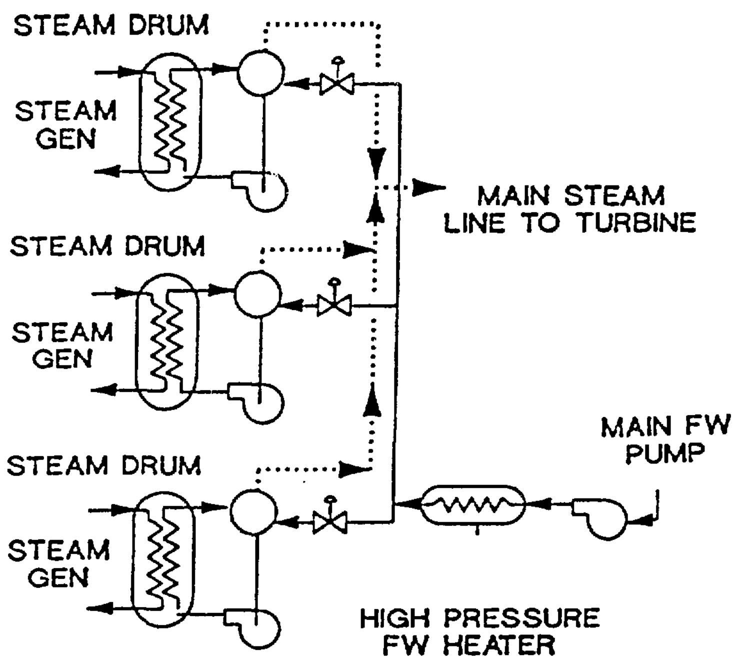

Figure 2.1. Schematic of feedwater control model.

To understand the coupling effects between multiple steam generators, consider a scenario for increasing flow to one steam generator. To increase the flow, the feedwater valve opens. The pressure at the header decreases because of increased line losses and because the feedwater pump's head decreases as flow increases. If the remaining feedwater valves are held constant, the decrease in header pressure results in a decrease in flow to the remaining steam generators. The magnitudes of resistances which determine the coupling effect are somewhat arbitrary at this point in the PRISM design. However, for point of reference, the current study shows that a 10% increase in one feedwater valve position causes a 10.6% increase in that steam generator flow and 4.7% decrease in the remaining steam generators.

The steam generator in this PRISM design is a forced circulation drum boiler which produces saturated steam. The steam generator is modeled using a combination of MMS modules: a saturated flash tank models the drum, pipes are used for the downcomer and riser, and a pump module simulates the recirculation pump. The sodium side of the steam generator is not modeled. The heat transfer into the riser is approximated using the wall heat conduction feature of the MMS pipe module. The heat transfer coefficient in the wall heat conduction equation is an input which is set so that the heat transfer rate in the steam generator matches the heat balance specification. The surface temperature of the pipe wall is an input parameter which represents the average sodium temperature. The surface temperature can be used as a time-varying boundary condition to drive the steam generator and its controller. The feedwater flow enters the drum, and steam and blowdown flow exit the drum.

The system model has a total of 29 state variables. Of these, each steam generator has eight. The remaining five are in the feedwater network.

2.2 Design Procedure for Feedwater Controller

The design procedure for the feedwater controller uses an innovative approach which combines a state feedback controller with a feedforward controller.

The feedforward controller is able to compensate for the coupling effects of the multi-modular system. The major benefit to this approach is to divide one large multivariate control system for the power block into three smaller systems by using the feedforward terms to account for the generator-to-generator coupling effects. The feedback portion of the controller uses the linear quadratic regulator and optimal state estimator methods to calculate the compensator matrices. The feedforward portion uses a method described by Friedland in Reference 3 in which a linear, algebraic formula calculates the appropriate gains for the feedforward signals. The formula for the feedforward gains is given later in this section.

The feedforward signals consist of all the external disturbances and reference inputs acting on a single steam generator. There are three categories of feedforward signals. These include the coupling terms such as the flow to the other steam generators, the time-varying boundary conditions such as sodium temperature, and reference inputs such as the level setpoint.

Collectively, the feedforward signals are called the "exogenous" inputs to the subsystem. An application of this approach is also given in Reference 4.

The linearized state space model including the exogenous inputs has the form,

$$\dot{x} = Ax + Bu + Ex_o$$

$$y = Cx + Du + Fx_o \ ,$$

where x represents the state of the feedwater system, u represents the controls, y represents the observable or measurable quantities, and x_o represents the exogenous inputs. The matrices A through F represent the linearized coefficient matrices for the process model. Lists of the variables composing the x, y, and u vectors are given in Table 2.1. As is customary, the integral of level error is included as a state variable. Including this state guarantees that the level error is zero at steady state. By choice of the designer, the exogenous inputs are all measurable or known quantities which may be used directly in the control law. There is no need to design an estimator for the exogenous inputs.

Table 2.1. State space variables for feedwater control model

State Variables - x	Observables - y
Mixture enthalpy leaving riser	Drum level
Integral of level error	Drum pressure
Pressure at FW pump discharge	Integral of level error
Pressure at FW header	
Pressure at recirc. pump suction	**Exogenous Inputs - x_o**
Pressure at steam drum	
Downcomer flow	Steam flow
Mixture enthalpy of steam/water in drum	Blowdown flow
Metal temperature in SG tube	Feedwater flows to other SG's in in power block (2 values)
	Pressure at feedwater pump suction
Controllable - u	Sodium average temperature
	Feedwater temperature
FW valve demand	Drum level setpoint

The linearized system matrices can be calculated numerically from the MMS simulation model using a built-in function, ANALYZ, of the ACSL simulation language. The linearized system matrices are calculated by the perturbation method. Each input variable in the x, u, and x_o vectors are individually perturbed by a small amount. The coefficients are calculated as the ratio of the change in the derivatives, x, or in the observable variables, y, to the change in the input variables (u, x, or x_o).

The states of the model are not directly observable so the design procedure includes both an estimator and a regulator design. By the separation theorem (Reference 2), the design calculations for the observer and the regulator may be performed entirely separately. Let us first consider the regulator design.

The design procedure using state feedback plus exogenous inputs results in a control law for the system involving two terms.

$$u = Gx + G_o x_o \ .$$

The first term in the control law is the state variable feedback. The second is the feedforward term. The calculations of G and G_o are carried out by matrix-algebraic manipulations using $Matrix_X$. The linear quadratic regulator method to calculate G solves the algebraic Riccati equation which optimizes a quadratic performance index. This calculation is completely automatic in $Matrix_X$. The inputs to the calculation consist of the A and B components of state space matrices and weight matrices for the performance index. The weight matrices are the "knobs" or tuning parameters for the linear-quadratic method. For the level controller design, the weight matrices are tuned by identifying the eigenvalues associated with level and pressure dynamics and adjusting the weights to put these eigenvalues in the range from -0.001 to -0.1 $seconds^{-1}$. These eigenvalues give moderate to slow response time for the state feedback on level. Faster response requires a larger force on the system which is not possible because the limit of the control valve travel.

A formula for the feedforward gain that is consistent with the linear quadratic regulator given above is given by Friedland as the following,

$$G_o = -R^{-1}B'(A - BG)^{-1}ME \ ,$$

where R is the weight matrix of the controls vector, A, B and E are components of the state space matrices defined previously, G is the state feedback matrix, and M is the solution matrix for the algebraic Riccati equation which is obtained while solving for G.

The final step in the design is the estimator problem. This design uses a full state observer. The optimal observer calculation entails finding an observer gain, K that minimizes another quadratic performance index. Again, the calculation is fully automated by $Matrix_X$. The inputs are the A and C components of the state space system and noise covariance matrices. The resulting differential equation for the estimator state vector, $\hat{x}$, is the following,

$$\dot{\hat{x}} = A\hat{x} + Bu + Ex_o + K(y - c\hat{x}) \ .$$

By substituting in the control law for the u vector and simplifying, the following equation for the observer is obtained.

$$\dot{\hat{x}} = (A - B_cG - KC)\hat{x} + Ky + (E - BG_o)x_o ,$$

where $B_c = B - KD$.

The controller equation based on the estimator simply uses $\hat{x}$ in place of x.

$$u = G\hat{x} + G_o x_o .$$

The estimator and controller equations are readily programmed for use with the non-linear MMS simulation. The solution of the differential equations for the estimator are solved continuously in the simulation using the same, continuous numerical integration methods used for the process simulation.

2.3 Simulation Results

A simulation of the level controller installed on the original non-linear MMS simulation model permits the control design to be tested in a series of transients to determine that the control design meets the transient performance requirements. The simulation of the controller also verifies that assumptions and approximations such as linearity are applicable.

In a multi-modular plant, it is imperative that the loss of one module does not cause the remaining modules to trip. Since the steam generators are the most closely coupled components in the power block, the control of steam generator drum level following the loss of one module is a key transient for measuring the control system performance. A number of initiating events result in the loss of one module. Of these, the most abrupt drop in heat flow to the steam generator is caused by the loss of the intermediate loop pump. Although the model cannot simulate the sodium side transient exactly, a bounding transient can be easily set up. In our steam generator model, the sodium temperature is a time-varying boundary condition. A step-change transient which changes the sodium temperature from its starting value to a value near the steam temperature is effectively the same as the loss of the intermediate pump. In fact, the loss in heat source is somewhat more severe since the step-change neglects both the intermediate flow coast down and the stored thermal energy of the sodium.

The coupling effect among the steam generators and the response of the feedwater controller can be described as follows. The decrease in

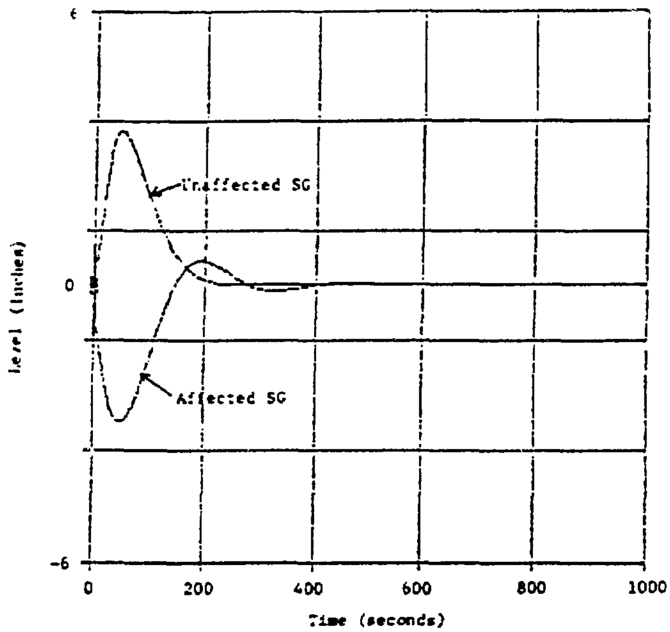

Figure 2.2. Level response to a step change in sodium temperature to one steam generator in a power block.

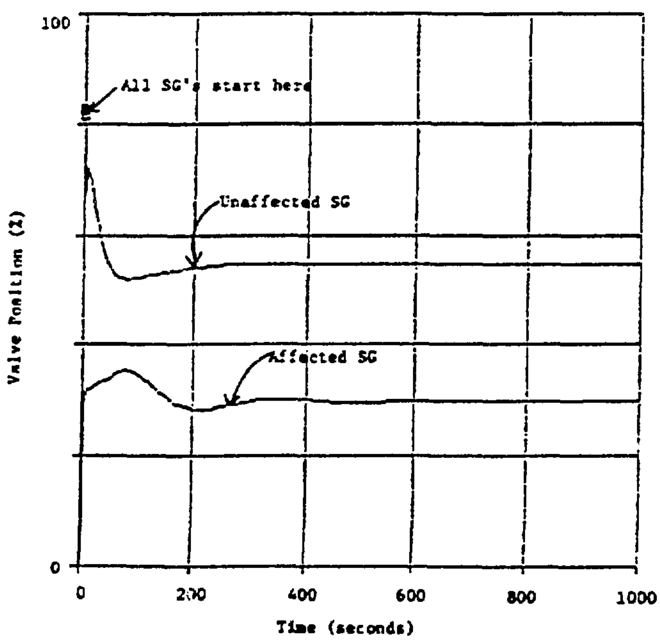

Figure 2.3. Feedwater control valve response to a step change in sodium temperature to one steam generator in a power block.

heat flow to the affected steam generator results in a drop in steam production and steam pressure. Without control action, the drop in pressure would cause an increase in feedwater flow to the affected steam generator and decrease to the unaffected steam generators. The feedwater control action must reverse the natural response. The controller of the affected steam generator must decrease feedwater flow an amount corresponding to the drop in steam production. The controllers of the unaffected steam generators should respond to the changing flow and pressure at the feedwater header to keep their feedwater flows constant.

The drum level and feedwater control valve position responses to this transient are shown in Figures 2.2 and 2.3. The feedforward signal for sodium temperature causes the feedwater valve in the affected steam generator to close completely after the step change. Clearly, the feedforward effect goes in the proper direction, but it is only approximate in magnitude. In this case, it over-compensates for the loss of heat flow and closes the valve completely. The drum level drops about four inches. The state variable feedback terms for level then respond slowly by opening the valve and bringing the level back to the setpoint. In the unaffected steam generators, the prompt response of the level controller is to compensate through the feedforward term for feedwater flow to the affected steam generator. Since the flow to the affected steam generator goes down, this feedforward signal causes the feedwater valves in the unaffected steam generators to close in anticipation of a rise in feedwater header pressure. Again, the feedforward effect is in the proper direction, but the amount of the feedforward compensation slightly underestimates the required valve response. A rise in level of about four inches results. The state variable feedback terms return the level to the setpoint.

In all respects, the response of the controller meets the desired performance. The maximum deviations in level are acceptable. The control is smooth and compensates correctly for the coupling effects among the steam generators in a power block. Also, the control in the affected steam generator remains stable even though the final operating condition is far from the point at which the plant model was linearized. This is an indication that the control system is robust to changes in the operating point.

The only point of concern in the control is that the feedwater valve in the affected steam generator closes fully at the outset of the test transient. The saturation is very short and does not affect this transient. However, the concern is that the feedwater controller (in its present form) does not prevent windup when the forward element (the feedwater valve in this case) saturates.

3.0 Summary and Conclusions

The modern control theory provides many powerful calculation tools to design control systems which perform better than conventional control designs and which do not require costly trial and error tuning in the field. The challenge to designers of controls for advanced reactors is to find the most practical way to apply the broad general theory of modern control to specific control problems. The question is not whether modern control design applies to advanced reactor design but how to apply the concept. The designer must select the best calculational method for a control design, simplify it down to the minimum, and demonstrate through simulation that the controller works.

This paper presents studies on two controllers in the advanced liquid metal reactor which have been developed recently, the reactivity controller and the feedwater controller. The design procedure included development of simulation models, adaptation of special control design techniques to address the specific control problems, and testing through simulation of the controller performance.

The first of the controllers, the reactivity controller, developed a 51 state model with full state feedback and then showed that the controller could be reduced to a much simpler controller using only four directly measured states for feedback. The simplification was very successful. Although the system eigenvalues were shifted slightly (i.e. non-optimum), the performance of the simplified control system was still slightly better than the conventional proportional-integral controller.

The second controller study involving the steam generator level controller showed that a special technique in modern control theory, the exogenous variables technique, can be used to compensate for coupling effects in the multi-modular reactor design. Again, the simulation tests were very successful. The control system handled a relatively severe transient in one steam generator and at the same time did not upset the remaining steam generators in a power block.

The major directions for future control design activities for the PRISM plant include additional testing and further simplification of the designs presented here and extension to other controllers in the hierarchy of the plant control system. The studies presented in this paper demonstrate that multivariate control design for the PRISM reactor is producing benefits in sound control designs and reduced engineering and computer time for those designs.

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