

RAMONA-4B

A Computer Code With Three-Dimensional Neutron Kinetics for BWR and SBWR System Transient

Models and Correlations

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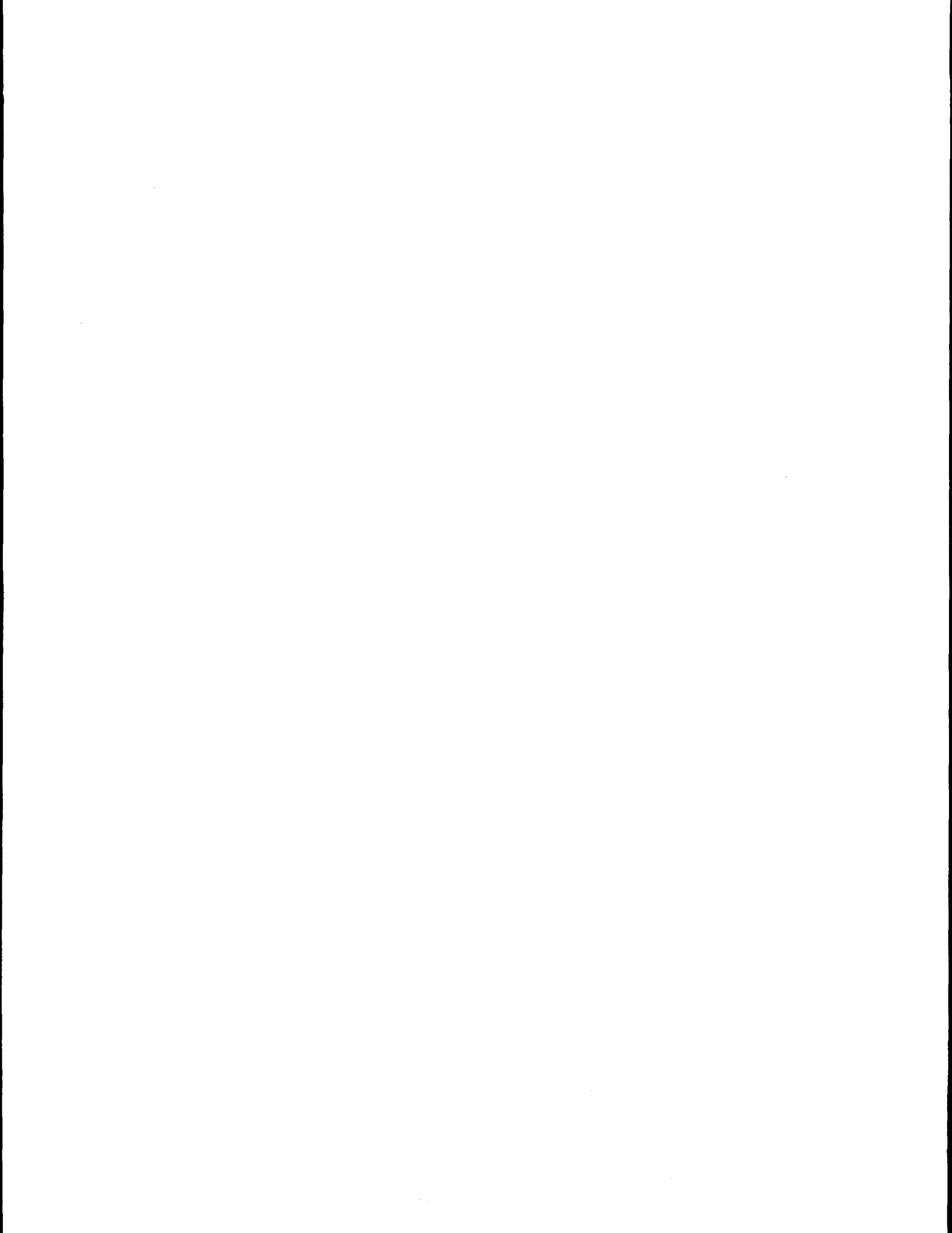
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ABSTRACT

This document describes the major modifications and improvements made to the modeling of the RAMONA-3B/MOD0 code since 1981, when the code description and assessment report [Wulff et al., 1984a] was completed. The new version of the code is RAMONA-4B.

RAMONA-4B is a systems transient code for application to different versions of Boiling Water Reactors (BWR) such as the current BWR, the Advanced Boiling Water Reactor (ABWR), and the Simplified Boiling Water Reactor (SBWR). This code uses a three-dimensional neutron kinetics model coupled with a multichannel, non-equilibrium, drift-flux, two-phase flow formulation of the thermal hydraulics of the reactor vessel. The code is designed to analyze a wide spectrum of BWR core and system transients and instability issues.

Chapter 1 is an overview of the code's capabilities and limitations; Chapter 2 discusses the neutron kinetics modeling and the implementation of reactivity edits. Chapter 3 is an overview of the heat conduction calculations. Chapter 4 presents modifications to the thermal-hydraulics model of the vessel, recirculation loop, steam separators, boron transport, and SBWR specific components. Chapter 5 describes modeling of the plant control and safety systems. Chapter 6 presents the modeling of Balance of Plant (BOP). Chapter 7 describes the mechanistic containment model in the code. The content of this report is complementary to the RAMONA-3B code description and assessment document [Wulff et al., 1984a].



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EXECUTIVE SUMMARY

The RAMONA-4B code is the latest version of RAMONA codes which is designed to analyze a wide spectrum of core and system transients for Boiling Water Reactors (BWR). The code has a three-dimensional neutron kinetics model coupled with a multichannel, non-equilibrium, drift-flux, two-phase flow formulation of the thermal hydraulics of the reactor vessel.

Since 1979, when the RAMONA-III code was acquired by the US Nuclear Regulatory Commission from Scandpower, Norway, numerous modifications and improvements to the code were made at Brookhaven National Laboratory (BNL). The last documented version of the code was RAMONA-3B/MOD0 [Wulff et al., 1984a]. Several new models were developed and implemented into the code, and existing models were modified. RAMONA-4B has the following new models incorporated since RAMONA-3B/MOD0:

- post-CHF heat transfer and vapor generation,
- heat structure,
- drift-flux model for the thermal-hydraulic calculation of the vessel,
- wall friction factor calculation including the laminar-flow regime,
- new two-phase multiplier for the pressure form losses,
- recirculation-pump model based on homologous curves,
- steam condensation on feedwater and/or safety injection water,
- system pressure regulator,
- BWR specific feedwater control system,
- Standby Liquid Control System (SLCS),
- new boron transport model,
- isolation condenser,
- balance of plant,
- low-pressure properties, and
- flow-dependent loss coefficients.

An editing procedure for calculating the total core reactivity has been developed and, is available in RAMONA-4B. This procedure provides the code users with an important tool to interpret the total core power history predicted in the transient calculations.

The RAMONA-4B code can be used for simulating the following types of plant transients:

- Transients with reactivity changes,
- Transients with system pressure changes,
- Transients with coolant inventory or mass flow rate change,
- Transients with coolant temperature changes.

RAMONA-4B has been used to simulate a combination of transients, which initiated instabilities. RAMONA-4B has limitations. It cannot be used to model large break loss-of-coolant accidents.

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NOMENCLATURE

SYMBOL	DESCRIPTION
A	Flow area
$C_{p,l}$	Liquid specific heat
C_0	Zuber-Findley void distribution parameter
C_{11}, C_{12}, C_{13}	Coefficients in vapor generation model, Equations (6.4), (6.5), and (6.6)
D	Diameter of jet, Chapter 4.4.4.2
D_b	Core barrel outer diameter, Chapter 4.4.4.2
d_{rod}	Diameter of a fuel rod
D_1, D_2	Diffusion coefficients for fast and thermal neutrons, respectively
E	Error signal, Chapter 5.2
e	Internal energy
F	Normalization integral, Equation (2.6)
f	Control rod fraction, Table 8.1
f_l	Wall friction factor
G	Mass flux
g	Gravitational acceleration
H, H_R	Pump head and rated head, respectively, Chapter 4.5
h	Heat transfer coefficient; enthalpy; time step, Chapter 2; relative pump head, Chapter 4.5
h_{fg}	Latent heat of evaporation
j	Volumetric flux
K_{eff}	Core Effective Multiplication Factor, Chapter 2

NOMENCLATURE (Cont'd)

SYMBOL	DESCRIPTION
k_v	Vapor heat conductivity
$k_{2-\phi}$	Two-phase friction multiplier, Chapter 4.4.1
L	Length of jet, Chapter 4.4.4.2
M	Component momentum, Equation (4.84)
$[M]$	Matrix of the fuel and core properties
M_g, M_l	Total vapor and liquid masses in the vessel, respectively
N_{rod}	Number of fuel rods in a computational cell
$p, \dot{p}$	Pressure and pressure time derivative, respectively
q_t'''	Volumetric rate of energy deposition into liquid
q_t'	Linear heating rate for liquid in a channel
Q_{wall}	Volumetric energy transfer rate into a cell from a heated wall
Re	Reynolds number
S	Trimmed signal in the feedwater control system, Chapter 5.2
St	Stanton number
T	Temperature; transient amplitude function, Equation (2.1)
$T_{m,o}, T_{f,o}$	Reference moderator and fuel temperatures ($^{\circ}F$), respectively, Table 8.1
T_r	Input signal to the feedwater flow regulator, Chapter 5.2
T_s, T_l	Saturation and liquid temperatures, respectively
u	Internal energy
V	Volume

NOMENCLATURE (Cont'd)

SYMBOL	DESCRIPTION
v_g, v_l	Vapor and liquid phasic velocities, respectively
v_{gj}	Void-fraction-weighted, area-averaged vapor drift velocity
W	Volumetric flow rate
w	Mass flow rate
z	"Non-boiling" length, Chapter 6.2

GENERAL (GREEK)

SYMBOL	DESCRIPTION
α	Void fraction; relative pump speed, Chapter 4.5
β	Total delayed neutron fraction, empirical constant for two-phase form loss
β_i	Neutron fraction from i-th delayed precursor group
Γ_{ph}	Bulk vapor generation rate, Equation (4.116)
Γ_v	Vapor generation rate per unit volume
Γ_{wall}	Heated wall vapor generation rate, Equation (4.117)
δ	Thickness of an annular layer of vapor, Equation (4.66)
ϵ_i	Effective precursor concentration for the i-th delayed neutron group
θ	Thermal-to-fast neutron current ratios
Λ	Prompt neutron life-time
λ_{elf}	Extrapolation length
λ_i	Decay constant for i-th group of delayed neutron precursors
ν	Number of neutrons per fission, Chapter 2; relative pump flow rate, Chapter 4.5; kinematic viscosity
δ_{sub}	Subcooling parameter, Equation (6.1)
ρ	Total reactivity, Chapter 2
ρ_D	Doppler reactivity
ρ_l, ρ_f, ρ_g	Liquid, saturated liquid, and vapor densities, respectively
ρ_s	Scram reactivity
ρ_{Ti}	Moderator temperature reactivity
ρ_v	Void reactivity

GENERAL (GREEK) (Cont'd.)

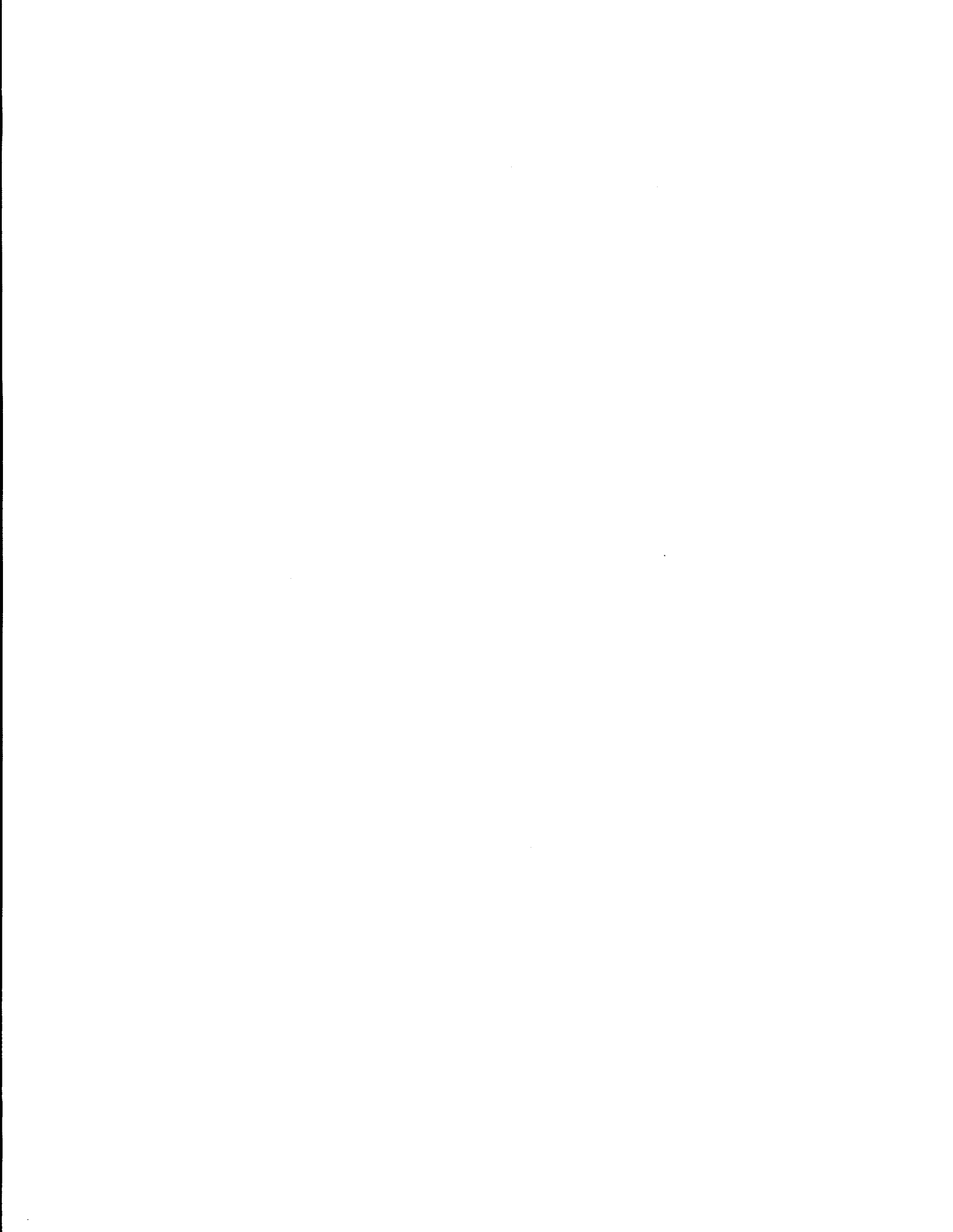
SYMBOL	DESCRIPTION
Σ	Macroscopic cross section
Σ_{a1}, Σ_{a2}	Macroscopic absorption cross section for fast and thermal neutrons, respectively
Σ_{f1}, Σ_{f2}	Macroscopic fission cross section for fast and thermal neutrons, respectively
Σ_{r1}, Σ_{r2}	Macroscopic removal cross section for fast and thermal neutrons, respectively
σ	Surface tension coefficient
T	Torque, Chapter 4.5; time constant, Chapter 5.2
T_{fw}	Feedwater Temperature, time constant of the feedwater flow rate measuring system, Chapter 5.2
T_{stm}	Time constant of the steamline flow rate measuring system, Chapter 5.2
Φ	Neutron flux
Φ^*	Adjoint flux
Ψ_{tot}	Total vessel vapor generation rate

SUPERSCRIPTS

SYMBOL	DESCRIPTION
<i>ann</i>	Annular
<i>ch- t</i>	Churn-Turbulent
<i>DC</i>	Downcomer
<i>in</i>	Inlet
<i>j</i>	Index of a hydraulic channel, Chapter 4
<i>R</i>	Riser
<i>CHF</i>	Critical Heat Flux
<i>CU</i>	Carry-Under
<i>el</i>	Electric, Chapter 4.5
<i>f</i>	Film, Chapter 4.4.4.2
<i>FB</i>	Film Boiling
<i>fr</i>	Friction, Chapter 4.5
<i>fw</i>	Feedwater
<i>g</i>	Vapor
<i>hyd</i>	Hydraulic, Chapter 4.5
<i>in</i>	Inlet
<i>int</i>	Interpolation
<i>j</i>	Index of a hydraulic channel; jet, Chapter 4.4.4.2
<i>k</i>	Cell index
<i>LP</i>	Lower Plenum

SUPERSCRIPTS (Cont'd.)

SYMBOL	DESCRIPTION
<i>l</i>	Liquid
<i>m</i>	Mixture
<i>out</i>	Outlet
<i>PSP</i>	Pressure Suppression Pool
<i>R</i>	Riser; rated, Chapter 4.5; requested, Chapter 5.2
<i>RHR</i>	Residual Heat Removal
<i>sat</i>	Saturation
<i>stm</i>	Steam line, Chapter 5.2
<i>sub</i>	Subcooling
<i>w</i>	Wall
<i>O</i>	Core inlet, Chapter 4



ABBREVIATIONS

SYMBOL	DESCRIPTION
ATWS	Anticipated Transient Without Scram
BOP	Balance of Plant
CHF	Critical Heat Flux
dlt	Dilatation
ECC	Emergency Core Cooling
HT Regime	Heat-Transfer Regime
LPE	Lower-Plenum Exit
MSIV	Main Steam Isolation Valve
NPC	Number of Parallel Channels
PPS	Plant Protection System
PSP	Pressure-Suppression Pool
RHR	Residual-Heat Removal
RI	Riser Inlet
SRV	Safety/Relief Valve
WLC	Water-Level Cell

1. INTRODUCTION

RAMONA-4B is the latest version of RAMONA code, developed for USNRC to include the capability to simulate Simplified Boiling Water Reactor (SBWR). This version of the code includes all the improvements and modifications made to RAMONA-3B/MOD0 since 1981. The current version of the code includes all the components needed to model a Boiling Water Reactor (BWR) [Wulff et al., 1984a]. In addition, it includes models for heat structures, chimney, Isolation Condenser (IC), Standby Liquid Control System (SLCS), recirculation loop dynamics, steam separators, complete control systems, Balance of Plant (BOP), and containment. The code capability in the area of process modeling has also been expanded to include drift-flux formulation, reverse-flow capability, flow-dependent loss coefficients, and low-pressure properties. Furthermore, the hydraulic time step control includes the numerical stability limit calculated from the maximum eigenvalue of the Jacobian matrix in addition to the accuracy limit based on the truncation error.

1.1 Report Outline

This document describes the major modifications and improvements made to the modeling of the RAMONA-3B/MOD0 code since 1981. The content of the manual is complementary to the RAMONA-3B description and assessment report [Wulff et al., 1984a] which describes the models, solution techniques, and the results of the code developmental assessment.

Chapter 1 is an overview of the code's capabilities and limitations and provides a summary of all major modifications and improvements made to the code over the past several years. Chapters 2 through 7 provide some details on the code's principal models and on the modifications and corrections to the neutron kinetics, thermal hydraulics, and plant systems modeling. The layout of the report was selected to cover all the major parts of the code: neutron kinetics, heat conduction, thermal hydraulics, plant protection and control systems, balance of plant, and containment. The size of each section is indicative of the extent of the code modifications in the specific area.

1.2 Code Objectives

The RAMONA-4B code is designed to analyze a wide spectrum of core and system transients for the current Boiling Water Reactor (BWR), the Advanced Boiling Water Reactor (ABWR), and the Simplified Boiling Water Reactor (SBWR). The code uses a three-dimensional neutron kinetics model coupled with a multichannel, nonequilibrium, drift-flux, two-phase-flow formulation of the thermal hydraulics of the reactor vessel.

There are four major classes of BWR transients where the RAMONA-4B code can be effectively applied:

- Reactivity Transients driven by control-rod movements and boron injection,
- System Pressure Transients: Load Rejection, Turbine Trip, MSIV Closure, Pressure Regulator Failure,
- Coolant Inventory Transients: Loss of Feedwater, Inadvertent Opening of SRVs and others,
- Coolant Temperature Transients: Loss of Feedwater Heater, ECC injection, etc.

1. Introduction

The three-dimensional neutron kinetics capability of RAMONA-4B makes the code a unique tool for predicting transients and accidents where the spatial core power variations are expected to be significant. The code can also predict instabilities occurring in the reactor system during operation or at start-up.

1.3 Code General Features

A schematic of the BWR system which can be modeled with RAMONA-4B is shown in Figure 1.1. The system consists of a feedwater sparger, upper and lower downcomers, a combined recirculation loop with recirculation and jet pumps, a lower plenum, a core, a riser, including an upper plenum and steam separators, a steam dome, a steam line with safety/relief valves, main steam isolation valve, turbine bypass valve, and turbine stop valve, balance of plant, and containment. The code also has models for plant protection systems, control systems, safety injection systems, Standby Liquid Control System (SLCS), Isolation Condensers (IC), Balance of Plant (BOP), and containment. A schematic diagram of the SBWR system which can be modeled with RAMONA-4B is shown in Figure 1.2.

The reactor core is modeled by a group of parallel coolant channels and a bypass channel. Each coolant (thermal hydraulic) channel represents one or more fuel channels and is interfaced with one or more fuel bundles. The reactor power, including decay heat, is calculated in a three-dimensional geometry, although a one-dimensional calculation is also possible. The fission power calculation takes into account the feedbacks due to the fuel and coolant temperatures, void fraction, and presence of control rods or boron. Changes in power shape and total reactor power due to the control-rods' movements (including accidental rod drop and scram) and boron injection into any section of the reactor can be calculated. Most of the energy generated by nuclear fission and fission product decay is deposited in the fuel pellets; a user-specified fraction of the total power can be directly deposited in the coolant. Thermal conduction through the fuel pellet, gas gap, and fuel cladding eventually transfers the heat generated in the fuel to the coolant.

The thermal-hydraulic calculation of RAMONA-4B is based on a four-equation, nonequilibrium, drift-flux model. The four balance equations are: conservation of (1) vapor mass, (2) mixture mass, (3) mixture momentum, and (4) mixture energy. Two-phase mixture is allowed in the downcomer, lower plenum, core, and riser. The steam dome (above the two-phase mixture level) is occupied by saturated vapor, whereas the recirculation loop is assumed to contain only single-phase liquid.

In the two-phase mixture regions, **the liquid can be subcooled, saturated, or superheated. However, the vapor is assumed to be at saturation.** Nonequilibrium vapor generation or condensation is calculated using a model described in [Wulff, 1984a]. In addition to the vapor generation model, the constitutive-relations package includes correlations for single and two-phase flow wall friction, two-phase multiplier for loss coefficients, heat transfer correlations for pre- and post-CHF regimes, a critical-heat-flux (CHF) correlation, and a library of thermal properties for the coolant and fuel-rod materials.

Elevations of the mixture and collapsed water levels in the downcomer/steam dome region are calculated. Prediction of the collapsed water level is important since the feedwater and the safety injection systems, HPCI and RCIC, are regulated and activated based on its location. A Standby Liquid Control System (SLCS) for injecting highly concentrated borated water into the reactor vessel also is modeled: the code integrates the boron transport equation to calculate the boron concentration as a function of space and time. The Isolation-Condenser system (IC) specific to SBWR [GE SSAR, 1992] is also modeled.

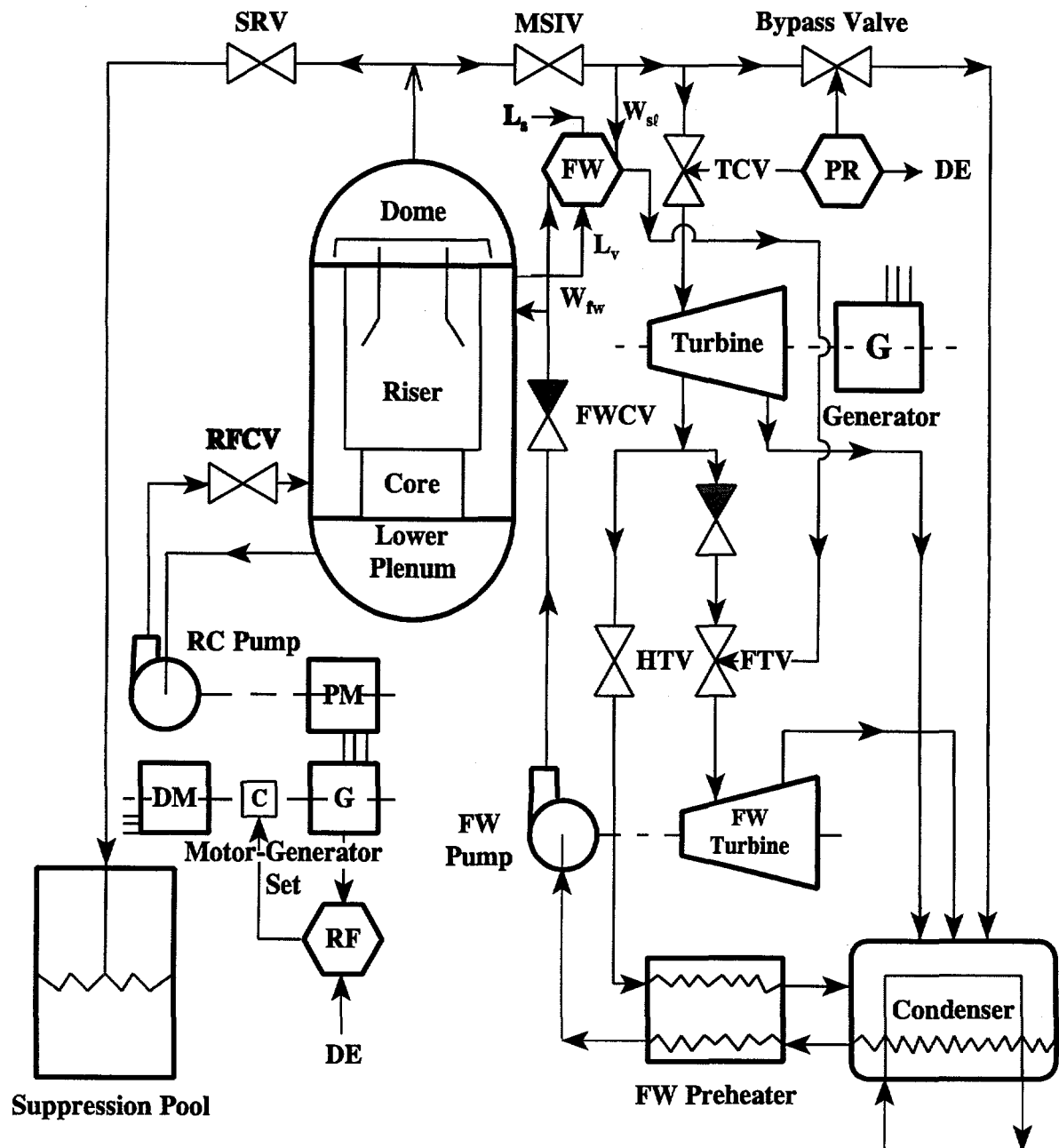
The reactor pressure vessel (RPV) contains a large amount of structures. During normal operation or plant transients with little variation in pressure, the heat structures are in equilibrium with the coolant and do not play an important role.

1. Introduction

However, during startup transients or transients where the coolant pressure varies significantly, the stored energy in the structures becomes important. RAMONA-4B has heat structure models to simulate RPV wall and internal solid structures.

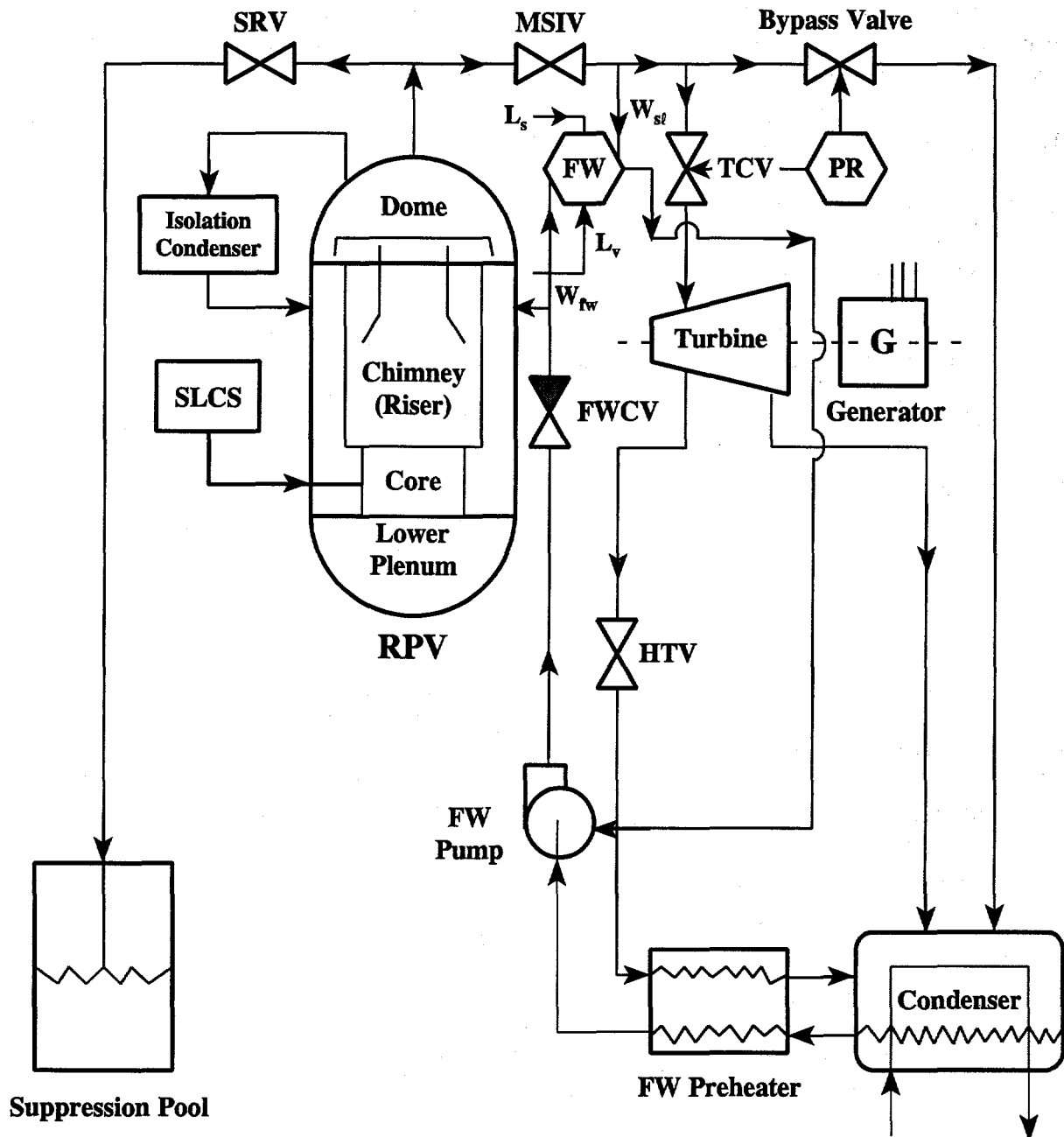
Steam line dynamics between the reactor vessel and turbine is accounted for by solving the vapor mass and momentum conservation equations [Wulff et al., 1984a]. Adiabatic expansion and/or compression of steam is assumed in the steam line model. Models for the safety/relief valves, main steam isolation valve, and turbine bypass and stop valves are included. The code has a model for the pressure regulator. However, the code does not yet have a model for the recirculation flow control.

1. Introduction



RF = Recirculation Flow Controller FW = Feedwater Flow Controller PR = Pressure Regulator

Figure 1.1 Schematic of the BWR System.



FW = Feedwater Flow Controller PR = Pressure Regulator

Figure 1.2 Schematic of the SBWR System with Isolation Condenser.

1. Introduction

A plant protection system (PPS) designed to control the reactor during abnormal transients and accidents also is modeled in RAMONA-4B. This system includes the reactor scram, MSIV closure, turbine and recirculation-pump trips based on transient time (user input) or setpoints for the thermal-hydraulic and neutronic system parameters.

A simplified flowchart of the RAMONA-4B calculational logic is shown in Figure 1.3. Neutron kinetics and thermal hydraulics are the two major parts of the code; heat conduction in the fuel rods link these two parts. Figure 1.4 shows a nodalization diagram of the reactor pressure vessel employed by RAMONA-4B.

1.4 Code Capabilities

As it follows from the previous discussion, RAMONA-4B can be used to calculate the following types of transients:

1. Transients with Reactivity Changes, such as:

- Gradual or sudden withdrawal or insertion of control rods. This includes accidental drop of a single control rod, a cluster of rods, or an arbitrary combination of rods,
- Full or partial reactor scram, or ATWS,
- Boron injection, and
- Transients which induce changes in the moderator density (e.g., void collapse due to pressure rise in the reactor vessel).

2. Transients with System Pressure Changes, such as:

- Electric generator load rejection with fast closure of the turbine control valve,
- Turbine trip with closure of the turbine stop valve,
- Main steam isolation valve closure, and
- Failure of pressure regulator.

3. Transients with Coolant Inventory and/or Mass Flow Rate Changes, such as:

- Faulty opening of safety and/or relief valves,
- Changes in feedwater flow. User-specified feedwater flow rate vs. time,
- Inadvertent actuation of safety injection system, and
- Trip or seizure of both recirculation pumps.

4. **Transients with Coolant Temperature Changes, such as:**

- Loss of feedwater heater. User-specified feedwater temperature vs. time or calculated from BOP.

RAMONA-4B's capability to predict most of the transients listed above has been demonstrated through several calculations: (a) Peach Bottom 2 Turbine Trip Test Simulations, (b) an MSIV Closure Partial-ATWS Calculation, and

1. Introduction

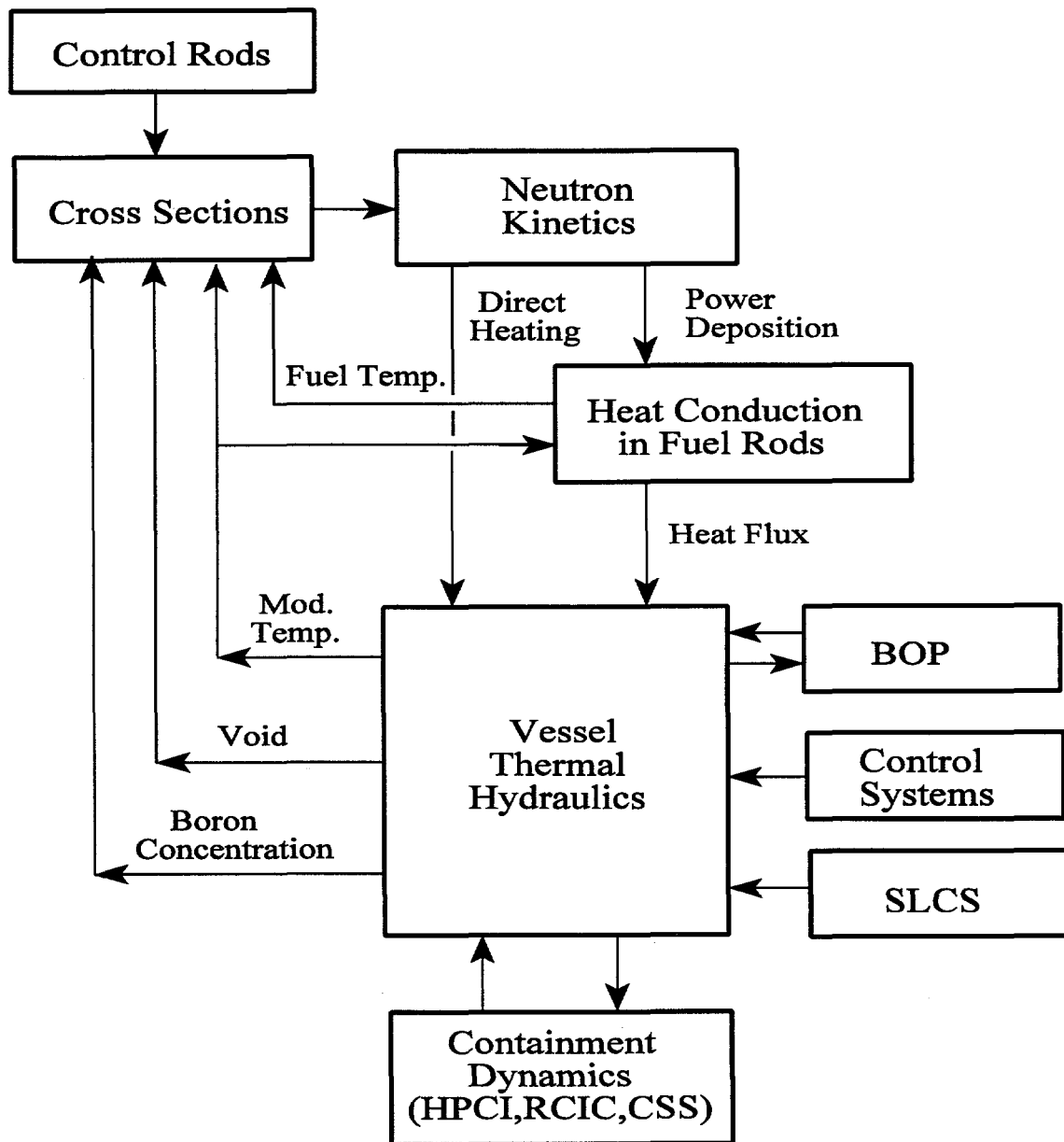


Figure 1.3 Simplified Flowchart of RAMONA-4B Calculational Logics.

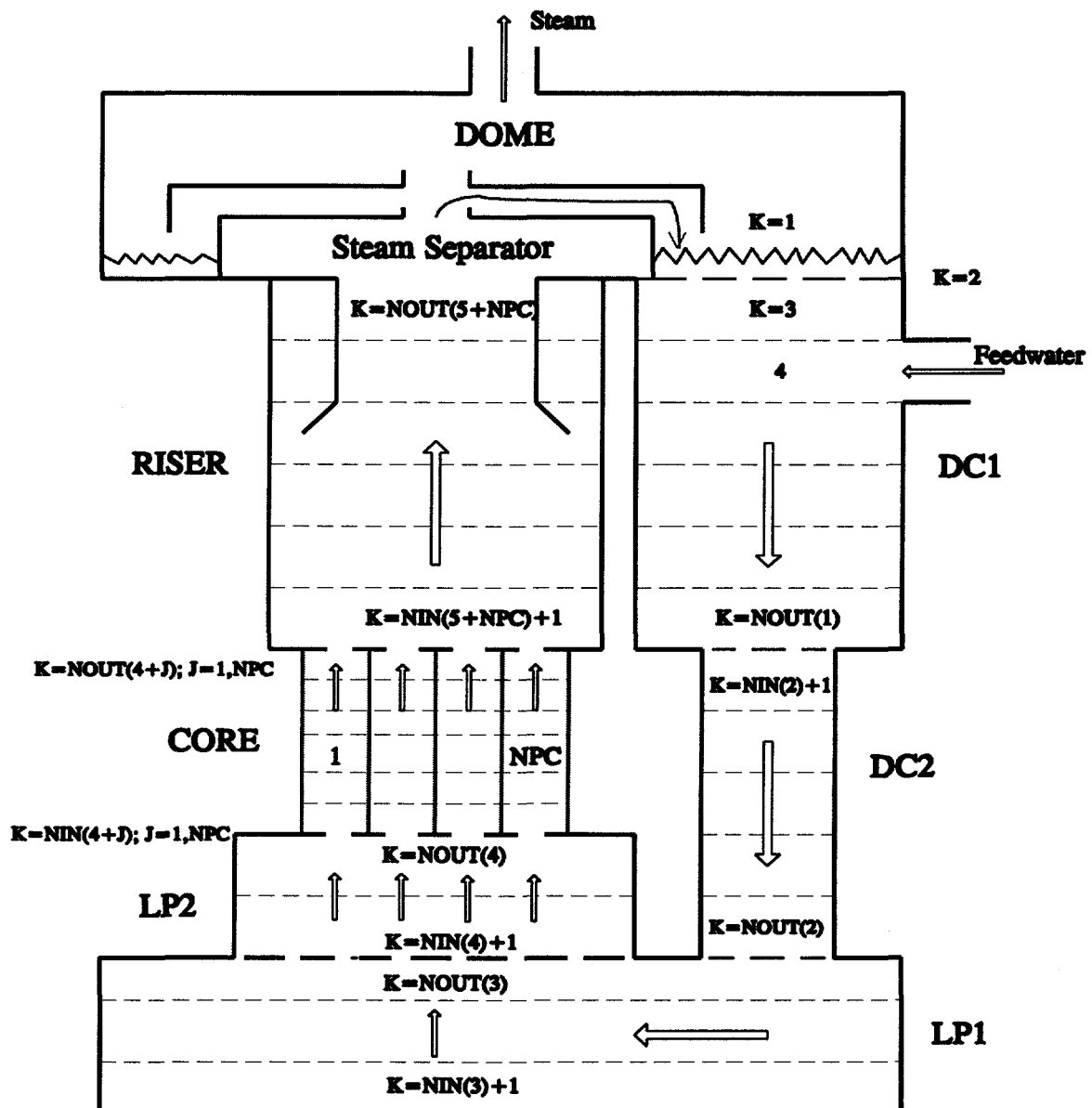


Figure 1.4 RAMONA-4B Nodalization for Reactor Pressure Vessel.

1. Introduction

(c) a Control Rod Drop Accident (CRDA) Analysis. Results of these analyses are discussed in the previous RAMONA-3B description and assessment report [Wulff et al., 1984a]. An MSIV-closure full-ATWS event also has been calculated using RAMONA-3B at BNL [Saha et al., 1987]. A set of runs has also been performed with the RAMONA-4B version of the code for ATWS and Stability prediction [Cheng et al., 1993, 1994, Khan et al., 1992, 1993, 1994, Rohatgi et al., 1994].

RAMONA-4B can also be used for non-reactor applications such as simulation of thermal-hydraulic experiments conducted in electrically heated test facilities. In these applications, the power applied to the test section has to be specified as an input. Also, the test facility to be simulated should have a general configuration similar to a BWR (Figure 1.1); it should include a downcomer, a lower plenum, a core, a riser, and a steam dome.

While RAMONA-4B does model the recirculation loop dynamics, it employs an effective single loop to simulate the actual two recirculation loops. This imposes unnecessary limitation for simulating a trip or failure of one recirculation pump. We recommend implementation of two recirculation loops with explicit representation of individual recirculation and jet pumps in each loop. In addition, the future improvement should allow prediction of two-phase flow in the recirculation loops.

Since RAMONA-4B assumes the vapor phase to be at saturation, another worthwhile improvement is to remove this assumption and allow the vapor phase to be superheated.

1.5 Code Limitations

As mentioned in the introduction, RAMONA-4B is a plant transient code and, therefore, it does not include the models for (i) a counter-current flow limitation, (ii) a quench-front propagation, and (iii) a metal-water reaction, which are required for Loss of Coolant Accident (LOCA) predictions. Also, RAMONA-4B restricts the vapor phase to be at saturation. Thus, RAMONA-4B is not applicable to the following types of transients:

- Large or small break in the recirculation loop,
- Large break in the steam line. However, a small break in the steam line (e.g., stuck-open safety/relief) can be analyzed, and
- Transients where a long period of core uncover may occur (e.g., degraded core accidents).

1.6 Major Changes from RAMONA-3B/MOD0/CYCLE4

Since the time of completion of the RAMONA-3B description and assessment document in 1984 [Wulff et al., 1984a], the code has been substantially modified. There were basically three classes of changes: model developments and modifications, general code improvements, and code corrections.

1.6.1 Model Developments and Modifications

Several new models were developed and implemented into the code and numerous modifications were made to the existing models. This section lists these models; more detailed information on modifications to the modeling can be found in Chapters 2 through 7.

1. Introduction

- The drift-flux model has been implemented in the code to replace the slip model for the hydraulic calculations in the reactor pressure vessel.
- In addition to the two-phase mixture level, the elevation of the collapsed water level in the downcomer is also calculated. The level tracking method has been extensively modified to be more accurate and robust. The predicted collapse water level is passed to the models of the plant protection systems, emergency core cooling systems (RCIC and HPCI), and the feedwater control system. The HPCI and RCIC models can be turned "on" or "off" depending on the elevation of the collapsed water level.
- A model for steam condensation on feedwater and/or safety injection liquid was implemented; it allows the prediction of the situation during an accident when the two-phase mixture level drops below the elevation of the feedwater spargers in the downcomer, and liquid is injected into the steam environment.
- The wall-friction-factor calculation was modified to include the laminar-flow regime.
- The core inlet form losses have been modified from being held at a constant value to a Reynold's-number-dependent loss coefficient.
- A new two-phase multiplier for the pressure form loss calculations was implemented.
- The property library for water has been extended to cover from low pressure region (0.03 bar) to 200 bar.
- A realistic recirculation loop dynamics model with motor-generator set has been implemented. In addition, a new recirculation pump model based on homologous curves for the head and torque [Madni et al., 1978] was added as an option.
- The code's capability to calculate natural circulation was limited to only the transient with a recirculation-pump trip. The capability has been extended to predict natural circulation for both the transient and steady state with no recirculation or jet pumps.
- A procedure for collapsing cross sections for a three-dimensional core model to a set of cross sections for a one-dimensional core model was developed and tested. The one-dimensional neutron kinetics model can be used at great savings of computer time for the transient (or steady state) calculations where the effects of power distribution in the radial directions are not expected to be significant (cylindrical geometry).
- An editing procedure for calculating the total core reactivity as well as reactivity components (Doppler, void, moderator temperature, etc.) was developed by Scandpower, Norway for BNL and implemented in RAMONA-4B. This feature provides an important tool for interpreting the history of the core power predicted in the transient. The reactivity changes due to variations in the void fraction, moderator, and fuel temperatures, and control-rod insertion are calculated as four separate output variables.
- A new model for the isolation condenser (IC) has been developed and implemented in the code. This component is available in some BWRs, and will be an integral part of the SBWR.

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- A Standby Liquid Control System (SLCS) model has been developed and implemented in the code. As a part of the passive safety system in the SBWR, the boron is injected into the vessel from a pressurized boron tank.
- A detailed boron transport model with capability for boron flow reversal has been implemented to replace a simple lumped-parameter model.
- The LAMBDA subroutine, which was used in the earlier slip model version of RAMONA for estimating the numerical stability limit associated with the explicit integration of hydraulic state equations, was revised for the new drift-flux model for two-phase flow and implemented in the code.
- A mechanistic steam separator model developed by General Electric Company for the joint USNRC-EPRI-GE sponsored BWR Refill-Reflood Program has been implemented in the code. This capability allows prediction of time-dependent steam carry-under, liquid carry-over, effective L/A ratio, and separator pressure drop.
- Genuine control system models for the pressure regulator, feedwater controller, and recirculation flow controller of a BWR have been developed and implemented in the code. In addition, a level control system model was developed and implemented for keeping the water level near the top of active fuel (TAF) as required by the Emergency Procedure Guidelines (EPG).
- A model for the reactor water cleanup (RWCU) system was developed and implemented in the code. This capability is needed for startup transient simulations.
- First-principle mechanistic models for various BOP components (turbines, condensers, feedwater heaters, feedwater pumps, etc.) have been developed and implemented in the code. This capability together with the control systems make it possible to simulate operational transients and startup transients. It also eliminates the necessity of having to prescribe time-dependent boundary conditions for many system transients. It also provides the capability to study the effect of BOP component failure and the feedback effect of BOP on thermal-hydraulic stability.
- A mechanistic containment model for the calculation of the water temperature and level of the Pressure Suppression Pool (PSP) as well as the drywell and wetwell dynamics has been developed and implemented in the code. This capability allows the prediction of the transients where prolonged dumping into PSP of steam released through the Safety/Relief valves can raise concern about the containment integrity (for example, during an anticipated transient without scram, ATWS).

1.6.2 General Code Improvements

Numerous code improvements have been made to the RAMONA-3B code since the code description was completed in 1981 and published in 1984 [Wulff, et al., 1984a]. They are summarized as follows:

- A bypass flow adjustment calculation was made available by specifying through the input the fraction of the total core flow that passes through the bypass region of the core. An adjustment to the **bypass entrance form loss coefficient** is made during the steady-state iterations.

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- The iterative procedure for calculating the local vapor generation rate and void fraction at the steady state has been improved to be faster and more robust by means of Newton-Raphson iteration.
- A capability for plotting the three-dimensional core void fraction was implemented into the code. This provides important data needed for interpreting the spatial behavior of the core power, which is influenced by the void fraction distribution (void reactivity effect).
- A new scheme for assigning cross sections to neutronics cells in the core has been implemented.
- A capability for periodic restart dumps controlled by users was made available to provide a restart capability after an unexpected termination of a computer run. (The original version created a restart file at the end of a calculation.)
- The wrapup/restart capability has been extended to include all the new models implemented in the code.
- The physical limits imposed on the void fraction ($0 \leq \alpha \leq 1$) in the earlier version has been revised to impose the limits instead on the local vapor mass, the state variable which determines the void fraction. This guarantees the consistency between the two variables.
- Continuous monitoring of the computer time spent in different parts of the code is now available to the users for optimizing the plant model in terms of the code's running speed.
- A special message file (fort.3) has been added to record and store information on all trips triggered during the calculation, safety/relief or main steam isolation valves actions, control-rods' movements, messages generated by the numerical procedures, and time-step control.
- A debug file (fort.64) has been added to store information on various models for debugging.
- Code structure has been improved by defining a common set for common blocks. The variable names have been made consistent among the subroutines. Read and write files have been reduced, and the code has been installed on workstations and is being used to address SBWR transients.
- The graphics package has been expanded to include the NASADIG program. The PAD program has also been modified to produce the required input for NASADIG code.
- The time-dependent boundary conditions in the code was limited to either a linear ramp or a sinusoidal function. Many realistic boundary conditions can not be represented by these functions. A new table-lookup function capability has been added in the code for the following operational variables:
 - (1) Core thermal power vs. Time,
 - (2) Feedwater flow vs. Time,
 - (3) Feedwater temperature vs. Time,
 - (4) Steam flow vs. Time,

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(5) Level setpoint vs. Time (needed for EPG level control).

- A need arose in the investigation of SBWR startup instability to hold the system pressure constant once it reached a prescribed value. An option has therefore been added in the code to do just that via the input switch IPRES=-1 and the target pressure PTARGET.

1.6.3 Code Corrections

Corrections to the following parts of the code were made:

- Logic in the calculations for the steam flow boundary condition with "no steam line option" through the input variable NSTMOP=0 on Card 880000,
- Two-phase friction multiplier calculation,
- Static driving head calculation in the steam dome region,
- Void fraction and moderator temperature averaging procedures in the reactivity feedback calculations,
- Mixture level tracking in the steam dome and downcomer regions,
- Eigenvalue calculations at time zero, and
- Decay-heat model.

Also, the code's option for the "hydraulics only" calculation (input variable ITYP=5 or 7 on Card 140000) has been corrected. This option can be used to perform fast-running thermal-hydraulics calculations with the core power imposed as a boundary condition.

2. MODELING OF NEUTRON KINETICS

The purpose of this chapter is to familiarize a new RAMONA-4B user with the basics of the code's neutron kinetics model and with the code improvements in the neutron kinetics area. This should help the user better understand the input instructions to be presented in the RAMONA-4B User's Manual. However, for further modeling details, one must consult the RAMONA-3B description and assessment document [Wulff et al., 1984a].

2.1 Overview

The neutron kinetics model of RAMONA-4B starts from a two-group diffusion theory. Simplifications are made in treating the thermal neutron leakage that reduce the formulation to the 1-1/2 group, coarse-mesh diffusion model in a three-dimensional rectangular coordinate system with a maximum of six delayed-neutron precursor groups. The model allows for the prediction of the distribution of time-dependent three-dimensional fission power in the reactor core.

Radial boundary conditions are given for all neutronic channels at the core periphery (a neutronic channel is one or several fuel bundles in the RAMONA-4B terminology). This may include channels with only a corner at the core boundary, or channels with one or more sides constituting the boundary. The core radial, top, and bottom boundary conditions are specified with parameters which are related to the extrapolation length for the fast flux and the albedo for the thermal flux.

In accordance with the formulation, the fast-flux coarse-mesh equations are solved with the assumption of an asymptotic thermal-flux distribution. The nodal volume-averaged fast and thermal fluxes are obtained by proper averaging of the fluxes at the cell's midpoint and its six neighbors. Details of the neutronics model can be found in Chapter 6.2.4 of the RAMONA-3B description and assessment document [Wulff et al., 1984a].

Power generation in any cell is a sum of the prompt and decay energy deposition rates. The prompt component is proportional to the instantaneous fission rate, whereas the decay energy deposition rate is calculated using the 1979 ANS standard 5.1 for decay heat generated by fission products. The decay energy deposition is delayed relative to the prompt fission energy, and its magnitude is a function of the fission rate history. As shown in Figure 1.3, the cross-section dependencies on fuel and moderator temperatures, void fraction, boron concentration, and control-rod movement are taken into account for every cell in the core in the neutron kinetics calculation.

In RAMONA-4B, all partial differential equations are first transformed into ordinary differential equations in time via proper spatial integration. The initial, or steady-state conditions are then obtained by setting the time derivatives to zero and iterating to obtain the eigenvalue, $1/k_{\text{eff}}$, of the system of equations. For calculations of transients, an implicit time-differencing scheme with the Gauss-Seidel iteration is used to integrate the prompt-neutron kinetics equations; the delayed-neutron equations are integrated by using an explicit method. The neutron kinetics and fuel heat-conduction equations use a master time step, whereas the transient thermal-hydraulic equations are integrated using a series of substeps. Automatic time step control algorithms [Wulff et al., 1984a] are used to determine the master step and the substeps and to control their synchronization.

2. Modeling of Neutron Kinetics

2.2 Governing Equations of Neutron Kinetics

2.2.1 Standard Two-Group Diffusion Theory Model

The neutron kinetics model of RAMONA-4B is based on the standard two-group diffusion equations [Henry 1975, Chapter 7]:

$$\begin{aligned} \frac{\partial}{\partial t} \left(\frac{\phi_1}{v_1} \right) = \sum_j (1 - \beta^j) [v_1^j \Sigma_{f1}^j \phi_1 + v_2^j \Sigma_{f2}^j \phi_2] \\ + \sum_{i=1}^6 \lambda_i C_i - \Sigma_1 \phi_1 + \nabla \cdot D_1 \nabla \phi_1, \end{aligned} \quad (2.2.1)$$

$$\frac{\partial}{\partial t} \left(\frac{\phi_2}{v_2} \right) = \Sigma_{21} \phi_1 - \Sigma_2 \phi_2 + \nabla \cdot D_2 \nabla \phi_2, \quad (2.2.2)$$

$$\frac{\partial C_i}{\partial t} = \sum_j \beta_i^j (v_1^j \Sigma_{f1}^j \phi_1 + v_2^j \Sigma_{f2}^j \phi_2) - \lambda_i C_i; \quad i = 1, 2, 3, \dots, 6. \quad (2.2.3)$$

Here the subscript 1 denotes the fast group of neutrons, the subscript 2 stands for the thermal group of neutrons, the subscript i represents the delayed neutron precursor group, the superscript j denotes the fissionable isotope, the symbol ϕ is the neutron flux, the Σ is the macroscopic cross section, the v is the number of neutrons per fission, β is the total delayed neutron fraction, D_1 and D_2 are diffusion coefficients of group 1 and group 2; respectively, C_i is the i th group delayed neutron precursor concentration, β_i is the delayed neutron fraction of the i th delayed neutron precursor group, and λ_i is the corresponding decay constant. Note that we have considered the general case where there may be several fissionable isotopes in the reactor core. In what follows, we shall drop the isotope index j , keeping in mind that the related parameters (v and Σ_f) must include the contribution from all the fissionable isotopes.

It should be mentioned that the parameters in the above equations are time and space dependent in general. The time and space dependence of the neutron kinetic parameters stem from the feedback effects due to fission heating which affects the thermal-hydraulics. The neutron kinetics equations are therefore nonlinear and difficult to solve. The difficulty is circumvented in practice by solving the neutron kinetics equations in tandem with the thermal-hydraulics equations. A further simplification is made in practice: the spatial dependence of neutron kinetic parameters is reduced to the dependence on material compositions which correspond to the nodalization structure used for the thermal-hydraulics calculation.

For convenience, the two-group diffusion equations are often cast in more compact matrix form:

$$\frac{\partial}{\partial t} ([V]^{-1} [\phi]) = [A][\phi] + \sum_j [\lambda_p^j] (1 - \beta^j) [F^j]^T [\phi] + \sum_{i=1}^6 [\lambda_i] \lambda_i C_i, \quad (2.2.4)$$

$$\frac{\partial C_i}{\partial t} = \sum_j \beta'_j [F^j]^T [\phi] - \lambda_i C_i; \quad i=1,2,3,\dots,6. \quad (2.2.5)$$

Here the following matrices have been defined:

$$[V]^{-1} = \begin{bmatrix} \frac{1}{v_1} & 0 \\ 0 & \frac{1}{v_2} \end{bmatrix}, \quad (2.2.6)$$

$$[A] = \begin{bmatrix} \nabla \cdot D_1 \nabla - \Sigma_1 & 0 \\ \Sigma_{21} & \nabla \cdot D_2 \nabla - \Sigma_2 \end{bmatrix}, \quad (2.2.7)$$

$$[F^j]^T = \begin{bmatrix} v'_1 \Sigma'_{j1} & v'_2 \Sigma'_{j2} \end{bmatrix}, \quad (2.2.8)$$

$$[\chi'_p] = \begin{bmatrix} 1 \\ 0 \end{bmatrix}, \quad (2.2.9)$$

$$[\chi_i] = \begin{bmatrix} 1 \\ 0 \end{bmatrix}, \quad (2.2.10)$$

$$[\phi] = \begin{bmatrix} \phi_1 \\ \phi_2 \end{bmatrix}. \quad (2.2.11)$$

Equations (2.2.4) and (2.2.5) are to be solved subject to the boundary condition at the outer boundary (S):

$$\nabla \phi_g(r) + \frac{1}{\lambda_g}(r) \phi_g(r) = 0 \quad \text{for } r \in S, \quad (2.2.12)$$

and the initial condition at $t=0$:

$$-[A][\phi] = \frac{1}{k_{eff}} [\chi'_p] [F^j]^T [\phi]. \quad (2.2.13)$$

2. Modeling of Neutron Kinetics

The above equation is an eigenvalue problem, so an eigenvalue $1/k_{\text{eff}}$ is introduced. Physically, k_{eff} can be interpreted as the effective neutron multiplication factor of the reactor defined as the ratio of total productions of neutrons to total losses of neutrons. Thus, $k_{\text{eff}} = 1$ when the reactor is exactly critical. This is the desired initial condition to start a reactor transient.

A real reactor is geometrically complex and materially heterogeneous. To solve the neutron kinetics equations for a complex three-dimensional reactor core, one needs literally in excess of a hundred thousand spatial mesh points to represent the heterogeneities of the reactor core. This is impractical and the usual remedy is to seek coarse-mesh solutions. In this approximation, we partition the reactor into a reasonable number of coarse-mesh volumes, customarily referred to as "nodes". However, another degree of approximation is necessary before the coarse-mesh approximation can be made. That is, the geometrical complexity and material heterogeneity must be homogenized by means of some spatial averaging procedure known as "homogenization". This procedure produces a set of effective homogenized two-group parameters for the coarse-mesh nodes.

2.2.2 The 1½ Group Coarse-Mesh Diffusion Theory Model

RAMONA-4B employs the so-called "1½ group coarse-mesh diffusion theory model" for neutron kinetics. In this section, we shall discuss how this simplified model was developed from the more rigorous two-group diffusion theory model. In so doing, we can address the assumptions and approximations that must be made, and assess their implications and limitations.

The idea of using the 1½ group approximation is intrinsically related to the desire of seeking accurate coarse-mesh solutions to the two-group diffusion equations for a large three-dimensional reactor core. Note that the fast neutrons do not see the small heterogeneities in a coarse-mesh node due to their much longer mean free path (mfp). For example, for the Peach Bottom-2 core at the end of cycle 2, the fast neutron mfp is about 37 cm at the bottom of the core with no voids, 50 cm at the midplane with 40% voids, and 65 cm at the top of the core with 67% voids. In contrast, the thermal neutron mfp is much shorter, about 10 cm. This suggests that much is to be gained if one could solve just the fast group diffusion equation and then derive the thermal neutron flux from the fast neutron flux by some means. This is precisely how the 1½ group coarse-mesh diffusion theory model came about.

To derive the 1½ group diffusion equations, we retain the divergence term only for the fast neutron flux in Eq. (2.2.1) and assume that

- (i) the divergence term for the thermal neutron flux can be either neglected or approximated by some means.

For simplicity, we further assume that

- (ii) the average neutron speeds, v_1 and v_2 , are independent of space and time, and that
- (iii) a single set of effective delayed neutron parameters, β_1 and λ_1 , can be defined such that they too are independent of space and time.

Assumption (i) is the essence of the 1½ group diffusion theory model and its validity depends on the problem at hand. The assumption is generally good for a homogeneous region of large size (much larger than the thermal neutron diffusion length so that the thermal neutron leakage is insignificant). It is bad for small regions, near the core boundary, and near any large material discontinuities. Therefore, the 1½ group diffusion theory model should be used in conjunction with a coarse-mesh nodalization with the nodal size being 6 inches or larger.

Assumption (ii) is a good approximation in general and will not incur significant error.

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Assumption (iii) may not be valid for an exposed core with a nonuniform burnup distribution. Usually, an effective set of delayed neutron parameters is obtained via a exposure-weighted averaging scheme. This helps alleviate the error introduced by this assumption.

With these assumptions, the two-group diffusion theory equations reduce to the following form:

$$\begin{aligned} \frac{1}{v_1} \frac{\partial \phi_1}{\partial t} = & (1 - \beta) [v_1 \Sigma_{f1} \phi_1 + v_2 \Sigma_{f2} \phi_2] \\ & + \sum_{i=1}^6 \lambda_i C_i - \Sigma_1 \phi_1 + \nabla \cdot D_1 \nabla \phi_1, \end{aligned} \quad (2.2.14)$$

$$\frac{1}{v_2} \frac{\partial \phi_2}{\partial t} = \Sigma_{21} \phi_1 - \Sigma_2 \phi_2 + \Lambda_2, \quad (2.2.15)$$

$$\frac{\partial C_i}{\partial t} = \beta_i (v_1 \Sigma_{f1} \phi_1 + v_2 \Sigma_{f2} \phi_2) - \lambda_i C_i; \quad i = 1, 2, 3, \dots, 6. \quad (2.2.16)$$

Note that the spatial part of the thermal neutron flux equation (2.2.15) becomes trivial without having to deal with the divergence term. In its place, the term Λ_2 was introduced. The default option in RAMONA-4B is to neglect the thermal neutron leakage ($\Lambda_2 = 0$). There is an option to calculate Λ_2 by an approximate one-dimensional problem. For details, the reader is referred to the RAMONA-3B report [Wulff et al., 1984a].

2.2.3 Boundary Conditions

There are two common ways of prescribing the boundary conditions for neutron kinetics. The first way is to represent explicitly the core reflectors with zero neutron flux imposed on the outer reflector boundary. The second approach is to use albedos to prescribe the boundary conditions without explicit representation of the reflectors. The latter is adopted by RAMONA-4B. In this approach, a matrix albedo $[\alpha]$ is defined as the ratio of neutron current to neutron flux at the boundary:

$$[\alpha] = \begin{bmatrix} \alpha_{11} & 0 \\ \alpha_{21} & \alpha_{22} \end{bmatrix} = \frac{[J_B]}{[\phi_B]}, \quad (2.2.17)$$

where $[J_B]$ is the boundary current vector and $[\phi_B]$ is the boundary flux vector. In neutron diffusion theory, the boundary current vector is defined as

$$[J_B] = -[D][\nabla \phi_B] \approx [D] \frac{[\phi_i] - [\phi_B]}{\xi/2}, \quad (2.2.18)$$

where $[\phi_i]$ is the adjacent interior flux vector and ξ is the mesh spacing next to the boundary of interest ($\xi = \Delta x$ on the horizontal plane and $\xi = \Delta z$ in the axial direction).

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Eliminating the boundary current vector $[J_B]$ from Eqs. (2.2.17) and (2.2.18), then solving for the boundary flux vector yields

$$[\phi_B] = \left[[I] + \frac{\xi}{2} [D]^{-1} [\alpha] \right]^{-1} [\phi_I], \quad (2.2.19)$$

where $[I]$ is the unity vector. Carrying out the matrix inversion, we obtain the following two-group boundary fluxes:

$$\phi_{B1} = \frac{\phi_{I1}}{1 + \frac{\xi}{2} \frac{\alpha_{11}}{D_1}}, \quad (2.2.20)$$

and

$$\phi_{B2} = \frac{-\frac{\xi}{2} \frac{\alpha_{21}}{D_2} \phi_{I1}}{\left(1 + \frac{\xi}{2} \frac{\alpha_{11}}{D_1} \right) \left(1 + \frac{\xi}{2} \frac{\alpha_{22}}{D_2} \right)} + \frac{\phi_{I2}}{1 + \frac{\xi}{2} \frac{\alpha_{22}}{D_2}}. \quad (2.2.21)$$

In the $1\frac{1}{2}$ group diffusion theory model of RAMONA-4B, Eq. (2.2.20) is utilized to treat the boundary condition for the fast neutron flux in terms of the fast neutron boundary current:

$$J_{B1} = \alpha_{11} \phi_{B1} = \frac{\alpha_{11} \phi_{I1}}{1 + \frac{\xi}{2} \frac{\alpha_{11}}{D_1}}. \quad (2.2.22)$$

$$\alpha_{11} = \frac{D_1}{\lambda_1}. \quad (2.2.23)$$

It is assumed that the fast neutron flux extrapolates linearly to zero at some distance λ_1 beyond the boundary, where λ_1 is known as the "fast extrapolation length". In diffusion theory, it can be readily shown that α_{11} is simply $\frac{D_1}{\lambda_1}$. With this α_{11} , the fast neutron boundary current reduces to

$$J_{B1} = \frac{D_1 \phi_{I1}}{\lambda_{eff}}, \quad (2.2.24)$$

where

$$\lambda_{eff} = \lambda_1 + \frac{\xi}{2}. \quad (2.2.25)$$

The boundary current in Eq. (2.2.24) is used to treat the fast flux divergence term, $\nabla \cdot D_1 \nabla \phi_1$, in the boundary nodes only.

For the thermal neutron flux, the expression given by Eq. (2.2.21) is not used in RAMONA-4B. Instead, the thermal neutron boundary condition is taken into account by introducing a source term, $\delta_B S_B \phi_1$, in the balance equation (2.2.15):

$$\frac{1}{v_2} \frac{\partial \phi_2}{\partial t} = \Sigma_{21} \phi_1 - \Sigma_2 \phi_2 + \Lambda_2 + \delta_B S_B \phi_1, \quad (2.2.26)$$

where δ_B is unity in the boundary nodes only and zero elsewhere. Physically, we may interpret the introduced source term as the additional thermal neutrons reflecting back into the core from the reflector per fast neutron leaking into the reflector.

To derive S_B in the introduced source term, we define a parameter θ as the ratio of the net inward thermal neutron current ($-J_{B2}$) to the net outward fast neutron current (J_{B1}) at the boundary surface:

$$\theta = \frac{-J_{B2}}{J_{B1}}. \quad (2.2.27)$$

Using Eq. (2.2.24), we obtain

$$-J_{B2} = \frac{\theta D_1 \phi_{II}}{\lambda_{eff}}. \quad (2.2.28)$$

Note that $-J_{B2}$ is the number of thermal neutrons per unit surface area leaking into the boundary node from the reflector; whereas the introduced source term is the number of thermal neutrons per unit volume leaking into the boundary node from the reflector. Thus, we have

$$S_B \phi_{II} = \frac{-J_{B2}}{\xi} = \frac{D_1 \theta \phi_{II}}{\xi \lambda_{eff}}, \quad (2.2.29)$$

whence

$$S_B = \frac{D_1 \theta}{\xi \lambda_{eff}}. \quad (2.2.30)$$

The above equation is applicable to the top and bottom boundary nodes which have only one surface facing the reflector. For the radial (side) boundary nodes, one must consider the possibility that some of the boundary nodes may have more than one surface facing the reflector. Thus, the right-hand side of Eq. (2.2.30) should carry a number n for the side boundary nodes to account for the multiple reflector-facing surfaces.

Furthermore, the side boundary condition may vary along the axial direction primarily because of the void distribution. Steam voids significantly affect the diffusion coefficient which dictates the effective extrapolation length λ_{eff} . The axial variation of the side boundary condition is taken into account via a first-order Taylor expansion for $1/\lambda_{eff}$ as follows:

$$\frac{1}{\lambda_{eff}^s} = \left(\frac{1}{\lambda_{eff}^s} \right)_{ref} + \frac{\partial}{\partial D_1} \left(\frac{1}{\lambda_{eff}^s} \right)_{ref} (D_1 - D_{1,ref}), \quad (2.2.31)$$

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where the subscript "ref" denotes the reference condition chosen for determining a single set of the side boundary condition parameters as required by RAMONA-4B, and the superscript S stands for the side boundary nodes.

The source term for the side boundary nodes can then be written as

$$S_B^S = \left(\frac{n}{\lambda_{eff}^S} \right) \left(\frac{\theta_s}{\Delta x} \right) D_1^S, \quad (2.2.32)$$

where θ_s is the current ratio defined by Eq. (2.2.27) for the side boundary nodes with their fast diffusion coefficients being D_1^S .

Considering all the boundary nodes in a three-dimensional Cartesian coordinate, we can summarize the source term as follows:

$$\delta_B S_B = \delta_B^B \frac{D_1^B \theta_B}{\Delta z \lambda_{eff}^B} + \delta_B^T \frac{D_1^T \theta_T}{\Delta z \lambda_{eff}^T} + \delta_B^S \frac{D_1^S n \theta_s}{\Delta x \lambda_{eff}^S}, \quad (2.2.33)$$

where the superscript B denotes the bottom boundary nodes, the superscript T represents the top boundary nodes, and the superscript S stands for the side boundary nodes.

2.2.4 Initial Conditions

Appropriate initial conditions are necessary to start a transient calculation. The governing equations for the initial conditions of neutron kinetics can be derived from Eqs. (2.2.14), (2.2.15) and (2.2.16) by setting the time derivatives to zero:

$$-\nabla \cdot D_1 \nabla \phi_1 + \Sigma_1 \phi_1 = \frac{1}{k_{eff}} \left(\nu_1 \Sigma_{f1} \phi_1 + \nu_2 \Sigma_{f2} \phi_2 \right), \quad (2.2.34)$$

$$-\lambda_2 + \Sigma_2 \phi_2 = \Sigma_{21} \phi_1 + \delta_B S_B \phi_1, \quad (2.2.35)$$

$$C_i = \frac{\beta_i}{\lambda_i} \left(\nu_1 \Sigma_{f1} \phi_1 + \nu_2 \Sigma_{f2} \phi_2 \right); \quad i = 1, 2, 3, \dots, 6. \quad (2.2.36)$$

The first two equations above constitute an eigenvalue problem of the matrix form:

$$[A] [\phi] = \frac{1}{k_{eff}} [F]^T [\phi]. \quad (2.2.37)$$

Here we have introduced an eigenvalue $1/k_{\text{eff}}$. The desired initial condition is that the reactor is exactly critical ($k_{\text{eff}} = 1$). In practice, it is not always possible to obtain this ideal initial condition because of the uncertainties in the two-group parameters used and the method employed to solve the eigenvalue equation (2.2.37). The usual practice is to calculate k_{eff} to within 0.5% of unity, then divide both $v_1 \Sigma_{f1}$ and $v_2 \Sigma_{f2}$ by the calculated k_{eff} to make the reactor exactly critical prior to the transient calculation.

2.2.5 Solution Methods

The system of governing equations for neutron kinetics is solved numerically by means of the standard finite-difference approximation in space and time. For the spatial differencing, we partition the reactor core into a number of coarse-mesh regions called "nodes", with a three-dimensional mesh spacing of (h_x, h_y, h_z) subject to the restriction that $h_x = h_y \neq h_z$. A typical node is a 6-inch cube. The finite-difference approximation in space is based on a seven-point, center-meshed, differencing scheme. For the temporal differencing, we discretize the time scale into a number of contiguous discrete time points. The finite-difference approximation in time is based on an implicit time integration scheme. The derivation of the finite-difference neutron kinetics equations is presented in Section 6.2 of the RAMONA-3B report [Wulff et al., 1984a].

For steady state, three iteration loops are used to obtain the eigenvalue and eigenfunction of equation (2.2.37): an inner iteration to calculate the eigenfunction, an outer iteration to obtain the eigenvalue, and a "power-void" iteration to account for the thermal-hydraulic feedback. The details are given in Section 6.6.1 of the RAMONA-3B report [Wulff et al., 1984a].

For the transient, the neutron kinetic solution requires a double iteration loop: an inner iteration to solve the fast flux equation and an outer iteration to account for the thermal-hydraulic feedback. The details are presented in Section 6.7 of the RAMONA-3B report [Wulff et al., 1984a].

2.2.6 Cross Section Model

The two-group cross sections and diffusion coefficients are functions of space and time as a consequence of their dependence on the material composition, density and temperature. The material composition in the reactor core changes due to fuel burnup (often referred to as exposure), fission product buildup (especially xenon), soluble boron injection, and the control rod movement including reactor scram. The density changes with steam voids and temperature which, in turn, change with the thermal-hydraulic condition. The material temperature changes due to fission heating.

Thus, the two-group cross sections should be represented as a function of nine variables: the void fraction α , the moderator (coolant) temperature T_m , the fuel temperature T_f , the control fraction f_c , the boron concentration C_B , the xenon concentration X_e , the fuel type F , the exposure E , and the exposure-weighted void fraction α_E , the so-called "history-dependent void fraction". Symbolically, we can write the expression,

$$\Sigma_g^j = \Sigma_g^j(\alpha, T_m, T_f, f_c, C_B, X_e, F, E, \alpha_E), \quad (2.2.38)$$

where the subscript g denotes group index and the superscript j represents the cross section type.

The functional form of Eq. (2.2.38) is by and large empirical in that experimental evidence and practical experience dictate its choice. In this sense, the cross section model is analogous to the constitutive relations used in thermal-hydraulics. At present, RAMONA-4B employs the following correlation for the two-group cross sections:

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$$\begin{aligned} \Sigma_g^j = & (1 - f_c) \sum_{n=1}^3 a_n^{uc} \alpha^{n-1} + f_c \sum_{n=1}^3 a_n^c \alpha^{n-1} + a_t (T_t - T_{t,0}) \\ & + a_f (\sqrt{T_f} - \sqrt{T_{f,0}}) + \delta \Sigma_g(C_B, \alpha) + \delta \Sigma_X + (\delta \Sigma_L)_g. \end{aligned} \quad (2.2.39)$$

The first two terms account for the effect of void feedback and the presence of control rods. The third term accounts for the effect of moderator feedback with a_t being the moderator temperature coefficient. The fourth term represents the effect of Doppler feedback with a_f being the fuel temperature coefficient. The term $\delta \Sigma_g(C_B, \alpha)$ accounts for the effect of boron injection should it occur. The term $\delta \Sigma_X$ accounts for the effect of fission product poisoning. Finally, the term $(\delta \Sigma_L)_g$ attempts to include the effect of transverse leakage of neutrons if fewer than three dimensions are used to represent the reactor core.

The boron effect is taken into account in the fast and thermal absorption cross sections only as follows:

$$\delta \Sigma_g(C_B, \alpha) = C_0 \sigma_{a,g} (1 - f^*) f_w (1 - \alpha) \rho_t C_B; \quad g=1, 2, \quad (2.2.40)$$

where $C_0 = 0.11 \times 10^{-10}$, C_B is the boron concentration, α is the void fraction, $\sigma_{a,g}$ is the microscopic absorption cross section of Boron-10 (typically, $\sigma_{a,1} = 22$ to 48 barns and $\sigma_{a,2} = 1300$ to 3500 barns, depending on the neutron spectrum in the water of a fuel cell, f_w is the ratio of bundle hydraulic flow area to total bundle cross-sectional area, and f^* is a correction factor to allow for conservatism and uncertainties.

The effect of fission product poisoning is predominantly due to xenon buildup in the core. The problem of xenon buildup is a transient problem by itself. The xenon concentration tends to approach an equilibrium condition after a while. It is usually assumed that the xenon buildup has reached its equilibrium condition in representing its effect on the cross sections. The correction due to xenon poisoning is made on the thermal absorption and fission cross sections only as follows:

$$\delta \Sigma_X^j = a_X^j \left[\frac{1 + b_X^j}{1 + b_X^j \left(\frac{q_0'''}{q'''} \right)} - 1 \right], \quad (2.2.41)$$

where q''' is the local power density, q_0''' is the corresponding reference value at rated power, a_X^j and b_X^j are the fitting constants treated as user-specified input, and the superscript j denotes either absorption or fission cross section.

The correction due to transverse leakages is approximated by the expression,

$$(\delta \Sigma_L)_g = D_g B_g^2; \quad g=1, 2, \quad (2.2.42)$$

where B_g^2 is the geometric buckling defined as

$$B_g^2 = \left[\frac{-\nabla^2 \phi_g}{\phi_g} \right]_{(x,y)} \quad (2.2.43)$$

Note that the effects of exposure and history-dependent void fraction are not explicitly represented in Eq. (2.2.39) since the exposure does not change during the transient of interest. (Exposure accumulates appreciably only in a duration of months or years). However, the effect of exposure, at least, should be taken into account in generating the coefficients for Eq. (2.2.39).

2.2.7 Decay Heat Model

The thermal-hydraulic calculations in RAMONA-4B require the volumetric heat generation rate (or power density) q''' . The power density is calculated from the sum of the prompt fission heat and the decay heat:

$$q''' = q_p''' + q_d''' , \quad (2.2.51)$$

where q_p''' is the prompt power density and q_d''' is the decay heat density.

The prompt power density is related to the fission density F_f :

$$q_p''' = K (1 - H_0) F_f , \quad (2.2.52)$$

where $K = 3.2041 \times 10^{-11}$ joules/fission, H_0 is the fraction of delayed energy density, and

$$F_f = \Sigma_{f1} \phi_1 + \Sigma_{f2} \phi_2 . \quad (2.2.53)$$

The amount of delayed fission energy released at time t due to the decay of fission products per fission at time t' per unit time, denoted by $h_f(t-t')$, is a measured quantity and has been tabulated as a function of time for different fissionable nuclides [ANS Standard 5.1, 1978].

2.3 Reactivity Edits

A procedure for calculating the total core reactivity has been developed and implemented in RAMONA-3B (this work was performed by L. Moberg at Scandpower in 1984). The reactivity edits are very helpful for interpreting the history of the total core power predicted in the transient calculations. Along with the calculation of the total core reactivity, an evaluation of four different reactivity components is performed in the course of the transient calculation: fuel temperature (Doppler), void, moderator temperature, and effects of control-rods feedback. These reactivity components are calculated using the methods of perturbation theory as described in 2.3.2.

2.3.1 Total Reactivity Calculation

The total reactivity is calculated using the method referred to as "Inverse Reactivity Calculation". In this method a conventional set of point-kinetics equations is used with the transient total core power predicted by the 3-D (or 1-D) RAMONA-4B calculation used as the amplitude function:

$$\frac{dT}{dt} = \frac{\rho - \beta}{\Lambda} T + \sum_{i=1}^6 \lambda_i e_i , \quad (2.3.1)$$

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$$\frac{de_i}{dt} = \frac{\beta_i}{\Lambda} T - \lambda_i e_i, \quad (2.3.2)$$

where e_i is the effective precursor concentration for the i -th delayed-neutron group.

The point-kinetics equations (2.3.1) and (2.3.2) are derived from the three-dimensional two-group diffusion equations (2.2.4) and (2.2.5) by weighing the latter with an adjoint flux and integrating over space and neutron energy. The parameters of the point-kinetics model are expressed as a spatial integral as expressed by $\langle \dots \rangle$ of nuclear properties (cross sections as a function of fuel and moderator temperatures, void fraction, and control-rod positions), the adjoint flux vector $[W]$, and the flux vector $[\phi]$:

$$\rho = \frac{1}{F} \langle [W], [M] [\phi] \rangle, \quad (2.3.3)$$

$$\Lambda = \frac{1}{F} \langle [W], [V]^{-1} [\phi] \rangle, \quad (2.3.4)$$

$$\beta_i = \frac{1}{F} \langle [W], \sum_j \beta_i^j [\chi_i] [F^j]^T [\phi] \rangle, \quad (2.3.5)$$

$$F = \langle [W], \sum_j \{ (1 - \beta^j) [\chi_p^j] + \sum_i \beta_i^j [\chi_i] \} [F^j]^T [\phi] \rangle, \quad (2.3.6)$$

$$M = [A] + \sum_j \{ [\chi_p^j] (1 - \beta^j) [F^j]^T + \sum_i \beta_i^j [\chi_i] [F^j]^T \}, \quad (2.3.7)$$

$$\beta = \sum_{i=1}^6 \beta_i, \quad (2.3.8)$$

$$T = \langle [W], [V]^{-1} [\phi] \rangle, \quad (2.3.9)$$

where ρ is total reactivity, Λ is the prompt neutron lifetime, F is a normalization integral, and $[M]$ is a matrix of the fuel and core properties. Note that the derivation of the point-kinetics equations is rigorous, provided that the neutron flux distribution used in the lumped parameters listed above satisfy equations (2.2.42) and (2.2.5).

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The objective of this part of the reactivity edits is to obtain the total reactivity in terms of the transient amplitude function, T , core average prompt-neutron lifetime, delayed-neutron parameters, β_i and λ_i , and transient delayed-neutron precursors concentrations. The parameters Λ and β_i are evaluated at initial conditions and remain constant during the transient calculations. Note that the values of β_i are specified through the RAMONA-4B input, while Λ is a function of the flux distribution and has to be calculated at the beginning of the transient calculation.

Let us start with integrating Eq. (2.3.2) over time:

$$\epsilon_i(t) = \epsilon_{i0} e^{-\lambda_i t} + \frac{\beta_i}{\Lambda} \int_0^t T(t') e^{\lambda_i t'} dt', \quad (2.3.10)$$

where

$$\epsilon_{i0} = \frac{\beta_i}{\lambda_i \Lambda} T(0). \quad (2.3.11)$$

Introducing a finite-difference representation of the solution (2.3.10), one arrives at

$$\epsilon_i(t_n) = \epsilon_{i0} e^{-\lambda_i(t_{n-1}+h)} + \frac{\beta_i}{\Lambda} e^{-\lambda_i(t_{n-1}+h)} \left\{ \int_0^{t_{n-1}} T(t') e^{\lambda_i t'} dt' + \int_{t_{n-1}}^{t_n} T(t') e^{\lambda_i t'} dt' \right\},$$

or

$$\epsilon_i(t_n) = \epsilon_i(t_{n-1}) e^{-\lambda_i h} + \frac{\beta_i}{\Lambda} e^{\lambda_i(t_{n-1})} e^{-\lambda_i h} \int_{t_{n-1}}^{t_n} T(t') e^{\lambda_i t'} dt', \quad (2.3.12)$$

where $h = t_n - t_{n-1}$ is the integration time step.

An approximation of the amplitude function $T(t')$ in the time interval $[t_{n-1}, t_n]$ by an exponential function leads to:

$$T(t_{n-1} + \tau) = T(t_{n-1}) e^{\gamma \tau} \quad \text{for } 0 < \tau < h, \quad (2.3.13)$$

where

$$\gamma = \frac{1}{h} \ln \left(\frac{T(t_n)}{T(t_{n-1})} \right). \quad (2.3.14)$$

Substitution of (2.3.13) into (2.3.12) provides the final form of the expression for the effective precursor concentrations:

$$\epsilon_i(t_n) = \epsilon_i(t_{n-1}) e^{-\lambda_i h} + \frac{\beta_i}{\Lambda(\gamma + \lambda_i)} \left[T(t_n) - T(t_{n-1}) e^{-\lambda_i h} \right]. \quad (2.3.15)$$

In order to obtain an expression for the total reactivity, Eq. (2.3.1) is rewritten as:

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$$\frac{\lambda}{T(t)} \frac{dT(t)}{dt} = \rho - \beta + \frac{\lambda}{T(t)} \sum_{i=1}^6 \lambda_i \epsilon_i(t) . \quad (2.3.16)$$

Combining (2.3.16) and (2.3.13) and taking the limit with $h \rightarrow 0$, one obtains the expression:

$$\rho = \beta + \lambda \gamma - \frac{\lambda}{T(t_n)} \sum_{i=1}^6 \lambda_i \epsilon_i(t_n) . \quad (2.3.17)$$

Based on the above, the procedure for predicting the total reactivity consists of the following steps:

- Obtain the relative power at time t_n from the main RAMONA-4B calculation [the amplitude function $T(t)$],
- Calculate γ , Equation (2.3.14), using $T(t_n)$ and $T(t_{n-1})$,
- Calculate the precursor concentrations $\epsilon_i(t_n)$ using Equations (2.3.15),
- Finally, calculate the total reactivity using Equation (2.3.17).

2.3.2 Reactivity Components

Four reactivity components; namely, the void, doppler, moderator temperature, and scram reactivities are calculated using the perturbation-theory approach, which is based on monitoring variations of the void fraction, fuel and coolant temperatures, and control-rod positions in the course of a transient. Definitions of these components are given below as a weighted spatial average denoted by the symbol $\langle \dots \rangle$ as a function of the material property matrix $[M]$. The total reactivity is defined as

$$\rho = \frac{1}{F} \langle [W], [M] [\phi] \rangle . \quad (2.3.18)$$

Introduction of a small perturbation to $[M]$,

$$[M] = [M_0] + [\Delta M] , \quad (2.3.19)$$

results in a perturbation to the neutron flux vector:

$$[\phi] = [\phi_0] + [\Delta \phi] . \quad (2.3.20)$$

This leads to the following expression for the total reactivity, Equation (2.3.18):

$$\rho = \frac{1}{F} \langle [W], [M_0] [\phi_0] \rangle + \langle [W], [M_0] [\Delta \phi] \rangle + \langle [W], [\Delta M] [\phi] \rangle . \quad (2.3.21)$$

In Equation (2.3.21), the first term on the right hand side is equal to zero since the reactor is assumed critical at the initial state. Also, the second term is equal to zero if the initial adjoint flux is used as the weighing function (the initial adjoint flux is calculated by setting the second term to zero), i.e.,

$$[W] = \left[\phi_0^+ \right]. \quad (2.3.22)$$

In this case the expression for the total reactivity becomes:

$$\rho = \frac{1}{F} \langle [\phi_0^+], [\Delta M] [\phi] \rangle. \quad (2.3.23)$$

It should be pointed out that $[\phi]$ in (2.3.23) is the transient three-dimensional flux satisfying Eqs. (2.2.4) and (2.2.5).

$$[\Delta M] = [\Delta M_v] + [\Delta M_D] + [\Delta M_{T_m}] + [\Delta M_s], \quad (2.3.24)$$

Assuming that the perturbations to the property matrix $[M]$ can be separated into four feedback components, i.e., void, fuel and moderator temperatures, and control-rod fraction (scram component), one arrives at the following expression for the total reactivity:

$$\rho(t) = \rho_D(t) + \rho_v(t) + \rho_{T_m}(t) + \rho_s(t), \quad (2.3.25)$$

where

$$\rho_D(t) = \frac{1}{F} \langle [W], [\Delta M_D] [\phi] \rangle, \quad (2.3.26)$$

$$\rho_v(t) = \frac{1}{F} \langle [W], [\Delta M_v] [\phi] \rangle, \quad (2.3.27)$$

$$\rho_{T_m}(t) = \frac{1}{F} \langle [W], [\Delta M_{T_m}] [\phi] \rangle, \quad (2.3.28)$$

$$\rho_s(t) = \frac{1}{F} \langle [W], [\Delta M_s] [\phi] \rangle, \quad (2.3.29)$$

As it follows from Eqs. (2.3.26) through (2.3.29), four spacial integrations must be carried out at each time step to obtain the transient reactivity components. Some particulars of these calculations are described below.

Equations (2.3.26) through (2.3.29) for reactivity components can be written in a generalized form within the two-energy-groups formalism:

$$\rho_x = \frac{1}{S_1 + S_2} \left[(\Delta S_1)_x + (\Delta S_2)_x + (\Delta G_1)_x + (\Delta G_2)_x - (\Delta A_1)_x - (\Delta A_2)_x + (\Delta R_1)_x \right], \quad (2.3.30)$$

where the subscript x denotes one of the four state variables responsible for the particular reactivity feedback: void fraction, fuel temperature (Doppler), moderator temperature, or control-rod fraction. Different components of Eq.

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(2.3.30) are the source $(\Delta S_i)_x$, leakage $(\Delta G_i)_x$, absorption $(\Delta A_i)_x$ for two energy groups, and fast-neutron removal $(\Delta R_i)_x$; respectively:

$$(\Delta S_i)_x = \int \phi_{i0}^+ \Delta (v_i \Sigma_{fi})_x \phi_i dV, \quad (2.3.31)$$

$$(\Delta G_i)_x = \int \phi_{i0}^+ (\nabla \cdot \Delta D_i) \nabla \phi_i dV, \quad (2.3.32)$$

$$(\Delta A_i)_x = \int \phi_{i0}^+ (\Delta \Sigma_{ai})_x \phi_i dV, \quad (2.3.33)$$

$$(\Delta R_i)_x = \int \phi_{i0}^+ (\Delta \Sigma_{Ri})_x \phi_i dV, \quad (2.3.34)$$

The first-order approximation for perturbation of any of the cross-section parameters (Σ) is used:

$$(\Delta \Sigma)_x \approx \left(\frac{\partial \Sigma}{\partial x} \right) \cdot \Delta x, \quad (2.3.35)$$

where x denotes the state variable (void fraction, fuel temperature, etc.), and the derivative is evaluated at initial conditions. Taking into account the RAMONA-4B cross-section representation, the particular perturbations can be easily produced.

The numerical evaluation of the spatial integral (2.3.31) through (2.3.34) is carried out by summation over all neutronic cells using predicted values of cell-averaged fast and thermal fluxes. For example, the source integral S can be calculated as follows:

$$S_i = \sum_n \left(\phi_{i0}^+ \right)_n (v \Sigma_{fi})_n \phi_{i,n} \Delta V_n; \quad i=1, 2, \quad (2.3.36)$$

where $(v \Sigma_{fi})_n$ is the neutron production cross section in cell n .

Calculation of the leakage integral (2.3.32) for $(\Delta G_i)_x$ requires the use of Green's theorem in that the volume integral is reduced to a surface integral. Therefore, the problem reduces to taking the integral (2.3.32) for fast and thermal fluxes over the neutronic cells constituting the core boundaries.

Using the expressions for the boundary currents, Eqs. (2.2.30) through (2.2.33), in terms of the fast flux at the center of the boundary cells and the fast-flux effective extrapolation lengths λ_{eff} , one arrives at the expression:

$$\begin{aligned}
 (\Delta G_1)_x &= \sum_{\text{boundary}} \phi_{10}^+ \left[\delta_B^T \frac{R \Delta(D_1)_x}{h_x \lambda_{\text{eff}}^T} \right. \\
 &\quad \left. \delta_B^B \frac{R \Delta(D_1)_x}{h_x \lambda_{\text{eff}}^B} + \delta_B^S \frac{n}{h_x} \left(\frac{1}{\lambda_{\text{Ref}}^{\text{eff}}} + 2C_{\text{Ref}} D_{10} - C_{\text{Ref}} D_1^{\text{Ref}} \right) \Delta(D_1)_x \right] \phi_1.
 \end{aligned}
 \tag{2.3.37}$$

Here δ_B^T , δ_B^B , and δ_B^S are the Kronecker deltas equal to zero everywhere except for the top, bottom, and side boundary cells where it is equal to unity, n is the number of the cell surfaces adjacent to the reflector, $R = h_x^2$, ϕ is the flux in the center of a cell. The details of these derivations can be found in the MEKIN-B report [Aronson et al., 1980].

The leakage integral (2.3.32) for the thermal flux is calculated with an assumption that there is no direct leakage of the thermal neutrons from the core into the reflector region, and that the main part of the thermal flux at the core boundary is formed by the leaking fast neutrons after their thermalization in the reflector and return into the core. These neutrons are modeled in RAMONA-4B by using the reflector albedo parameters. The source of the thermalized neutrons from the reflector region is given by Eq. (2.2.33).

The final form of the thermal leakage integral used in the RAMONA-4B code is:

$$(\Delta G_2)_x = \sum_{\text{boundary}} \phi_{20}^+ \left[\delta_B^T \frac{R \theta_T}{\lambda_{\text{eff}}^T h_x} + \delta_B^B \frac{R \theta_B}{\lambda_{\text{eff}}^B h_x} + \delta_B^S \frac{n \theta_s}{h_x} \left(\frac{1}{\lambda_{\text{Ref}}^{\text{eff}}} + 2C_{\text{Ref}} D_{10} - C_{\text{Ref}} D_1^{\text{Ref}} \right) \right] \Delta D_1 \phi_2. \tag{2.3.38}$$

The adjoint fluxes used as weighting functions are calculated from the following approximate formula due to Wade [Wade 1968]:

$$\phi_1^+ \approx \phi_1, \tag{2.3.39}$$

$$\phi_2^+ \approx \frac{\nu_2 \Sigma_{f2}}{\Sigma_2 k_{\text{eff}}} \phi_1 \tag{2.3.40}$$

for fuel region, and

$$\phi_2^+ \approx \frac{\Sigma_1}{\Sigma_{21}} \left(1 - \frac{D_1}{\Sigma_1 M^2} \right) \phi_1 \tag{2.3.41}$$

for non-fuel region. Here $M^2 = D_1/\Sigma_1 + D_2/\Sigma_2$ is the migration area.

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2.4 Assessments

The neutronic package of RAMONA-4B is basically unchanged from the original RAMONA-3 which was acquired from Scandpower in 1978. The neutronic model of RAMONA-3 was developed from the 3D core simulator PRESTO which is based on the well-known coarse-mesh diffusion model due to Borresen [Borresen, 1978]. The PRESTO code has been extensively validated by Scandpower against actual operating reactor data [Borresen, 1979]. Furthermore, BNL has used RAMONA-3B to calculate both the radial and axial power distributions in very good agreement with the Browns Ferry-3 cycle-5 measurements [Carew, 1993]. The comparisons are shown in Figures 2.1 through 2.3.

Validations against transient data are limited primarily because of the lack of good test data available. The Peach Bottom-2 turbine trip tests have been analyzed using RAMONA-3B with good agreement on the transient reactor power [Wulff et al., 1984a] as shown in Figures 2.4 through 2.6. The Browns Ferry 1 recirculation pump trip test and generator load rejection test have also been analyzed with good agreement on the reactor power response [Cheng, 1987a] and [Cheng, 1987b]. Figures 2.7 and 2.8 show the comparisons on the reactor power response for these tests, respectively.

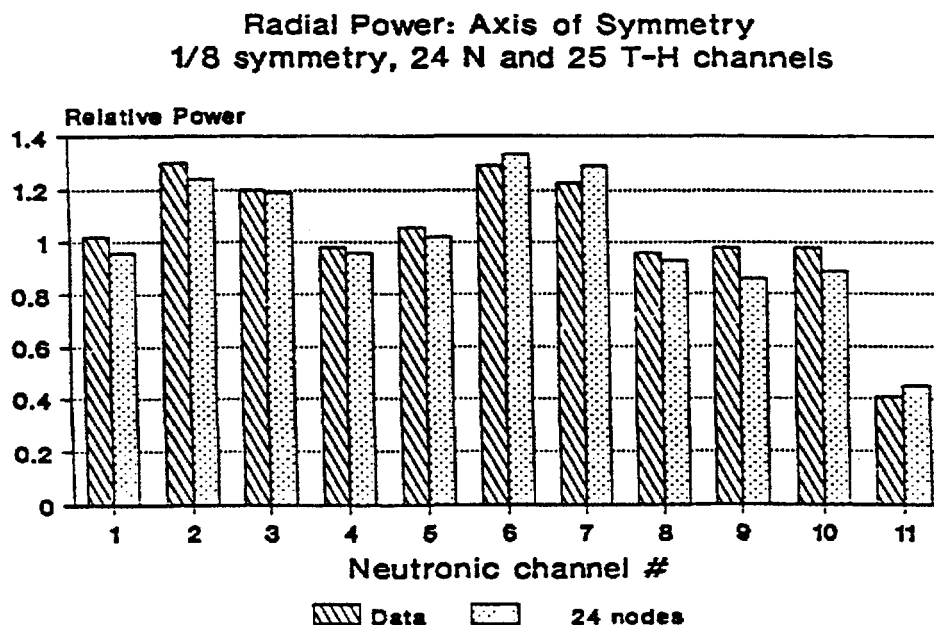


Figure 2.1 Comparison of Calculated and Measured Radial Power Distributions Along the Horizontal Axis.

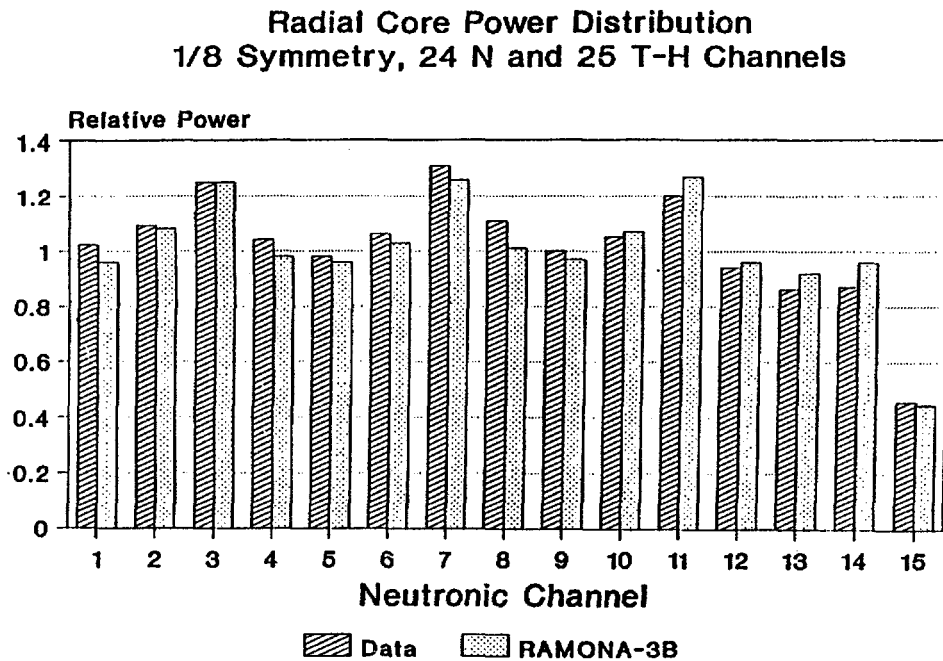


Figure 2.2 Comparison of Calculated and Measured Radial Power Distributions Along the Vertical Axis

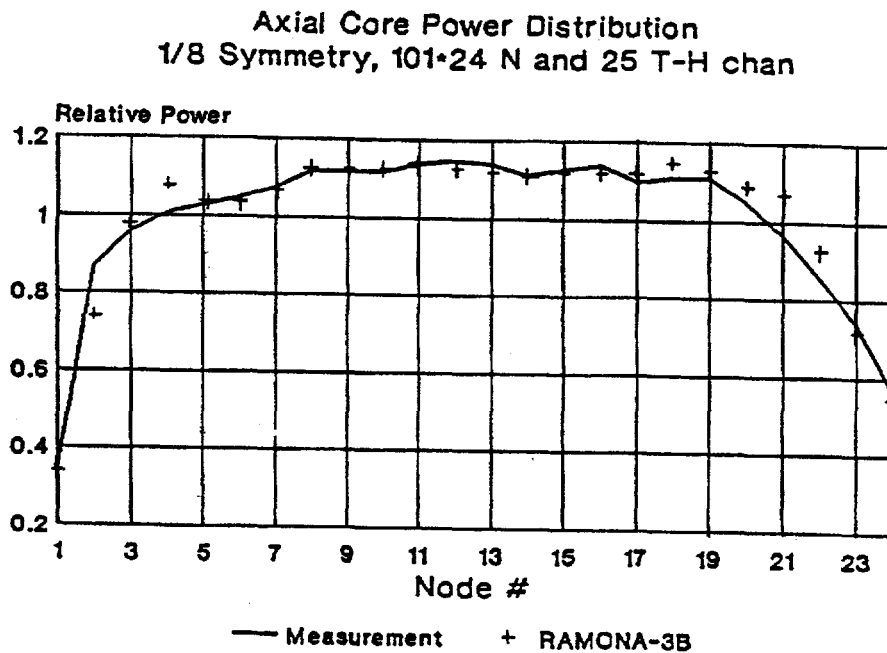


Figure 2.3 Comparison of Calculated and Measured Axial Power Distributions.

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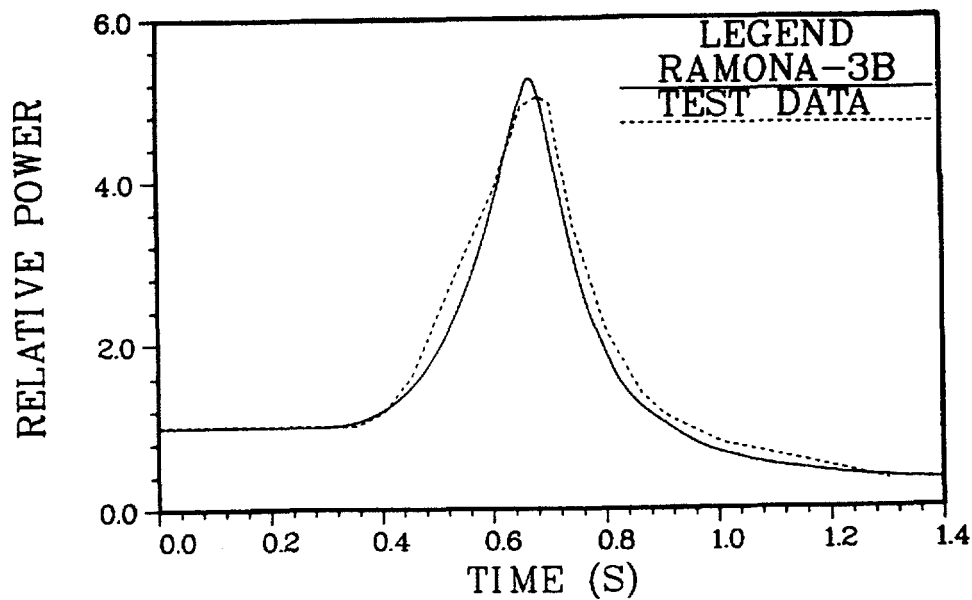


Figure 2.4 Comparison of Transient Reactor Power for Turbine Trip Test No. 1

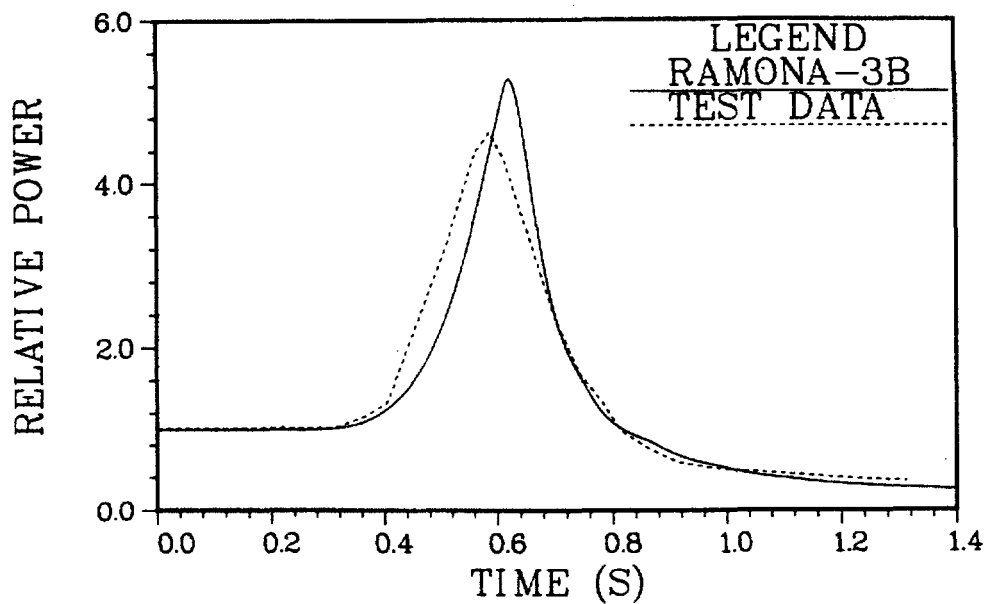


Figure 2.5 Comparison of Transient Reactor Power for Turbine Trip Test No. 2

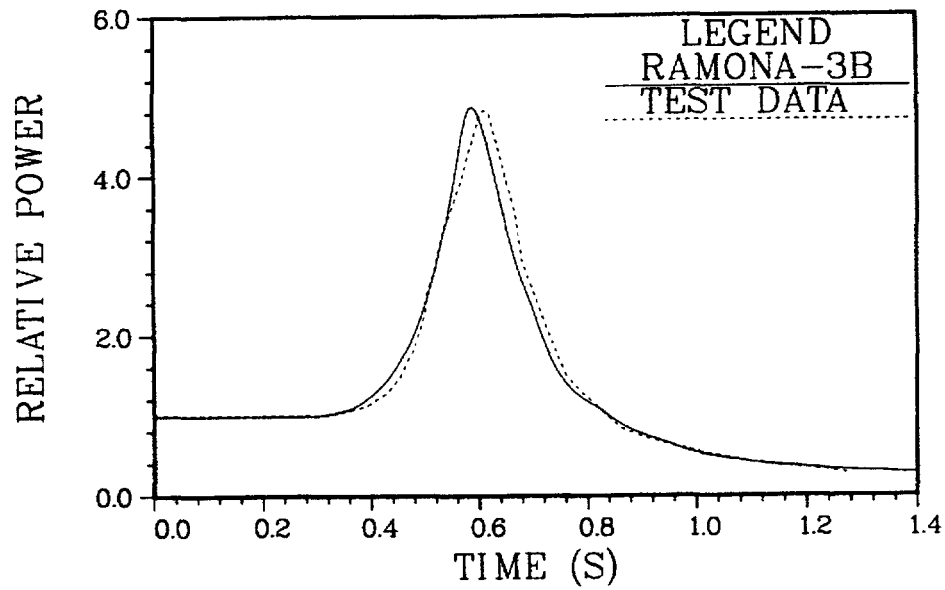


Figure 2.6 Comparison of Transient Reactor Power for Turbine Trip Test No. 3

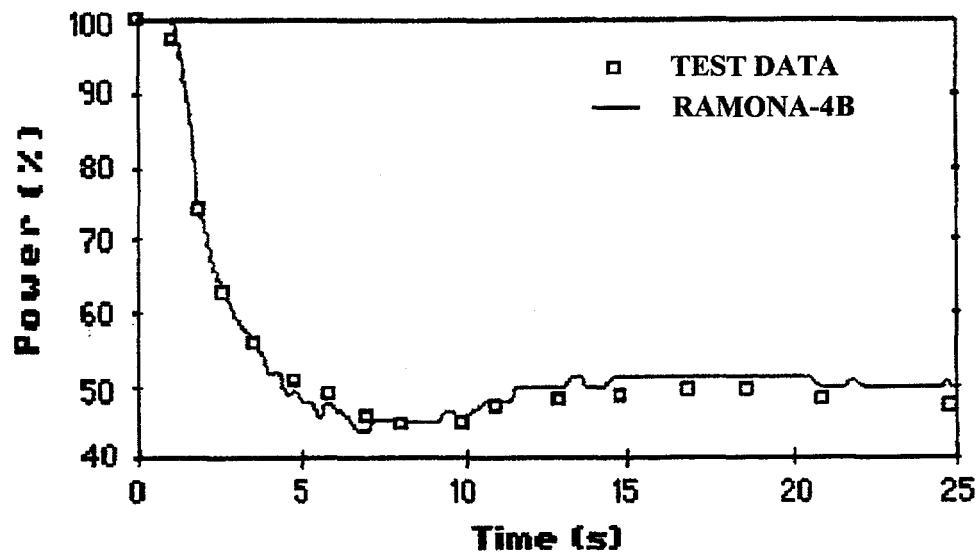


Figure 2.7 Comparison of Reactor Power for the Recirculation Pump Trip

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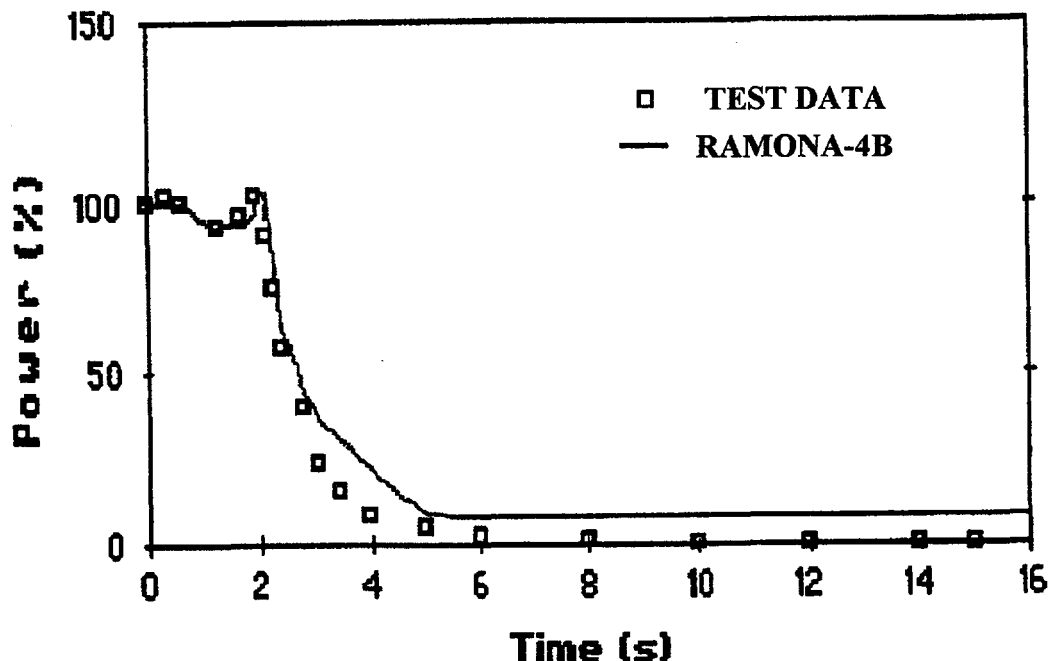


Figure 2.8 Comparison of Reactor Power Response for the Generator Load Rejection Test

2.5 Recommendations

2.5.1 An Improved 3D Cross Section Representation

The majority of system codes with a space-time neutron kinetics model employ a composition map to represent nuclear cross sections in the core with a reasonable number of compositions (or material types). Usually, the number of compositions allowed is less than 100, thus necessitating a coarse-mesh representation of the cross sections in the core. This is sufficient for a fresh core at the beginning of life, but inadequate for an exposed core, especially at the end of a fuel cycle, due to the three-dimensional (3D) distribution of exposure (fuel burnup) and/or history-dependent void fraction.

In case of RAMONA-4B, the current cross section representation permits a maximum of 1000 compositions (used to be 80). An auxiliary code BLEND must be used to account for the effect of exposure and void history. BLEND takes the 3D exposure and void history distributions as input and produces a fuel composition map for a coarse-mesh representation of the core as well as the cross sections and their feedback coefficients of the compositions, which are then input to RAMONA-4B. This procedure is tedious and cumbersome. Thus, an improved cross section representation which will expedite cross section preparation and improve accuracy is highly desirable. The idea is to eliminate the use of BLEND and include the exposure and void history dependence of the cross sections and their feedback coefficients directly in RAMONA-4B. Following is an improved 3D cross section representation recommended for future improvement.

We shall assume that the effect of exposure and void history can be represented by a quadratic function. This is justified because the cross section data generated by a lattice physics code (e.g., CASMO) follow closely a linear or quadratic law with respect to fuel exposure and void history. We further assume that the effect of void history on the Doppler and moderator temperature feedback can be represented at the reference void history of 40% and that the Doppler and moderator temperature coefficients are mostly affected by the instantaneous void fraction. This is a good approximation because the void history affects primarily the void feedback and because the core average void fraction throughout most of the fuel cycle is about 40%. Thus, for the cross section of type j, we may write:

$$\Sigma_g^j = (a_1 + a_2 \alpha_H + a_3 \alpha_H^2) + (b_1 + b_2 \alpha_H + b_3 \alpha_H^2) \alpha + (c_1 + c_2 \alpha_H + c_3 \alpha_H^2) \alpha^2 + (d_1 + d_2 \alpha + d_3 \alpha^2) (\sqrt{T_f} - \sqrt{T_{f0}}) + (e_1 + e_2 \alpha + e_3 \alpha^2) (T_m - T_{m0}), \quad (2.5.1)$$

where

- α_H = history dependent void fraction,
- α = instantaneous void fraction,
- T_f = fuel temperature,
- T_m = moderator temperature,
- T_{f0} = reference fuel temperature at the initial condition,
- T_{m0} = reference moderator temperature at the initial condition.

Each of the coefficients in the above equation is a quadratic function of exposure (E):

$$a_n = a_{n1} + a_{n2} E + a_{n3} E^2, \quad (2.5.2)$$

$$b_n = b_{n1} + b_{n2} E + b_{n3} E^2, \quad (2.5.3)$$

$$c_n = c_{n1} + c_{n2} E + c_{n3} E^2, \quad (2.5.4)$$

$$d_n = d_{n1} + d_{n2} E + d_{n3} E^2, \quad (2.5.5)$$

$$e_n = e_{n1} + e_{n2} E + e_{n3} E^2, \quad (2.5.6)$$

where $n=1,2,3$.

2. Modeling of Neutron Kinetics

We see that the above equations contain 45 coefficients. We must also consider both the uncontrolled (rod out) and controlled (rod in) state. Thus, we will need a total of 90 coefficients for each cross section type. Since there are 9 cross section types ($D_1, D_2, S_1, *_2, \blacksquare*_1, \blacksquare*_2, *_{st}, \blacksquare_1, \blacksquare_2$) for a two-group diffusion model, a total of 810 coefficients will be required for each fuel type. There may be 10 or less fuel types in a BWR core. For instance, the SBWR has 5 fuel types in the equilibrium cycle. Considering a maximum of 10 fuel types, we will have 8100 coefficients for a complete representation of the two-group cross sections in terms of the exposure, void history, and instantaneous void effects.

Note that, given the 3D exposure and void history distributions as well as the fuel loading map, we can calculate with the new representation (Eq. 1) the truly 3D nodal cross sections for the neutronic solution without making any gross approximation with regard to the 3D effect of the exposure and void history. Another advantage of this cross section representation is that a single set of cross sections once generated is applicable to any point in the fuel cycle.

The two-group cross sections for each fuel type are usually generated by a lattice physics code (e.g., CASMO) as a function of exposure from the beginning of life to the end of the equilibrium cycle at the instantaneous void fraction of 0.0, 0.4, and 0.7 for the history dependent void fraction of 0.0, 0.4, and 0.7 with the fuel and moderator temperature at the rated condition and a perturbed condition for both the uncontrolled and controlled state. Clearly, a great many cases must be run to obtain the complete set of cross sections. Table 2.1 summarizes the cases of a typical cross section generation required for a BWR.

The coefficients for the new cross section representation can be generated as follows:

1. For each exposure point, obtain the quadratic fits for the instantaneous void fraction $\bullet$ at $\bullet_H = 0.0, 0.4$, and 0.7 . These quadratic coefficients are then used to obtain the quadratic fits for the history dependent void fraction $\bullet_H$. This yields the coefficients a_n, b_n , and c_n .
2. Next, obtain the quadratic fits for the fuel and moderator temperature feedback in terms of the instantaneous void fraction $\bullet$ at the history dependent void fraction of 0.4 for all the exposure points. This yields the coefficients d_n and e_n .
3. Finally, use a least-square fit to obtain the quadratic fits for the exposure using all the exposure points. This yields the coefficients $a_{n1}, a_{n2}, a_{n3}, b_{n1}, b_{n2}, b_{n3}, c_{n1}, c_{n2}, c_{n3}, d_{n1}, d_{n2}, d_{n3}, e_{n1}, e_{n2}, e_{n3}$.

Table 2.1 Summary of Cross Section Generation

					Exposure (GWD/MT)
α_H	α	T_f	T_m	Control	0.0, 0.2, 1.0, ..., 35, 55
0.0	0.0	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.4	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.7	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Perturbed	Perturbed	Out	
0.4	0.0	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.4	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.7	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Perturbed	Perturbed	Out	
0.7	0.0	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.4	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
	0.7	Reference	Reference	Out	X X X ... X X
		Perturbed	Perturbed	In	X X X ... X X
		Perturbed	Perturbed	Out	

where X=case to be run.

Obviously, an auxiliary code must be written to do the quadratic fitting described above. For the instantaneous void and history dependent void, the three values of the void fraction (say, x_1 , x_2 , and x_3) give rise to a system of simultaneous equations of the form:

2. Modeling of Neutron Kinetics

$$\begin{aligned}a_1 + a_2 x_1 + a_3 x_1^2 &= y_1 \\a_1 + a_2 x_2 + a_3 x_2^2 &= y_2 \\a_1 + a_2 x_3 + a_3 x_3^2 &= y_3\end{aligned}\tag{2.5.7}$$

where y_1 , y_2 , and y_3 are the cross section values corresponding to x_1 , x_2 , and x_3 . The solution of the above equations yields the coefficients a_1 , a_2 , and a_3 .

For the exposure fits, we can use any well established least-square fitting routine, for example, the subroutine LFIT on P. 513 of the book entitled "Numerical Recipes" by W. H. Press, B. P. Flannery, S. A. Teukolsky, and W. T. Vetterling. The PC program PSIPLLOT, which has an excellent nonlinear curve fitting capability, can also be used for this purpose.

These quadratic coefficients must be generated for all fuel types (<10). They will be input to RAMONA-4B in place of the existing ones along with the 3D exposure and void history distributions.

2.5.2 Point Kinetics Option

RAMONA-4B is a complete system code with 3D neutron kinetics. While it is important to predict three-dimensional power distributions for transients resulting from local disturbances, there are many system transients with core-wide disturbances such as that due to system pressure, where the effect of three-dimensional power distributions is of secondary importance. For these system transients, point kinetics is adequate to provide the global power response. We recommend that the conventional point kinetics model as described in Section 2.3.2 be added to the RAMONA-4B code as an option. The variable implicit method as used in the BNL Engineering Plant Analyzer (EPA) [Wulff et al., 1984b] can be used to solve the point kinetics Equations (2.3.1) and (2.3.2).

3. MODELING OF HEAT CONDUCTION

This chapter describes the modeling of heat conduction in RAMONA-4B. Section 3.1 describes the thermal-energy transport and storage in fuel rods and Section 3.2 describes that of the structural components in the reactor pressure vessel. The latter is specially important for the simulation of startup transients.

3.1 Heat Conduction in Fuel Rods

3.1.1 Governing Equations of Heat Conduction

The general transient heat conduction equation in an arbitrary geometry is given by

$$\rho C_p \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) + q''', \quad (3.1.1)$$

where ρ , C_p , k , T , q''' , and t designate density, specific heat at constant pressure, thermal conductivity, temperature, volumetric heat generation rate, and time; respectively. Since the fuel element is very long and its radius is very short, the axial and azimuthal conduction is negligible. Thus, the primary heat conduction in the fuel element is in the radial direction. A fuel element is composed of fuel pellet, gas gap and cladding as shown in Figure 3.1. In this case, the general heat conduction equation (3.1.1) reduces to the following form:

For Fuel Pellet:

$$(\rho C_p)_f \frac{\partial T_f}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r} \left(r k_f \frac{\partial T_f}{\partial r} \right) + q_f''', \quad \text{for } 0 \leq r < R_f, \quad (3.1.2)$$

where the subscript f denotes the fuel element.

For Gas Gap:

$$\frac{\partial}{\partial r} \left(k_g \frac{\partial T_g}{\partial r} \right) = 0, \quad \text{for } R_f < r < R_{ci}, \quad (3.1.3)$$

where the subscript g stands for the gas gap.

For Cladding:

$$(\rho C_p)_c \frac{\partial T_c}{\partial t} = \frac{\partial}{\partial r} \left(k_c \frac{\partial T_c}{\partial r} \right) + q_c''', \quad \text{for } R_{ci} \leq r \leq R_{co}, \quad (3.1.4)$$

where the subscript c represents the cladding.

3. Modeling of Heat Conduction

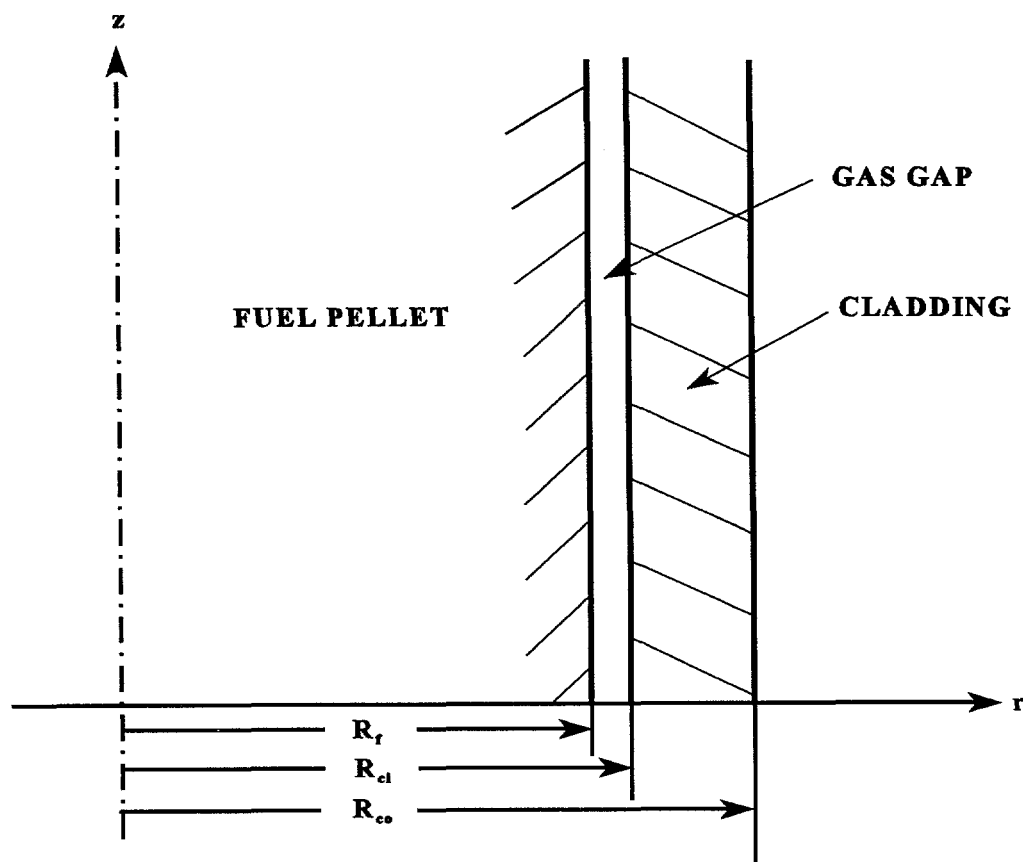


Figure 3.1 Schematic of Fuel Element Geometry.

3.1.2 Boundary Conditions

The boundary conditions imposed for the governing heat conduction equations (3.1.2) through (3.1.4) are the continuity conditions for the heat flux and for the temperature as follows:

$$\frac{\partial T_f}{\partial r} = 0 \quad \text{at } r = 0, \quad (3.1.5)$$

$$k_f \frac{\partial T_f}{\partial r} = k_g \frac{\partial T_g}{\partial r} \quad \text{at } r = R_f, \quad (3.1.6)$$

$$T_f(R_f) = T_g(R_f) \quad \text{at } r = R_f, \quad (3.1.7)$$

$$k_g \frac{\partial T_g}{\partial r} = k_c \frac{\partial T_c}{\partial r} \quad \text{at } r = R_{ci}, \quad (3.1.8)$$

$$T_g(R_{ci}) = T_c(R_{ci}) \quad \text{at } r = R_{ci}, \quad (3.1.9)$$

$$-k_c \frac{\partial T_c}{\partial r} = \bar{h}_c [T_c(R_{co}) - T_\ell] \quad \text{at } r = R_{co}, \quad (3.1.10)$$

where $\bar{h}_c$ is the convective heat transfer coefficient and T_ℓ is the coolant temperature.

Equation (3.1.3) is integrated the radius r and then combined with Eqs. (3.1.6) through (3.1.9) to yield

$$\frac{k_g}{\delta} [T_c(R_{ci}) - T_f(R_f)] = k_f \left[\frac{\partial T_f}{\partial r} \right]_{R_f}, \quad (3.1.11)$$

$$\frac{k_g}{\delta} [T_c(R_{ci}) - T_f(R_f)] = k_c \left[\frac{\partial T_c}{\partial r} \right]_{R_{ci}}, \quad (3.1.12)$$

3. Modeling of Heat Conduction

where $\delta = R_{ci} - R_f$ is the gap width.

3.1.3 Solution Method

The heat conduction equations for the fuel element are solved along with the boundary conditions by means of the standard finite-difference method in space and implicit integration in time. The fuel element is partitioned into a number of concentric rings as shown in Figure 3.2. Typically, four rings in the pellet and two rings in the cladding are used with the gas gap represented explicitly as a single ring.

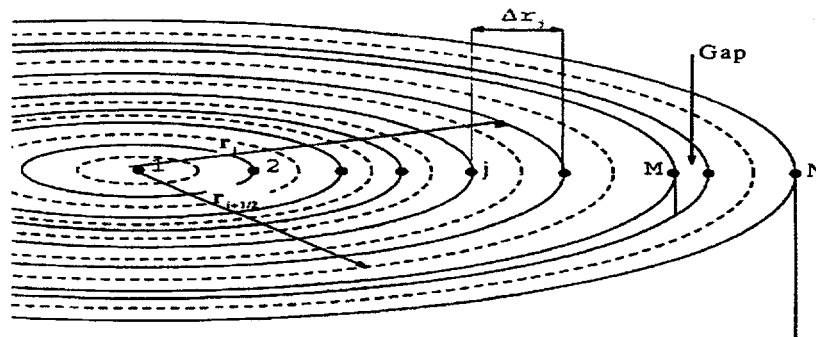


Figure 3.2 Finite-Differenced Fuel Rod Geometry.

The finite-differenced conduction equations are numerically solved by an iterative predictor-corrector method for the fuel pellet and by a successive-substitution procedure for the cladding. The reader is referred to the earlier RAMONA-3B description and assessment document [Wulff, et al., 1984a] for details.

There are two primary restrictions in the fuel conduction model of RAMONA-4B. One restriction is the flat-source assumption in the fuel pellet. The non-uniform distribution of pellet heat source in a high-burnup fuel will affect the thermal time constant, hence the stability of the core. The other restriction is the use of one set of fuel rod parameters for the core. This makes it extremely difficult to model a core with a mixture of 7 x 7 and 8 x 8 fuel assemblies. We recommend that these restrictions be removed in the future improvement.

3.1.4 Constitutive Relations

In this section we specify the thermophysical properties of fuel pellet and cladding, and the gas gap conductance. The convective heat transfer coefficient is described in Chapter 4, Section 4.3.2.

3. Modeling of Heat Conduction

The thermal conductivity k_f for the fuel pellet is given by the equation,

$$k_f = \frac{C_1}{1 + C_2 T_f}, \quad (3.1.13)$$

where C_1 and C_2 are user-specified input constants, and T_f is the local temperature in the fuel pellet. Typical values used are $C_1 = 8.5 \text{ W/m}^\circ\text{C}$ and $C_2 = 0.002 /^\circ\text{C}$.

The volumetric heat capacity of the fuel pellet is treated as an input constant ROCF on Card 720000. The following value is currently used:

$$(\rho C_p)_f = 3.157 \times 10^6 \frac{\text{J}}{\text{m}^3 \text{ } ^\circ\text{C}}. \quad (3.1.14)$$

The thermal conductivity of the cladding is treated as an input constant RCLA on Card 391000. The following value is currently used:

$$k_c = 13.85 \frac{\text{W}}{\text{m } ^\circ\text{C}}. \quad (3.1.15)$$

The volumetric heat capacity of the cladding is also treated as an input constant RCCA on Card 720000. The following value is currently used:

$$(\rho C_p)_c = 2.127 \times 10^6 \frac{\text{J}}{\text{m}^3 \text{ } ^\circ\text{C}}. \quad (3.1.16)$$

The gas gap conductance is given by the correlation:

$$\frac{k_g}{\delta} = \text{Min}(C_3 + C_4 \bar{T}_f + C_5 \bar{T}_f^2, C_6), \quad (3.1.17)$$

where C_3 , C_4 , C_5 and C_6 are user-specified constants on Card 391000, and $\bar{T}_f$ is the average pellet temperature. Currently used values for these constants are:

$$\begin{aligned} C_3 &= C_6 = 5.678 \times 10^3 \frac{\text{W}}{\text{m}^2 \text{ } ^\circ\text{C}}, \\ C_4 &= C_5 = 0. \end{aligned} \quad (3.1.18)$$

3. Modeling of Heat Conduction

3.2 Stored Energy in Heat Structures

3.2.1 Introduction

Considering the mass of the RPV and the internal structures, the combined heat capacity exceeds 389 MJ/°C. Therefore, a small change in temperature is associated with a large heat transfer. Heat release for a slow transient can be even higher than the system decay heat, which is significant during a system shutdown. Startup and other system transients will also be affected by the heat structures. During the system heat up at the rate of 55°C/hr, significant heat absorption by the structural mass of the system is expected. Importance of structural heat transfer during a system transient is evident, and the following sections will illustrate the methodology adopted in RAMONA-4B in order to account for this effect.

At a steady-state operation, the net energy transfer of the system represents power generation in the core, which is balanced by the steam production rate, system mechanical losses, and steady heat loss to the surrounding atmosphere. The operating condition of the reactor and the design specifications determine each of these energy transfer rates quantitatively. Net balance is ensured by the plant calculation.

In order to estimate the heat loss to the surroundings, several steps in modeling and computation were followed in a sequence. During steady-state operation, the temperature of the heat structures will remain at equilibrium. However, it will differ in magnitude for each component type within the RPV.

Heat loss from the RPV surface to the surrounding air will take place through the Downcomers 1 and 2, Lower plenum 1, and the steam dome. These structures are in direct contact with the surrounding air on one side and high-temperature coolant flow on the other side. Temperatures on these surfaces are calculated in order to obtain the rate of heat transfer. In this document we shall refer to another type of structural material of the RPV called the 'internal structures'. During steady-state operation the internal structures corresponding to the Lower plenum 1 and 2, core assembly, and riser will remain at equilibrium with the coolant temperature.

Implementation of the heat-structure model in the RAMONA-4B code has been completed in the following several steps:

- (1) Selection of heat-structure geometry and material within the RPV
- (2) Identification of valid heat-transfer equations
- (3) Implementation of heat-transfer model into a general stand-alone code
- (4) Interfacing the stand-alone code into RAMONA-4B, which includes
 - (a) Classification of types of heat structures in each component
 - (b) Estimation of physical and thermodynamic properties of fluid within each component adjacent to the structures
 - (c) Estimation of surface heat-transfer coefficients
 - (d) Calculation of rate of heat transfer with heat structures
 - (e) Modification of the energy balance equation of each component
 - (f) Input, Output, Graphics, and Restart Modification

Application of the heat-structure model will depend upon the type of the calculation being performed, i.e., (i) For steady state, or (ii) For a Transient.

3.2.2 Selection of Heat-Structure Geometry

Heat transfer during a transient is a strong function of the physical properties and geometric dimensions of a heat structure. Relative dimensions of each of the components in and the corresponding structures of the RPV are shown schematically in Figure 3.3. According to the model, there can be two types of heat structures associated with each component. **(1) Internal Structures and , (2) Boundary Structures.**

The internal structures are surrounded by fluid within a component. The temperature of this fluid and the thermal capacity of this structure and the surface heat-transfer coefficients determine the heat transfer to these structures. The type of structures which fall into internal-structure category include:

- (1) Control-Rod Drives
- (2) Lower and Upper Tie Plates
- (3) Fuel Assembly and Support Structure
- (4) Dryer and Separator Assembly

The boundary structures are solids separating fluids at two different thermal states on its two sides. Therefore, the direction and magnitude of heat transfer will depend on the characteristics of these fluids. The flow and thermal condition of the fluid adjacent to the structure determine the heat-transfer coefficient and corresponding heat flow rate. In case of multiple structures of any component, the net heat transfer to the fluid is the algebraic sum of the individual heat transfers.

According to Figure 3.3, the heat-structure distribution between the components vary depending upon their location within the RPV. The nomenclature used in this figure is in accordance with the description of the components defined inside RAMONA-4B. The boundary structures include:

- (1) RPV Outer Wall at Downcomer 1
- (2) RPV Outer Wall at Downcomer 2
- (3) RPV Outer Wall at Lower Plenum 1
- (4) RPV Outer Wall at Steam Dome
- (5) Shroud Surface at Downcomer 1
- (6) Shroud Surface at Downcomer 2
- (7) Riser Surface

3. Modeling of Heat Conduction

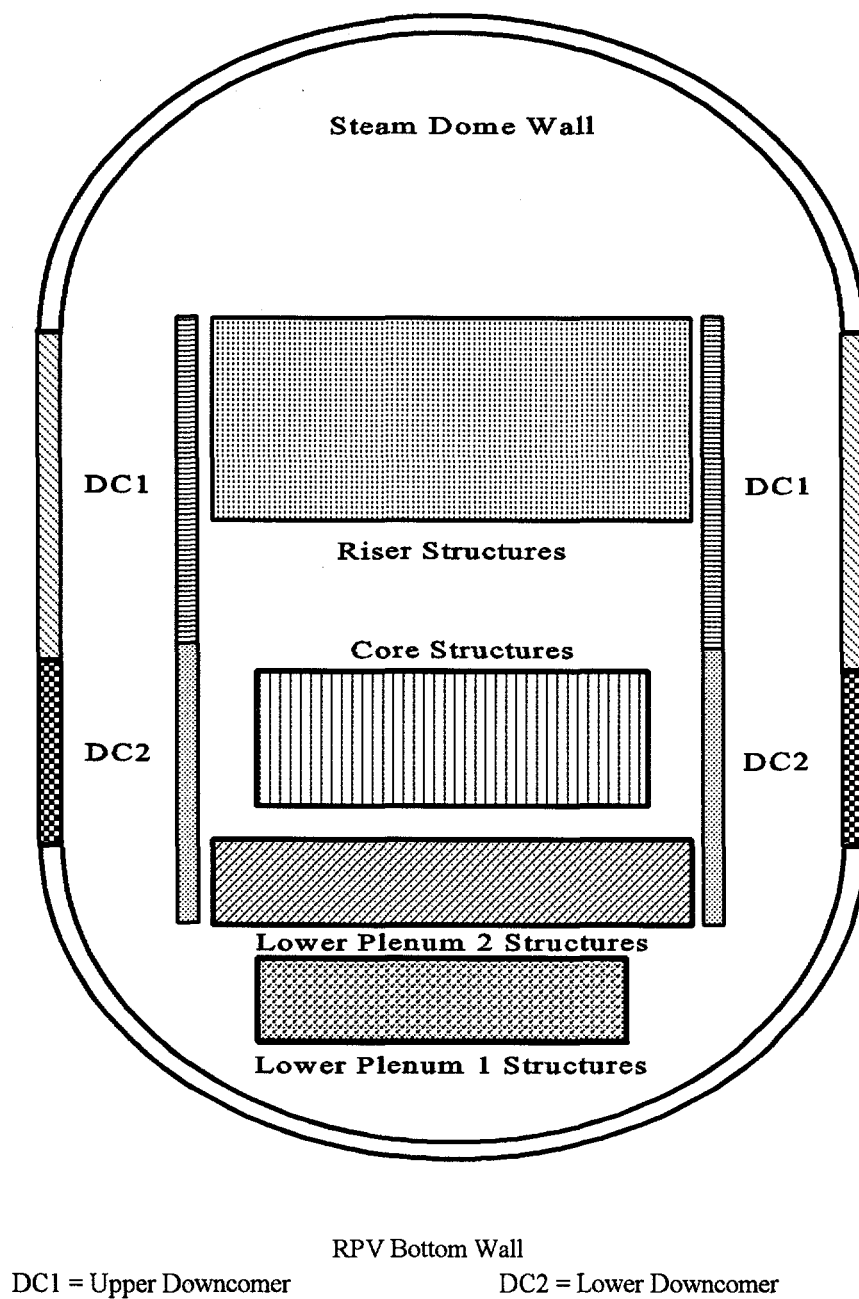


Figure 3.3 Distribution of Heat Structures in RPV for RAMONA-4B.

The total number of heat structures and their identifications are given in Table 3.1.

Table 3.1 Heat Structure Distribution between Components

Component Name	Number of Internal Structures	Number of Boundary Structures
Downcomer 1	0	2
Downcomer 2	0	2
Lower Plenum 1	1	1
Lower Plenum 2	1	0
Riser	1	1

3.2.3 Selection of Heat-Structure Materials

The structural materials of the RPV are defined through the input to the code. Each structure of each component can be individually prescribed to be of one homogeneous material. There are two types of materials which are available for selection; namely,

- (a) Stainless steel, and
- (b) Zirconium

Material of the steam dome has been preselected to be of stainless steel. The physical properties corresponding to the structural materials have been collected from (GE SSAR, 1992).

3.2.4 Heat-Transfer Equations

The rate of heat transfer within each solid structure is calculated from Fourier's equation of heat transfer. Convective heat transfer within the liquid and gaseous region is also used to obtain the surface heat transfer of the structure. Selection of an appropriate set of equations was done on the basis of certain valid assumptions pertaining to the geometry and physical properties.

3.2.4.1 Heat Transfer Coefficients

(a) Heat-Transfer Coefficient with Liquid

Any solid structure in contact with a liquid is subject to convective heat transfer with the liquid. The magnitude of heat transfer is a function of the heat-transfer coefficient, surface area, and the temperature difference between the surface and the bulk liquid. The heat-transfer coefficient is calculated assuming turbulent flow in a smooth tube [Holman, 1990] as follows:

$$\bar{h} = \frac{N_{\ell} K_{\ell}}{L} \quad (3.2.1)$$

3. Modeling of Heat Conduction

For fully developed **turbulent** flow,

$$Nu = 0.023 Re^{0.8} Pr^{0.4}, \quad (3.2.2)$$

where

$$Re = \frac{\rho_l \bar{V} D_h}{\nu}, \quad (3.2.3)$$

and

$\bar{V}$	=	Average fluid velocity m/s,
K_l	=	Conductivity of the liquid W/m K,
L	=	Height of the structure m,
ν	=	Kinematic viscosity m ² /s,
D_h	=	Hydraulic diameter m,
Pr	=	Prandlt Number,
Re	=	Reynolds Number,
Nu	=	Nusselt Number,
ρ_l	=	Density of the liquid kg/m ³ .

(b) Heat-Transfer Coefficient with Ambient Air

External to the RPV surface, the surrounding air is at a higher temperature than the bulk air temperature of the containment space. Insulation of the outer surface is provided to prevent excessive heat loss from the RPV to the surrounding. However, the vertical height of 25 m has a potential of natural-convection heat removal from the system. A simplified correlation for natural-convection heat transfer for air [Holman, 1990] is given by,

$$h = B \left(\frac{\Delta T}{L} \right)^{0.25} \quad (W/m^2/^{\circ}C), \quad (3.2.4)$$

where	B	=	Coefficient (suggested value; $B = 1.42$, [Table 7.2, Holman, 1990]),
	L	=	Height of Component (m),
	ΔT	=	$T_{wall} - T_{\infty}$,
	T_{wall}	=	Outer Wall Temperature ($^{\circ}C$),
	T_{∞}	=	Air Temperature ($^{\circ}C$).

(c) Heat Transfer for Steam Dome

The heat transfer across the steam dome structure is calculated based on given information on the heat transfer coefficient and ambient air temperature. These are inputs to the code and may be changed accordingly. Unlike the other components of the RPV, this component is treated differently at steady state because it does not have the high heat transfer coefficient in the inner side which is related to liquid flow. The steam in the dome may be assumed to be saturated at the dome pressure. Using a prescribed outer surface temperature, the rate of heat transfer for the dome to the surrounding air is calculated.

At steady state the rate of heat transfer from the steam dome surface is therefore calculated as,

$$q_{Dome} = h_{air} A_{Dome} (T_o - T_{\infty}) \text{ Watts,} \quad (3.2.5)$$

where h_{air} = Heat transfer coefficient of air $W/m^2/^{\circ}C$,
 A_{Dome} = Outer surface area of the steam dome m^2 ,
 T_o = Outer surface temperature $^{\circ}C$,
 T_{∞} = Ambient air temperature $^{\circ}C$.

Example of Heat Transfer for Steam Dome:

The following is an example of heat transfer through the dome for the SBWR. Here the steam dome region has been divided into two parts, according to the availability of information. The upper hemispherical head and the connected vertical section together consist the component called steam dome. The heights corresponding to these regions are 3.0 m and 3.954 m, respectively.

#Given the average heat transfer coefficient h_{air} in the outer surface as .907 $W/m^2-^{\circ}C$

#Given the minimum air temperature T_o , outside the vessel and surface insulation are as follows:

Outside the bellows, bottom head and inside vessel support skirt: $38^{\circ}C$

Outside vessel support skirt: $38^{\circ}C$

Top head: $57^{\circ}C$

Using the following surface area for the upper dome region and the vertical region below the upper dome we can estimate the design heat-transfer conditions.

Outer surface area in the upper dome region: $56.54 m^2$

Outer surface area in the vertical region: $74.531 m^2$

Total steam-dome surface area: $131.07 m^2$

The temperature on the surface of the structure is needed in order to calculate the heat transfer. For the SBWR, this was estimated to be $T_o = 274^{\circ}C$.

Therefore, estimated heat-transfer rates are:

In the upper dome region: $q_{max}'' = 1.89 \times 10^{-4} MW/m^2 \times 56.54 m^2 = 0.010686 MW$

In the vertical region: $q_{max}'' = 2.04 \times 10^{-4} MW/m^2 \times 74.531 m^2 = 0.015204 MW$

Total steam-dome heat transfer = $q_{Dome}'' = 0.0259 MW$

3. Modeling of Heat Conduction

Given at operating conditions, the average maximum heat-transfer rates are as follows:

In the refueling bellows region: 65 Btu/ft²-hr (0.2046 kW/m²)

In the top head region: 60 Btu/ft²-hr (0.189 kW/m²)

Other Pertinent Quantities of Steam-Dome Structure:

$$\text{Biot number } Bi = h_{\text{air}} S / k = h_{\text{air}} (r_o - r_i) / k_s = 0.00269$$

Since the Bi in the outer surface is less than 0.2, constant temperature in the heat structure material can be assumed.

Thickness of the structure	$r_o - r_i$	= 0.11 m
Volume of the structure	V	= 14.4178 m ³
Mass of the structure	M	= 112473 kg (for 1% carbon steel)

3.2.4.2 Heat Transfer in Internal Structures

Transient Heat Transfer:

Transient heat transfer between the fluid and the structure of each component has been calculated on the basis of their orientation. The internal and the boundary structures have different interfaces for heat transfer.

For transient heat transfer, the internal structures are treated as lumped solids with a uniform surface heat-transfer coefficient. The heat-transfer coefficient is a function of the surrounding fluid properties. For the lower plenums 1 and 2, and the core assembly and riser, the heat-transfer coefficients are calculated from the coolant flow conditions. In case of the steam dome, the assumptions for internal structures are valid. However, a constant heat-transfer coefficient is used. The time constant associated with each solid is estimated assuming constant physical properties.

The governing equation for the transient heat transfer is:

$$\rho C V \frac{dT}{dt} = -h A (T - T_f), \quad (3.2.6)$$

or in finite-difference form via implicit integration,

$$T^{n+1} \left(1 + \frac{h A}{\rho C V} \Delta t \right) = T^n + \left(\frac{h A}{\rho C V} \Delta t \right) T_f^{n+1}, \quad (3.2.7)$$

where

T^{n+1}	=	New time temperature of the structure °C,
T^n	=	Old time temperature of the structure °C,
T_f	=	Fluid temperature at old time °C,
h	=	Fluid heat-transfer coefficient W/m ² -°C,
C	=	Specific heat of structural material J/kg°C,

ρ = Density of the structural material kg/m^3 ,
 A = Heat-transfer area m^2 .

The above relation for convective cooling is valid for the internal structures within the RPV. These structures have been treated as equivalent masses which are effective in heat transfer only during transient operation of the reactor.

Steady-State Heat Transfer:

In case of steady-state operation $T = T_r$ in Equation (3.2.6), therefore no heat-transfer calculations are performed for the internal structures.

3.2.4.3 Heat Transfer in Boundary Surface Structures

Transient Temperature Solution:

The structures falling in this category has been described in section 3.2.1. Steady-state and transient heat transfer of these structures are calculated based on the integral solution to the transient heat-conduction equation [Wulff 1980]:

Governing Equation:

$$\rho c \frac{\partial T}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r} \left(r k \frac{\partial T}{\partial r} \right) + q''' \quad (3.2.8)$$

Two Boundary Conditions:

$$\frac{\partial T}{\partial r} = \frac{h_i}{k} (T_i - T_{i,\infty}) \quad \text{at } r = R_i > 0, t \geq 0, \quad (3.2.9)$$

$$\frac{\partial T}{\partial r} = \frac{h_j}{k} (T_{j,\infty} - T_j) \quad \text{at } r = R_j > R_i, t \geq 0. \quad (3.2.10)$$

One Initial Condition:

$$T(0, r) = f(r) \quad R_i \leq r \leq R_j. \quad (3.2.11)$$

Here $T_{i,\infty}$ and $T_{j,\infty}$ are coolant temperatures, T_i and T_j are solid surface temperatures on the corresponding surfaces, and $f(r)$ is an arbitrary initial function.

(a) General Solution Scheme

There are different ways to solve the partial differential equation (PDE), Eq. (3.2.8), subject to the boundary conditions given by Eqs. (3.2.9) and (3.2.10). The method followed in RAMONA-4B is an integral approach, where the PDE of Eq. (3.2.8) is converted into an ordinary differential equation by defining an averaging operator $\langle \rangle$

3. Modeling of Heat Conduction

$$\langle \phi \rangle = \frac{2}{R_j^2 - R_i^2} \int_{R_i}^{R_j} \phi(r) \cdot r dr. \quad (3.2.12)$$

Applying the operator on the governing equation and the boundary conditions, we have

$$\frac{d\langle T \rangle}{dt} = \frac{2 [R_i h_i (T_{i,\infty} - T_i) + R_j h_j (T_{j,\infty} - T_j)]}{\rho C_p (R_j^2 - R_i^2)}, \quad (3.2.13)$$

subject to

$$\langle T \rangle = \langle f(r) \rangle \quad \text{at } t = 0. \quad (3.2.14)$$

Alternatively, we have for a slab

$$\frac{d\langle T \rangle}{dt} = \frac{\alpha}{S^2} [(Bi)_i (T_{i,\infty} - T_i) + (Bi)_j (T_{j,\infty} - T_j)], \quad (3.2.15)$$

where

$$\begin{array}{lll} \alpha & = & \text{Thermal diffusivity} = k / \rho C_p \\ Bi & = & \text{Biot Number} = S^* h / k \end{array}$$

Equation (3.2.15) is an ordinary differential equation with an exact solution for $\langle T \rangle$, if T_i and T_j , the surface temperatures, are known. This equation is numerically integrated in RAMONA-4B in the explicit form for each hydraulics time step. The surface temperatures have been solved in the integral approach as follows.

(b) Surface -Temperature Solution

Assuming a quadratic polynomial for the temperature distribution, such that

$$\tilde{T}(r, \tau) = a(\tau) + b(\tau) \zeta + c(\tau) \zeta^2, \quad (3.2.16)$$

where

$$\zeta = \frac{r - R_i}{R_j - R_i}, \quad (3.2.17)$$

and

$$\langle \tilde{T}(\tau) \rangle = \frac{1}{V} \int_V \tilde{T}(r, \tau) dV; \quad (3.2.18)$$

i.e., the assumed temperature distribution is correct over the integrated volume.

Coefficients a , b , and c in Eq. (3.2.16) are time-dependent coefficients, which are solved using the two boundary conditions and the initial condition shown in section 3.2.4.3.

Temperature at Surface 1 ($\zeta = 0$):

$$T_i = \beta_{11} T_{i,\infty} + \beta_{12} \langle T \rangle + \beta_{13} T_{j,\infty}. \quad (3.2.19)$$

Temperature at Surface 2 ($\zeta = 1$):

$$T_j = \beta_{21} T_{i,\infty} + \beta_{22} \langle T \rangle + \beta_{23} T_{j,\infty}. \quad (3.2.20)$$

Here the coefficients β 's are functions of Biot number as follows:

$$\left. \begin{aligned} \beta_{11} &= \frac{(Bi)_1 [4 + (Bi)_2]}{D}, \\ \beta_{12} &= \frac{6[2 + (Bi)_2]}{D}, \\ \beta_{13} &= \frac{2(Bi)_2}{D}, \\ \beta_{21} &= \frac{2(Bi)_1}{D}, \\ \beta_{22} &= \frac{6[2 + (Bi)_1]}{D}, \\ \beta_{23} &= \frac{(Bi)_2 [4 + (Bi)_1]}{D}, \end{aligned} \right\} \quad (3.2.21)$$

where $(Bi)_j = (Sh_j)/k$, for $j = \text{surfaces } 1, 2$. $\langle T \rangle$ is the spatially averaged slab temperatures, and

$$D = 12 + 4[(Bi)_1 + (Bi)_2] + (Bi)_1 (Bi)_2. \quad (3.2.22)$$

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(c) Interior Temperature Distribution

The temperature within the solid can also be obtained from Eq. (3.2.19), following the distribution within the solid structure,

$$T(\zeta, \tau) = T_i + b \zeta + c \zeta^2, \quad 0 \leq \zeta \leq 1, \quad (3.2.23)$$

where ζ is defined in Eq. (3.2.17).

Steady-State Temperature Solution:

The steady-state temperature for the boundary heat structures can be expressed as

$$\langle T(o) \rangle = \frac{\left\{ -T_i \left[(\beta_{11} - 1) (Bi)_1 + \beta_{21} (Bi)_{21} \right] - T_o \left[\beta_{13} (Bi)_1 + (\beta_{23} - 1) (Bi)_2 \right] \right\}}{\left[\beta_{12} (Bi)_1 + \beta_{22} (Bi)_2 \right]}, \quad (3.2.24)$$

where the coefficients are as prescribed for transient solutions of boundary structures.

Limitations To Heat-Structure Implementation in RAMONA-4B

- Physical properties k , ρ , and C_p vary from (5-10)% with a change of temperature from 100°C to 200°C, which are assumed to be constant.
- Knowledge of the heat-transfer coefficient is generally in the $\pm 25\%$ range.
- Limited knowledge of system boundary conditions. The RPV surface temperatures need to be assessed.

Example of Boundary Heat-Structure Calculation:

DOWNCOMER 1

Structure 1:

Time Constant:

$$\tau_1 = \frac{\rho C V}{h A} = 516 \text{ s}$$

Thermal Diffusivity:

$$\alpha = \frac{k}{\rho C} = 0.1 \times 10^{-4} \frac{m^2}{s}$$

3. Modeling of Heat Conduction

Biot Number:

$$Biot = \frac{\bar{h}_o s_1}{k_1} = 1.931$$

Fourier Number:

$$F_o = \frac{\tau \alpha}{s^2} = 2.064$$

Inverse Diffusivity:

$$\frac{s_1^2}{\alpha} = 250 \text{ sec}$$

Therefore,

$$\tau_1 > \frac{s_1^2}{\alpha} .$$



4. MODELING OF THERMAL HYDRAULICS

4.1 Overview

The thermal hydraulics in RAMONA-4B is based upon one-dimensional, non-equilibrium, drift-flux formulation of two-phase flow; liquid can be subcooled, saturated, or superheated while vapor is always at saturation. Four conservation equations for vapor mass, mixture mass, mixture momentum, and mixture energy describe the coolant dynamics in the vessel. Conservation equations for vapor mass and momentum describe the acoustic effects in the steam line due to actions of the turbine, bypass, or safety valves which are modeled as a part of the steam line. A boron-mass-conservation equation is used to predict the boron transport in the reactor vessel. Some of the details of the formulations are given elsewhere [Wulff et al., 1984a].

A single pressure is computed in the reactor vessel, and it is used for calculating all phasic properties. This technique eliminates the acoustic effects in the vessel and contributes significantly to the computing efficiency of RAMONA-4B.

Closed-contour integral momentum equations are used to calculate the axial phasic velocities in closed loops: the loops share one-dimensional components with each other (Downcomer, Lower Plenum, and Riser), while each loop includes one of the core parallel channels. Also, integration of the partial differential equation for the mixture mass conservation is transformed into a simple quadrature in space. These modeling techniques significantly increase the computing speed of RAMONA-4B without loss in accuracy.

The constitutive package includes the description of nonequilibrium vapor generation and condensation, wall shear, flow dependent loss coefficient, two-phase multipliers, and wall heat transfer. The package for wall heat transfer includes a model for the post-CHF regime.

The code models individual components specific to BWR systems of a typical U.S. design. These include models for jet pumps, recirculation pumps, and steam separators. However, all recirculation loops and stream lines are lumped together into one effective recirculation loop and one equivalent steam line, respectively. The code calculates the location of the mixture and collapsed-water levels in the reactor vessel.

Liquid from the High Pressure Coolant Injection (HPCI) and Reactor Core Isolation Cooling (RCIC) systems is injected into the feedwater line. The boron solution from the Standby Liquid Control System (SLCS) can be injected anywhere in the reactor vessel.

The code also includes models which are specific to Simplified Boiling Water Reactors (SBWR) such as Isolation Condenser (IC), passive Standby Liquid Control System (SLCS), and the option for natural circulation operation at steady state.

The thermal-hydraulic equations and the boron transport equations are integrated by the explicit first-order Euler method, whereas the steam-line dynamics is calculated by the fourth-order Runge-Kutta-Simpson method. The time steps for integration of the vessel thermal hydraulics are always smaller or equal to the neutronic time steps. Furthermore, the integration time steps for the steam-line equations are smaller than those in the vessel thermal-hydraulic calculation since the steam-line model is designed to predict the acoustic effects.

4. Modeling of Thermal Hydraulics

4.2 Changes from RAMONA-3B/MOD0/CYCLE4

The development and modifications of the thermal-hydraulic models are described in the sections that follow. The material is arranged in several major groups: Balance Equations, Constitutive Equations, Special Models, SBWR Components, Low-Pressure Properties, and general code improvements. The formal description of the models is available in the previous documentation [Wulff et al., 1984a].

Modifications to the balance equations (Section 4.3) include development of the system pressure equation with fewer approximations (Section 4.3.1) and implementation of a reverse-flow capability (Section 4.3.2). The reverse-flow logic for the components' internal cells is given in Section 4.3.2.1, and the corresponding logic for the interfaces between the multichannel component (core) and one-dimensional components (lower plenum and riser) is described in Section 4.3.2.2.

The section on constitutive relationships (Section 4.4) describes the single-phase and two-phase momentum transfer of the wall (Section 4.4.1), wall heat transfer in the post-CHF regime (Section 4.4.2), and interfacial momentum and mass transfer (Sections 4.4.3 and 4.4.4). Specifically, the newly implemented drift-flux model description and flow calculations based on this model are given in Section 4.4.3.1, and the equilibrium vapor generation model for the post-CHF regime is described in Section 4.4.4.1. A model was developed for predicting the condensation rate in cases where subcooled liquid (either feedwater or ECC) injected through the feedwater sparger enters the steam environment in the upper downcomer region of a BWR vessel (Section 4.4.4.2).

Section 4.5 describes the special models implemented into RAMONA-4B. A recirculation-pump model based on the nondimensional equations for the pump head and torque (homologous curves) was implemented following a single-phase pump model developed at BNL [Madni et al., 1978]; this model is described in Chapter 4.5.1. Chapter 4.5.2 describes the steam carry-under calculations in the steam dome and downcomer regions.

The heat structure model used in RAMONA-4B is described in Section 4.5.3. Steady-state and transient heat transfer from the RPV to the surrounding air has been modeled here. Section 4.5.4 describes the boron transport model which allows arbitrary injection of boron into the RPV. In Section 4.6, SBWR specific component models are described. This includes the Isolation Condenser (IC), Standby Liquid Control System (SLCS), and a model to accommodate the large chimney structure of SBWR. Low-pressure thermodynamic properties of steam and water have also been implemented in the code and are documented in Section 4.7. Miscellaneous other improvements of the code are reported in Section 4.8.

A comprehensive description of modeling the control and safety systems is presented in Chapter 5. Pressure and feedwater control systems are provided in Sections 5.2 and 5.3. A recirculation-flow-control-system model is given in section 5.4. Balance of Plant Models (BOP) are presented in Section 5.5.

4.3 Formulation of Balance Equations

4.3.1 Field Equations for Thermal-Hydraulics of Coolant

RAMONA-4B employs a drift-flux mixture model for nonhomogeneous, nonequilibrium two-phase flow of coolant. The basic field equations are the volume and time averaged conservation equations for vapor mass, liquid mass, mixture energy, and mixture momentum as given below:

Vapor Mass:

$$\frac{\partial}{\partial t}(\alpha \rho_g) + \nabla \cdot (\alpha \rho_g \vec{v}_g) = \Gamma_v \quad (4.3.1)$$

Liquid Mass:

$$\frac{\partial}{\partial t}[(1-\alpha)\rho_l] + \nabla \cdot [(1-\alpha)\rho_l \vec{v}_l] = -\Gamma_v \quad (4.3.2)$$

Mixture Energy:

$$\frac{\partial}{\partial t}[\alpha \rho_g e_g + (1-\alpha)\rho_l e_l] + \frac{\partial}{\partial z}[\alpha \rho_g h_g v_g + (1-\alpha)\rho_l h_l v_l] = \frac{q_w'}{A} + (1-\alpha)q_l''' \quad (4.3.3)$$

Mixture Momentum:

$$\frac{\partial G_m}{\partial t} + \frac{\partial}{\partial z}[\alpha \rho_g v_g^2 + (1-\alpha)\rho_l v_l^2] = -\frac{\partial P}{\partial z} - g_z \rho_m - F_w - F_L \quad (4.3.4)$$

where g_z is the axial component of the gravitational constant (positive for downward flow and negative for upward flow), F_w is the wall friction, and F_L is the form loss.

Here the standard nomenclature is used. The mixture parameters are related to the phasic parameters as follows:

$$G_m = \alpha \rho_g v_g + (1-\alpha)\rho_l v_l, \quad (4.3.5)$$

and

$$\rho_m = \alpha \rho_g + (1-\alpha)\rho_l. \quad (4.3.6)$$

The wall friction F_w is given by the equation,

$$F_w = f_{t0} \phi_{t0}^2 \frac{G_m |G_m|}{2 \rho_l d_h}, \quad (4.3.7)$$

4. Modeling of Thermal Hydraulics

and the form loss is represented by the equation,

$$F_L = K \Phi_l^2 \frac{G_m |G_m|}{2 \rho_l}, \quad (4.3.8)$$

where f_{l0} is the single-phase friction factor, Φ_{l0}^2 is the two-phase friction multiplier, d_h is the hydraulic diameter, K is the form loss coefficient, and Φ_l^2 is the two-phase multiplier for form loss.

The vapor generation rate Γ_v consists of two terms, one due to bulk boiling and the other due to wall heating; namely, $\Gamma_v = \Gamma_{v,b} + \Gamma_{v,w}$. The bulk boiling term $\Gamma_{v,b}$ is prescribed by the empirical correlation [Wulff et al., 1984a]:

$$\Gamma_{v,b} = \frac{1}{h_{fg}} [C_{11} + C_{12} \alpha (1 - \alpha)] [(T_l - T_s) + C_{13} |T_l - T_s|], \quad (4.3.9)$$

and the wall heating term $\Gamma_{v,w}$ is given by the equation,

$$\Gamma_{v,w} = \frac{\frac{q'_w}{A}}{h_{fg} + C_p (T_s - T_l) \frac{\rho_l}{\rho_g} + \frac{1}{2} C_p (T_w - T_s) \left(\frac{1}{\rho_g} - \frac{1}{\rho_l} \right) \rho_l}. \quad (4.3.10)$$

One of the unique modeling features of RAMONA-4B is the use of mixture flux divergence equation instead of the liquid mass equation. This has the advantage of reducing time integration to spatial summation using a simple quadrature. The mixture flux divergence equation can be derived by adding the vapor and liquid mass equations. The detail is given in the 1984 RAMONA-3B report [Wulff et al., 1984a]. The mixture flux divergence equation is given as follows:

$$\begin{aligned} \nabla \cdot \vec{j}_m = \Gamma_v & \left[\frac{\rho_l - \rho_g}{\rho_l \rho_g} + \frac{1}{\rho_l^2} (h_g - h_l) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] - \dot{p} \left[\frac{\alpha}{\rho_g} \frac{d\rho_g}{dp} + \frac{1 - \alpha}{\rho_l} \left(\frac{\partial \rho_l}{\partial p} \right)_u \right] \\ & - \frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p \left\{ (1 - \alpha) q_l''' - \nabla \cdot [(1 - \alpha) \vec{q}_l] \right\}, \end{aligned} \quad (4.3.11)$$

where $\dot{p}$ is the rate of pressure change.

Another unique modeling feature of RAMONA-4B is the use of integrated loop momentum equation instead of the local momentum equation (4.3.4). This greatly reduces the stiffness of the local momentum equation whose time integration via explicit integration requires very small time steps, thus contributing to the computing efficiency of RAMONA-4B. The loop momentum equations are derived by integrating Eq. (4.3.4) along all the closed coolant flow loops in the reactor vessel. For a core with multiple coolant channels as in an actual BWR, all the coolant channels share a common external flow segment consisting of the riser, downcomer and lower plenum. The loop momentum equations in RAMONA-4B are given by

$$\frac{dI_j}{dt} = D_j - F_j - L_j; \quad j=1,2,3,\dots \quad (4.3.12)$$

where the subscript j denotes the loop index, I_j is the loop momentum, D_j is the driving potential of loop j , F_j is the friction loss of loop j , and L_j is the form loss of loop j , all of which are integrated over the entire loop space.

The driving head of loop j is given by the equation,

$$D_j = \sum_{k=1}^{k=k_{\max}} \left[((1 - \alpha_k) \rho_{\ell,k} + \alpha_k \rho_g) g_{z,k} \Delta Z_k + \delta_{np,k} \Delta P_{\text{pump}} \right], \quad (4.3.13)$$

the friction loss of loop j is given by

$$F_j = \sum_{k=1}^{k=k_{\max}} f_{\ell 0,k} \Phi_{\ell 0,k} \frac{w_k |w_k| \Delta Z_k}{2 \rho_{\ell,k} A_k^2 D_{h,k}} \quad (4.3.14)$$

and the form loss of loop j is given by

$$L_j = \sum_{k=1}^{k=k_{\max}} \left(\left[\frac{K_k \Phi_{\ell,k}^2 w_k |w_k|}{2 \rho_{\ell,k} A_{k,\min}^2} \right]_{\text{inlet}} + \left[\frac{K_k \Phi_{\ell,k}^2 w_k |w_k|}{2 \rho_{\ell,k} A_{k,\min}^2} \right]_{\text{exit}} \right), \quad (4.3.15)$$

where

- α_k = average void fraction in cell k ,
- $\rho_{\ell,k}$ = average liquid density in cell k ,
- ρ_g = average vapor density independent of k ,
- ΔZ_k = spacing of cell k ,
- Δp_{pump} = pump head,
- $g_{z,k}$ = gravitational constant,
- $f_{\ell 0,k}$ = single-phase friction factor in cell k ,
- $\Phi_{\ell 0,k}^2$ = two-phase multiplier for friction in cell k ,
- w_k = mixture mass flow rate at exit of cell k ,
- A_k = flow area of cell k ,
- $A_{k,\min}$ = minimum flow area at inlet or exit of cell k ,
- $D_{h,k}$ = hydraulic diameter of cell k ,
- K_k = loss coefficient at inlet or exit of cell k ,
- $\Phi_{\ell,k}^2$ = two-phase multiplier for form loss in cell k ,
- $\delta_{np,k}$ = Kronecker delta function (=1 if $k=np$, =0 otherwise).

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4.3.2 System Pressure Equation

This section describes the modified model for the transient pressure calculation in the vessel. The new model uses fewer approximations as compared to the model previously implemented [Wulff et al., 1984a]. Note that RAMONA-4B predicts one value for the pressure in the reactor vessel; namely, the average vessel pressure.

Integration of Equation (4.3.11) over the entire reactor vessel yields:

$$\begin{aligned}
 -\frac{w_{fw}}{\rho_{fw}} + \frac{\omega_{sl}}{\rho_g} = \Psi_{tot} \left[\frac{\rho_l - \rho_g}{\rho_l \rho_g} + \frac{1}{\rho_l^2} (h_g - h_l) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] \\
 - \dot{P} \int_{Vessel} \left[\frac{\alpha}{\rho_g} \frac{d\rho_g}{dp} \frac{1-\alpha}{\rho_l} \left(\frac{\partial \rho_l}{\partial p} \right)_u \right] dV - Q_{tot} \left[\frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p \right],
 \end{aligned} \tag{4.3.16}$$

where w_{fw} is feedwater flow rate including all ECC and boron injections, w_{se} is steam flow rate including the IC steam flow, and Q_{tot} is the total rate of the energy deposition into the coolant in the core region.

The following approximations are made for different terms in Equation (4.3.16):

- **Assumption 1.** The term $\frac{\rho_l - \rho_g}{\rho_l \rho_g}$ is approximated as $\frac{\rho_f - \rho_g}{\rho_f \rho_g}$;
- **Assumption 2.** The term $\frac{1}{\rho_l^2} (h_g - h_l) \left(\frac{\partial \rho_l}{\partial u} \right)_p$ is approximated as $\frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p$;
- **Assumption 3.** The last term in equation (4.3.2), $\frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p$ is approximated as $\frac{1}{\rho_{l,eff}^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p$.

The density of subcooled liquid in RAMONA-4B is calculated [Wulff et al., 1984a, Equation 4.4.76 and 4.4.82] as follows:

$$\rho_l = \rho_f + \left(\frac{\rho_l}{\partial T} \right)_p (T_l - T_{sat}), \tag{4.3.17}$$

$$\left(\frac{\partial \rho_l}{\partial u} \right)_p = \frac{1}{C_{p,l}} \left(\frac{\partial \rho_l}{\partial T} \right)_p. \tag{4.3.18}$$

The equation for the system pressure can be obtained from Equation (4.3.16) using assumptions 1 through 3:

$$\dot{p} = \frac{\frac{\omega_{fw}}{\rho_{fw}} - \frac{\omega_{sl}}{\rho_g} + \Psi_{tot} \left[\frac{\rho_f - \rho_g}{\rho_f \rho_g} + \frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] - \frac{Q_{tot}}{\rho_{l,eff}^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p}{\int_{vessel} \left[\frac{\alpha}{\rho_g} \frac{d\rho_g}{dp} + \frac{1-\alpha}{\rho_l} \left(\frac{\partial \rho_l}{\partial p} \right)_u \right] dV} \quad (4.3.19)$$

The density $\rho_{l,eff}$ was introduced in Assumption 3 to account for the inaccuracy incurred during the integration of Equation (4.3.11) when the local values of the terms being integrated were replaced by their cell averages. As shown in the estimation below, the value of $\rho_{l,eff}$ differs only by a fraction of a percent from the liquid density at the core entrance.

Derivation of $\rho_{l,eff}$:

The expression for $\rho_{l,eff}$ can be derived by using equation (4.3.3) for a steady-state condition ($\dot{p} = 0$):

$$\frac{\omega_{fw}}{\rho_{fw}} - \frac{\omega_{sl}}{\rho_g} + \Psi_{tot} \left[\frac{1}{\rho_g} - \frac{1}{\rho_f} + \frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] - \frac{Q_{tot}}{\rho_{l,eff}^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p = 0, \quad (4.3.20)$$

or,

$$\frac{Q_{tot}}{\rho_{l,eff}^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p = \frac{\omega_{fw}}{\rho_{fw}} - \frac{\omega_{sl}}{\rho_g} + \Psi_{tot} \left[\frac{1}{\rho_g} - \frac{1}{\rho_f} + \frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right]. \quad (4.3.21)$$

Solving for $\rho_{l,eff}$ we obtain

$$\rho_{l,eff} = \frac{\sqrt{Q_{tot} \left(\frac{\partial \rho_l}{\partial u} \right)_p}}{\frac{\omega_{fw}}{\rho_{fw}} - \frac{\omega_{sl}}{\rho_g} + \Psi_{tot} \left[\frac{1}{\rho_g} - \frac{1}{\rho_f} + \frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right]}. \quad (4.3.22)$$

The calculation procedure for $\rho_{l,eff}$ includes two parts:

- **STEADY STATE:** calculate $C = \frac{\rho_{l,eff}}{\rho_{l,entrance}^{core}}$ for use in the transient calculation;

4. Modeling of Thermal Hydraulics

- **TRANSIENT:** calculate $\rho_{l,eff} = C \times \rho_{l,entrance}^{core}$ for use in the $\dot{p}$ Equation (4.3.19).

An estimation of coefficient C is performed below for a steady-state condition. The parameters were taken from a sample RAMONA-4B steady-state calculation:

$$p = 7.0 \text{ MPa},$$

$$\rho_g = 36.536 \text{ kg/m}^3,$$

$$h_g = 2.773450 \text{ E}+6 \text{ J/kg},$$

$$\rho_f = 740.028 \text{ kg/m}^3,$$

$$h_f = 1.26741 \text{ E}+6 \text{ J/kg},$$

$$\rho_{fw} = 878.966 \text{ kg/m}^3,$$

$$\left(\frac{\partial \rho_l}{\partial u} \right)_p = -3.75 \times 10^{-4} \text{ kg}^2/\text{m}^3,$$

$$w_{sl} = w_{fw} = \Psi_{tot} = 1681.174 \text{ kg/s},$$

$$\rho_{l,entrance}^{core} = 762.2 \text{ kg/m}^3,$$

$$Q_{tot} = 3.293 \times 10^9 \text{ W}.$$

The value of $\rho_{l,eff}$ calculated by using Equation (4.3.22) is:

$$\rho_{l,eff} = 763.481 \text{ kg/m}^3,$$

and the value of coefficient C is:

Finally, the system pressure Equation (4.3.19) is rewritten in the RAMONA-4B implementation form:

$$C = \frac{\rho_{l,eff}}{\rho_{l,entrance}^{core}} = 1.00168.$$

$$\dot{p} = \frac{W_{fw} + W_{l,ic} + W_{ecc} + W_{binj} - W_{sl} - W_{s,ic} + W_{\Gamma_v} + W_{expan}^{core}}{\int_{V_{vessel}} \left[\frac{\alpha}{\rho_g} \frac{d\rho_g}{dp} + \frac{(1-\alpha)}{\rho_l} \left(\frac{\partial \rho_l}{\partial p} \right)_u \right] dV} \quad (4.3.23)$$

In Equation (4.3.23), W_{fw} is the feedwater volumetric flow rate:

$$W_{fw} = \frac{w_{fw}}{\rho_{l,fw}}, \quad (4.3.24)$$

where w_{fw} is the feedwater mass flow rate.

(Note that the original RAMONA-3B formulation of Equation (4.3.16) used the liquid density in the cell where the injected feedwater mixes with the saturated liquid exiting the steam separators instead of using the density of the feedwater liquid.)

W_{ecc} is the emergency core cooling volumetric flow rate:

$$W_{ecc} = \frac{w_{ecc}}{\rho_{l,ecc}} \quad (4.3.25)$$

where w_{ecc} is the ECC mass flow rate. W_{binj} is the volumetric boron injection flow rate.

(Note that the original RAMONA-3B formulation of Equation (4.3.25) used the liquid density in the cell where the injected feedwater mixes with the saturated liquid exiting the steam separators instead of using the density of the ECC water. Note that the ECC and feedwater are injected into the same downcomer cell.)

W_{sl} is the steam-line volumetric flow rate

$$W_{sl} = \frac{w_{sl}}{\rho_g}, \quad (4.3.26)$$

where w_{sl} is the total mass flow rate of steam at the entrance to the steam line. $W_{s,ic}$ is the volumetric steam flow rate to the IC.

W_{Tv} is the entire vessel mixture dilatation rate due to phase change:

$$W_{Tv} = \psi_{tot} \left[\frac{\rho_f - \rho_g}{\rho_f \rho_g} + \frac{1}{\rho_f^2} (h_g - h_f) \left(\frac{\partial \rho_l}{\partial u} \right)_p \right]. \quad (4.3.27)$$

The second term in the square brackets of Equation (4.3.27) is new in the RAMONA-4B formulation. It represents the liquid contraction/expansion taking place during evaporation or condensation in the two-phase mixture. The contribution made by this term to the $\dot{p}$ equation is significant and, it is of the same order of magnitude as the contribution made by the injected feedwater, W_{fw} .

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In Equation (4.3.23) $W_{\text{expan}}^{\text{core}}$ represents the thermal expansion of the subcooled liquid as it heats up to saturation and becomes superheated:

$$W_{\text{expan}}^{\text{core}} = - \int_{\text{heated area}}^{\text{core}} \left[\frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] q_{w,l} dA_h - \int_{\text{volume}}^{\text{core}} \left[\frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p \right] q_l dV. \quad (4.3.28)$$

This term is approximated in the code as follows:

$$W_{\text{expan}}^{\text{core}} = - Q_{\text{tot}} \left[\frac{1}{\rho_{l,\text{eff}}^2} \left(\frac{\partial \rho_l}{\partial u} \right)_p \right]. \quad (4.3.29)$$

The denominator of Equation (4.3.23) is calculated as follows:

$$\int_{\text{Vessel}} \left[\frac{\alpha}{\rho_g} \frac{d\rho_g}{dp} + \frac{(1-\alpha)}{\rho_l} \left(\frac{\partial \rho_l}{\partial p} \right)_u \right] dV \approx \frac{M_g}{\rho_g^2} \frac{d\rho_g}{dp} + \frac{M_l}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial p} \right)_u, \quad (4.3.30)$$

where M_g and M_l are total mass of steam and liquid in the vessel, respectively. Note that the first term in Equation (4.3.30) is the vapor compressibility and the second term is the liquid compressibility, the evaluation of which is discussed in Section 4.8.2. The original RAMONA and RAMONA-3B neglects this term completely. This makes it inapplicable to the pure liquid case and may incur significant error in the case of low void fractions.

4.3.3 Reverse-Flow Capability

4.3.3.1 Component Internal Computational Cells

Liquid or steam flow can reverse in any internal computational cell of the 1-D components or in the core heated and bypass channels during transient calculations.

Description of the logic accounting for the flow reversal in the steam mass and mixture energy and momentum conservation calculations as implemented in RAMONA-4B is presented in this section. We start from a description of the calculation procedure for the normal flow direction.

4.3.3.1.1 Steam and Liquid Mass Calculations

Mass of steam in the k -th computational cell is calculated by solving the following mass-conservation equation:

$$\dot{m}_{g,k} = w_{g,k-1} - w_{g,k} + \Gamma_{v,k} V_k \quad (4.3.31)$$

where k and $k-1$ specify the computational cell and the adjacent upstream (donor) cell, respectively as illustrated in Figure 4.1.

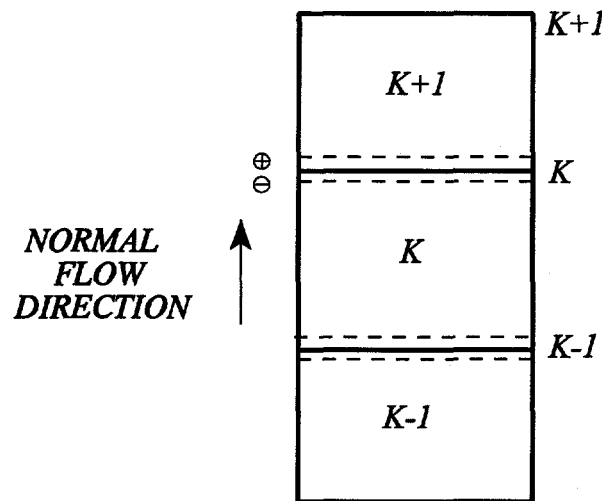


Figure 4.1 Schematic of Donor-Cell Coupling for Component Internal Cell in Thermal-Hydraulic Model: Normal Flow Direction

Equation (4.3.31) can be written in the following form by using the vapor velocity and the cell average void fraction:

$$\dot{m}_{g,k} = \rho_g A_k (\alpha_{k-1} v_{g,k-1} - \alpha_k v_{g,k}) + \Gamma_{v,k} V_k \quad (4.3.32)$$

where A_k and V_k are the flow area and cell volume, α_k is the cell average void fraction, v_g is the steam velocity at the interface k , and $\Gamma_{v,k}$ is the volumetric vapor generation rate. (Note that ρ_g does not have an index - steam is at saturation in all cells.)

Steam velocity v_{gk} can be calculated following the drift-flux formulation:

$$v_{g,k} = C_{o,k} j_{m,k} + v_{g,j,k}, \quad (4.3.33)$$

where $j_{m,k}$ is the mixture volumetric flux at interface k .

Steam velocity at the exit of the upstream cell $k-1$, $v_{g,k-1}$, is provided by the similar calculation for cell $k-1$.

Implementation of this formulation with the modifications needed to calculate the flow-reversal situations is described below; the approach developed for the BNL Engineering Plant Analyzer [Cheng et al., 1985] is used.

To account for the flow reversal in an internal cell k , we apply the following reverse-flow logic:

$$\dot{m}_{g,k} = \frac{1}{2} \left\{ \left(w_{g,k-1}^- + |w_{g,k-1}^-| + w_{g,k-1}^+ - |w_{g,k-1}^+| \right) - \left(w_{g,k}^- + |w_{g,k}^-| + w_{g,k}^+ - |w_{g,k}^+| \right) \right\} + \Gamma_{v,k} V_k, \quad (4.3.34)$$

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where the terms in the two sets of parentheses define the steam mass flow rates through the interfaces $k-1$ and k . The steam mass flow rate at the interface k in Eq. (4.3.34) are defined as follows:

$$w_{g,k}^- = \rho_g A_k \alpha_k (C_{o,k} j_{m,k} + v_{gj,k}) \quad (4.3.35)$$

and,

$$w_{g,k}^+ = \rho_g A_k \alpha_{k+1} (C_{o,k+1} j_{m,k} + v_{gj,k+1}) . \quad (4.3.36)$$

The sketch in Figure 4.2 shows an example of the resulting mass flow rate at the interface k (see the second set of parentheses in Eq. (4.3.34)) depending on the history of the flow reversal: (a) steam flow at the interface $\ominus$ reverses first, and (b) steam flow at the interface $\oplus$ reverses first.

The liquid mass in cell k is calculated by using the following formula:

$$m_{l,k} = \left(V_k - \frac{m_{g,k}}{\rho_g} \right) \rho_{l,k} , \quad (4.3.37)$$

where V_k is the volume of cell k .

Note that the liquid mass calculation does not require an explicit calculation of the liquid mass flow rate; this calculation is affected by the occurrence of the flow reversal only indirectly through the steam flow rate and steam mass calculations. However, the liquid mass flow rate is needed for the mixture energy and momentum calculations described below.

The liquid flow rate calculations at flow-reversal conditions do not require a special logic; the definition of the volumetric flow rate, W_k is used instead:

$$W_k = \frac{w_{l,k}}{\rho_{l,k}} + \frac{w_{g,k}}{\rho_g} . \quad (4.3.38)$$

Therefore, in case of the liquid flow reversal, liquid density of the downstream cell, $\rho_{l,k+1}$ following the donor-cell convention, is used in the above relationship; the equation for the liquid mass flow rate follows:

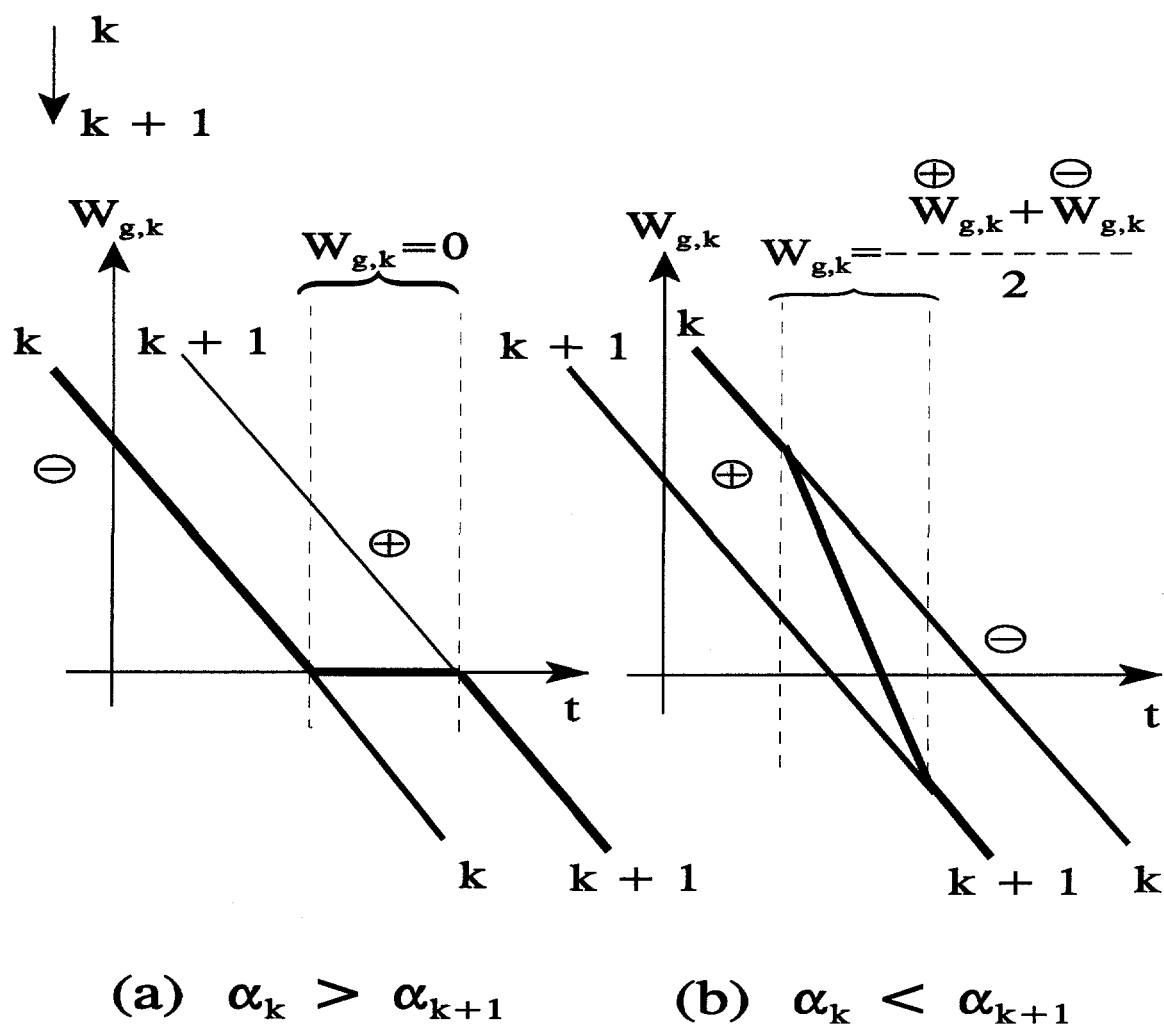


Figure 4.2 Flow Reversal Logic: Example of the Mass Flow Rate at the Internal Cell Interface k Depending on the History of Flow Reversal: (a) Vapor Flow at Interface $\ominus$ Reverses First, (b) Vapor Flow at Interface $\oplus$ Reverses First

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$$w_{l,k} = \left(W_k - \frac{w_{g,k}}{\rho_g} \right) \rho_{l,k+1}, \quad w_{l,k} < 0. \quad (4.3.39)$$

4.3.3.1.2 Mixture Energy and Momentum Calculation

The convection term of the mixture energy conservation equation for cell k in case of liquid flow reversal, $w_{l,k} < 0$, is written with the liquid internal energy of the downstream cell $k+1$:

$$(w_{g,k-1}e_g + w_{l,k-1}e_{l,k-1}) - (w_{g,k}e_g + w_{l,k}e_{l,k+1}) \quad (4.3.40)$$

This term will be used in energy balance [Wulff et al., 1984a, Equation 6.4.29].

The same approach is used in case of flow reversal in the mixture momentum equation for the liquid density in the liquid flow inertia term [Wulff et al., 1984a, Equation 6.4.15]; namely,

$$\frac{(w_{l,k})^2}{\rho_{l,k+1}}. \quad (4.3.41)$$

The next section discusses the branching between the one-dimensional lower plenum component and core parallel channels (diverging) and the branching between the core channels and the one-dimensional riser component (converging). The interfaces between the one-dimensional components of different flow areas is a specific case of the diverging/converging branching.

4.3.3.2 Interfaces Between 1-D and Multi-Channel Components

4.3.3.2.1 Lower Plenum - Core Interface

Let us introduce an "interface box" coupling the components; the box has no volume and is composed of three surfaces: the interface between the components, i , the upstream boundary of the box, $i-1$, and the downstream boundary of the box, $i+1$ (Figure 4.3). Here we follow the normal flow direction definitions for the "downstream" and "upstream" directions. Note that the boundary $i+1$ corresponds to the **downstream** (relative to the interface i) conditions for the core channels, and the boundary $i-1$ corresponds to the **upstream** conditions for the lower plenum.

At any given moment, the mixture volumetric flow is defined at the interface i : W_{LP} at the exit of the lower plenum (Lower Plenum Exit (LPE) cell) and W_n at the entrances to the NPC core parallel channels. (By definition, the mixture volumetric flow rate $W = j_m A$, where A is the flow area). The mass flow continuity condition is satisfied at this interface, which leads to the following volumetric flow continuity, assuming the density is constant at the interface:

$$W_{LP} = \sum_{n=1}^{NPC} W_n \quad (4.3.42)$$

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and the steam volumetric fluxes from the lower plenum and core parallel channels towards the interface are defined following the drift-flux model:

$$j_g = \alpha (C_o j_m + v_{gj}) . \quad (4.3.43)$$

The objective is to express the steam volumetric flux j_g , and mass flow rates w_g and w_l of the phases leaving and arriving at the interface I in terms of the mixture volumetric flux j_m in such a way that the drift-flux model conditions are met on the $I-1$ and $I+1$ boundaries of the interface box. **Values of the volumetric fluxes at the components' interfaces are known from the code calculations at any given time.** In effect, the interface box is a "flow homogenizer", which couples multiple nonhomogeneous, two-phase-flow sources and sinks.

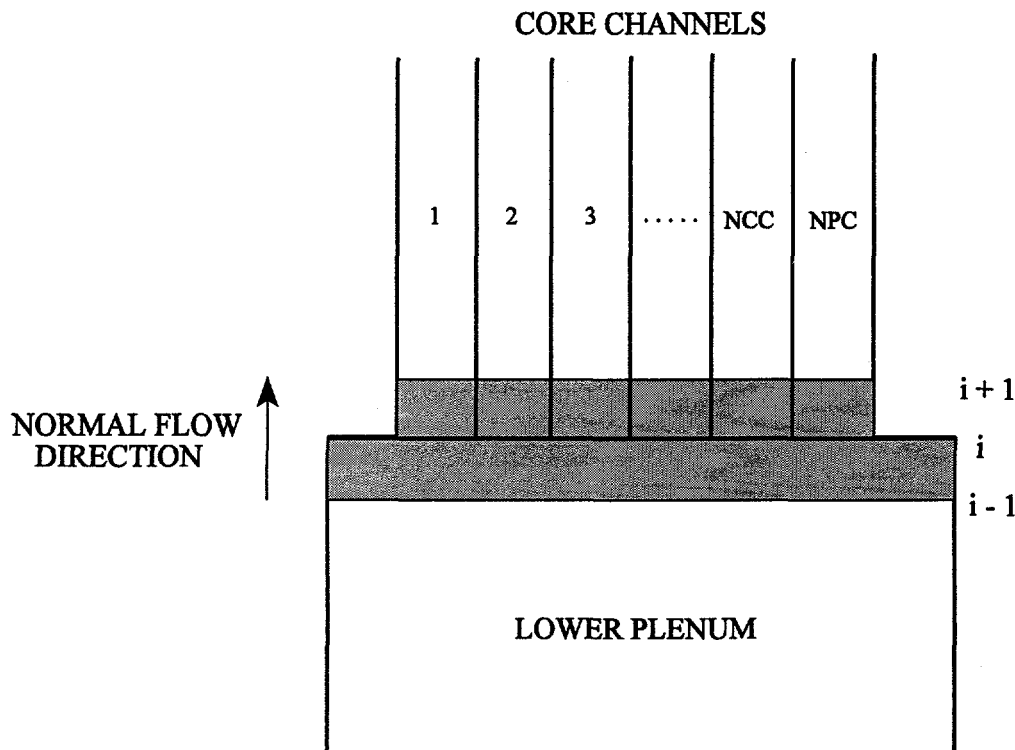


Figure 4.3 Schematic of Lower Plenum-Core Coupling in Thermal-Hydraulic Model.
(shaded areas indicate "Interface Box")

The first step is the calculation of the homogeneous mixture void fraction, α_p , at the interface. The steam volumetric fluxes at the interface box boundaries $I-1$ and $I+1$ are defined as:

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$$j_{g,LP}^{i-1} = \alpha^{i-1}(C_0^{i-1} j_{m,LPE} + v_{gj}^{i-1}) \quad (4.3.44)$$

and

$$j_{g,n}^{i+1} = \alpha_n^{i+1}(C_{0,n}^{i+1} j_{m,n} + v_{gj,n}^{i+1}), \quad (4.3.45)$$

where C_0 , v_{gj} , and α correspond to the properties of the donor cells - LPE cell for the $I-1$ boundary and the inlet cell of channel n for the $I+1$ boundary.

Using the definitions introduced above, one can calculate the **total volumetric steam in-flow** into the interface box:

$$W_{g,tot} = \frac{1}{2} \{A_{LP} (j_{g,LP}^{i-1} + |j_{g,LP}^{i-1}|) + \sum_{n=1}^{NPC} [A_n (j_{g,n}^{i+1} - |j_{g,n}^{i+1}|)]\}, \quad (4.3.46)$$

where A_n is the flow area of channel n .

The **total volumetric mixture in-flow** into the interface box is then calculated:

$$W_{m,tot} = \frac{1}{2} \{A_{LP} (j_{m,LP} + |j_{m,LP}|) + \sum_{n=1}^{NPC} [A_n (j_{m,n} - |j_{m,n}|)]\}, \quad (4.3.47)$$

Note that at the normal-flow-direction conditions, only the lower plenum is supplying steam and two-phase mixture into the interface box. A core channel starts supplying steam or mixture into the box only after the flow reversal in this particular channel.

Based on the last two calculations, the void fraction at the interface I is calculated:

$$\alpha_i = \left| \frac{W_{g,tot}}{W_{m,tot}} \right|. \quad (4.3.48)$$

The steam mass flow leaving the interface I and the lower plenum is calculated by using the following formula:

$$w_{g,LP} = \rho_g W_{g,LP} = -60 \frac{1}{2} \rho_g A_{LP} [j_{g,LP}^{i-1} + |j_{g,LP}^{i-1}| + \alpha_i (j_{m,LP} - |j_{m,LP}|)]. \quad (4.3.49)$$

The steam mass flow leaving the interface I and entering the core parallel channel n is calculated by using the following expression:

$$w_{g,n} = \rho_g W_{g,n} = \frac{1}{2} \rho_g A_n \left[j_{g,LP}^{i+1} + |j_{g,LP}^{i+1}| + \alpha_i (j_{m,n} - |j_{m,n}|) \right]. \quad (4.3.50)$$

To predict the liquid mass flow rates based on the volumetric flow rates, the **liquid density at the interface**, $\rho_{\ell,I}$ has to be determined. Also the **internal energy of the liquid at the interface**, $e_{\ell,I}$ is needed for the energy calculations.

The expressions for the total liquid mass in-flow and energy convection into the interface box can be written as follows:

$$w_{\ell,tot} = \rho_{\ell,I} W_{\ell,tot} = -60 \frac{1}{2} \left\{ \rho_{LP}^{i-1} A_{LP} (j_{\ell,LP}^{i-1} + |j_{\ell,LP}^{i-1}|) + \sum_{n=1}^{NPC} \left[\rho_{\ell,n}^{i+1} A_n (j_{\ell,n}^{i+1} - |j_{\ell,n}^{i+1}|) \right] \right\}, \quad (4.3.51)$$

and

$$E_{\ell,tot} = e_{\ell,I} w_{\ell,tot} = \frac{1}{2} \left\{ e_{\ell,LP}^{i-1} \rho_{LP}^{i-1} A_{LP} (j_{\ell,LP}^{i-1} + |j_{\ell,LP}^{i-1}|) + \sum_{n=1}^{NPC} \left[e_{\ell,n}^{i+1} \rho_{\ell,n}^{i+1} A_n (j_{\ell,n}^{i+1} - |j_{\ell,n}^{i+1}|) \right] \right\}. \quad (4.3.52)$$

where $\rho_{\ell,LP}$ and $\rho_{\ell,n}$ are liquid densities in the exit cell of the lower plenum and the entrance cell of the n -th core channel, respectively; $e_{\ell,LP}$ and $e_{\ell,n}$ are liquid internal energies in the exit cell of the lower plenum and the entrance cell of the n -th core channel, respectively.

Since the values of $W_{\ell,tot} j_{\ell,n}^{i-1}$ and $j_{\ell,n}^{i+1}$ are available from the previous calculations,

$$W_{\ell,tot} = W_{m,tot} - W_{g,tot}, \quad (4.3.53)$$

$$j_{\ell,LP}^{i-1} = j_{m,LP}^{i-1} - j_{g,LP}^{i-1}, \quad (4.3.54)$$

$$j_{\ell,n}^{i+1} = j_{m,n}^{i+1} - j_{g,n}^{i+1}, \quad (4.3.55)$$

the liquid density and internal energy at the interface can be calculated using the following two equations:

$$\rho_{\ell,I} = \left| \frac{w_{\ell,tot}}{W_{\ell,tot}} \right|, \quad (4.3.56)$$

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and

$$e_{\ell,i} = \frac{E_{\ell,tot}}{w_{\ell,tot}}. \quad (4.3.57)$$

Finally, the formula for the liquid mass flows at the exit of the lower plenum and entrances to the core channels are written:

$$w_{\ell,LP} = \frac{1}{2} \left[\rho_{\ell,LP}^{i-1} (W_{\ell,LP}^{i-1} + |W_{\ell,LP}^{i-1}|) + \rho_{\ell,i} (W_{\ell,LP} - |W_{\ell,LP}|) \right], \quad (4.3.58)$$

$$w_{g,n} = \rho_g W_{g,n}, \quad (4.3.59)$$

$$w_{\ell,n} = \frac{1}{2} \left[\rho_{\ell,i} (W_{\ell,n} + |W_{\ell,n}|) + \rho_{\ell,n}^{i+1} (W_{\ell,n}^{i+1} - |W_{\ell,n}^{i+1}|) \right], \quad (4.3.60)$$

where

$$W_{\ell,LP} = W_{m,LP} - W_{g,LP}, \text{ and } W_{\ell,n} = W_{m,n} - W_{g,n}.$$

Similarly, the convection part of the energy flux at the exit of the lower plenum and at the entrance to the core channels are calculated by using the formula:

$$w_{g,LP} e_g + \frac{1}{2} \left[e_{\ell,LP}^{i-1} (w_{\ell,LP} + |w_{\ell,LP}|) + e_{\ell,i} (w_{\ell,LP} - |w_{\ell,LP}|) \right] \quad (4.3.61)$$

for the exit from the lower plenum, and

$$w_{g,n} e_g + \frac{1}{2} \left[e_{\ell,i} (w_{\ell,n} + |w_{\ell,n}|) + e_{\ell,n}^{i+1} (w_{\ell,n} - |w_{\ell,n}|) \right] \quad (4.3.62)$$

for the entrance to the core channel.

4.3.3.2.2 Core-Riser Interface

Similar to the case of the diverging branching described in the previous section, Figure 4.4 shows the interface box used for converging branching. The three surfaces used in the derivations are: the interface between the components, I , the upstream boundary of the box, $I-1$, and the downstream boundary of the box, $I+1$. (We follow the normal flow-direction definitions for the "downstream" and "upstream" directions.) The boundary $I+1$ corresponds to the downstream (relative to the interface I) conditions for the riser, and the boundary $I-1$ corresponds to the upstream conditions for the core channels.

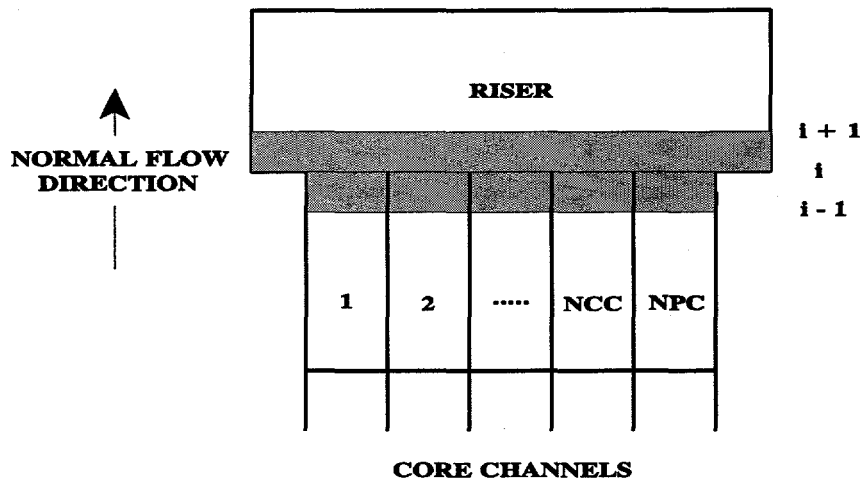


Figure 4.4 Schematic of Core-Riser Coupling in Thermal-Hydraulic Model.
(shaded areas indicate "Interface Box")

At any given moment, the mixture volumetric flow is defined at the interface I : W_n at the exit of the core channels, $n=1$ NPC, and W_{RI} at the entrance to riser; by definition, $W = j_m A$ where A is the flow area. The volumetric flow continuity condition is satisfied at this interface, assuming the continuity of density:

$$W_{RI} = \sum_{n=1}^{NPC} W_n, \quad (4.3.63)$$

and the steam volumetric fluxes from the core parallel channels and riser towards the interface are defined following the drift-flux model [Equations (4.3.64) and (4.3.65)].

The objective is to express the steam volumetric flux j_g and mass flow rates w_g and w_l of phases leaving and arriving at the interface I in terms of the mixture volumetric flux j_m in such a way that the drift-flux model conditions are met on the $I-1$ and $I+1$ boundaries of the interface box and the homogeneous model conditions are met at the interface I . The values of the volumetric fluxes at the components' interfaces are known at any given moment.

We first calculate the homogeneous mixture void fraction, α_i at the I interface.

The steam volumetric fluxes at the interface box boundaries $I-1$ and $I+1$ are defined as:

$$j_{g,n}^{I-1} = \alpha_n^{I-1} \left(C_{0,n}^{I-1} j_{m,n} + v_{g,n}^{I-1} \right), \quad (4.3.64)$$

and

$$j_{g,RI}^{I+1} = \alpha_{RI}^{I+1} \left(C_{0,RI}^{I+1} j_{m,RI} + v_{g,RI}^{I+1} \right), \quad (4.3.65)$$

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where C_0 , v_{gi} and α correspond to the properties of the respective donor cells-the core exit cell of the n -th channel for the $I - 1$ boundary and the riser inlet cell for the $I + 1$ boundary.

Using the definitions introduced above, one can calculate the **total volumetric steam in-flow** into the interface box:

$$W_{g,tot} = \frac{1}{2} \left[\sum_{n=1}^{NPC} A_n (j_{g,n}^{i-1} + |j_{g,n}^{i-1}|) + A_{RI} (j_{g,RI}^{i+1} - |j_{g,RI}^{i+1}|) \right], \quad (4.3.66)$$

where A_n is the flow area of channel n .

The **total volumetric mixture in-flow** into the interface box is then calculated as:

$$W_{m,tot} = \frac{1}{2} \left[\sum_{n=1}^{NPC} A_n (j_{m,n} + |j_{m,n}|) + A_{RI} (j_{m,RI} - |j_{m,RI}|) \right], \quad (4.3.67)$$

Note that at normal flow conditions, only the core channels supply steam and two-phase mixture into the interface box. The riser starts supplying steam or mixture into the box only after the flow reversal at the riser inlet.

The void fraction at the interface I can be calculated using Equations (4.3.66) and (4.3.67):

$$\alpha_i = \left| \frac{W_{g,tot}}{W_{m,tot}} \right|. \quad (4.3.68)$$

The mass flow rate of steam leaving interface I and entering the riser is calculated by using the following formula:

$$w_{g,RI} = \rho_g A_{RI} j_{g,RI} = \frac{1}{2} \rho_g A_{RI} \left[\alpha_i (j_{m,RI} + |j_{m,RI}|) + j_{g,RI}^{i+1} - |j_{g,RI}^{i+1}| \right]. \quad (4.3.69)$$

The mass flow rate of steam leaving the interface I and the core parallel channel n is calculated using the following formula:

$$w_{g,n} = \rho_g A_n j_{g,n} = \frac{1}{2} \rho_g A_n \left[\alpha_i (j_{m,n} + |j_{m,n}|) + j_{g,n}^{i-1} - |j_{g,n}^{i-1}| \right]. \quad (4.3.70)$$

To predict the liquid mass flow rates based on the volumetric flow rates, the liquid density at the interface, $\rho_{l,i}$ has to be determined. Also, the internal energy of the liquid at the interface, $e_{l,i}$ is needed for the energy convection calculations.

The expressions for the total liquid mass in-flow and liquid energy convection into the interface box can be written as follows:

$$w_{\ell,tot} = \rho_{\ell,i} W_{\ell,tot} = \frac{1}{2} \left\{ \sum_{n=1}^{NPC} \rho_{\ell,n} A_n (j_{\ell,n}^{i+1} + |j_{\ell,n}^{i+1}|) + \rho_{\ell,RI} A_R (j_{\ell,RI}^{i+1} - |j_{\ell,RI}^{i+1}|) \right\}, \quad (4.3.71)$$

$$E_{\ell,tot} = e_{\ell,i} w_{\ell,tot} = \frac{1}{2} \left\{ \sum_{n=1}^{NPC} e_{\ell,n}^{i+1} \rho_{\ell,n} A_n (j_{\ell,n}^{i+1} + |j_{\ell,n}^{i+1}|) + e_{\ell,RI}^{i+1} \rho_{\ell,RI} A_R (j_{\ell,RI}^{i+1} - |j_{\ell,RI}^{i+1}|) \right\}, \quad (4.3.72)$$

where $\rho_{\ell,RI}^{i+1}$ and $\rho_{\ell,n}^{i+1}$ are the cell-averaged liquid densities in the entrance cell of the riser and the exit cell of the n -th core channel, respectively; $e_{\ell,RI}^{i+1}$ and $e_{\ell,n}^{i+1}$ are liquid internal energies in the entrance cell of the riser and the exit cell of the n -th core channel, respectively.

Since the values of $W_{g,tot}$, $j_{g,RI}^{i+1}$, and $j_{g,n}^{i+1}$ are available from the previous calculations,

$$W_{\ell,tot} = W_{m,tot} - W_{g,tot}, \quad (4.3.73)$$

$$j_{\ell,RI}^{i+1} = j_{m,RI}^{i+1} - j_{g,RI}^{i+1}, \quad (4.3.74)$$

$$j_{\ell,n}^{i+1} = j_{m,n}^{i+1} - j_{g,n}^{i+1}, \quad (4.3.75)$$

the liquid density and internal energy at the interface can be calculated using Equations (4.3.71) and (4.3.72):

$$\rho_{\ell,i} = \frac{w_{\ell,tot}}{W_{\ell,tot}}, \quad (4.3.76)$$

and

$$e_{\ell,i} = \frac{E_{\ell,tot}}{w_{\ell,tot}}. \quad (4.3.77)$$

Finally, the formulae for the liquid mass flow rates at the entrance to the riser and exits from the core channels can be written as follows:

$$w_{\ell,RI} = \frac{1}{2} \left[\rho_{\ell,i} (W_{\ell,RI} + |W_{\ell,RI}|) + \rho_{\ell,RI}^{i+1} (W_{\ell,RI}^{i+1} - |W_{\ell,RI}^{i+1}|) \right], \quad (4.3.78)$$

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and

$$w_{\ell,n} = \frac{1}{2} \left[\rho_{\ell,n}^{i-1} (W_{\ell,n}^{i-1} + |W_{\ell,n}^{i-1}|) + \rho_{\ell,i} (W_{i,n} - |W_{i,n}|) \right]. \quad (4.3.79)$$

Similarly, the convection part of the energy flux at the entrance to the riser and at the exit from the core channels can be calculated:

$$w_{g,RI} e_g + \frac{1}{2} \left[e_{\ell,i} (w_{\ell,RI} + |w_{\ell,RI}|) + e_{\ell,RI}^{i+1} (w_{\ell,RI} - |w_{\ell,RI}|) \right] \quad (4.3.80)$$

for the riser entrance, and

$$w_{g,n} e_g + \frac{1}{2} \left[e_{\ell,n}^{i-1} (w_{\ell,n} + |w_{\ell,n}|) + e_{\ell,i} (w_{\ell,n} - |w_{\ell,n}|) \right] \quad (4.3.81)$$

for the core channels exits.

4.4 Constitutive Relations

4.4.1 Wall Shear and Form Losses

The wall shear is calculated as the sum of local wall frictional losses in all computational cells in a closed flow loop j :

$$F_j = \sum_{k=1}^{k=k_{\max}} f_{t0,k} \phi_{t0,k} \frac{w_k |w_k| \Delta Z_k}{2 \rho_{l,k} A_k^2 D_{h,k}} \quad (4.4.1)$$

where the subscript k is the cell index, $f_{t0,k}$ is the single-phase friction factor of cell k , $\phi_{t0,k}$ is the two-phase friction multiplier in cell k , w_k is the mass flow rate at cell k , $\rho_{l,k}$ is the liquid density in cell k , A_k is the local flow area, ΔZ_k is the local cell spacing, and $D_{h,k}$ is the hydraulic diameter of cell k .

To account for the laminar flow conditions in the wall friction calculations, the following correlations for the single-phase friction factor are used in RAMONA-4B:

$$\begin{aligned} f_t &= 0.128 & \text{for } Re \leq 500, \\ f_t &= \frac{64}{Re} & \text{for } 500 < Re \leq 1500, \\ f_t &= 0.184 Re^{-0.2} & \text{for } Re > 1500. \end{aligned} \quad (4.4.2)$$

The two-phase friction multiplier used for the wall shear calculation is the Becker correlation as in the earlier version:

$$\phi_{t0}^2 = 1 + 1.49 \times 10^8 \left(\frac{x}{p} \right)^{0.96}, \quad (4.4.3)$$

where p is the system pressure in N/m^2 , and x is the flow quality defined as

$$x = \frac{w_g}{w_g + w_t}. \quad (4.4.4)$$

The form loss is calculated for each closed flow loop j as the sum of the form losses at the inlet and exit of all flow segments (downcomer, lower plenum, core channels, and riser) including the form losses at spacer grids as follows:

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$$L_j = \sum_{k=1}^{k=k_{\max}} \left(\left[\frac{K_k \Phi_{\ell,k}^2 w_k |w_k|}{2 \rho_{\ell,k} A_{k,\min}^2} \right]_{inlet} + \left[\frac{K_k \Phi_{\ell,k}^2 w_k |w_k|}{2 \rho_{\ell,k} A_{k,\min}^2} \right]_{exit} \right), \quad (4.4.5)$$

where the subscript k is the local cell index, w_k is the mass flow rate at cell k , $\rho_{\ell,k}$ is the liquid density in cell k , $A_{k,\min}$ is the minimum value of the flow area at the form change (interface between flow segments), K_k is the form loss coefficient, and $\Phi_{\ell,k}$ is the two-phase multiplier for form loss.

The two-phase multiplier for form loss is calculated with the EPRI correlation [Ansari et al., 1981]:

$$\Phi_{\ell0,k} = 1 + \beta x_{eq,k} \left(\frac{\rho_{\ell,k}}{\rho_g} - 1 \right), \quad (4.4.6)$$

where β is an empirical constant treated as an input ($1 \leq \beta \leq 2$) and $x_{eq,k}$ is the equilibrium quality.

Since flow-dependent loss coefficients are important for the natural-circulation system of SBWR, RAMONA-4B treats the resistances due to abrupt changes in flow area and flow through orifices as a function of the Reynolds number. In RAMONA-4B this is accounted for by providing explicit loss coefficients for the inlet and exit of each flow segment and each fuel channel in the form:

$$K_k = a_k Re^{b_k} + c_k, \quad (4.4.7)$$

where a_k , b_k , and c_k are the flow-dependent loss coefficients treated as input data. Accurate data of these coefficients are not easily available. These values are specific to each design, and are different for laminar and turbulent flow regimes.

4.4.2 Fuel-Rod Heat Transfer

4.4.2.1 Heat Transfer Coefficients

The heat transfer coefficient is calculated for single-phase liquid from the Dittus-Boelter correlation for forced convection:

$$(h_c)_{FC} = \frac{k_\ell}{d_h} \left[0.023 N_{Re}^{0.8} N_{Pr}^{0.4} \right]_\ell, \quad (4.4.8)$$

where k_ℓ is the thermal conductivity of liquid water, d_h is the hydraulic diameter for heating, N_{re} is the Reynolds number of liquid water defined as

$$N_{Re} = \frac{G_m d_h}{\mu_l}, \quad (4.4.9)$$

and N_{Pr} is the Prandtl number of liquid water defined as

$$N_{Pr} = \frac{\mu_l C_{p,l}}{k_l}, \quad (4.4.10)$$

with $C_{p,l}$ representing the specific heat of liquid water at constant pressure and μ_l the dynamic viscosity of the liquid

For nucleate boiling, the heat transfer coefficient is computed using the Jens-Lottes correlation:

$$(h_c)_{NB} = 2.555 e^{6.45 \times 10^{-7} p} (T_w - T_s)^3, \quad (4.4.11)$$

where p is the vessel pressure, T_w is the wall temperature, and T_s is the saturation temperature corresponding to p .

4.4.2.2 Heat Transfer in Post-CHF Regime

The original RAMONA-3B post-CHF heat transfer package calculates the film-boiling heat transfer coefficient, h_{FB} as the maximum of values predicted by the Groeneveld 5.7 and Bromley correlations [Wulff et al., 1984a]. The new model predicts a **lower limit** for h_{FB} based on the laminar heat transfer for an annular flow of superheated vapor propagating along a fuel rod. In addition, the value of the heat transfer coefficient predicted by the code has an upper limit of "CHF heat-transfer coefficient," h_{CHF} . The latter heat-transfer coefficient is defined in Equation (4.4.17). The derivation of the modified post-CHF constitutive relations follows.

Let us consider a two-phase flow in a rod bundle consisting of n_{rod} fuel rods. All the vapor in a computational cell is assumed to flow through an annulus surrounding a fuel rod of diameter d_{rod} . Our objective is to calculate the thickness, δ , of the annular film separating the liquid core from the superheated rod surface and, subsequently, the laminar-flow heat-transfer coefficient defined as [Kays, 1980]:

$$h_{laminar} = 2.43 \frac{k_v}{\delta}. \quad (4.4.12)$$

Note that the value of the laminar-flow heat-transfer coefficient is 2.43 times higher than the pure-thermal-conduction heat-transfer coefficient.

The void fraction in a computational cell, α , can be expressed in terms of the cell geometry:

$$\alpha = \frac{\pi n_{rod} [(\delta + r_{rod})^2 - r_{rod}^2]}{A_{flow}} = \frac{n_{rod} \pi d_{rod}}{A_{flow}} \left(\delta + \frac{\delta^2}{d_{rod}} \right). \quad (4.4.13)$$

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The expression for the film thickness δ follows from (4.4.13):

$$\delta = \left(\frac{d_{rod}}{2} \right) \left(\sqrt{1 + \frac{4 \alpha B}{d_{rod}}} - 1 \right), \quad (4.4.14)$$

where

$$B = \frac{A_{flow}}{n_{rod} \pi d_{rod}}. \quad (4.4.15)$$

In the RAMONA-4B implementation, the value for δ has a lower limit, $\delta_{min} = 1.0 \times 10^{-10} m$ to protect from division by zero. (The heat-transfer coefficient predicted by using δ_{min} exceeds h_{CHF} defined below by approximately four orders of magnitude at $T_w - T_{sat} = 100^\circ C$. In this case, the logic described below selects the alternative value for h_{FB}).

The logic for selecting the value of h_{FB} is as follows:

$$h'_{FB \text{ intermediate}} = \max \{ h_{Groeneveld \ 5.7}, h_{Bromley} \}$$

$$h''_{FB \text{ intermediate}} = \min \{ h_{CHF}, h_{laminar} \}$$

$$h_{FB \text{ final}} = h_{FB} = \max \{ h'_{FB \text{ intermediate}}, h''_{FB \text{ intermediate}} \}$$

where

$$h_{CHF} = \frac{q_{CHF}}{T_w - T_{sat}}, \quad (4.4.16)$$

and q_{CHF} is the critical heat flux calculated according to the RAMONA-4B post-CHF constitutive equations package based on the RELAP4/MOD7 correlation [Condie, 1978] and the Zuber-Griffith correlation [Smith, 1976].

4.4.3 Interfacial Momentum Transfer

This section describes the drift-flux model and its implementation in RAMONA-4B. Detailed derivations of the mixture volumetric flows expressed in terms of the loop-integrated momenta are also given.

The drift-flux formulation has the following important advantages over the slip-model formulation previously used in the code:

- the drift-flux model is based on a wider experimental data base, and
- the drift-flux model is not limited to co-current, two-phase flows.

Notations of this section are identical to the names of the variables in the corresponding FORTRAN coding; the names are identical to make the reading of the source coding easier. In implementing the drift-flux model a modular programming approach was followed where possible. The following new subroutines and functions have been added

to the code: CNOT, COREIO, COREM, and DOWNCM. The rest of the vessel's thermal-hydraulics coding can be found in subroutines HYDRO1 and HYDRO2.

4.4.3.1 Drift-Flux Model Description

The Zuber-Findley's drift-flux formulation implemented in RAMONA-4B is based on the correlations derived in [Zuber and Findley, 1965] and described in [Ishii, 1977]. A similar model was used in the BNL Engineering Plant Analyzer [Wulff et al., 1984b].

The following set of correlations constitute the drift-flux model:

- Void Fraction-Weighted Liquid Velocity

$$v_l \equiv \frac{G_l}{\rho_l (1 - \alpha)} = \frac{(1 - \alpha C_o) j_m - \alpha v_{gj}}{1 - \alpha} \quad (4.4.17)$$

- Void Fraction-Weighted Steam Velocity

$$v_g = \frac{G_g}{\rho_g \alpha} = C_o j_m + v_{gj} \quad (4.4.18)$$

- Mixture Mass Flux

$$G_m = G_l + G_g = B_1 j_m - B_2 \quad (4.4.19)$$

where

$$B_1 = \rho_l - \alpha C_o (\rho_l - \rho_g), \quad (4.4.20)$$

and

$$B_2 = \alpha v_{gj} (\rho_l - \rho_g). \quad (4.4.21)$$

- Zuber-Findley Void-Fraction Distribution Parameter

$$C_o = \begin{cases} C_o^{ch-t} & \text{for } \alpha \leq 0.75, \\ \min \{ C_o^{ch-t}, C_o^{ann} \}, & \text{for } \alpha > 0.75, \end{cases} \quad (4.4.22)$$

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where,

$$C_0^{ch-t} = \left(1.2 - 0.2 \sqrt{\frac{\rho_g}{\rho_l}} \right) (1 - e^{-18\alpha}) \quad (4.4.23)$$

for the **churn-turbulent** flow regime, and

$$C_0^{ann} = 1 + \frac{1 - \alpha}{\alpha + 4 \sqrt{\frac{\rho_g}{\rho_l}}} \quad (4.4.24)$$

for the **annular** flow regime.

• Void-Fraction-Weighted, Area-Averaged Vapor Drift Velocity

$$v_{gj} = \begin{cases} v_{gj}^{ch-t} & \text{for } \alpha \leq \alpha_0 - 0.025, \\ v_{gj}^{ann} & \text{for } \alpha \geq \alpha_0 + 0.025, \end{cases} \quad (4.4.25)$$

where α_0 is given on next page,

$$v_{gj}^{ch-t} = 1.54 \left[\frac{(\rho_l - \rho_g) g \sigma}{\rho_l^2} \right]^{1/4} \quad (4.4.26)$$

for the **churn-turbulent** flow regime, and

$$v_{gj}^{ann} = \frac{(1 - \alpha) \sqrt{g D_h (\rho_l - \rho_g) \frac{(1 - \alpha)}{0.015 \rho_l}}}{\alpha + 4 \sqrt{\frac{\rho_g}{\rho_l}}} \quad (4.4.27)$$

for the **annular** flow regime. The coefficient 1.54 above is treated as an input parameter in Equation (4.4.26) for the in-bundle core flow calculations.

The following interpolation between the churn-turbulent and annular regimes is used for v_{gj} [Wulff, 1978]:

$$v_{gj} = \epsilon_{int} v_{gj}^{ch-t} + (1 - \epsilon_{int}) v_{gj}^{ann}, \quad (4.4.28)$$

where

$$\epsilon_{int} = 0.0625 \left\{ 8 + \epsilon \left[-15 + \epsilon^2 (10 - 3 \epsilon^2) \right] \right\}, \quad (4.4.29)$$

and $\epsilon = (\alpha - \alpha_0)/0.025$.

The value of α_0 , the flow-regime transition point, is calculated approximately by using Equations (4.4.23) and (4.4.24). The last term in Eq. (4.4.23) is approximately set to 1.0. It is a good approximation for $\alpha > 0.6$ as the term $(1 - e^{-18\alpha})$ has a value between 0.9998 and 1.0. The expression for α_0 is as follows:

$$\alpha_0 = \frac{1 - 0.8 \sqrt{\frac{\rho_g}{\rho_l}} \left(1 - \sqrt{\frac{\rho_g}{\rho_l}} \right)}{1 + 0.2 \left(1 - \sqrt{\frac{\rho_g}{\rho_l}} \right)}. \quad (4.4.30)$$

In the horizontal section of the vessel (Lower Plenum 1), C_0 and v_{gj} are set equal to 1 and 0; respectively.

It should be pointed out that at low void fractions the coefficient C_0 approaches zero. As a consequence, the direction of steam flow will be against the gravity. It is especially significant in the downcomer region where the flow is downward: the counter-current flow will develop if $|j_m| < \frac{|v_{gj}|}{C_0}$. In a typical BWR with $|j_m|$ of about 0.7 in the upper

part of the downcomer at nominal conditions, the counter-current flow regime is predicted at a void fraction of $\alpha \leq 0.05$ according to the model. Note that this regime will develop at the higher void fractions in the case of low-flow conditions.

Since the RAMONA-4B formulation for the flow calculations uses the mixture and phasic mass flow rates, w_m , w_l , and w_g instead of mass fluxes, G_m , G_l , and G_g , the following standard definitions for w_l and w_g are used in the code and later in this manual:

$$w_l = A \rho_l \left[(1 - \alpha C_0) j_m - \alpha v_{gj} \right], \quad (4.4.31)$$

$$w_g = A \rho_g \alpha \left[C_0 j_m + v_{gj} \right]. \quad (4.4.32)$$

4.4.3.2 Mixture Volumetric Flow Calculations

The steady-state calculations predict the vessel thermal-hydraulic conditions described by the mass and energy conservation equations with the time derivatives set to 0. Mass flow rates in the vessel components are calculated based on the mass flow rate imposed by the user at the core entrance: variable WCORIN on Input Card 600070. In the loop

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momentum equations, only the friction and pressure form losses in the vessel are predicted for the subsequent initialization of the jet-pump model for jet-pump BWRs. In the case of natural circulation (SBWR), the steady-state momentum balance is achieved by iteration (see Section 4.8.1).

The **transient calculation** starts from integrating the loop momentum equations written for a set of closed flow loops; one for each of the hydraulic parallel channels in the core. Derivation of the integrated loop momentum equations is described in the earlier report [Wulff et al., 1984a, Chapter 4.4.4.1].

The three major steps in the calculational procedure of the vessel flows are:

- calculation of the "reference" mixture volumetric flow at the entrance to downcomer, W_{m0} ,
- calculation of the mixture volumetric flows at the entrances to the core hydraulic channels, $w_{m,j}^{in}$ $i = 1 \cdots \text{NPC}$, and
- calculation of flow distributions in the vessel components.

Derivations needed to accomplish the first two steps using the integrated loop momentum approach are described here.

OBJECTIVE: Express the volumetric flow rates, $W_{m,0}$ and $W_{m,j}^{in}$ $j = 1 \cdots \text{NPC}$ in terms of the integrated loop momentum M_j .

The definition of the total momentum of a single component is:

$$M = \sum_{in}^{out} G_m \Delta z \equiv \sum_{in}^{out} (B_1 J_m - B_2) \Delta z, \quad (4.4.33)$$

where

$$G_m = B_1 J_m - B_2, \quad (4.4.34)$$

$$B_1 = \rho_\ell - \alpha C_0 (\rho_\ell - \rho_g), \quad (4.4.35)$$

$$B_2 = \alpha V_{gj} (\rho_\ell - \rho_g), \quad (4.4.36)$$

and *in* and *out* define the component's inlet and outlet cells.

Introducing the volumetric flow rate, $W_{m,k} = j_{m,k} A_k$, where A_k is the flow area, one can write:

$$M = \sum_{k=in}^{k=out} \left(B_{1,k} \frac{W_{m,k}}{A_k} - B_{2,k} \right) \Delta z_k. \quad (4.4.37)$$

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A vessel thermal-hydraulic loop is composed of the following groups of components (the number of loops is equal to the number of the thermal-hydraulic parallel channels modeling the core, NPC, as shown in Figure 1.4):

- Downcomer 1, Downcomer 2, Lower Plenum 1, Lower Plenum 2;
- Riser;
- Core Channel.

Integrated momenta for each of these three groups are given below.

Integrated Momentum for Downcomers 1, 2 and Lower Plena 1 and 2 designated collectively as M^{DC}

$$M^{DC} = - \sum_{k=D1, in}^{LP2, out} B_{2,k} \Delta z_k + \sum_{k=D1, in}^{LP2, out} \frac{B_{1,k}}{A_k} (W_{m,0} + W_{dl,t,k}^{DC}) \Delta z_k, \quad (4.4.38)$$

where

$$W_{dl,t,k}^{DC} = \Delta V_k \left[\frac{\rho_{l,k} - \rho_g}{\rho_{l,k} \rho_g} \Gamma_{v,k} - \frac{\alpha_k}{\rho_g} \frac{D\rho_g}{D_t} - \frac{1 - \alpha_k}{\rho_{l,k}} \frac{D\rho_{l,k}}{D_t} \right] + \delta_{k,n} (W_{fw} + W_{HPCI} + W_{RCIC} + W_{l,IC}) + \delta_{k,m} W_{b,inj} \quad (4.4.39)$$

is the mixture dilatation rate in cell k [Wulff et al., 1984a, Equation 4.4.115]. In RAMONA-4B coding, this term corresponds to variable WAUX. Here $\delta_{k,n}$ is the Kronecker delta function with the subscript n being the cell index where the feedwater flow W_{fw} , HPCI flow W_{HPCI} , RCIC flow W_{RCIC} , and IC return flow $W_{l,IC}$ enter the RPV. Similarly, $\delta_{k,m}$ is the Kronecker delta function with the subscript m being the cell index where the boron injection $W_{b,inj}$ takes place.

Integrated Momentum for Riser, M^R :

$$M^R = \sum_{k=R, in}^{R, out} \left(B_{1,k} \frac{W_{m,k}}{A_k} - B_{2,k} \right) \Delta z_k, \quad (4.4.40)$$

where

$$W_{m,k} = W_{m,0} + \sum_{i=D1, in}^{LP2, out} W_{dl,t,i} + \sum_{n=1}^{NPC} \sum_{j=nin}^{nout} W_{dl,t,j}^n + W_{dl,t,k}^R + \delta_{k,m} W_{b,inj} \quad (4.4.41)$$

and D1, in and LP2, out are the inlet of Downcomer 1 and exit of Lower Plenum 2. $W_{dl,t,k}^R$ has a similar expression as $W_{dl,t,k}^{DC}$ in Equation (4.4.39) without the term for $\delta_{k,n}$.

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Integrated Momenta for Core Channels, M_j^{core} , $j = 1 \dots NPC$:

$$M_j^{core} = \sum_{k=j, in}^{j, out} \left(B_{1,k}^j \frac{W_{m,k}^j}{A_k^j} - B_{2,k}^j \right) \Delta z_k = - \sum_{k=j, in}^{j, out} B_{2,k}^j \Delta z_k + \sum_{k=j, in}^{j, out} \frac{B_{1,k}^j}{A_k^j} (W_{m, in}^j + W_{dl t, k}^j) \Delta z_k, \quad (4.4.42)$$

Summing the momenta for all components of loop j , one obtains:

$$\begin{aligned} M_j = & - \sum_{k=DC1, in}^{R, out} B_{2,k} \Delta z_k + W_{m, 0} \sum_{k=DC1, in}^{LP2, out} \frac{B_{1,k}}{A_k} \Delta z_k + \sum_{k=DC1, in}^{LP2, out} \frac{B_{1,k}}{A_k} W_{dl t, k} \Delta z_k \\ & + \sum_{k=R, in}^{R, out} \frac{B_{1,k}}{A_k} (W_{m, 0} + W_{dl t}^{DC} + W_{dl t}^{core} + W_{dl t}^R) \Delta z_k + \sum_{k=j, in}^{j, out} \frac{B_{1,k}^j}{A_k^j} (W_{m, in}^j + W_{dl t, k}^j) \Delta z_k, \end{aligned} \quad (4.4.43)$$

where $W_{dl t}^{DC}$ and $W_{dl t}^{core}$ are the total mixture dilatation rates in the 1-D components located upstream of the core (Downcomer 1, Downcomer 2, Lower Plenum 1, and Lower Plenum 2) and in the core; respectively:

$$W_{dl t}^{DC} = \sum_{t=DC1, in}^{LP2, out} W_{dl t, t}^{DC}, \quad (4.4.44)$$

$$W_{dl t}^{core} = \sum_{n=1}^{NPC} \sum_{j=nin}^{nout} W_{dl t, j}^n. \quad (4.4.45)$$

Re-grouping Equation (4.4.26) in order to separate the core channel inlet volumetric rate, $W_{m, in}^j$, one obtains the following equation:

$$\begin{aligned} M_j = & W_{m, in}^j \sum_{k=j, in}^{j, out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k + \sum_{k=j, in}^{j, out} \frac{B_{1,k}^j}{A_k^j} W_{dl t, k}^j \Delta z_k \\ & - \sum_{k=DC1, in}^{R, out} B_{2,k} \Delta z_k + W_{m, 0} \sum_{k=DC1, in}^{LP2, out} \frac{B_{1,k}}{A_k} \Delta z_k + \sum_{k=DC1, in}^{LP2, out} \frac{B_{1,k}}{A_k} W_{dl t, k}^{DC} \Delta z_k \\ & + \frac{W_{m, 0} + W_{dl t}^{DC} + W_{dl t}^{core}}{A_R} \sum_{k=R, in}^{Rout} B_{1,k} \Delta z_k + \sum_{k=j, in}^{Rout} \frac{B_{1,k}}{A_R} W_{dl t, k}^R \Delta z_k. \end{aligned} \quad (4.4.46)$$

where A_R is the riser flow area.

Solving Equation (4.4.27) for $W_{m,in}^j$ one can obtain the core channel inlet volumetric flow rates as a function of the volumetric flow rate, $W_{m,o}$ at the entrance to Downcomer 1:

$$W_{m,in}^j = \frac{N_{m,in}^j}{D_{m,in}^j}, \quad (4.4.47)$$

where

$$\begin{aligned} N_{m,in}^j = & M_j + \sum_{k=DC1,in}^{R,out} B_{2,k} \Delta z_k - W_{m,o} \left[\sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right] \\ & - \sum_{k=j,in}^{j,out} \frac{B_{1,k}}{A_k} W_{dl,t,k}^j \Delta z_k - \sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dl,t,k}^{DC} \Delta z_k \\ & - \frac{W_{dl,t}^{DC} + W_{dl,t}^{core}}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k - \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dl,t,k}^R \Delta z_k, \end{aligned} \quad (4.4.48)$$

$$D_{m,in}^j = \sum_{k=j,in}^{j,out} \frac{B_{1,k}}{A_k^j} \Delta z_k. \quad (4.4.49)$$

One of the RAMONA-4B features relevant to the present derivation is the "core symmetry" option specified on Card 200000, parameter ISYM. The variable representing the core symmetry, **RMULT**, is a number specifying how many actual core channels are represented by one channel of the calculational model. For example, **RMULT**=8 means that a 1/8-core symmetry option is used.

Let us sum the volumetric flow rates at the entrances to all core channels in order to obtain the expression for the total core inlet flow rate $W_{m,o}$. Continuity of the volumetric flow at the lower plenum core interface leads to the equation:

$$RMULT \times \sum_{j=1}^{NPC} W_{m,in}^j = W_{m,o} + W_{dl,t}^{DC}. \quad (4.4.50)$$

Substituting Eq. (4.4.47) into Eq. (4.4.46) and performing the summation, one obtains the following expression:

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$$\begin{aligned}
 W_{m,0} + W_{dl,t}^{DC} = & \frac{RMULT}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \sum_{j=1}^{NPC} \left(M_j + \sum_{k=D1,in}^{R,out} B_{2,k} \Delta z_k - \sum_{k=D1,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dl,t}^{DC} \Delta z_k \right) \\
 & - \frac{RMULT}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \sum_{j=1}^{NPC} \left(\frac{W_{dl,t}^{DC} + W_{dl,t}^{core}}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right) \\
 & - \frac{RMULT}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \left(\sum_{k=R,in}^{R,out} \frac{B_{1,k}^j}{A_R} W_{dl,t}^R \Delta z_k + \sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} W_{dl,t}^j \Delta z_k \right) \\
 & - \frac{RMULT}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} W_{m,0} \left(\sum_{k=D1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right).
 \end{aligned} \tag{4.4.51}$$

Solving the above equation for $W_{m,0}$, we obtain the final expression for the total core inlet flow rate:

$$W_{m,0} = \frac{N_{W_{m,0}}}{1 + RMULT \cdot \left(\sum_{k=D1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right) \sum_{j=1}^{NPC} \left(\frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right)}, \tag{4.4.52}$$

where

$$\begin{aligned}
 N_{W_{m,0}} = & -W_{dl,t}^{DC} + RMULT \sum_{j=1}^{NPC} \left(\frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right) \left[M_j + \sum_{k=j,in}^{j,out} B_{2,k}^j \Delta z_k - \sum_{k=R,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} W_{dl,t,k} \Delta z_k \right] \\
 & + \frac{RMULT}{\sum_{j=1}^{NPC} \left(\frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right)} \left[\sum_{k=DI,in}^{LP2,out} B_{2,k} \Delta z_k + \sum_{k=R,in}^{R,out} B_{2,k} \Delta z_k - \sum_{k=DI,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dl,t,k}^{DC} \Delta z_k \right] \\
 & - \frac{W_{dl,t}^{DC}}{A_R} + \frac{W_{dl,t}^{core}}{A_R} \left[\sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k - \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dl,t,k}^R \Delta z_k \right] \sum_{j=1}^{NPC} \left(\frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right).
 \end{aligned} \tag{4.4.53}$$

4.4.4 Interfacial Mass and Energy Transfer

4.4.4.1 Vapor Generation in a Post-CHF Regime

The rate of vapor generation in a post-CHF cell in the core region is calculated as a sum of two components:

- (1) the rate of bulk vapor generation (condensation) and evaporation [Wulff, et al., 1984a Equation (4.4.57)],

$$\Gamma_{ph} = \frac{C_{11} + C_{12} \alpha (1 - \alpha)}{h_{fg}} \left[(T_\ell - T_{sat}) + C_{13} |T_\ell - T_{sat}| \right], \tag{4.4.54}$$

- (2) the rate of vapor generation due to wall heating following the equilibrium approximation,

$$\Gamma_w = \frac{Q_w}{h_{fg} + C_{pl}} \left[-(T_\ell - T_{sat}) \frac{\rho_\ell}{\rho_g} + \frac{1}{2} (T_w - T_{sat}) \left(\frac{\rho_\ell}{\rho_g} - 1 \right) \right]. \tag{4.4.55}$$

where Q_w is the volumetric energy transfer rate into a cell from the heated wall, in J/cm³.

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4.4.4.2 Condensation of Steam on Feedwater and ECC

A new model was developed for predicting the condensation rate in a case where a subcooled liquid (either feedwater or ECC) injected through the feedwater sparger enters the steam environment in the upper part of the downcomer region of a BWR. The injected liquid crosses the downcomer as an array of horizontal jets produced by the sparger nozzles, and subsequently mixes with the saturated water exiting from the steam separators. After mixing, the subcooled liquid flows as a film downward along the standpipes and along the core upper plenum and core shroud. The model calculates the condensation rate on both the liquid jets and the liquid film.

The condensation heat transfer on a subcooled liquid jet of diameter D can be predicted by using the correlation for the Stanton number following [Theofanous, 1979]:

$$St_j = 0.02 \left(\frac{L}{D} \right)^{-0.5}, \quad (4.4.56)$$

where L is the length of the liquid jet exposed to a pure steam environment, and D is the jet's diameter. This expression leads to the following heat-transfer coefficient (averaged over the length of the jet):

$$\bar{h}_j = \rho_{lj} v_{lj} C_{pj} St_j, \quad (4.4.57)$$

where v_{lj} is the liquid velocity at the nozzle exit.

This leads to the steam condensation rate on N liquid jets of temperature T_{lj} :

$$\Gamma_{v,jet} = - \frac{A_{TOT} \rho_{lj} C_{pj} v_{lj} St_j (T_{lj} - T_{sat})}{h_{fg}}, \quad (4.4.58)$$

where A_{TOT} is the total jets' surface area.

The second part of the model accounts for the condensation on the downflowing mixture composed of the original jets liquid, the newly formed condensate added to the jets, and the saturated liquid exiting from the steam separators. The velocity of the downflowing film is assumed to be equal to the free-fall velocity; the average film thickness is calculated by using the mass flow rate and the film velocity. A constant value of 0.0073 is used for the Stanton number following [Linehem, 1968] for the condensation on liquid films; namely,

$$\Gamma_{v,f} = -\frac{\pi}{h_{fg}} \left[0.0073 D_b H \rho_{l,f} C_{p,f} (T_{l,f} - T_{sat}) \right]. \quad (4.4.59)$$

Thus, the total steam condensation rate in the upper downcomer is simply the sum of $\Gamma_{v,jet}$ and $\Gamma_{v,f}$:

$$\begin{aligned} \Gamma_{v,TOT} = & -\frac{\pi}{h_{fg}} \left[0.02 N_{jets} \rho_{l,j} C_{p,j} v_{l,j} (T_{l,j} - T_{sat}) L^{0.5} D^{1.5} \right] \\ & -\frac{\pi}{h_{fg}} \left[0.0073 D_b H \rho_{l,f} C_{p,f} (T_{l,f} - T_{sat}) \right], \end{aligned} \quad (4.4.60)$$

where N is the number of jets, H is the film length - distance between the spargers and the downcomer water level, D_b is the outer diameter of the core barrel, and $T_{l,f}$ and $\rho_{l,f}$ are the liquid film temperature and density; respectively.

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4.5 Recirculation Flow Loop Dynamics

The BWR usually has two recirculation loops for core flow control. In RAMONA-4B, a single equivalent recirculation loop is modeled with the assumption that single-phase liquid prevails in the loop. The recirculation loop dynamics depends on the recirculation pump head which, in turn, depends on the motor/generator (M/G) set dynamics. We first present the recirculation loop dynamics in this section, then present the M/G set dynamics in the next section.

4.5.1 Recirculation Loop Drive Flow Equation

The recirculation loop drive flow rate, W_{rcl} , is governed by the equation,

$$\begin{aligned} \frac{1}{N_{rcl}} \left(\frac{L}{A} \right)_{eff} \frac{d W_{rcl}}{dt} = P_{rcp} + \left(K_{sct} + \frac{1}{2 \rho_m A_{sct}^2} \left(\frac{N_{rcl}}{N_{jtp}} \right)^2 \right) \left(\frac{W_{dc2} - W_{rcl}}{N_{rcl}} \right)^2 \\ - \left(K_{dr} + \frac{1}{2 \rho_m A_{noz}^2} \left(\frac{N_{rcl}}{N_{jet}} \right)^2 \right) \left(\frac{W_{rcl}}{N_{rcl}} \right)^2, \end{aligned} \quad (4.5.1)$$

where

- W_{rcl} = total recirculation drive flow rate,
- W_{dc2} = mixture mass flow rate entering the jet pumps in the lower downcomer,
- N_{rcl} = number of recirculation loops (an input parameter),
- N_{jtp} = number of jet pumps (an input parameter),
- $(L/A)_{eff}$ = effective flow inertia (L/A) of a single recirculation loop,
- ρ_m = mixture density in the jet pump resident cell ($k=n_p$),
- A_{sct} = suction flow area of a single jet pump,
- A_{noz} = nozzle flow area of a single jet pump,
- K_{sct} = loss coefficient at the suction of a jet pump,
- K_{dr} = total loss coefficient of the recirculation loop, given by

$$K_{dr} = K_{noz} + K_{rcl} + K_{dc2}, \quad (4.5.2)$$

- K_{noz} = loss coefficient at the nozzle of a jet pump,
- K_{rcl} = loss coefficient of a recirculation line,
- K_{dc2} = loss coefficient at the jet pump residing cell ($k=n_p$) in the lower downcomer,
- P_{rcp} = recirculation pump head from Eq. (4.5.3).

Note that, in Eq. (4.5.1), frictional losses are neglected.

4.5.1.1 Recirculation Pump Head

The recirculation pump head in Eq. (4.5.1) is calculated from the correlation,

$$P_{rcp} = H_1 \omega_p^2 + H_2 \left(\frac{W_{rcf}}{N_{rcf}} \right) + H_3 \left(\frac{W_{rcf}}{N_{rcf}} + H_4 \right) \left| \frac{W_{rcf}}{N_{rcf}} + H_4 \right|, \quad (4.5.3)$$

where ω_p is the angular speed of the recirculation pump, H_2 , H_3 and H_4 are the correlation constants treated as input parameters (typical values being $H_2 = -32.67 \text{ m}^{-1} \text{ s}^{-2}$, $H_3 = -0.108 \text{ m}^{-1} \text{ kg}^{-1}$, $H_4 = 0 \text{ kg s}^{-1}$), and H_1 is determined at $t=0$ from the steady-state condition,

$$\frac{d W_{rcf}}{d t} = 0, \quad (4.5.4)$$

which leads to

$$H_1 = \left(P_{rcp,0} - H_2 \left(\frac{W_{rcf,0}}{N_{rcf}} \right) - H_3 \left(\frac{W_{rcf,0}}{N_{rcf}} + H_4 \right) \left| \frac{W_{rcf,0}}{N_{rcf}} + H_4 \right| \right) \left(\frac{1}{\omega_{p,0}^2} \right), \quad (4.5.5)$$

where the subscript 0 stands for the steady state ($t=0$).

4.5.1.2 Recirculation Pump Speed Equation

The angular speed of the recirculation pump is governed by the equation of rotation,

$$\frac{d \omega_p}{d t} = \frac{S_{rpt} (M_{el})_{pm} - (M_{hy})_{rcp}}{I_{rcp+pm}}, \quad (4.5.6)$$

where S_{rpt} is the recirculation pump trip (RPT) signal (1=normal, 0=tripped), $(M_{el})_{pm}$ is the electrical torque of the pump motor, $(M_{hy})_{rcp}$ is the hydraulic torque of the recirculation pump, I_{rcp+pm} is the moment of inertia of the recirculation pump and pump motor assembly, which is treated as an input (typically, $I_{rcp+pm} = 1013 \text{ kg m}^2$).

The electrical torque of the pump motor is given by the equation,

$$(M_{el})_{pm} = (M_{el,0})_{pm} \left(\frac{\omega_g}{\omega_{g,0}} \right)^2 F_p, \quad (4.5.7)$$

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where $(M_{el,0})_{pm}$ is the reference electrical torque of the pump motor at steady state ($t=0$), ω_g is the generator angular speed of the M/G set, $\omega_{g,0}$ is that at steady state ($t=0$), and F_p is the induction motor torque characteristic function given by the correlation,

$$F_p = \frac{\xi_p}{2.6 \xi_p^2 + 0.019}, \quad (4.5.8)$$

with ξ_p defined as

$$\xi_p = 1 - \frac{2}{3} \frac{\omega_p}{\omega_g}. \quad (4.5.9)$$

The hydraulic torque of the recirculation pump is prescribed by the correlation,

$$(M_{hy})_{rcp} = T_1 \omega_p^2 + T_2 \omega_p W_{rc1} + T_3 (W_{rc1} + T_4 \omega_p) |W_{rc1} + T_4 \omega_p|, \quad (4.5.10)$$

where T_1 , T_2 , T_3 , and T_4 are the correlation constants treated as input parameters. The typical values are $T_1 = 0.8506 \text{ J s}^2$, $T_2 = 0.0408 \text{ J s}^2 \text{ kg}^{-1}$, $T_3 = -0.004351 \text{ J s}^2 \text{ kg}^{-2}$, $T_4 = -8.966 \text{ kg}$.

4.5.1.3 Jet Pump Head Equation

The jet pump head is required as the source term in the loop momentum equations in case of the forced circulation as in the current BWR. It is calculated from the relation,

$$P_{jtp} = -(\Delta P_{sct} + \Delta P_{thr} + \Delta P_{dif}), \quad (4.5.11)$$

where ΔP_{sct} is the pressure drop at the suction of the jet pump, ΔP_{thr} is the pressure drop at the throat, and ΔP_{dif} is the pressure drop across the diffuser of the jet pump.

The pressure drops are given by the following equations:

$$\Delta P_{sct} = \left(K_{sct} + \frac{1}{2 \rho_m A_{sct}^2} \left(\frac{N_{rc1}}{N_{jtp}} \right)^2 \right) \left(\frac{W_{dc2} - W_{rc1}}{N_{rc1}} \right)^2, \quad (4.5.12)$$

$$\Delta P_{thr} = \frac{1}{\rho_m A_{thr} N_{jtp}^2} \left(\frac{W_{dc2}^2}{A_{thr}} - \frac{W_{rc1}^2}{A_{noz}} - \frac{(W_{dc2} - W_{rc1})^2}{A_{sct}} \right), \quad (4.5.13)$$

$$\Delta P_{dif} = \left(K_{dif} - \frac{1}{2 \rho_m A_{thr}^2} \left(\frac{N_{rc1}}{N_{jtp}} \right)^2 \right) \left(\frac{W_{dc2}}{N_{rc1}} \right)^2, \quad (4.5.14)$$

where K_{dif} is the loss coefficient at the diffuser of the jet pump determined from the pressure drop in the jet pump diffuser $\Delta P_{dif,0}$ at steady state ($t=0$):

$$K_{dif} = \Delta P_{dif,0} \left(\frac{N_{rcf}}{W_{dc2}} \right)^2 + \frac{1}{2 \rho_m A_{thr}^2} \left(\frac{N_{rcf}}{N_{jtp}} \right)^2. \quad (4.5.15)$$

The steady-state $\Delta P_{dif,0}$ is obtained from the steady-state jet pump head $P_{jtp,0}$ using Eq. (4.5.11):

$$\Delta P_{dif,0} = -P_{jtp,0} - \Delta P_{thr,0} - \Delta P_{scf,0}. \quad (4.5.16)$$

where $P_{jtp,0}$ is determined from precise loop momentum balance at steady state.

4.5.2 Motor/Generator Set Dynamics

The motor/generator (M/G) set dynamics consists of the dynamic equations for the drive motor, the generator and the fluid coupler as shown in Figure 4.5.

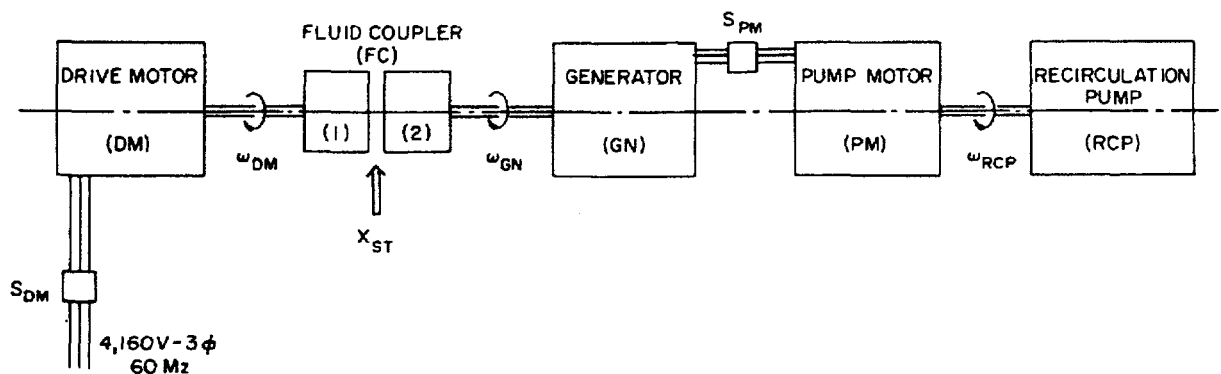


Figure 4.5 Schematic of the Motor/Generator Set in a BWR.

4.5.2.1 Drive Motor Angular Speed Equation

The angular speed of the drive motor, ω_{dm} , is governed by the equation of rotation,

$$\frac{d\omega_{dm}}{dt} = \frac{S_{dm}(M_{el})_{dm} - M_{c,1}}{I_{dm+c}}, \quad (4.5.17)$$

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where S_{dm} is the drive motor trip signal (1=normal and 0=tripped), I_{dm+c} is the moment of inertia of the drive motor and fluid coupler treated as an input (typical value being 1193 kg m²), $(M_{el})_{dm}$ is the electrical torque of the drive motor, and $M_{c,1}$ is the fluid coupler torque.

The electrical torque of the drive motor is given by

$$(M_{el})_{dm} = (M_{el,0})_{dm} F_{dm}, \quad (4.5.18)$$

where $(M_{el,0})_{dm}$ is the reference electrical torque of the drive motor at steady state ($t=0$) and F_{dm} is the torque characteristic function of the drive motor prescribed by the correlation,

$$F_{dm} = \frac{\xi_{dm}}{2.6 (\xi_{dm})^2 + 0.019}, \quad (4.5.19)$$

with ξ_{dm} defined as

$$\xi_{dm} = 1 - \frac{\omega_{dm}}{\omega_{ref}}. \quad (4.5.20)$$

Here ω_{ref} is the rated no-slip stator angular speed of the drive motor treated as an input parameter (typically, 125.6637 rad/s).

4.5.2.2 Generator Angular Speed Equation

The generator angular speed ω_g is governed by the equation of rotation,

$$\frac{d\omega_g}{dt} = \frac{M_{c,2} - S_{gpt}(M_{el})_{gn}}{I_{g+c}}, \quad (4.5.21)$$

where $M_{c,2}$ is the fluid coupler torque applied to the generator, $(M_{el})_{gn}$ is the electrical torque of the generator, and I_{g+c} is the moment of inertia of the generator/coupler assembly treated as an input parameter (typically, 1358.6 kg m²).

The generator electrical torque is calculated from the equation,

$$(M_{el})_{gn} = \frac{\omega_g^p}{\omega_g} \frac{(M_{el})_{pm}}{\eta_p \eta_g}, \quad (4.5.22)$$

where $(M_{el})_{pm}$ is given by Eq. (4.5.7), η_g is the generator efficiency treated as an input (typically, 0.96), and η_p is the pump motor efficiency prescribed by the correlation,

$$\eta_p = 1.060321 \xi_p^{0.1} (1 - \xi_p)^{0.25}, \quad (4.5.23)$$

where ξ_p is given by Eq. (4.5.9).

The generator-side coupler torque $M_{c,2}$ is obtained from the fluid coupler torque $M_{c,1}$:

$$M_{c,2} = M_{c,1} - \Delta M_c, \quad (4.5.24)$$

where ΔM_c is calculated from the relation,

$$\Delta M_c = \Delta M_{c,0} \left(\frac{\omega_g}{\omega_{g,0}} \right)^2 D_c, \quad (4.5.25)$$

with $\Delta M_{c,0}$ being the initial value. $D_c = 1$ if ω_{dm} is greater than or equal to ω_g , else $D_c = 7$. $M_{c,1}$ is calculated from Eq. (4.5.26).

4.5.2.3 Fluid Coupler Torque Equation

The fluid coupler torque $M_{c,1}$ is the output of the following first-order LAG:

$$\frac{d M_{c,1}}{dt} = \frac{M_{c,0} - M_{c,1}}{\tau_c}, \quad (4.5.26)$$

where τ_c is the time constant of the LAG treated as an input (typically, 0.1 sec) and $M_{c,0}$ is the static fluid coupler torque prescribed by the correlation,

$$M_{c,0} = M_{c,ref} x_c^3 \theta (1 + 2|\theta|^3), \quad (4.5.27)$$

with θ defined as

$$\theta = \frac{\omega_{dm} - \omega_g}{\omega_{ref}}. \quad (4.5.28)$$

In Eq. (4.5.27), x_c is the scoop tube position of the fluid coupler obtained from the recirculation flow control system described in Section 5.4.

4.5.2.4 Reference Parameters at Steady State

There are several reference parameters that must be specified at steady state. This is done by setting the time derivatives of the dynamic equations equal to zero. The following reference parameters are calculated as initial values at $t=0$:

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$$(M_{el,0})_{pm} = \frac{(M_{hy,0})_{rcp}}{F_{p,0}}, \quad (4.5.29)$$

where the subscript 0 denotes the steady state ($t=0$).

$$(M_{el,0})_{dm} = (M_{el,ref})_{dm} F_{dm,0}, \quad (4.5.30)$$

where $(M_{el,ref})_{dm}$ is an input parameter (typically, 48.633 KNm).

$$(M_{el,0})_{gn} = \frac{(M_{hy,0})_{rcp}}{\eta_{p,0} \eta_g} \left(\frac{\omega_{p,0}}{\omega_{g,0}} \right), \quad (4.5.31)$$

where η_g is the generator efficiency treated as an input (typically, 0.96) and $\eta_{p,0}$ is the pump motor efficiency at $t=0$ calculated by Eq. (4.5.7).

$$\Delta M_{c,0} = \frac{(M_{el,0})_{dm} - (M_{el,0})_{gn}}{D_{c,0}}, \quad (4.5.32)$$

where the subscript 0 denotes the steady state ($t=0$).

4.5.3 Alternative Recirculation Pump Model

An alternative recirculation-pump model is available in RAMONA-4B, which is based on the following non-dimensional equations [Madni et al., 1978] for the homologous curves of pump head and torque.

4.5.3.1 Pump Head

$$\frac{h}{\alpha^2} = C_o + C_1 \left(\frac{v}{\alpha} \right) + C_2 \left(\frac{v}{\alpha} \right)^2 + C_3 \left(\frac{v}{\alpha} \right)^3 + C_4 \left(\frac{v}{\alpha} \right)^4 + C_5 \left(\frac{v}{\alpha} \right)^5 \quad (4.5.33)$$

for

$$0 \leq \left| \frac{v}{\alpha} \right| \leq 1, \quad (4.5.34)$$

and

$$\frac{h}{v^2} = C_o + C_1 \left(\frac{\alpha}{v} \right) + C_2 \left(\frac{\alpha}{v} \right)^2 + C_3 \left(\frac{\alpha}{v} \right)^3 + C_4 \left(\frac{\alpha}{v} \right)^4 + C_5 \left(\frac{\alpha}{v} \right)^5 \quad (4.5.35)$$

for

$$0 \leq \left| \frac{\alpha}{v} \right| \leq 1. \quad (4.5.36)$$

Here,

$h = H/H_R$ = Pump Head/Pump Rated Head,

$\alpha = \Omega/\Omega_R$ = Pump Speed/Pump Rated Speed,

$v = Q/Q_R$ = Pump Flow Rate/Pump Rated Flow Rate.

The values for coefficients C_0 through C_5 are taken from [Madni, et al., 1978, Table A.I, page 40].

4.5.3.2 Pump Torque

$$\frac{\beta}{\alpha^2} = C_0 + C_1 \left(\frac{v}{\alpha} \right) + C_2 \left(\frac{v}{\alpha} \right)^2 + C_3 \left(\frac{v}{\alpha} \right)^3 + C_4 \left(\frac{v}{\alpha} \right)^4 + C_5 \left(\frac{v}{\alpha} \right)^5 \quad (4.5.37)$$

for

$$0 \leq \left| \frac{v}{\alpha} \right| \leq 1, \quad (4.5.38)$$

and

$$\frac{\beta}{v^2} = C_0 + C_1 \left(\frac{\alpha}{v} \right) + C_2 \left(\frac{\alpha}{v} \right)^2 + C_3 \left(\frac{\alpha}{v} \right)^3 + C_4 \left(\frac{\alpha}{v} \right)^4 + C_5 \left(\frac{\alpha}{v} \right)^5 \quad (4.5.39)$$

for

$$0 \leq \left| \frac{\alpha}{v} \right| \leq 1. \quad (4.5.40)$$

Here $\beta = T_{hyd}/T_R$ is the Ratio of hydraulic torque of pump and rated hydraulic torque of pump.

The values of coefficients C_0 through C_5 are taken from [Madni et al., 1978, Table A.II, Page 41].

The following equation is used for calculating the pump speed, Ω :

$$I \frac{d\Omega}{dt} = T_{el} - T_{hyd} - T_{fr}, \quad (4.5.41)$$

where

I = moment of inertia of the rotating element including shaft and impeller,

T_{el} = electric torque,

T_{hyd} = hydraulic torque,

T_{fr} = friction torque.

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A polynomial equation of the form,

$$T_{fr} = T_R (C_0 + C_1 \alpha + C_2 \alpha |\alpha|), \quad (4.5.42)$$

is used for the friction torque, and the values of the coefficients C_0 , C_1 , and C_2 are taken from [Madni et al., 1978].

The new model requires the pump-rated parameters (H_R , T_R , Ω_R , Q_R) as input; these parameters are available from the pump specifications.

4.6 Water Level Tracking

Many plant control and safety systems are actuated on the collapsed-liquid level in the downcomer in all types of BWRs. The collapsed level is detected by both narrow-range and wide-range monitors which sense the hydrostatic head in the downcomer between the bottom and the upper portion of the downcomer. BWRs have a mixture level in the upper portion of the downcomer outside of the steam separators in the normal operation. The carry-under steam is injected below this mixture level. The separated liquid falls as a film outside the separator partly below and partly above the mixture level in the normal operation. During an ATWS, the level may fall well into the downcomer. In this case, the separated liquid will fall as a film on the downcomer wall. The collapsed liquid level is a measure of liquid inventory in the downcomer. It is an important parameter for BWR safety.

The level tracking is done in the subroutine HYDRO1 in RAMONA-4B.

Both the mixture level and collapsed liquid level are tracked by RAMONA-4B in the steam dome and downcomer region of the reactor pressure vessel (RPV). The level tracking is done by keeping track of the mass inventory of vapor and liquid in these regions.

The vapor mass inventory in these regions is calculated from

$$M_{g,lv} = M_{g,RPV} - \sum_{k=k_1}^{k=N_{o,riser}} M_{g,k} \quad (4.6.1)$$

Here $M_{g,RPV}$ is the total vapor mass in the RPV obtained by integrating the overall vapor balance equation for the RPV,

$$\frac{d M_{g,RPV}}{dt} = \Psi_{tot} - (W_{sl} + W_{s,ic}), \quad (4.6.2)$$

where W_{sl} is the steam line mass flow rate, $W_{s,ic}$ is the steam mass flow rate going to the isolation condenser, and Ψ_{tot} is the total vapor generation rate in the RPV determined from the vapor generation rate in the steam dome ψ_1 and the sum of local vapor generation rates ψ_k :

$$\Psi_{tot} = \sum_{k=1}^{k=N_{o,riser}} \psi_k \quad (4.6.3)$$

In Eq. (1), $M_{g,k}$ is the local vapor mass in cell k obtained from integrating the local vapor mass balance equations, $k_1=k_0+1$ with k_0 being the cell index of the level residing cell, and $N_{o,riser}$ is the cell index of the last cell in the riser.

If $M_{g,lv}$ is greater than the maximum possible vapor mass in the dome,

$$(M_{g,dome})_{max} = \rho_g V_{dome}, \quad (4.6.4)$$

where ρ_g is the saturated vapor density, then the water level is in the downcomer region; otherwise, it is in steam dome.

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4.6.1 Level Tracking in Steam Dome

The liquid mass inventory in the steam dome is calculated from the equation,

$$M_{l,tv} = \rho_{l,1} \left(V_{dome} - \frac{M_{g,tv}}{\rho_g} \right) \quad (4.6.5)$$

where V_{dome} is the physical volume of the steam dome, $\rho_{l,1}$ is the liquid density in the dome, and ρ_g is the saturated vapor density. If the liquid mass in the dome $M_{l,tv}$ is less than 200 kg, the liquid is assumed at saturation ($T_{l,1} = T_s$); otherwise, the liquid temperature is calculated from

$$T_{l,1} = T_{s,0} + \frac{e_{f,1}}{C_{v,1}} \quad (4.6.6)$$

where $T_{s,0}$ is the reference saturation temperature corresponding to the system pressure at $t=0$, $C_{v,1}$ is the specific heat of the liquid in the dome, and $e_{f,1}$ is the liquid internal energy in the dome calculated from the equation,

$$e_{f,1} = \frac{E_{m,tv} - M_{g,tv} e_g}{M_{l,tv}} \quad (4.6.7)$$

Here e_g is the specific internal energy of saturated vapor and $E_{m,tv}$ is the mixture energy in the dome calculated from the equation,

$$E_{m,tv} = E_{m,RPV} - \sum_{k=kl}^{k=N_{o,rr}} E_{m,k} \quad (4.6.8)$$

where $E_{m,k}$ is the local mixture energy in cell k obtained from integrating the local mixture energy balance equations and $E_{m,RPV}$ is the total mixture energy in the RPV obtained by integrating the overall mixture energy balance equation for the R.V.:

$$\frac{dE_{m,RPV}}{dt} = Q_{tot} + (W_{e,fw} + W_{e,ic} + W_{e,bor} + W_{e,rwcu}) - (W_{e,st} + W_{e,ic} + W_{e,rwcu}) \quad (4.6.9)$$

Here Q_{tot} is the total heat generation rate in the R.V. obtained by summing the local heat generation rates in the core, the W_e 's are various energy flow rates, the subscript e stands for energy, the subscript fw represents feedwater, the subscript st denotes steam, the subscript ic denotes isolation condenser, and the subscript $rwcu$ stands for reactor water cleanup unit required during a startup.

Note that calculation of $T_{l,1}$ by Eq. (4.9.6) is necessary in order to calculate the vapor generation rate ψ_1 in the bulk water in the steam dome, which is needed in Eq. (4.9.14).

The collapsed liquid level in the steam dome is calculated as follows:

First, calculate the liquid level,

$$L_{l,dome} = \frac{M_{l,tv}}{\rho_{l,1}(A_{dome} - A_{sep})}, \quad (4.6.10)$$

where A_{dome} is the cross sectional area of steam dome and A_{sep} is that of the steam separator, which extends from the riser into the dome. If $L_{l,dome}$ is less than the separator height H_{sep} , Eq.(4.6.10) is the collapsed liquid level in the dome; otherwise, it is calculated from the equation,

$$L_{l,dome} = H_{sep} + \frac{\left(\frac{M_{l,tv}}{\rho_{l,1}} - H_{sep}(A_{dome} - A_{sep}) \right)}{A_{dome}}. \quad (4.6.11)$$

Calculation of the mixture level in the dome depends on whether or not the mixture level enters from the downcomer region into the dome. That is, if the level resident cell index k_0 is equal to 2, the mixture level is already in the dome; otherwise ($k_0 > 2$), it is entering from the downcomer. Note that k_0 at this point is the mixture level cell index determined at previous time step, thus properly indicates if the mixture level is entering from the downcomer into the dome.

If $k_0 = 2$, the mixture level is calculated from the following equation,

$$L_{m,dome} = \frac{L_{l,dome}}{(1 - \alpha_1)}, \quad (4.6.12)$$

where α_1 is the void fraction in the bulk water below the mixture level in the dome determined from the vapor mass in the bulk water below the mixture level, M_{gb} :

$$\alpha_1 = \frac{\frac{M_{gb}}{\rho_g}}{\frac{M_{gb}}{\rho_g} + \frac{M_{l,tv}}{\rho_{l,1}}}. \quad (4.6.13)$$

Here the vapor mass M_{gb} is obtained from integrating the vapor mass balance equation for the bulk water below the mixture level:

$$\frac{dM_{gb}}{dt} = W_{g,cu} + \psi_1 - W_{g,dci} - W_{g,up} \quad (4.6.14)$$

where $W_{g,cu}$ is the carry-under vapor flow rate from the steam separator, ψ_1 is the vapor generation rate in the bulk water below the mixture level, $W_{g,dci}$ is the vapor flow rate entering the downcomer at the downcomer entrance, and $W_{g,up}$ is the vapor flow rate leaving the bulk water at the mixture level. The vapor flow rate $W_{g,up}$ is given by

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$$W_{g,up} = \frac{\alpha_1 \rho_g V_{g,b} A_b}{(1 - \alpha_1)}, \quad (4.6.15)$$

where $V_{g,b}$ is the drift velocity at the mixture level calculated from the correlation,

$$V_{g,b} = 1.54 \left(\frac{(\rho_{l,1} - \rho_g) \sigma g}{\rho_{l,1}^2} \right)^{0.25}, \quad (4.6.16)$$

with σ being the surface tension and g being the gravitational constant (9.80665).

If $k_0 > 2$, the mixture level reenters the steam dome from the downcomer and is determined as follows:

First, calculate the vapor volume,

$$V_{g,b} = \frac{M_{g,tv}}{\rho_g} - A_{dome} (H_{dome} - H_{sep}). \quad (4.6.17)$$

The mixture level in the steam dome is then calculated from the equation,

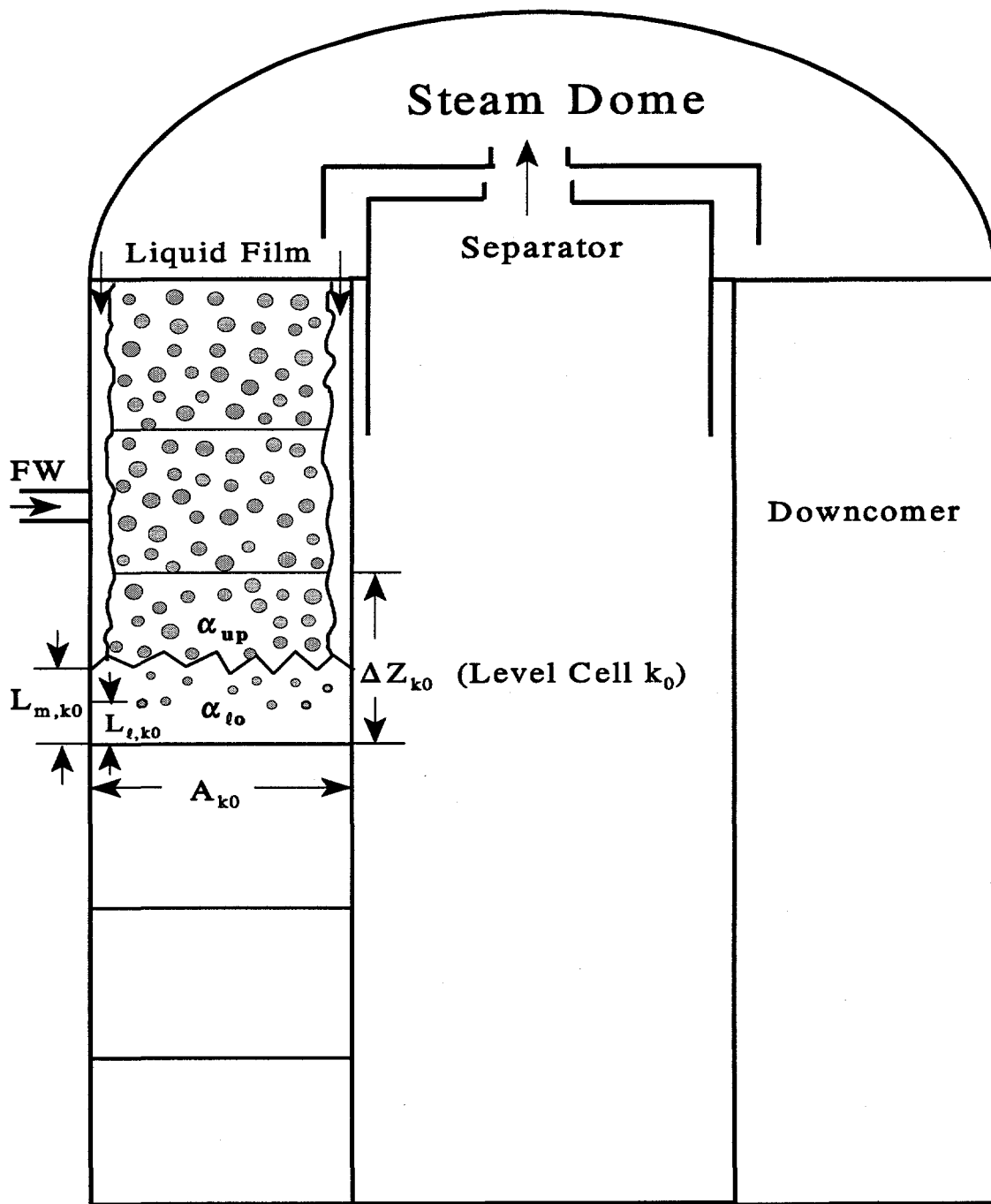
$$L_{m,dome} = \frac{H_{sep} - \frac{V_{g,b}}{A_b}}{(1 - \alpha_b)}, \quad (4.6.18)$$

where α_b is assumed to be the void fraction in the first cell of the upper downcomer ($\alpha_b = \alpha_3$), and the area A_b is equal to $(A_{dome} - A_{sep})$ if $V_{g,b} > 0$, or equal to A_{dome} if $V_{g,b} < 0$.

4.6.2 Level Tracking in Downcomer

The level tracking in downcomer is more complicated because it depends on the flow regime. It is assumed that the flow regime above the mixture level is annular flow, whereas that below the mixture level is churn-turbulent flow. In the downcomer, the level resident cell index k_0 is equal to or greater than 3, and the mixture level is tracked first, then the collapsed liquid level. The level tracking is done in both the upper downcomer (DC1) and the lower downcomer (DC2). Two level tracking indices, k_{wt} and n_{vf} , are used for level tracking in the downcomer. k_{wt} is used to determine if the mixture level is in the upper downcomer or the lower downcomer, while n_{vf} is used as a reset parameter in a predictor/corrector procedure. They are initialized as $k_{wt} = 2$ and $n_{vf} = 0$.

The level tracking in the downcomer is performed via the following steps as shown in Figure 4.6:



$L_{m,k0}$ = Mixture Level

$L_{t,k0}$ = Collapsed Liquid Level

Figure 4.6 Level Tracking in the Downcomer

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Step 1. First, calculate the total vapor mass in the region above the mixture level,

$$M_{g,dc} = M_{g,lv} - (M_{g,dome})_{\max}, \quad (4.6.19)$$

where $M_{g,lv}$ and $(M_{g,dome})_{\max}$ are obtained from Eqs. (4.6.1) and (4.6.2), respectively. The corresponding vapor volume is given by

$$V_{g,dc} = \frac{M_{g,dc}}{\rho_g}. \quad (4.6.20)$$

Step 2. Update the level tracking index k_{wl} :

$$k_{wl} = k_{wl} + 1. \quad (4.6.21)$$

Step 3. Calculate the vapor volume,

$$V_{g,lv} = V_{g,dc} - \alpha_{up} V_{dc,k}, \quad (4.6.22)$$

where $V_{dc,k}$ is the volume of cell k in the downcomer and α_{up} is the void fraction in the region above the mixture level, treated as an input at present. For an annular flow, α_{up} ranges from 0.8 to 0.99. The typical value of 0.95 is being used. Ishii specified the area-averaged drift velocity for annular flow from the momentum balance. Ishii's result leads to the following implicit expression for α_{up} ,

$$(1 + 75(1 - \alpha_{up})) \left((1 - \alpha_{up}) j_m - j_l \right)^2 = \alpha_{up}^3 \left(\frac{\rho_l j_l |j_l|}{\rho_g \sqrt{\alpha_{up}}} + 66.67 (1 - \alpha_{up})^3 \frac{\rho_l - \rho_g}{\rho_g \sqrt{\alpha_{up}}} g d_h \right), \quad (4.6.23)$$

where j_m is the mixture volumetric flux, j_l is the liquid volumetric flux above the mixture level, d_h is the hydraulic diameter, and g is the gravitational constant (9.80665). This expression can be solved iteratively by Newton-Raphson method for the given downcomer geometry to obtain a proper value for α_{up} . We recommend this as a future improvement.

Step 4. If $V_{g,lv} < 0$, the mixture level is in this cell k , calculate the total vapor volume in this cell by the equation,

$$V_{g,k} = V_{g,lv} + \alpha_{up} V_{dc,k}, \quad (4.6.24)$$

and calculate the total liquid volume in cell k by

$$V_{l,k} = V_{dc,k} - V_{g,k}. \quad (4.6.25)$$

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Step 5. If the level resident cell index k_0 from previous time step is not equal to k_{wl} , set $n_{vf}=1$ and the reset parameter IRESET=1, then do the predictor step described below. Note that setting $n_{vf}=1$ is to ensure a follow-up level calculation in a corrector step to be described later in Step 6 and that setting IRESET=1 will repeat the entire level calculation as described in Step 9. This is necessary because the previous k_0 used in Eqs. (4.6.1) and (4.6.2) was not current. If $k_0 = k_{wl}$, the mixture level remains in the same cell as previous time step, bypass the predictor step and go to Step 6.

Predictor Step:

The mixture level in cell $k=k_{wl}$ is calculated from the volume balance equation,

$$L_{m,k} = \frac{\Delta Z_k \alpha_{up} - \frac{V_{g,k}}{A_k}}{\alpha_{up} - \alpha_{lo}}, \quad (4.6.26)$$

where ΔZ_k is the height of cell k , A_k is the flow area, and α_{lo} is the void fraction in the region below the mixture level in cell k . In the predictor step, it is assumed that $\alpha_{lo} = \alpha_{k0}$, the void fraction in the cell below.

The collapsed liquid level in cell $k=k_{wl}$ is related to the mixture level by

$$L_{l,k} = L_{m,k}(1 - \alpha_{lo}). \quad (4.6.27)$$

The vapor mass in the region below the mixture level in cell $k=k_{wl}$ is simply

$$M_{g,lo} = \alpha_{lo} \rho_g L_{m,k} A_k. \quad (4.6.28)$$

RAMONA-4B uses the downcomer entrance, the interface between the steam dome and upper downcomer, as the reference zero for level tracking, above which it is positive and below which it is negative. With respect to this reference, the downcomer mixture level and collapsed liquid level become, for the upper downcomer (DC1),

$$L_{m,dc1} = \Delta Z_k(k-2) - L_{m,k}, \quad (4.6.29)$$

and

$$L_{l,dc1} = \Delta Z_k(k-2) - L_{l,k}, \quad (4.6.30)$$

respectively. In Eqs. (4.6.29) and (4.6.30), k is the index for the level resident cell k_{wl} in DC1.

For the lower downcomer (DC2), the levels with respect to the reference zero are given by

$$L_{m,dc2} = \Delta Z_k(k-2) - N_{o,1} - L_{m,k}, \quad (4.6.31)$$

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and

$$L_{\ell,dc2} = \Delta Z_k(k - 2) - N_{o,1} - L_{\ell,k}, \quad (4.6.32)$$

respectively, where $N_{o,1}$ is the last cell index of the upper downcomer and $N_{o,2}$ is that of the lower downcomer.

Step 6. If $n_{vf} = 1$, follow the corrector step below; otherwise, go to Step 7.

Corrector Step: Calculate the vapor mass in cell k from the vapor volume by Eq. (4.6.21),

$$M_{g,k} = V_{g,k} \rho_g. \quad (4.6.33)$$

The mixture level in cell $k = k$ is calculated from the equation,

$$L_{m,k} = \Delta Z_k - \frac{M_{g,k} - M_{g,lo}}{\rho_g A_k}, \quad (4.6.34)$$

where $M_{g,lo}$ is obtained from the predictor step by Eq. (4.6.28).

Next, calculate the void fraction below the mixture level as follows:

If $L_{m,k} > 0.0001 \Delta Z_k$,

$$\alpha_{lo} = \frac{M_{g,lo}}{\rho_g L_{m,k} A_k}, \quad (4.6.35)$$

otherwise, α_{lo} is set equal to the void fraction in the cell below cell k_{wl} . The collapsed liquid level in cell k is then obtained by Eq. (4.6.27).

Step 7. If the reset parameter $n_{vf} = 0$, then $k_{wl} = k_o$, the mixture level and collapsed liquid level are calculated from Eqs. (4.6.26), (4.6.27), (4.6.29), (4.6.30), (4.6.31), and (4.6.32); respectively.

Step 8. If $k_{wl} < \text{or equal to } N_{o,1}$, the last cell index of the upper downcomer (DC1), the mixture level is still in DC1, so go to Step 2 and repeat the level calculation for the next cell in DC1.

If $N_{o,1} < k_{wl} < N_{o,2}$, the last cell index of the lower downcomer (DC2), the mixture level is in DC2, go to Step 2 and continue the level calculation in DC2 until $k_{wl} > N_{o,2}$, then print the message "WATER LEVEL AT BOTTOM OF DOWNCOMER IN HYDRO1" and stop the calculation.

Step 9. Check the reset switch IRESET: If IRESET = 1, then repeat the entire level tracking from the beginning including the calculations by Eqs. (4.6.1) and (4.6.2); otherwise, it is the end of level tracking for the current time step.

4.7 Low-Pressure Thermodynamic Properties

4.7.1 Accuracy Assessment

The RAMONA-4B code employs two sets of thermodynamic property functions; namely, the original Scandpower correlations (Wulff and others, 1984), and the THOR correlations (Wulff, 1981) modified from the Jordan's (Jordan, 1967). The Scandpower correlations are used for the vessel thermal-hydraulics and the THOR correlations are used for the steam line dynamics and the new drift-flux two-phase flow dynamics.

As part of Q/A performance, we have assessed the accuracy of these property functions against the most recent NBS thermodynamic property library (Haar, Gallagher, and Kell, 1984) for pressures ranging from 0.02 to 200 bar. Most of the RAMONA-4B thermodynamic property functions are accurate to better than 0.5% over the wide pressure range of 0.02 to 200 bar except for the following:

1. The accuracy of the surface tension is very poor (-11% at 1 bar, -82% at 70 bar, and -99% at 200 bar).
2. The error of the saturated vapor compressibility is rather large (-5.9% at 1 bar, -15% at 70 bar, -51% at 200 bar).
3. The error of the temperature derivative of saturated water density (dp/dT) is also significant (+16% at 1 bar, +7.5% at 70 bar, and +68% at 200 bar).
4. The error of the $(dp/dP)_H$ function is very large over a wide range of pressures and temperatures (e.g., -15% to -24% at 70 bar for ΔT of -100 °C to +50 °C).
5. The error of the $(dp/dH)_p$ function is also large over a wide range of pressures and temperatures (e.g., -5% to -50% at 1 bar for ΔT of -100 °C to +50 °C).

In summary, the majority of RAMONA-4B thermodynamic property functions are sufficiently accurate for BWR safety analyses. However, the property functions for the surface tension, vapor compressibility, dp/dT , $(dp/dP)_H$, and $(dp/dH)_p$ need improvement, especially at low and high pressures.

4.7.2 New Correlations

New accurate correlations have been developed for the five RAMONA-4B property functions listed above. The new correlations were developed by a least-squared nonlinear curve fitting with piecewise polynomials (spline functions) to the generated data from the NBS library for the pressure range of 0.02 to 200 bar and the temperature (T_f - T_s) range of -350 °C to +50 °C. The PC program PSIPLLOT (Poly Software International, 1993) was used to obtain the new correlations using the Marquadt-Levenberg method.

4.7.2.1 Surface Tension

The new correlation for surface tension is taken from VDI (Schmidt, 1969):

$$\sigma = 10^{-3} \cdot \left(\frac{a_1(T_k - T_s)^2}{1 + \beta(T_k - T_s)} + \sum_{n=2}^{n=5} a_n(T_k - T_s)^n \right), \quad (4.7.1)$$

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where

σ = surface tension in N/m,
 T_s = saturation temperature in K,
 T_k = 647.3 K,
 a_1 = 1.160936807E-01,
 a_2 = 1.121404688E-03,
 a_3 = -5.752805180E-06,
 a_4 = 1.286274650E-08,
 a_5 = -1.149719290E-11,
 β = 0.83.

4.7.2.2 Saturated Vapor Compressibility

The saturated vapor compressibility in RAMONA-4B is defined as

$$\gamma_g = -\frac{1}{\rho_g^2} \frac{d\rho_g}{dP}, \quad (4.7.2)$$

where P is the pressure and ρ_g is the saturated vapor density.

The new correlations for γ_g in unit of $m^5/kg/N$ are given as follows:

A. For $0.02 \leq P < 0.1$ bar,

$$\gamma_g = \sum_{n=1}^{n=10} a_n \left(\frac{P - 0.02}{0.08} \right)^{n-1}, \quad (4.7.3)$$

where

P = pressure in bar,
 a_1 = -3.34245253E-02,
 a_2 = 2.44040871E-01,
 a_3 = -1.11402712E+00,
 a_4 = 3.29661088E+00,
 a_5 = -5.92837130E+00,
 a_6 = 5.37550601E+00,
 a_7 = 7.66361165E-02,
 a_8 = -5.02654153E+00,
 a_9 = 4.31982504E+00,
 a_{10} = -1.21171343E+00.

B. For $0.1 \leq P < 1$ bar,

$$\gamma_g = \sum_{n=1}^{n=11} a_n \left(\frac{P - 0.1}{0.90} \right)^{n-1}, \quad (4.7.4)$$

where P = pressure in bar,
 $a_1 = -1.45898703E-03$,
 $a_2 = 2.02479585E-02$,
 $a_3 = -1.49759787E-01$,
 $a_4 = 6.58899609E-01$,
 $a_5 = -1.76008685E+00$,
 $a_6 = 2.73966879E+00$,
 $a_7 = -1.96730360E+00$,
 $a_8 = -5.96909877E-01$,
 $a_9 = 2.30361667E+00$,
 $a_{10} = -1.66794544E+00$,
 $a_{11} = 4.21014674E-01$.

C. For $1 \leq P < 5$ bar,

$$\gamma_g = \sum_{n=1}^{n=11} a_n \left(\frac{P-1}{4} \right)^{n-1}, \quad (4.7.5)$$

where P = pressure in bar,
 $a_1 = -1.68489195E-05$,
 $a_2 = 1.29132012E-04$,
 $a_3 = -7.15674587E-04$,
 $a_4 = 3.10709039E-03$,
 $a_5 = -1.01694552E-02$,
 $a_6 = 2.40372311E-02$,
 $a_7 = -3.96923426E-02$,
 $a_8 = 4.42406219E-02$,
 $a_9 = -3.15560964E-02$,
 $a_{10} = 1.29640167E-02$,
 $a_{11} = -2.32843318E-03$.

D. For $5 \leq P < 40$ bar,

$$\gamma_g = \sum_{n=1}^{n=11} a_n \left(\frac{P-5}{35} \right)^{n-1}, \quad (4.7.6)$$

where P = pressure in bar,
 $a_1 = -7.57158281E-07$,
 $a_2 = 9.80126838E-06$,
 $a_3 = -8.43733407E-05$,
 $a_4 = 5.04552305E-04$,
 $a_5 = -2.04856645E-03$,
 $a_6 = 5.58714316E-03$,
 $a_7 = -1.01625677E-02$,
 $a_8 = 1.21119113E-02$,
 $a_9 = -9.05958160E-03$,
 $a_{10} = 3.85181952E-03$,
 $a_{11} = -7.09396023E-04$.

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E. For $40 \leq P < 120$ bar,

$$\gamma_g = \sum_{n=1}^{n=11} a_n \left(\frac{P - 40}{80} \right)^{n-1}, \quad (4.7.7)$$

where

P	=	pressure in bar,
a ₁	=	-1.44480926E-08,
a ₂	=	5.41362868E-08,
a ₃	=	-1.56562168E-07,
a ₄	=	3.94712010E-07,
a ₅	=	-8.63724270E-07,
a ₆	=	1.54924358E-06,
a ₇	=	-2.12664068E-06,
a ₈	=	2.09004721E-06,
a ₉	=	-1.36397353E-06,
a ₁₀	=	5.24812630E-07,
a ₁₁	=	-8.96377991E-08.

F. For $120 \leq P \leq 200$ bar,

$$\gamma_g = \sum_{n=1}^{n=11} a_n \left(\frac{P - 120}{80} \right)^{n-1}, \quad (4.7.8)$$

where

P	=	pressure in bar,
a ₁	=	-2.03476934E-09,
a ₂	=	2.11553037E-09,
a ₃	=	-2.32116365E-09,
a ₄	=	2.62349086E-09,
a ₅	=	-7.68615676E-09,
a ₆	=	2.71780723E-08,
a ₇	=	-6.88257671E-08,
a ₈	=	1.10779220E-07,
a ₉	=	-1.09014897E-07,
a ₁₀	=	5.98243424E-08,
a ₁₁	=	-1.41140858E-08.

These correlations for γ_g are accurate to better than 0.1%.

4.7.2.3 Saturated Water Density Derivative With Respect to Temperature

The new correlations for the saturated water density derivative with respect to temperature $d\rho_f/dT$ in unit of $\text{kg/m}^3/^\circ\text{C}$ are given as follows:

A. For $0.02 \leq P \leq 1$ bar,

$$\frac{d\rho_f}{dT} = \sum_{n=1}^{n=11} b_n \left(\frac{P - 0.02}{0.98} \right)^{n-1}, \quad (4.7.9)$$

where P = pressure in bar,
 $b_1 = -0.18296005E+00$,
 $b_2 = -6.94597004E+00$,
 $b_3 = 9.44299846E+01$,
 $b_4 = -8.28122093E+02$,
 $b_5 = 4.41371271E+03$,
 $b_6 = -1.47075461E+04$,
 $b_7 = 3.12821411E+04$,
 $b_8 = -4.24002312E+04$,
 $b_9 = 3.54007063E+04$,
 $b_{10} = -1.65778460E+04$,
 $b_{11} = 3.32917341E+03$.

B. For $1 \leq P \leq 5$ bar,

$$\frac{d\rho_f}{dT} = \sum_{n=1}^{n=11} b_n \left(\frac{P-1}{4} \right)^{n-1}, \quad (4.7.10)$$

where P = pressure in bar,
 $b_1 = -7.10872593E-01$,
 $b_2 = -5.00063175E-01$,
 $b_3 = 9.36556984E-01$,
 $b_4 = -2.28148639E+00$,
 $b_5 = 5.10027232E+00$,
 $b_6 = -8.84207563E+00$,
 $b_7 = 1.05826223E+01$,
 $b_8 = -7.75172371E+00$,
 $b_9 = 2.68391755E+00$,
 $b_{10} = 9.43301335E-02$,
 $b_{11} = -2.39756493E-01$.

C. For $5 < P \leq 200$ bar,

$$\frac{d\rho_f}{dT} = \sum_{n=1}^{n=11} b_n \left(\frac{P-5}{195} \right)^{n-1}, \quad (4.7.11)$$

where P = pressure in bar,
 $b_1 = -9.28810343E-01$,
 $b_2 = -5.55141281E+00$,
 $b_3 = 5.06649639E+01$,
 $b_4 = -4.39738747E+02$,
 $b_5 = 2.44458482E+03$,
 $b_6 = -8.67255189E+03$,
 $b_7 = 1.97651736E+04$,
 $b_8 = -2.87563386E+04$,
 $b_9 = 2.57802774E+04$,
 $b_{10} = -1.29674737E+04$,
 $b_{11} = 2.80053821E+03$.

These correlations for $d\rho_f/dT$ are accurate to better than 0.2%.

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4.7.2.4 Partial Derivative of Liquid Density With Respect to Pressure at Constant Enthalpy

The new correlation for this partial derivative was obtained by nonlinear piecewise polynomial curve fitting using the NBS library as follows:

A. For $0.02 \leq P < 5$ bar,

$$\left(\frac{\partial \rho_l}{\partial P} \right)_{h_l} = \sum_{m=1}^{m=7} a_m \left(\frac{P - 0.02}{4.98} \right)^{m-1}, \quad (4.7.12)$$

where P is pressure in bar and the coefficients a_m 's are a function of temperature:

1. For $-350^\circ\text{C} \leq T_l - T_s < -150^\circ\text{C}$,

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.13)$$

where T_l is the liquid temperature in $^\circ\text{C}$, T_s is the saturation temperature in $^\circ\text{C}$, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	4.9839296E-07	-1.9000750E-12
2	-2.1810642E-09	-3.6957770E-14
3	1.1308465E-08	-1.2424130E-13
4	-3.6086800E-08	4.1436370E-14
5	5.9164940E-08	-6.9347560E-13
6	-4.7429375E-08	5.5971890E-13
7	1.4717710E-08	-1.7370555E-13

2. For $-150^\circ\text{C} \leq T_l - T_s < -50^\circ\text{C}$,

$$a_m = \sum_{n=1}^{n=11} b_{m,n} \left(\frac{T_l - T_s + 150}{100} \right)^{n-1}, \quad (4.7.14)$$

where the coefficients $b_{m,n}$ are given by

n	$b_{1,n}$	$b_{2,n}$	$b_{3,n}$
1	4.9867800E-7	-2.1755740E-9	1.132956E-8
2	-1.5519990E-7	1.1253360E-5	-5.605817E-5
3	1.6008830E-6	-1.1361290E-4	5.040074E-4
4	-5.9433900E-6	4.0768320E-4	-1.448039E-3
5	9.0192150E-6	-5.8826060E-4	1.168985E-3
6	-1.9052660E-6	1.0674190E-4	9.446795E-4
7	-8.6987890E-6	5.0492720E-4	-9.273291E-4
8	4.7470100E-6	-1.9878380E-4	-1.152791E-3
9	7.4053200E-6	-4.7133800E-4	3.889194E-4
10	-8.6554500E-6	4.6809790E-4	1.293525E-3
11	2.5837230E-6	-1.2656910E-4	-7.143457E-4

4. Modeling of Thermal Hydraulics

n	$b_{4,n}$	$b_{5,n}$	$b_{6,n}$
1	-3.6163460E-8	5.9301440E-8	-4.754460E-8
2	9.0124810E-5	-1.9916090E-5	-6.292445E-5
3	-4.6899280E-4	-8.9900710E-4	1.766950E-3
4	-7.8526930E-4	9.1888460E-3	-1.229168E-2
5	7.2684100E-3	-2.7818910E-2	3.192530E-2
6	-9.0423510E-3	2.3604160E-2	-2.342621E-2
7	-6.2904330E-3	2.2765470E-2	-2.717846E-2
8	1.2215665E-2	-3.1438540E-2	3.266745E-2
9	9.0334520E-3	-2.8753180E-2	3.211485E-2
10	-1.9647180E-2	5.2950380E-2	-5.568023E-2
11	7.6194895E-2	-1.9566130E-2	2.015374E-2

n	$b_{7,n}$
1	1.4754680E-8
2	6.6750380E-4
3	-9.6895570E-3
4	5.8194020E-2
5	-1.9042020E-1
6	3.7792174E-1
7	-4.8179930E-1
8	4.0857180E-1
9	-2.3287642E-1
10	8.4342120E-2
11	-1.4915460E-2

3. For $-50^{\circ}\text{C} \leq T_t - T_s < 0.0^{\circ}\text{C}$,

$$a_m = \sum_{n=1}^{n=11} b_{m,n} \left(\frac{T_t - T_s + 50}{50} \right)^{n-1}, \quad (4.7.15)$$

where the coefficients $b_{m,n}$ are given by

n	$b_{1,n}$	$b_{2,n}$	$b_{3,n}$
1	4.9673166E-7	1.3698396E-7	1.564380E-6
2	-1.2117310E-7	1.9191028E-6	-2.696416E-6
3	2.9620640E-6	-2.0114100E-5	-9.102963E-5
4	-3.1035150E-5	2.3623510E-4	7.698929E-4
5	1.7370770E-4	-1.4058260E-3	-3.901376E-3
6	-5.8232700E-4	4.9843370E-3	1.188114E-2
7	1.2278130E-3	-1.1060750E-2	-2.272651E-2
8	-1.6409720E-3	1.5466120E-2	2.769813E-2
9	1.3486360E-3	-1.3222720E-2	-2.092405E-2
10	-6.2155240E-4	6.3077070E-2	8.938769E-3
11	1.2290790E-4	-1.2855280E-2	-1.651535E-3

4. Modeling of Thermal Hydraulics

n	$b_{4,n}$	$b_{5,n}$	$b_{6,n}$
1	-7.1227360E-6	1.3227630E-5	-1.127681E-5
2	4.3418840E-6	-3.8790770E-6	3.909096E-6
3	3.3705200E-4	-5.6202860E-4	3.825182E-4
4	-2.8469750E-3	4.6794965E-3	-2.956908E-3
5	1.4600329E-2	-2.4001620E-2	1.462191E-2
6	-4.5592400E-2	7.5532530E-2	-4.495968E-2
7	9.0754380E-2	-1.5271710E-1	9.029129E-2
8	-1.1655064E-1	2.0041382E-1	-1.196859E-1
9	9.3586120E-2	-1.6495640E-1	1.007136E-1
10	-4.2704390E-2	7.7202250E-2	-4.849484E-2
11	8.4429440E-3	-1.5637740E-2	1.012544E-2

n	$b_{7,n}$
1	3.6269550E-6
2	5.0552240E-6
3	-2.7152270E-4
4	2.4690730E-3
5	-1.2940800E-2
6	4.1094894E-2
7	-8.2956420E-2
8	1.0749070E-1
9	-8.6617722E-2
10	3.9488370E-2
11	-7.7745420E-3

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.16)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	5.1616475E-7	-7.1622690E-11
2	1.4932312E-6	-2.4132922E-9
3	-7.6692498E-6	1.4824270E-8
4	2.3620974E-5	-4.8082040E-8
5	-3.8064120E-5	7.9476880E-8
6	3.0206660E-5	-6.4019730E-8
7	-9.3116060E-5	1.9928840E-8
8	0.0	0.0
9	0.0	0.0
10	0.0	0.0
11	0.0	0.0

B. For $5 \leq P \leq 200$ bar,

$$\left(\frac{\partial \rho_t}{\partial P} \right)_{h_t} = \sum_{m=1}^{m=11} a_m \left(\frac{P-5}{195} \right)^{m-1}, \quad (4.7.17)$$

where the coefficients a_m 's are a function of temperature:

1. For $-350 \text{ }^\circ\text{C} \leq T_t - T_s < -50 \text{ }^\circ\text{C}$,

$$a_m = \sum_{n=1}^{n=11} b_{m,n} \left(\frac{T_t - T_s + 350}{300} \right)^{n-1}, \quad (4.7.18)$$

where the coefficients $b_{m,n}$ are given by

n	$b_{1,n}$	$b_{2,n}$	$b_{3,n}$
1	4.9867694E-7	-1.9194752E-8	1.044959E-7
2	-1.4066202E-7	1.2274300E-5	-1.559206E-4
3	2.8260910E-6	-2.8338990E-4	3.622279E-3
4	-1.4091480E-5	2.1928920E-3	-2.910208E-2
5	-3.2771790E-5	-6.7580380E-3	1.034869E-1
6	5.1383360E-4	3.4886596E-3	-1.513497E-1
7	-1.8941400E-3	2.9235390E-2	-3.737457E-2
8	3.4684340E-3	-7.9289710E-2	4.277242E-1
9	-3.4670430E-3	9.0547030E-2	-5.831892E-1
10	1.8110470E-3	-5.0236280E-2	3.446258E-1
11	2.5837230E-6	1.1093960E-2	-7.829492E-2

n	$b_{4,n}$	$b_{5,n}$	$b_{6,n}$
1	-4.9302214E-7	1.0564894E-6	-1.091187E-6
2	7.3442900E-4	-1.2854580E-3	1.197772E-3
3	-1.7160140E-2	2.8958540E-2	-2.685036E-2
4	1.4240266E-1	-2.2937861E-3	2.153705E-1
5	-5.5865480E-1	8.4162070E-1	-8.263737E-1
6	1.1200462E0	-1.4854633E0	1.652743E0
7	-1.0309698E0	8.5156870E-1	-1.608387E0
8	4.2508780E-4	1.1503570E0	2.897312E-1
9	8.2880622E-1	-2.2800349E0	8.360844E-1
10	-6.4929129E-1	1.4656521E0	-7.166524E-1
11	1.6367980E-1	-3.4202279E-1	1.831581E-1

4. Modeling of Thermal Hydraulics

n	$b_{7,n}$
1	4.1911205E-7
2	-3.2032560E-4
3	6.5007152E-3
4	-4.3889640E-2
5	1.1389658E-1
6	-7.2641660E-3
7	-5.9355160E-1
8	1.3766326E0
9	-1.4541413E0
10	7.6360917E-1
11	-1.6147752E-1

2. For $-50^{\circ}\text{C} \leq T_l - T_s < 0.0^{\circ}\text{C}$,

$$a_m = \sum_{n=1}^{n=11} b_{m,n} \left(\frac{T_l - T_s + 50}{50} \right)^{n-1}, \quad (4.7.19)$$

where the coefficients $b_{m,n}$ are given by

n	$b_{1,n}$	$b_{2,n}$	$b_{3,n}$
1	6.5981058E-7	2.7717919E-6	-6.957239E-6
2	1.4090390E-7	-4.0905980E-6	3.541589E-5
3	-3.8445650E-7	1.3963163E-4	-8.638920E-4
4	4.6594530E-6	-1.4968830E-3	7.522087E-3
5	-2.8703970E-5	8.5740360E-3	-3.217207E-2
6	1.0669962E-4	-2.9280290E-2	7.152307E-2
7	-2.5029170E-4	6.2509155E-2	-7.061578E-2
8	3.7311620E-4	-8.4132250E-2	-1.442578E-2
9	-3.4272030E-4	6.9302326E-2	1.010270E-1
10	1.7678070E-4	-3.1874006E-2	-8.717572E-2
11	-3.9156397E-5	6.2636272E-3	2.515048E-2

n	$b_{4,n}$	$b_{5,n}$	$b_{6,n}$
1	1.8742209E-5	-2.7084703E-5	2.005010E-5
2	-1.6299670E-4	3.7953215E-4	-3.316413E-4
3	3.9302100E-3	-9.1853000E-3	8.109515E-3
4	-3.4027426E-2	8.1740610E-2	-7.303613E-2
5	1.4380161E-1	-3.6112206E-1	3.256270E-1
6	-3.1033626E-1	8.5019466E-1	-7.692014E-1
7	2.7337920E-1	-9.8204070E-1	8.761413E-1
8	1.5386013E-1	1.9493606E-1	-1.194544E-1
9	-5.5377196E-1	7.6858718E-1	-7.921565E-1
10	4.5062518E-1	-7.8258794E-1	7.822634E-1
11	-1.2732134E-1	2.3916443E-1	-2.380381E-1

n	$b_{7,n}$
1	-5.9538010E-6
2	1.5509770E-4
3	-3.6524180E-3
4	3.1603580E-2
5	-1.3343193E-1
6	2.8744605E-1
7	-2.5150000E-1
8	-1.4738105E-1
9	5.1952405E-1
10	-4.2199707E-1
11	1.1925992E-1

3. For $T_l - T_s \geq 0.0$ °C (superheated),

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.20)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	7.9958123E-7	-3.7596440E-10
2	4.0332847E-6	-1.4799800E-9
3	5.3266481E-6	-4.8918700E-8
4	-2.0630911E-4	7.8299865E-7
5	1.8765638E-3	-5.8947270E-6
6	-8.5181820E-3	2.4896232E-5
7	2.2682981E-2	-6.3662473E-5
8	-3.6801097E-2	1.0055670E-4
9	3.5843391E-2	-9.5975963E-5
10	-1.9279657E-2	5.0762510E-5
11	4.4140633E-3	-1.1441056E-5

These correlations for $(\partial\rho/\partial P)_h$ are accurate to better than 0.5%.

4.7.2.5 Partial Derivative of Liquid Density With Respect to Enthalpy at Constant Pressure

The new correlation for this partial derivative was obtained by nonlinear piecewise polynomial curve fitting using the NBS library. In the following, P is the pressure in bar and all the temperatures are in °C.

4. Modeling of Thermal Hydraulics

A. For $0.02 \leq P < 0.15$ bar,

1. For $-350^\circ\text{C} \leq T_l - T_s < -250^\circ\text{C}$,

$$\left(\frac{\partial p_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.21)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0806922 \times 10^{-5} + 5.8858902 \times 10^{-11} (T_l - T_s), \quad (4.7.22)$$

$$a_2 = -6.5224936 \times 10^{-8} - 1.4043340 \times 10^{-11} (T_l - T_s).$$

2. For $-250^\circ\text{C} \leq T_l - T_s < -25^\circ\text{C}$,

$$\left(\frac{\partial p_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.23)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0806922 \times 10^{-5} + 5.8858902 \times 10^{-11} (T_l - T_s), \quad (4.7.24)$$

$$a_2 = -6.1822286 \times 10^{-8} - 4.2405322 \times 10^{-13} (T_l - T_s).$$

3. For $-25^\circ\text{C} < T_l - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial p_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.02}{0.13} \right)^{m-1}, \quad (4.7.25)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_l - T_s + 25}{25} \right)^{n-1}, \quad (4.7.26)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(I), I=1,2,3,4,\dots,10$$

$$a_1 \quad -2.1590227\text{E-}5, 1.1439236\text{E-}6, 1.7888688\text{E-}5, 7.1565590\text{E-}6, \\ -3.5039367\text{E-}5, -2.1903959\text{E-}5, -3.2176954\text{E-}5, 8.6068083\text{E-}6, \\ 6.9688589\text{E-}6, 2.5263919\text{E-}5$$

- a_2 3.0195632E-5, 2.4150183E-4, -5.1452421E-4, -1.9996464E-3,
 2.7788630E-4, 2.4548190E-3, 1.6953223E-3, -1.0425538E-3,
 -2.5506732E-3, 1.1854634E-3
- a_3 -8.5426097E-5, 7.2067194E-3, -1.9743365E-1, 1.6287610E+0,
 -6.8869731E+0, 1.6870505E+1, -2.4654709E+1, 2.1133092E+1,
 -9.7927890E+0, 1.8927629E+0
- a_4 -2.6185703E-4, -1.3141087E-2, 3.3414236E-1, -2.7469558E+0,
 1.1890711E+1, -2.9990501E+1, 4.5241966E+1, -4.0148604E+1,
 1.9328709E+1, -3.8962308E+0
- a_5 9.2599465E-5, 1.7897949E-3, -5.5171400E-3, -2.9029316E-4,
 1.0185550E-2, -6.8067834E-3, -2.0864234E-3, 5.3751087E-3,
 -4.1034580E-3, 1.2269712E-3
- a_6 2.7460578E-4, 8.8183772E-5, -1.8057056E-2, 9.8620762E-2,
 -3.4887064E-1, 7.7592003E-1, -8.8648345E-1, 3.6075282E-1,
 1.1134096E-1, -9.3567357E-2
- a_7 1.8644729E-4, 5.3970998E-3, -1.4539703E-1, 1.1737938E+0,
 -4.9817057E+0, 1.2375959E+1, -1.8459618E+1, 1.6236243E+1,
 -7.7583308E+0, 1.5535259E+0
- a_8 5.3329760E-5, -1.1420020E-3, 1.6290660E-3, -1.3583221E-3,
 7.2491145E-3, -1.8334389E-3, -1.4616633E-2, -7.8819653E-4,
 2.2512887E-2, -1.1645112E-2
- a_9 -1.1945359E-4, 8.3851877E-6, 1.5709258E-3, 4.6719311E-3,
 -1.9457117E-2, 1.2683287E-3, 3.0737467E-2, 2.5467517E-4,
 -3.8754585E-2, 1.9819433E-2
- a_{10} -2.2011790E-4, 1.0134880E-4, 2.1494275E-2, -1.3045443E-1,
 4.1815570E-1, -7.7531381E-1, 6.9549736E-1, -8.8133592E-2,
 -2.6747878E-1, 1.2632912E-1

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 0.02}{0.13} \right)^{m-1}, \quad (4.7.27)$$

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.28)$$

4. Modeling of Thermal Hydraulics

where T_ℓ is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-4.3295000E-5	0.0
2	-2.2111133E-4	0.0
3	3.6111270E-4	0.0
4	-2.2212400E-4	0.0
5	-1.3859546E-4	0.0
6	1.5051930E-4	0.0

B. For $0.15 \leq P < 0.5$ bar,

1. For $-350 \text{ }^\circ\text{C} \leq T_\ell - T_s < -250 \text{ }^\circ\text{C}$,

$$\left(\frac{\partial \rho_\ell}{\partial h_\ell} \right)_P = a_1 + a_2 P, \quad (4.7.29)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0802010 \times 10^{-5} + 7.7979490 \times 10^{-11} (T_\ell - T_s), \quad (4.7.30)$$

$$a_2 = -6.5224936 \times 10^{-8} - 1.4043340 \times 10^{-11} (T_\ell - T_s).$$

2. For $-250 \text{ }^\circ\text{C} \leq T_\ell - T_s < -75 \text{ }^\circ\text{C}$,

$$\left(\frac{\partial \rho_\ell}{\partial h_\ell} \right)_P = a_1 + a_2 P, \quad (4.7.31)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0802010 \times 10^{-5} + 7.7979490 \times 10^{-11} (T_\ell - T_s), \quad (4.7.32)$$

$$a_2 = -6.1796266 \times 10^{-8} - 2.8129659 \times 10^{-13} (T_\ell - T_s).$$

3. For $-75 \text{ }^\circ\text{C} < T_\ell - T_s < -45 \text{ }^\circ\text{C}$,

$$\left(\frac{\partial \rho_\ell}{\partial h_\ell} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.15}{0.35} \right)^{m-1}, \quad (4.7.33)$$

$$a_m = \sum_{n=1}^{n=N} b_{m,n} \left(\frac{T_\ell - T_s + 75}{30} \right)^{n-1}, \quad (4.7.34)$$

4. Modeling of Thermal Hydraulics

where $N=6$ for $m=1,2,3,4,5,6,7,8,10$ and $N=10$ for $m=9$. The coefficients $b_{m,n}$ are given by

$$b_{m,n}(I), I=1,2,3,4,5,6$$

$$a_1 \quad -2.0824382E-5, 5.2368889E-7, -4.6190145E-6, 1.2014281E-5, \\ -1.2014058E-5, 4.0892255E-6$$

$$a_2 \quad 2.8394952E-7, -1.0217850E-4, 1.3910526E-3, -6.0298281E-3, \\ 9.8055711E-3, -5.1832829E-3$$

$$a_3 \quad -4.3734132E-6, -3.8059978E-4, 2.3109882E-3, 1.5031274E-3, \\ -1.3543073E-2, 1.0169491E-2$$

$$a_4 \quad 1.2137330E-5, 3.0256825E-4, -2.5698619E-3, 5.2886784E-3, \\ -3.6943792E-3, 6.9347302E-4$$

$$a_5 \quad -1.0893746E-5, 5.1056547E-4, -3.8996083E-3, 4.9969218E-3, \\ 2.5331895E-3, -4.1513858E-3$$

$$a_6 \quad 8.0751416E-6, 3.8258674E-4, -2.8436035E-3, 2.9264398E-3, \\ 3.7915921E-3, -4.2748674E-3$$

$$a_7 \quad -1.1908984E-5, 1.4633219E-4, -1.0626415E-3, 6.7419965E-4, \\ 2.5952424E-3, -2.3648584E-3$$

$$a_8 \quad 8.2660530E-6, -4.9981943E-5, 3.3928823E-4, -3.3745576E-4, \\ -2.8377601E-4, 3.2884915E-4$$

$$a_9 \quad 8.9757177E-6, -3.0200278E-5, 1.3806291E-5, 2.1738783E-5, \\ -1.8985075E-5, -5.5511574E-7, -1.1240843E-5, 1.6215344E-5, \\ -5.5434244E-6, 1.3779875E-6$$

$$a_{10} \quad 9.2916173E-6, -4.4549918E-4, 3.3190589E-3, -4.1975465E-3, \\ -2.1683656E-3, 3.5038820E-3$$

4. For $-45^\circ\text{C} \leq T_t - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.15}{0.35} \right)^{m-1}, \quad (4.7.35)$$

$$a_m = \sum_{n=1}^{n=N} b_{m,n} \left(\frac{T_t - T_s + 45}{45} \right)^{n-1}, \quad (4.7.36)$$

4. Modeling of Thermal Hydraulics

where $N=6$ for $m=1,2,3,10$ and $N=10$ for $m=4,5,6,7,8,9$. The coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

$$a_1 \quad -2.0593735E-5, -1.2095006E-4, -4.3539617E-6, 7.4928982E-5, \\ -4.9087789E-5, 5.9094875E-6$$

$$a_2 \quad -1.2150677E-4, -8.5693492E-5, 6.0209537E-4, -6.8102405E-4, \\ 7.7100134E-5, 1.4250326E-4$$

$$a_3 \quad 5.6927521E-5, 7.4540980E-4, -4.3306801E-3, 9.3835960E-3, \\ -8.9554385E-3, 3.1413554E-3$$

$$a_4 \quad 3.2656970E-5, -2.8336334E-5, 2.4640667E-5, 6.1183618E-6, \\ -2.4287626E-5, 5.3432219E-6, -4.0851505E-6, 1.8712087E-5, \\ -7.7752544E-6, -1.9851402E-6$$

$$a_5 \quad -2.2394855E-5, -3.3173473E-4, 1.5442762E-3, -2.2898443E-3, \\ 2.5311554E-4, 1.4088878E-3, 4.0194509E-4, -8.2463701E-4, \\ -9.1582450E-4, 7.5129099E-4$$

$$a_6 \quad -3.2484128E-5, 5.7854960E-5, -5.3291513E-5, 1.1136689E-5, \\ 6.9623953E-6, 1.8430928E-5, -1.3796916E-5, 3.9829702E-7, \\ -1.6814993E-5, 1.3244507E-5$$

$$a_7 \quad -2.4956320E-5, -1.1078990E-4, 4.0614532E-4, -3.2651220E-4, \\ -1.9529312E-4, 9.9415665E-5, 1.5949301E-4, 1.0390131E-4, \\ -3.5733909E-5, -9.7549122E-5$$

$$a_8 \quad 5.1616759E-6, 7.9601785E-6, -1.6649931E-5, 1.6692294E-5, \\ -6.9670946E-6, 8.4922913E-6, -1.2228207E-5, 8.8586571E-6, \\ -1.0935060E-5, 6.8277663E-6$$

$$a_9 \quad -5.1028337E-6, 1.1182846E-4, -3.8898020E-4, 3.1958465E-4, \\ 1.5891029E-4, -7.1920773E-5, -1.6369743E-4, -7.4822244E-5, \\ 1.1063084E-5, 9.8783866E-5$$

$$a_{10} \quad -2.676178E-5, 1.6266984E-5, -1.0111664E-5, 1.0467351E-5, \\ -9.4872368E-6, 3.4681336E-6$$

5. For $T_t - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial p_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 0.15}{0.35} \right)^{m-1}, \quad (4.7.37)$$

$$a_m = b_{m,1} + b_{m,2}(T_t - T_s), \quad (4.7.38)$$

4. Modeling of Thermal Hydraulics

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-1.1407101E-4	0.0
2	-5.3043377E-5	0.0
3	-5.4816874E-5	0.0
4	1.2138989E-4	0.0
5	4.0129600E-6	0.0
6	-5.3591765E-5	0.0

C. For $0.50 \leq P < 1.25$ bar,

1. For $-350 \text{ °C} \leq T_l - T_s < -250 \text{ °C}$,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.39)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0797461 \times 10^{-5} + 9.4600000 \times 10^{-11} (T_l - T_s), \quad (4.7.40)$$

$$a_2 = -6.5224936 \times 10^{-8} - 1.4043340 \times 10^{-11} (T_l - T_s).$$

2. For $-250 \text{ °C} \leq T_l - T_s < -100 \text{ °C}$,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.41)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0797461 \times 10^{-5} + 9.4600000 \times 10^{-11} (T_l - T_s), \quad (4.7.42)$$

$$a_2 = -6.1785823 \times 10^{-8} - 2.3066400 \times 10^{-13} (T_l - T_s).$$

3. For $-100 \text{ °C} < T_l - T_s < -75 \text{ °C}$,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.5}{0.75} \right)^{m-1}, \quad (4.7.43)$$

4. Modeling of Thermal Hydraulics

$$a_m = \sum_{n=1}^{n=6} b_{m,n} \left(\frac{T_t - T_s + 100}{25} \right)^{n-1}, \quad (4.7.44)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,\dots,6$

a_1 -2.0848050E-5, 2.3881769E-6, -9.4690559E-6, 1.1817072E-5,
-1.0411492E-5, 6.1071907E-6

a_2 4.1047960E-7, -5.9388700E-5, 1.1738502E-4, 1.1814177E-4,
-2.4211646E-5, -1.5809986E-4

a_3 -3.6673651E-6, -1.1933907E-5, 4.7555069E-5, 1.8549729E-5,
-1.0788654E-4, -2.0819273E-4

a_4 9.7560587E-6, 5.5872890E-4, -1.1765419E-3, -9.3334947E-4,
1.8938637E-4, 1.6284780E-3

a_5 -1.1316181E-5, -4.2182708E-5, 8.6299688E-5, 1.0928422E-4,
2.1190151E-5, -5.0216306E-5

a_6 9.5995626E-6, -3.9054705E-4, 8.2692364E-4, 7.0314002E-4,
-1.1746735E-4, -1.0969793E-3

a_7 -1.0078181E-5, -3.4853632E-4, 7.3298959E-4, 6.1693578E-4,
-1.2690387E-4, -1.0080218E-3

a_8 9.4079811E-6, -1.3339238E-4, 2.5927693E-4, 2.2889825E-4,
-6.1549399E-5, -3.7840532E-4

a_9 -1.1550236E-5, 1.0275029E-4, -2.6711377E-4, -1.9565915E-4,
2.5439457E-5, 3.4304874E-4

a_{10} 7.3958470E-6, 2.9972411E-4, -7.1243163E-4, -5.5097099E-4,
1.0684584E-4, 9.6482415E-4

4. For $-75^\circ\text{C} \leq T_t - T_s < 45^\circ\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.5}{0.75} \right)^{m-1}, \quad (4.7.45)$$

$$a_m = \sum_{n=1}^{n=6} b_{m,n} \left(\frac{T_\ell - T_s + 75}{30} \right)^{n-1}, \quad (4.7.46)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,6$$

$$a_1 \quad -2.0293091\text{E-}5, 5.3224531\text{E-}7, -2.2078919\text{E-}4, 1.5224520\text{E-}4, \\ 1.0785005\text{E-}4, -1.0456970\text{E-}4$$

$$a_2 \quad -5.8950441\text{E-}6, -1.2850730\text{E-}3, 5.1297332\text{E-}3, -8.6214756\text{E-}3, \\ 6.6686179\text{E-}3, -1.9518123\text{E-}3$$

$$a_3 \quad -2.6557573\text{E-}4, 4.1500000\text{E-}3, -1.6778356\text{E-}2, 3.0068056\text{E-}2, \\ -2.5053407\text{E-}2, 7.9149201\text{E-}3$$

$$a_4 \quad 2.7645797\text{E-}4, -3.2414820\text{E-}3, 1.2846380\text{E-}2, -2.2932313\text{E-}2, \\ 1.9087234\text{E-}2, -6.0278765\text{E-}3$$

$$a_5 \quad 1.1289305\text{E-}4, -1.7266790\text{E-}3, 6.5099259\text{E-}3, -1.0825161\text{E-}2, \\ 8.3445421\text{E-}3, -2.4384809\text{E-}3$$

$$a_6 \quad -6.5266357\text{E-}5, 7.0238066\text{E-}4, -2.5478701\text{E-}3, 4.2026681\text{E-}3, \\ -3.2321305\text{E-}3, 9.4376051\text{E-}4$$

$$a_7 \quad -1.4347142\text{E-}4, 1.5282343\text{E-}3, -5.6648117\text{E-}3, 9.3872223\text{E-}3, \\ -7.2292708\text{E-}3, 2.1197980\text{E-}3$$

$$a_8 \quad -7.5665248\text{E-}5, 1.0451480\text{E-}3, -3.8997583\text{E-}3, 6.4717093\text{E-}3, \\ -4.9864463\text{E-}3, 1.4571183\text{E-}3$$

$$a_9 \quad -3.1026198\text{E-}6, -1.9734244\text{E-}5, 2.8493715\text{E-}5, -3.0002498\text{E-}6, \\ -2.9447884\text{E-}5, 1.8065096\text{E-}5$$

$$a_{10} \quad 1.1527558\text{E-}4, -1.1930336\text{E-}3, 4.4053460\text{E-}3, -7.2927152\text{E-}3, \\ 5.6134130\text{E-}3, -1.6392828\text{E-}3$$

5. For $-45^\circ\text{C} \leq T_\ell - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial p_\ell}{\partial h_\ell} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 0.5}{0.75} \right)^{m-1}, \quad (4.7.47)$$

4. Modeling of Thermal Hydraulics

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_l - T_s + 45}{45} \right)^{n-1}, \quad (4.7.48)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

$$a_1 \quad -8.5017107\text{E-}5, -8.1331957\text{E-}5, 1.5530782\text{E-}5, 1.2186292\text{E-}5, \\ -1.7539939\text{E-}5, 7.4224334\text{E-}6, -7.1046945\text{E-}6, 1.3458109\text{E-}5, \\ -1.0532065\text{E-}5, 2.9851873\text{E-}6$$

$$a_2 \quad -6.6024748\text{E-}5, 4.2769611\text{E-}5, -2.8757755\text{E-}5, 9.4454249\text{E-}6, \\ -2.9961461\text{E-}6, 1.4028190\text{E-}5, -1.1047048\text{E-}5, 5.9765102\text{E-}6, \\ -1.3952833\text{E-}5, 8.7109796\text{E-}6$$

$$a_3 \quad 3.5601952\text{E-}5, -3.4879467\text{E-}5, 2.3992169\text{E-}5, 7.3623519\text{E-}6, \\ -2.3950078\text{E-}5, 4.9857658\text{E-}6, -4.4385390\text{E-}6, 1.8664852\text{E-}5, \\ -7.6072674\text{E-}6, -1.8942082\text{E-}6$$

$$a_4 \quad 8.3989034\text{E-}6, 5.1280320\text{E-}6, -8.6190305\text{E-}6, 1.0038042\text{E-}5, \\ -1.0475657\text{E-}5, 1.0363043\text{E-}5, -8.9048483\text{E-}6, 1.0621273\text{E-}5, \\ -1.1474627\text{E-}5, 4.8657183\text{E-}6$$

$$a_5 \quad -2.3132982\text{E-}5, 1.2007311\text{E-}5, -1.2684537\text{E-}5, 1.0111959\text{E-}5, \\ -8.8687190\text{E-}6, 1.1086068\text{E-}5, -9.4012528\text{E-}6, 9.6315265\text{E-}6, \\ -1.1985508\text{E-}5, 5.6780484\text{E-}6$$

$$a_6 \quad 3.6067499\text{E-}6, 4.8321936\text{E-}6, -6.1241921\text{E-}6, 9.4506213\text{E-}6, \\ -1.1619833\text{E-}5, 1.0031138\text{E-}5, -8.4618299\text{E-}6, 1.1269161\text{E-}5, \\ -1.1219276\text{E-}5, 4.2851336\text{E-}6$$

$$a_7 \quad -9.9740096\text{E-}6, -1.0324027\text{E-}6, -1.3728315\text{E-}6, 9.0568343\text{E-}6, \\ -1.3569829\text{E-}5, 9.2605041\text{E-}6, -7.8146470\text{E-}6, 1.2424321\text{E-}5, \\ -1.0667349\text{E-}5, 3.3186195\text{E-}6$$

$$a_8 \quad 1.2200568\text{E-}5, -2.8891892\text{E-}6, -2.1031486\text{E-}7, 9.0234472\text{E-}6, \\ -1.4035701\text{E-}5, 9.0504302\text{E-}6, -7.6756785\text{E-}6, 1.2705503\text{E-}5, \\ -1.0518942\text{E-}5, 3.0964805\text{E-}6$$

$$a_9 \quad -8.7226732\text{E-}6, -1.4882584\text{E-}6, -4.1426735\text{E-}6, 1.2761165\text{E-}5, \\ -1.2925617\text{E-}5, 7.5393636\text{E-}6, -9.1749641\text{E-}6, 1.2480074\text{E-}5, \\ -9.8096974\text{E-}6, 3.5247503\text{E-}6$$

$$a_{10} \quad 8.8777935\text{E-}6, 1.1259133\text{E-}7, -3.4760338\text{E-}6, 9.4555793\text{E-}6, \\ -1.2652699\text{E-}5, 9.5283104\text{E-}6, -8.1866501\text{E-}6, 1.1884228\text{E-}5, \\ -1.0871810\text{E-}5, 3.8277072\text{E-}6$$

6. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 0.5}{0.75} \right)^{m-1}, \quad (4.7.49)$$

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.50)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-1.4994181E-4	0.0
2	-4.2230918E-5	0.0
3	2.0740870E-5	0.0
4	5.7006046E-6	0.0
5	-2.2121420E-5	0.0
6	1.1238256E-5	0.0

D. For $1.25 \leq P < 6.0$ bar,

1. For -350 °C $\leq T_l - T_s < -250$ °C,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.51)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = \sum_{n=1}^{n=6} b_{n,1} \left(\frac{T_l - T_s + 350}{100} \right)^{n-1}, \quad (4.7.52)$$

$$a_2 = -6.5224936 \times 10^{-8} - 1.4043340 \times 10^{-11} (T_l - T_s).$$

and the coefficients $b_{m,1}$ are given by

m	$b_{m,1}$
1	-2.0855124E-5
2	4.0181685E-6
3	-2.3876962E-5
4	3.0305650E-5
5	2.0610086E-5
6	-3.6396178E-5

4. Modeling of Thermal Hydraulics

2. For $-250\text{ }^{\circ}\text{C} \leq T_t - T_s \leq -150\text{ }^{\circ}\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = -2.0812728 \times 10^{-5} - 6.1735846 \times 10^{-8} P. \quad (4.7.53)$$

3. For $-150\text{ }^{\circ}\text{C} < T_t - T_s < -100\text{ }^{\circ}\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 1.25}{4.75} \right)^{m-1}, \quad (4.7.54)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_t - T_s + 150}{50} \right)^{n-1}, \quad (4.7.55)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,4$

a_1 -2.0899115E-5, 1.8150633E-6, -1.1724837E-5, 1.2330217E-5

a_2 8.1454443E-8, 1.0417384E-4, -1.0985228E-4, -2.0464440E-4

a_3 -2.9310834E-6, 3.0835679E-4, -3.6965778E-5, -2.1591971E-4

a_4 8.9489639E-6, -1.1235122E-3, 2.4228959E-4, 1.1034465E-3

a_5 -1.2832384E-5, -2.6413684E-4, -8.7052805E-6, 1.8073131E-4

a_6 9.7135108E-6, 6.2351761E-4, -1.6665910E-4, -6.1338357E-4

a_7 -7.9661209E-6, 6.7297184E-4, -1.2658780E-4, -5.8093584E-4

a_8 1.1951447E-5, 1.8473547E-4, -7.3510041E-6, -9.3372632E-5

a_9 -1.0954904E-5, -2.7863525E-4, 6.3317557E-5, 3.0267386E-4

a_{10} 3.7060337E-6, -3.0382363E-4, 1.0673755E-5, 2.3540720E-4

4. For $-100\text{ }^{\circ}\text{C} \leq T_f - T_s \leq -75\text{ }^{\circ}\text{C}$,

$$\left(\frac{\partial \rho_f}{\partial h_f} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 1.25}{4.75} \right)^{m-1}, \quad (4.7.56)$$

$$a_m = \sum_{n=1}^{n=6} b_{m,n} \left(\frac{T_f - T_s + 100}{25} \right)^{n-1}, \quad (4.7.57)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,4,\dots,6$

- a_1 -1.8366246E-5,-8.1344050E-6,-1.6937871E-4, 1.3035303E-4,
6.3251266E-5,-7.2929923E-5
- a_2 -2.1038864E-4,-1.1341058E-3, 5.2842291E-3,-9.7475203E-3,
8.2655332E-3,-2.6497275E-3
- a_3 5.2540258E-5, 4.5020953E-3,-2.0206104E-2, 3.8704578E-2,
-3.4258094E-2, 1.1429212E-2
- a_4 2.3117289E-4,-4.7924733E-3, 2.0602424E-2,-3.9210691E-2,
3.4653642E-2,-1.1555912E-2
- a_5 -1.0507638E-4,-1.0462931E-3, 4.7810288E-3,-8.8138780E-3,
7.4725656E-3,-2.3952132E-3
- a_6 -1.4680645E-4, 1.8740486E-3,-7.7920496E-3, 1.4735649E-2,
-1.2993675E-2, 4.3275800E-3
- a_7 -4.2328751E-5, 1.6437007E-3,-6.8965705E-3, 1.2548116E-2,
-1.0603963E-2, 3.3952891E-3
- a_8 9.6295673E-5, 1.2249456E-4,-4.9067124E-4, 3.4372789E-4,
2.1210921E-4,-2.1969561E-4
- a_9 7.6317854E-5,-7.9222718E-4, 3.0715881E-3,-5.5124880E-3,
4.6413831E-3,-1.4840737E-3
- a_{10} -5.4413730E-5,-3.4594371E-4, 1.2907712E-3,-1.6340654E-3,
7.4273141E-4,-4.3819261E-5

4. Modeling of Thermal Hydraulics

5. For $-75^{\circ}\text{C} \leq T_t - T_s < 0.0^{\circ}\text{C}$,

$$\left(\frac{\partial p_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=10} a_m \left(\frac{P - 1.25}{4.75} \right)^{m-1}, \quad (4.7.58)$$

$$a_m = \sum_{n=1}^{n=6} b_{m,n} \left(\frac{T_t - T_s + 75}{75} \right)^{n-1}, \quad (4.7.59)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,4,\dots,6$

a_1 -7.5300203E-5,-1.4455206E-4, 5.5100339E-5, 1.0899613E-5,
-4.2090740E-5, 1.9138775E-5

a_2 -1.9165779E-4, 2.0328723E-4,-1.8393828E-4, 5.1813845E-5,
6.4928184E-5,-4.5196503E-5

a_3 2.2386461E-4,-3.3989560E-4, 4.7483289E-4,-4.3504413E-4,
1.8654304E-4,-1.8460448E-5

a_4 -7.1353327E-5, 1.5106347E-4,-1.9541811E-4, 8.2370693E-5,
7.2847413E-5,-5.7784751E-5

a_5 -1.0658015E-4, 1.3428967E-4,-1.4793749E-4, 5.8127033E-5,
5.0151796E-5,-3.9732078E-5

a_6 4.6654412E-6,-1.0335232E-5, 2.2511896E-5,-1.9220809E-6,
-2.4764428E-5, 1.4568345E-5

a_7 4.4066188E-5,-8.5384086E-5, 9.7949041E-5,-2.5158256E-5,
-5.7000742E-5, 3.7274770E-5

a_8 6.4121717E-5,-7.1935323E-5, 7.2139759E-5,-1.3522186E-5,
-4.4956747E-5, 2.8046969E-5

a_9 4.4136306E-7,-7.2298497E-6,-2.9505197E-6, 1.2659500E-5,
-1.2050387E-5, 4.2403257E-6

a_{10} -4.4698580E-5, 7.0625271E-5,-8.0585529E-5, 3.6482536E-5,
2.1048722E-5,-1.9086052E-5

6. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 1.25}{4.75} \right)^{m-1}, \quad (4.7.60)$$

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.61)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-1.7684377E-4	0.0
2	-1.0019798E-4	0.0
3	9.2170814E-5	0.0
4	-2.9566330E-5	0.0
5	-4.0304000E-5	0.0
6	2.8397415E-5	0.0

E. For $6 \leq P < 19$ bar,1. For -350 °C $\leq T_l - T_s < -200$ °C,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.62)$$

where the coefficients a_1 and a_2 are given by

$$a_1 = -2.0772956 \times 10^{-5} + 1.7864200 \times 10^{-10}(T_l - T_s), \quad (4.7.63)$$

$$a_2 = -6.3569987 \times 10^{-8} - 8.8171850 \times 10^{-12}(T_l - T_s).$$

2. For -200 °C $\leq T_l - T_s \leq -100$ °C,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 6}{13} \right)^{m-1}, \quad (4.7.64)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_l - T_s + 200}{100} \right)^{n-1}, \quad (4.7.65)$$

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where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4$$

$$a_1 \quad -2.0814013E-5, 5.4265381E-5, -5.9643693E-5, -9.5142645E-5$$

$$a_2 \quad -6.1546916E-8, -5.8200992E-4, 3.1421620E-5, 4.3931388E-4$$

$$a_3 \quad 1.520926E-12, 5.3851305E-4, -5.9593923E-5, -3.9306311E-4$$

$$a_4 \quad -1.000384E-13, -3.0239692E-5, -1.3773245E-5, 2.8841796E-5$$

$$a_5 \quad -1.100602E-12, -3.7252485E-4, 2.5795590E-5, 3.0157145E-4$$

$$a_6 \quad 5.866264E-13, 2.1057458E-4, -3.3335127E-5, -1.5004871E-4$$

3. For $-100^\circ\text{C} \leq T_t - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial p_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-6}{13} \right)^{m-1}, \quad (4.7.66)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_t - T_s + 100}{100} \right)^{n-1}, \quad (4.7.67)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

$$a_1 \quad -1.2136540E-4, -1.3470326E-4, 3.0284231E-5, 2.6668229E-5, \\ -2.4258264E-5, -6.8396365E-6, -1.3129824E-5, 1.9375745E-5, \\ -1.3552943E-6, -1.1285743E-6$$

$$a_2 \quad -1.1130819E-4, 9.8679522E-5, -6.7393559E-5, -1.1934915E-5, \\ 3.5060848E-6, 2.8505516E-5, -4.4699480E-6, 1.5086079E-6, \\ -2.1956744E-5, 1.0834731E-5$$

$$a_3 \quad 8.5820167E-5, -1.0369458E-4, 5.4607218E-5, 3.2380144E-5, \\ -2.7805873E-5, -1.0743678E-5, -1.3584354E-5, 2.1847328E-5, \\ 9.8729460E-7, -2.9944255E-6$$

$$a_4 \quad -1.5163365E-5, 3.5928166E-5, -2.5259235E-5, 4.5299346E-6, \\ -5.6699997E-6, 1.5656407E-5, -8.1121729E-6, 7.5038636E-6, \\ -1.4571238E-5, 6.9495211E-6$$

a_5 -4.5151369E-5, 3.7373906E-5, -2.4610937E-5, 2.9008083E-6,
-7.3144054E-6, 1.5308741E-5, -7.2780436E-6, 8.6377486E-6,
-1.4102536E-5, 6.1353366E-6

a_6 2.7179012E-5, -2.5717476E-5, 9.9862545E-6, 1.6393067E-5,
-1.5802708E-5, 4.0168695E-6, -1.0173832E-5, 1.4268162E-5,
-7.5351052E-6, 2.1386307E-6

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-6}{13} \right)^{m-1}, \quad (4.7.68)$$

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.69)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-2.2648497E-4	0.0
2	-7.4000222E-5	0.0
3	3.6782649E-5	0.0
4	1.7950322E-6	0.0
5	-2.8091569E-5	0.0
6	1.4738877E-5	0.0

F. For $19 \leq P < 47$ bar,

1. For -350 °C $\leq T_l - T_s < -250$ °C,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = a_1 + a_2 P, \quad (4.7.70)$$

where the coefficients a_1 and a_2 are given by

$$\begin{aligned} a_1 &= -2.0769739 \times 10^{-5} + 1.9362000 \times 10^{-10} (T_l - T_s), \\ a_2 &= -6.3339443 \times 10^{-8} - 8.3868600 \times 10^{-12} (T_l - T_s). \end{aligned} \quad (4.7.71)$$

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2. For $-250\text{ }^{\circ}\text{C} \leq T_l - T_s \leq -150\text{ }^{\circ}\text{C}$,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-19}{28} \right)^{m-1}, \quad (4.7.72)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_l - T_s + 250}{100} \right)^{n-1}, \quad (4.7.73)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,4$

a_1 -2.0821888E-5, 4.9315008E-5, -5.9411443E-5, -9.1731808E-5

a_2 -6.1171809E-8, -5.5525544E-4, 3.5651826E-5, 4.2948775E-4

a_3 1.521039E-12, 5.2855740E-4, -7.2436492E-5, -4.0708951E-4

a_4 -3.662484E-13, -1.1346815E-4, 6.8252041E-6, 1.1238303E-4

a_5 -7.943244E-13, -2.6810314E-4, 1.3229416E-5, 72.1759271E-4

a_6 4.880515E-13, 71.7525584E-4, -3.1488230E-5, -1.2541589E-4

3. For $-150\text{ }^{\circ}\text{C} \leq T_l - T_s < 0.0\text{ }^{\circ}\text{C}$,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-19}{28} \right)^{m-1}, \quad (4.7.74)$$

$$a_m = \sum_{n=1}^{n=6} b_{m,n} \left(\frac{T_l - T_s + 150}{150} \right)^{n-1}, \quad (4.7.75)$$

where the coefficients $b_{m,n}$ are given by

$b_{m,n}(i), i=1,2,3,4,\dots,6$

a_1 -1.2270569E-4, -1.9274488E-4, 6.2968395E-5, 2.0341825E-5,
-5.9234820E-5, 1.5941503E-5

a_2 -9.0151423E-5, 8.7946928E-5, -6.9099371E-5, -8.5658829E-6,
1.0482639E-5, -4.4427220E-6

a_3 4.9023261E-5, -6.6669812E-5, 1.9711573E-5, 3.6543117E-5,
-1.3983680E-5, -8.7480813E-6

a_4 5.7354936E-6, 9.2628234E-6, -3.6898010E-6, 8.1317118E-6,
-1.5385529E-5, 7.2587594E-6

a_5 -3.7280520E-5, 3.1284773E-5, -1.7659277E-5, 2.2707351E-6,
-1.0641020E-5, 8.2286862E-6

a_6 1.8350056E-5, -1.5553822E-5, -1.4134327E-6, 1.9453546E-5,
-1.0915551E-5, 4.8230102E-7

4. For $T_\ell - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial p_\ell}{\partial h_\ell} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-19}{28} \right)^{m-1}, \quad (4.7.76)$$

$$a_m = b_{m,1} + b_{m,2}(T_\ell - T_s), \quad (4.7.77)$$

where T_ℓ is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-2.7535843E-4	0.0
2	-7.3928263E-5	0.0
3	1.5927150E-5	0.0
4	1.1301056E-5	0.0
5	-2.3827220E-5	0.0
6	1.0419764E-5	0.0

G. For $47 \leq P < 98$ bar,

1. For -350 °C $\leq T_\ell - T_s < -300$ °C,

$$\left(\frac{\partial p_\ell}{\partial h_\ell} \right)_P = a_1 + a_2 P, \quad (4.7.78)$$

where the coefficients a_1 and a_2 are given by

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$$a_1 = -2.0694866 \times 10^{-5} + 4.1824000 \times 10^{-10} (T_\ell - T_s), \quad (4.7.79)$$

$$a_2 = -6.4790040 \times 10^{-8} - 1.2738640 \times 10^{-11} (T_\ell - T_s).$$

2. For $-300^\circ\text{C} \leq T_\ell - T_s \leq -200^\circ\text{C}$,

$$\left(\frac{\partial \rho_\ell}{\partial h_\ell} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-47}{51} \right)^{m-1}, \quad (4.7.80)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_\ell - T_s + 300}{100} \right)^{n-1}, \quad (4.7.81)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4$$

$$a_1 \quad -2.0820338\text{E-}5, 4.4018657\text{E-}5, -5.8784688\text{E-}5, -8.7488991\text{E-}5$$

$$a_2 \quad -6.0969989\text{E-}8, -5.3215345\text{E-}4, 3.3381635\text{E-}5, 4.1170869\text{E-}4$$

$$a_3 \quad 1.766965\text{E-}12, 6.0858857\text{E-}4, -8.7426599\text{E-}5, -4.7986240\text{E-}4$$

$$a_4 \quad -1.446357\text{E-}12, -4.7296685\text{E-}4, 6.2383045\text{E-}5, 4.2079607\text{E-}4$$

$$a_5 \quad 5.201851\text{E-}13, 1.7137196\text{E-}4, -5.1868484\text{E-}5, -1.5500713\text{E-}4$$

$$a_6 \quad -3.592908\text{E-}14, -2.4094446\text{E-}7, -5.9456706\text{E-}6, 2.2664298\text{E-}5$$

3. For $-200^\circ\text{C} \leq T_\ell - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial \rho_\ell}{\partial h_\ell} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-47}{51} \right)^{m-1}, \quad (4.7.82)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_\ell - T_s + 200}{200} \right)^{n-1}, \quad (4.7.83)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

- a_1 -1.2310599E-4, -2.2643265E-4, 3.3306961E-5, 4.5526299E-5,
-3.1816321E-5, -3.3069340E-5
- a_2 -8.7184572E-5, 9.3440487E-5, -1.7543958E-5, -6.5348657E-5,
-6.5068478E-5, 1.1186978E-5, 3.4628951E-5, 6.0254390E-5,
1.2843919E-6, -5.8965902E-5
- a_3 4.1343437E-5, -6.6099575E-5, -1.4272977E-5, 5.9627872E-5,
2.7899670E-5, 9.9075880E-6, -3.9794713E-5, -2.5711504E-5,
-2.6112136E-5, 3.3710245E-5
- a_4 1.0190237E-5, 4.1700464E-6, 5.7646150E-6, 2.7347474E-6,
-2.2692352E-5, 7.3268822E-6, -5.3295331E-7, 2.3493315E-5,
-6.1625670E-6, -1.1641442E-5
- a_5 -3.5542399E-5, 3.2041266E-5, 5.2316414E-6, -1.7348375E-5,
-3.7290767E-5, 7.6747030E-6, 1.1031915E-5, 3.6787431E-5,
-1.1119547E-6, -2.2635695E-5
- a_6 1.6491809E-5, -1.5143931E-5, -1.2233103E-5, 2.5722867E-5,
2.5771868E-6, 1.0592113E-5, -1.9145643E-5, -1.4963697E-6,
-1.5703011E-5, 1.6706167E-5

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-47}{51} \right)^{m-1}, \quad (4.7.84)$$

$$a_m = b_{m,1} + b_{m,2}(T_l - T_s), \quad (4.7.85)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-3.3554985E-4	0.0
2	-9.3296964E-5	0.0
3	4.1222351E-7	0.0
4	1.2609382E-5	0.0
5	-2.1169510E-5	0.0
6	8.3402398E-6	0.0

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H. For $98 \leq P < 186$ bar,

1. For $-350^\circ\text{C} \leq T_t - T_s < -300^\circ\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.86)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_t - T_s + 350}{50} \right)^{n-1}, \quad (4.7.87)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4$$

$$a_1 \quad -2.6881390\text{E-}5, -3.9331406\text{E-}6, 5.5448177\text{E-}5, -9.2036714\text{E-}5, \\ -7.0517453\text{E-}5, 5.4144752\text{E-}5, 8.7181681\text{E-}5, 8.5278739\text{E-}5, \\ -1.3034215\text{E-}5, -1.0171606\text{E-}4$$

$$a_2 \quad -4.6901531\text{E-}6, 3.7274623\text{E-}4, -2.6303045\text{E-}3, 3.1881413\text{E-}3, \\ 2.3851373\text{E-}3, -7.3141820\text{E-}4, -2.6130043\text{E-}3, -2.3186773\text{E-}3, \\ -3.9049181\text{E-}4, 2.5159022\text{E-}3$$

$$a_3 \quad -4.0741883\text{E-}6, -1.6378214\text{E-}3, 9.7104347\text{E-}3, -8.4440612\text{E-}3, \\ -8.7875548\text{E-}3, -1.0131561\text{E-}3, 4.8078920\text{E-}3, 5.8266481\text{E-}3, \\ 2.6858832\text{E-}3, -2.8945758\text{E-}3$$

$$a_4 \quad 1.0921718\text{E-}5, 3.8161893\text{E-}4, 4.2086043\text{E-}3, -1.7014585\text{E-}2, \\ -3.9786837\text{E-}3, 1.5041650\text{E-}2, 2.1566939\text{E-}2, 1.3407962\text{E-}2, \\ -5.1034101\text{E-}3, -2.8702796\text{E-}2$$

$$a_5 \quad -1.2080738\text{E-}5, 4.7824298\text{E-}3, -3.9363250\text{E-}2, 5.7980392\text{E-}2, \\ 3.4044006\text{E-}2, -2.6249043\text{E-}2, -5.6522208\text{E-}2, -4.3278561\text{E-}2, \\ 2.5992555\text{E-}3, 6.6066925\text{E-}2$$

$$a_6 \quad 4.7415882\text{E-}6, -4.0749387\text{E-}3, 2.8286816\text{E-}2, -3.5759943\text{E-}2, \\ -2.3856715\text{E-}2, 1.2984843\text{E-}2, 3.2791280\text{E-}2, 2.6500021\text{E-}2, \\ 1.7306625\text{E-}4, -3.7042072\text{E-}2$$

2. For $-300^\circ\text{C} \leq T_t - T_s < -200^\circ\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.88)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_t - T_s + 300}{100} \right)^{n-1}, \quad (4.7.89)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4$$

$$a_1 \quad -2.6072724E-5, -1.9177510E-4, -4.2752286E-5, 8.2773397E-5$$

$$a_2 \quad -2.2665922E-4, 2.8739972E-4, 6.5061254E-6, -1.3480560E-4$$

$$a_3 \quad 2.4961449E-4, -4.3861542E-4, -3.3165569E-5, 2.4971890E-4$$

$$a_4 \quad -1.8178089E-4, 4.3718154E-4, -3.3057522E-6, -2.4237613E-4$$

$$a_5 \quad 4.7863470E-5, -2.0179512E-4, -1.1304311E-5, 1.3828869E-4$$

$$a_6 \quad 7.0988459E-6, 3.5644957E-5, -1.5611032E-5, -1.4586515E-5$$

3. For $-200^\circ\text{C} \leq T_t - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial p_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 98}{102} \right)^{m-1}, \quad (4.7.90)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_t - T_s + 200}{200} \right)^{n-1}, \quad (4.7.91)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

$$a_1 \quad -1.7789485E-4, -1.8008041E-4, 1.7222663E-5, -4.2171020E-6, \\ -5.1610684E-5, -1.4781776E-5, -2.4782534E-6, 3.4951479E-5, \\ -2.8164466E-6, -4.6698573E-5$$

$$a_2 \quad -6.7620743E-5, 4.7756005E-5, -2.9951714E-5, -7.8645074E-5, \\ -7.7900419E-5, 2.4255116E-5, 8.4917579E-5, 1.2172551E-4, \\ 3.9171706E-6, -2.2549405E-4$$

$$a_3 \quad 2.7444368E-5, -5.3218221E-5, 7.0245555E-5, -1.2127150E-5, \\ -5.9165819E-5, 6.6664284E-6, 4.1374741E-5, 7.3450408E-5, \\ -5.7721577E-6, -1.2709738E-4$$

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$$a_4 \quad 9.7173014\text{E-}6, 9.9583559\text{E-}6, -1.0473752\text{E-}5, 7.7949555\text{E-}6, \\ -9.8418005\text{E-}6, 1.3932556\text{E-}5, -4.3309142\text{E-}6, 1.3579213\text{E-}5, \\ -1.2546103\text{E-}5, -2.0155040\text{E-}6$$

$$a_5 \quad -2.6944536\text{E-}5, 2.2340633\text{E-}5, -2.5919419\text{E-}5, 5.6579198\text{E-}6, \\ -4.1091831\text{E-}6, 1.6840102\text{E-}5, -7.4367369\text{E-}6, 7.9894093\text{E-}6, \\ -1.4033242\text{E-}5, 7.4057482\text{E-}6$$

$$a_6 \quad 1.2543857\text{E-}5, -1.1666981\text{E-}5, 4.9578397\text{E-}6, 1.6927174\text{E-}5, \\ -1.3589518\text{E-}5, 3.5438309\text{E-}6, -1.4103434\text{E-}5, 9.3535481\text{E-}6, \\ -8.7047141\text{E-}6, 1.0721613\text{E-}5$$

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.92)$$

$$a_m = b_{m,1} + b_{m,2} (T_l - T_s), \quad (4.7.93)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-4.2868084E-4	0.0
2	-1.9848636E-4	0.0
3	-3.1087049E-5	0.0
4	-3.4578135E-5	0.0
5	-6.5031000E-5	0.0
6	5.3282032E-5	0.0

I. For $186 \leq P \leq 200$ bar,

1. For -350 °C $\leq T_l - T_s < -300$ °C,

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.94)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_l - T_s + 350}{50} \right)^{n-1}, \quad (4.7.95)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

- a_1 -3.1988492E-5, -1.3344417E-4, 4.8697278E-5, 2.0024476E-5,
 -2.6977532E-5, -2.0769088E-6, -1.0619419E-5, 1.5047565E-5,
 -6.8917339E-6, 5.6672690E-6
 a_2 -1.2293524E-5, -2.2589170E-5, 8.7114074E-5, -4.1768954E-5,
 -6.9662858E-5, 8.1181549E-6, 2.9547344E-5, 5.0711910E-5,
 -2.0009908E-6, -3.4691811E-5
 a_3 -1.7996682E-5, 1.2629316E-4, -3.0149935E-4, 1.4703272E-4,
 1.6242168E-4, 2.4639301E-5, -1.1464221E-4, -1.0502716E-4,
 -4.1947933E-5, 1.1707983E-4
 a_4 2.4596929E-5, -1.0615449E-4, 2.4150015E-4, -1.0111628E-4,
 -1.5423792E-4, -3.0032661E-6, 7.6545364E-5, 1.0441920E-4,
 1.3432465E-5, -8.4771964E-5
 a_5 -1.0657652E-5, -1.7765667E-5, 4.1337919E-5, -1.1608800E-5,
 -3.8484537E-5, 7.7475506E-6, 7.3618705E-6, 2.8246956E-5,
 -6.9161137E-6, -1.1639593E-5
 a_6 2.3242888E-7, 3.9359184E-5, -9.4906459E-5, 5.1461970E-5,
 4.1371479E-5, 1.4461154E-5, -4.1130241E-5, -2.4725084E-5,
 -2.0842798E-5, 3.9515065E-5

2. For $-300^\circ\text{C} \leq T_t - T_s < -200^\circ\text{C}$,

$$\left(\frac{\partial p_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.96)$$

$$a_m = \sum_{n=1}^{n=4} b_{m,n} \left(\frac{T_t - T_s + 300}{100} \right)^{n-1}, \quad (4.7.97)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4$$

- a_1 -2.6072724E-5, -1.9177510E-4, -4.2752286E-5, 8.2773397E-5
 a_2 -2.2665922E-4, 2.8739972E-4, 6.5061254E-6, -1.3480560E-4
 a_3 2.4961449E-4, -4.3861542E-4, -3.3165569E-5, 2.4971890E-4
 a_4 -1.8178089E-4, 4.3718154E-4, -3.3057522E-6, -2.4237613E-4

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$$a_5 \quad 4.7863470E-5, -2.0179512E-4, -1.1304311E-5, 1.3828869E-4$$

$$a_6 \quad 7.0988459E-6, 3.5644957E-5, -1.5611032E-5, -1.4586515E-5$$

3. For $-200^\circ\text{C} \leq T_t - T_s < 0.0^\circ\text{C}$,

$$\left(\frac{\partial \rho_t}{\partial h_t} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P - 98}{102} \right)^{m-1}, \quad (4.7.98)$$

$$a_m = \sum_{n=1}^{n=10} b_{m,n} \left(\frac{T_t - T_s + 200}{200} \right)^{n-1}, \quad (4.7.99)$$

where the coefficients $b_{m,n}$ are given by

$$b_{m,n}(i), i=1,2,3,4,\dots,10$$

$$a_1 \quad -1.7789485E-4, -1.8008041E-4, 1.7222663E-5, -4.2171020E-6, \\ -5.1610684E-5, -1.4781776E-5, -2.4782534E-6, 3.4951479E-5, \\ -2.8164466E-6, -4.6698573E-5$$

$$a_2 \quad -6.7620743E-5, 4.7756005E-5, -2.9951714E-5, -7.8645074E-5, \\ -7.7900419E-5, 2.4255116E-5, 8.4917579E-5, 1.2172551E-4, \\ 3.9171706E-6, -2.2549405E-4$$

$$a_3 \quad 2.7444368E-5, -5.3218221E-5, 7.0245555E-5, -1.2127150E-5, \\ -5.9165819E-5, 6.6664284E-6, 4.1374741E-5, 7.3450408E-5, \\ -5.7721577E-6, -1.2709738E-4$$

$$a_4 \quad 9.7173014E-6, 9.9583559E-6, -1.0473752E-5, 7.7949555E-6, \\ -9.8418005E-6, 1.3932556E-5, -4.3309142E-6, 1.3579213E-5, \\ -1.2546103E-5, -2.0155040E-6$$

$$a_5 \quad -2.6944536E-5, 2.2340633E-5, -2.5919419E-5, 5.6579198E-6, \\ -4.1091831E-6, 1.6840102E-5, -7.4367369E-6, 7.9894093E-6, \\ -1.4033242E-5, 7.4057482E-6$$

$$a_6 \quad 1.2543857E-5, -1.1666981E-5, 4.9578397E-6, 1.6927174E-5, \\ -1.3589518E-5, 3.5438309E-6, -1.4103434E-5, 9.3535481E-6, \\ -8.7047141E-6, 1.0721613E-5$$

4. For $T_l - T_s \geq 0.0$ °C (superheated),

$$\left(\frac{\partial \rho_l}{\partial h_l} \right)_P = \sum_{m=1}^{m=6} a_m \left(\frac{P-98}{102} \right)^{m-1}, \quad (4.7.100)$$

$$a_m = b_{m,1} + b_{m,2} (T_l - T_s), \quad (4.7.101)$$

where T_l is the liquid temperature in °C, T_s is the saturation temperature in °C, and the coefficients b_m 's are given by

m	$b_{m,1}$	$b_{m,2}$
1	-4.2868084E-4	0.0
2	-1.9848636E-4	0.0
3	-3.1087049E-5	0.0
4	-3.4578135E-5	0.0
5	-6.5031000E-5	0.0
6	5.3282032E-5	0.0

These correlations for $(\partial \rho_l / \partial h_l)_P$ are accurate to better than 2%.

4.7.3 Liquid Enthalpy

The RAMONA-4B code computes the liquid temperature in each thermal-hydraulic cell instead of the liquid enthalpy. Since some thermodynamic property functions requires the liquid enthalpy as input, there is a need for an accurate liquid enthalpy function as a function of pressure and temperature. With such a function, the liquid enthalpy can be obtained from the liquid temperature which is calculated in RAMONA-4B.

We have developed a liquid enthalpy function called HL(P,T_l) for use in RAMONA-4B. The function calculates the liquid enthalpy from the liquid temperature by iteration as follows:

$$h_l^{(n)} = h_l^{(n-1)} + C_{pl}(h_l^{(n-1)}, P) [T_l - T_l(h_l^{(n-1)}, P)], \quad (4.7.102)$$

where the superscript (n) stands for iteration counter, T_l is the input liquid temperature, $C_{pl}(h_l, P)$ and $T_l(h_l, P)$ are 2D property functions available in RAMONA-4B. The initial guess is taken to be the saturated liquid enthalpy h_f . The iteration is terminated if the absolute relative error on h_l is less than 0.000003. The accuracy of this function has been assessed against the NBS library and found to be excellent over a wide range of pressure and temperature.

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4.8 Miscellaneous Improvements

A number of improvements have been made in RAMONA-4B over the previous version. They are described as follows.

4.8.1 Steady-State Momentum Balance for Natural Circulation

The steady-state flow calculation in RAMONA-4B requires a double iteration loop to converge on an equal pressure drop across the parallel core channels and to satisfy the loop momentum balance. The inner loop iterates on the core pressure drop and the outer loop iterates on the momentum balance. In the earlier version RAMONA-3B, the outer iteration loop is replaced by an adjustment of the jet pump head to satisfy the loop momentum balance. This simple adjustment is not applicable to a SBWR with natural circulation.

For general applicability of the code to both natural and forced circulation, an outer iteration loop has been added to the steady-state flow calculation so that the loop momentum balance is achieved by adjusting the loss coefficients at the core inlet. This approach has worked quite well for both the SBWR and BWR.

Note that, for any closed flow loop, the following momentum balance must be satisfied at steady state:

$$\Delta P_{ex} + \Delta P_{j,core} = 0, \quad (4.8.1)$$

where ΔP_{ex} is the pressure drop in the external part of the flow loop outside the core, and $\Delta P_{j,core}$ is the core pressure drops across the parallel channel j. Note that, for any parallel channel j, ΔP_{ex} is the same. An outer iteration loop is employed to satisfy Eq.(4.8.1). The iteration proceeds as follows:

$$K_{j,ci}^{(n)} = K_{j,ci}^{(n-1)} \left| \frac{L_{co,j}^*}{L_{co,j}} \right|, \quad (4.8.2)$$

$$L_{co,j}^* = P_{head} + D_{ex} - F_{ex} - L_{ex} + D_{co,j} - F_{co,j},$$

where K is the loss coefficient, the superscript (n) represents the iteration counter, the subscript j is the core parallel channel index, the subscript ci stands for core inlet, the subscript co refers to core, the subscript ex represents the ex-core region of the flow loop, and

ΔP_{head}	= pump head,
D_{ex}	= driving potential of the ex-core region,
F_{ex}	= friction loss of the ex-core region,
L_{ex}	= form loss of the ex-core region,
$D_{co,j}$	= driving potential of the flow loop j in the core,
$F_{co,j}$	= friction loss of the flow loop j in the core,
$L_{co,j}$	= form loss of the flow loop j in the core.

The initial guesses of the loss coefficients are the input values which must be nonzero. The above algorithm converges nicely until the condition (4.8.1) is met within a specified criterion (say, 0.001).

4.8.2 Liquid Compressibility

The liquid compressibility that appears in the system pressure equation and the flux divergence equation is very important for the condition of low void fractions, especially for pure liquid. The original RAMONA did not account for it. Such a function has been developed for the current version RAMONA-4B.

The liquid compressibility in RAMONA-4B is defined as

$$\theta_l = -\frac{1}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial P} \right)_{u_l}, \quad (4.8.3)$$

where

$$\begin{aligned} \theta_l &= \text{liquid compressibility at constant internal energy } u_l, \\ \rho_l &= \text{liquid density,} \\ P &= \text{pressure.} \end{aligned}$$

We wish to derive an expression for the liquid compressibility in terms of the partial derivatives of liquid density with respect to pressure and enthalpy, respectively, for which accurate polynomial fits have been developed as described in the previous sections.

Note that the liquid density is a function of pressure and enthalpy h_l :

$$\rho_l = \rho_l(P, h_l). \quad (4.8.4)$$

Therefore,

$$\left(\frac{\partial \rho_l}{\partial P} \right)_{u_l} = \left(\frac{\partial \rho_l}{\partial P} \right)_{h_l} + \left(\frac{\partial \rho_l}{\partial h_l} \right)_P \left(\frac{\partial h_l}{\partial P} \right)_{u_l}. \quad (4.8.5)$$

Since the liquid specific enthalpy h_l is given by

$$h_l = u_l + \frac{P}{\rho_l}, \quad (4.8.6)$$

we have

$$\left(\frac{\partial h_l}{\partial P} \right)_{u_l} = \frac{1}{\rho_l} - \frac{P}{\rho_l^2} \left(\frac{\partial \rho_l}{\partial P} \right)_{u_l}. \quad (4.8.7)$$

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Substituting Eq.(4.8.7) into Eq.(4.8.5) and solving for $(\partial\rho/\partial P)_w$, we obtain

$$\left(\frac{\partial\rho_l}{\partial P}\right)_{u_l} = \frac{\left(\frac{\partial\rho_l}{\partial P}\right)_{h_l} + \frac{1}{\rho_l}\left(\frac{\partial\rho_l}{\partial h_l}\right)_P}{1 + \frac{P}{\rho_l^2}\left(\frac{\partial\rho_l}{\partial h_l}\right)_P}. \quad (4.8.8)$$

Therefore,

$$\theta_l = -\frac{\left(\frac{\partial\rho_l}{\partial P}\right)_{h_l} + \frac{1}{\rho_l}\left(\frac{\partial\rho_l}{\partial h_l}\right)_P}{\rho_l^2 + P\left(\frac{\partial\rho_l}{\partial h_l}\right)_P}. \quad (4.8.9)$$

Note that the partial derivatives in Eq.(4.8.9) are the new property functions given in Sections 4.7.2.4 and 4.7.2.5. Also, in order to use Eq.(4.8.9), one needs a 2D function for the liquid density and liquid enthalpy as a function of both pressure and temperature. These two functions do exist in RAMONA-4B as $ROL(h_l, P)$ for the liquid density and $HL(P, T_l)$ for the liquid enthalpy whose accuracies have been assessed against the NBS library to be better than 0.5% over a wide range of pressures and temperatures.

4.8.3 Two-Phase Multiplier for Form Losses

The two-phase form-loss multiplier accounts for the increase of local form losses due to the presence of voids, thus has an impact on the thermal-hydraulic instability in a BWR or SBWR. The original correlation used a homogeneous equilibrium model (HEM), which has been replaced by a revised correlation to account for the nonhomogeneous and nonequilibrium two-phase flow (Gitznick, 1985). The revised correlation is given by

$$\Phi_l^2 = 1 + \beta x \left(\frac{\rho_l}{\rho_g} - 1 \right), \quad (4.8.10)$$

where x is the flow quality (ratio of vapor mass flow over mixture mass flow) and β is a constant treated as an input parameter ($1 \leq \beta \leq 2$).

4.8.4 Liquid Density and Liquid Enthalpy

RAMONA-4B computes the liquid density approximately as follows:

$$\rho_l \approx \rho_f + \frac{d\rho_f}{dT}(T_l - T_s), \quad (4.8.11)$$

where

T_l = liquid temperature,
 T_s = saturation temperature, and

$$\frac{d\rho_f}{dT} = C_1 + C_2 P + C_3 P^2, \quad (4.8.12)$$

with

$$\begin{aligned} C_1 &= -0.8071\text{E}+00, \\ C_2 &= -0.1774\text{E}-06, \\ C_3 &= 0.5289\text{E}-14. \end{aligned}$$

Note that Eq.(4.8.11) is good only near the saturation condition and that Eq.(4.8.12) is not very accurate. The previous version RAMONA-3B also computes the feedwater density by Eqs.(4.8.11) and (4.8.12). This is not good because feedwater temperature is usually far below the saturation temperature.

It should be pointed out that a very accurate 2D function for the liquid density does exist in RAMONA-4B. It is called $ROL(h,P)$ whose accuracy has been assessed against the NBS library to be very good. This function is therefore used in RAMONA-4B for the feedwater density.

RAMONA-3B also computes the liquid enthalpy approximately as follows:

$$h_l \approx h_f + c_v(T_l - T_s), \quad (4.8.13)$$

where c_v is the specific heat of liquid.

Note also that Eq.(4.8.13) is good only near the saturation condition and we should compute the liquid enthalpy as a function of pressure and temperature.

The liquid enthalpy can be determined from the liquid temperature and pressure, both of which are computed by RAMONA-4B. A 2D function, called $HL(P,T_l)$, has been developed for the liquid enthalpy as discussed in Section 4.7.3. The accuracy of this function is excellent with respect to the NBS library.

Furthermore, the HL function requires the liquid-specific-heat function $CPL(H,P)$ at constant pressure which is also available in RAMONA-4B. The accuracy of the $CPL(H,P)$ function has been shown to be very good against the NBS library. These 2D functions, $ROL(h,P)$ and $HL(P,T_l)$, are therefore used in RAMONA-4B instead of Eqs.(4.8.11) and (4.8.13).

4.8.5 Acceleration of Vapor Generation and Void Fraction Iteration

The steady-state thermal-hydraulic solutions are obtained from the conservation equations by subroutines SEARCH, FUNCSS, and the function VOIDSS using iterative procedures. These iterative procedures are inefficient. We have improved them by means of the well-known Newton-Raphson method. In what follows, we present the mathematical basis of the new iterative scheme for computing the steady-state thermal-hydraulic solutions of RAMONA-4B.

4. Modeling of Thermal Hydraulics

4.8.5.1 Mathematical Basis

The thermal-hydraulic steady state is the solution of the vapor mass balance equation,

$$\frac{dm_{g,i}}{dt} = w_{g,i-1} - w_{g,i} + \psi_i, \quad (4.8.14)$$

the mixture mass balance equation,

$$\frac{dm_i}{dt} = w_{i-1} - w_i, \quad (4.8.15)$$

the mixture energy balance equation,

$$\frac{dE_i}{dt} = (w_{f,i-1}e_{f,i-1} - w_{f,i}e_{f,i}) + (w_{g,i-1} - w_{g,i})e_g + P(W_{i-1} - W_i) + Q_i, \quad (4.8.16)$$

and the loop momentum equations [Wulff et al., 1984a],

$$\frac{dI_j}{dt} = D_j - F_j - L_j, \quad (4.8.17)$$

such that

$$\frac{dm_{g,i}}{dt} = 0, \quad (4.8.18)$$

$$\frac{dm_i}{dt} = 0, \quad (4.8.19)$$

$$\frac{dE_i}{dt} = 0, \quad (4.8.20)$$

$$\frac{dI_j}{dt} = 0, \quad (4.8.21)$$

where

P	= system pressure,
$m_{g,i}$	= vapor mass in cell i ,
m_i	= mixture mass in cell i ,
E_i	= mixture internal energy in cell i ,
Ψ_i	= vapor generation rate in cell i ,
$w_{g,i}$	= vapor mass flow rate at exit of cell i ,
$w_{f,i}$	= liquid mass flow rate at exit of cell i ,
w_i	= mixture mass flow rate at exit of cell i ,
W_i	= mixture volumetric flow rate at exit of cell i ,
e_g	= vapor specific internal energy,
$e_{f,i}$	= liquid specific internal energy in cell i ,
Q_i	= heating rate in cell i ,
I_j	= integrated loop momentum of flow loop j ,
D_j	= driving potential of flow loop j ,
F_j	= frictional loss of flow loop j ,
L_j	= form loss of flow loop j .

Equations (4.8.18) through (4.8.21) give rise to

$$w_{g,i} = w_{g,i-1} + \Psi_i, \quad (4.8.22)$$

$$w_i = w_{i-1}, \quad (4.8.23)$$

$$e_{f,i} = \frac{w_{f,i-1}e_{f,i-1} + (w_{g,i-1} - w_{g,i})e_g + P(W_{i-1} - W_i) + Q_i}{w_{f,i}}, \quad (4.8.24)$$

$$D_j - F_j - L_j = 0. \quad (4.8.25)$$

Note that

$$e_{f,i} = C_{v,i}(T_{f,i} - T_s^0), \quad (4.8.26)$$

therefore,

$$T_{f,i} = T_s^0 + \frac{e_{f,i}}{C_{v,i}}, \quad (4.8.27)$$

where T_s^0 is the reference saturation temperature at initial pressure.

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The mixture volumetric flow rate is given by

$$W_i = \frac{w_{g,i}}{\rho_g} + \frac{w_{f,i}}{\rho_{f,i}}, \quad (4.8.28)$$

where ρ_g is the vapor density, and $\rho_{f,i}$ is liquid density computed approximately as follows:

$$\rho_{f,i} = \rho_f + \frac{d\rho_f}{dT}(T_{f,i} - T_s) \quad (4.8.29)$$

with ρ_f being the saturated-liquid density.

Equation (4.8.23) indicates that the mixture mass flow rate is constant at steady state, thus

$$w_{f,i} = w_i - w_{g,i}, \quad (4.8.30)$$

where w_i is the mixture mass flow rate which is simply the total core inlet mass flow rate known at steady state.

The vapor generation rate is the sum of the bulk boiling term $\psi_{B,i}$ and the surface boiling term $\psi_{S,i}$:

$$\psi_i = \psi_{B,i} + \psi_{S,i}, \quad (4.8.31)$$

where

$$\psi_{B,i} = \frac{\Delta V_i}{h_{fg}} [C_{11} + C_{12}\alpha_i(1 - \alpha_i)][T_{f,i} - T_s + C_{13}|T_{f,i} - T_s|], \quad (4.8.32)$$

$$\psi_{S,i} = \frac{Q_{w,i}}{h_{fg} + C_{p,i}[(T_s - T_{f,i})\frac{\rho_{f,i}}{\rho_g} + \frac{1}{2}(T_{w,i} - T_s)(\frac{\rho_{f,i}}{\rho_g} - 1)]}, \quad (4.8.33)$$

with h_{fg} being the latent heat of evaporation, T_s the saturation temperature, $T_{w,i}$ the clad wall temperature, and $Q_{w,i}$ being the wall heating rate.

The cell void fraction α_i is assumed to be that at its exit, which can be determined from the vapor mass flow rate $w_{g,i}$ and the mixture volumetric flow rate W_i via the drift-flux correlation as follows:

$$\alpha_i = \frac{w_{g,i}}{\rho_g(C_0 W_i + V_{gj} A_i)}, \quad (4.8.34)$$

$$C_0 = \min(C_{0,1}, C_{0,2}), \quad (4.8.35)$$

$$C_{0,1} = \left(1.2 - 0.2 \sqrt{\frac{\rho_g}{\rho_{f,i}}} \right) (1 - e^{-18\alpha_i}), \quad (4.8.36)$$

$$C_{0,2} = 1 + \frac{1 - \alpha_i}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}}, \quad (4.8.37)$$

$$V_{gj} = 1.54 \left(\frac{(\rho_{f,i} - \rho_g) g \sigma}{\rho_{f,i}^2} \right)^{0.25}, \quad (4.8.38)$$

for the churn-turbulent flow regime, and

$$V_{gj} = \frac{(1 - \alpha_i) \sqrt{\frac{g D_h (\rho_{f,i} - \rho_g) (1 - \alpha_i)}{0.015 \rho_{f,i}}}}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}}, \quad (4.8.39)$$

for the annular-flow regime, where A_i is the flow area of cell i , D_h is the hydraulic diameter, σ is the surface tension, and g is the gravitational acceleration.

4.8.5.2 Iterative Solutions for Steady State

4.8.5.2.1 Void Iteration

Given the flow rates, We can solve Eqs.(4.8.34) through (4.8.39) for the void fraction by means of Newton-Raphson iteration.

First, let

$$F_i = \alpha_i - \frac{w_{g,i}}{\rho_g (C_0 W_i + V_{gj} A_i)}. \quad (4.8.40)$$

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Then the solution of $F_i=0$ for α_i is given by

$$\alpha_i^{(n)} = \alpha_i^{(n-1)} - \frac{F_i^{(n)}}{F_i'^{(n)}}, \quad (4.8.41)$$

where n is the iteration counter and F_i' is the derivative of F_i with respect to α_i .

Upon differentiation of Eq.(4.8.40), we obtain

$$F_i'^{(n)} = 1 + \left(\frac{w_{g,i}}{\rho_g} \right) \frac{W_i \frac{dC_0}{d\alpha_i} + A_i \frac{dV_{gj}}{d\alpha_i}}{(C_0 W_i + V_{gj} A_i)^2}, \quad (4.8.42)$$

where

$$\frac{dC_0}{d\alpha_i} = 18 \left(1.2 - 0.2 \sqrt{\frac{\rho_g}{\rho_{f,i}}} \right) e^{-18\alpha_i}, \quad (4.8.43)$$

$$\frac{dV_{gj}}{d\alpha_i} = 0, \quad (4.8.44)$$

for churn-turbulent flow ($\alpha_i \leq 0.75$), and

$$\frac{dC_0}{d\alpha_i} = - \frac{1}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}} \left(1 + \frac{1 - \alpha_i}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}} \right), \quad (4.8.45)$$

$$\frac{dV_{gj}}{d\alpha_i} = - \frac{\beta_i}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}} \left(1 + \frac{(1 - \alpha_i) g D_h (\rho_{f,i} - \rho_g)}{2 \beta_i^2} + \frac{1 - \alpha_i}{\alpha_i + 4 \sqrt{\frac{\rho_g}{\rho_{f,i}}}} \right), \quad (4.8.46)$$

$$\beta_i = \sqrt{\frac{g D_h (\rho_{f,i} - \rho_g) (1 - \alpha_i)}{0.015 \rho_{f,i}}}, \quad (4.8.47)$$

for annular flow ($\alpha_i > 0.75$).

Test results indicate that the Newton-Raphson iteration above has gained a speedup factor of 3 to 5 relative to the old simple method of successive substitution. Typically, it takes 2 to 4 iterations to converge on the void fraction to within 0.00001.

4.8.5.2.2 Vapor Generation Rate Iteration

The vapor generation occurs predominantly in the reactor core where fission power is generated. The vapor generation rate can be computed from Eqs.(4.8.31) through (4.8.33) by Newton-Raphson iteration, given the void fraction and the coolant temperature. To this end, let

$$Y_i = \Psi_i - (\psi_{B,i} + \psi_{S,i}). \quad (4.8.48)$$

The Newton-Raphson iteration gets the solution of $Y_i=0$ as follows:

$$\psi_i^{(n)} = \psi_i^{(n-1)} - \frac{Y_i^{(n)}}{Y_i'^{(n)}}, \quad (4.8.49)$$

where n is the iteration counter and Y_i' is the derivative of Y_i with respect to Ψ_i :

$$Y_i'^{(n)} = \left(\frac{dY_i}{d\Psi_i} \right)^{(n)}. \quad (4.8.50)$$

Evaluating the derivative of Y_i with respect to Ψ_i yields

$$Y_i'^{(n)} = 1 + \left(\frac{Q_{w,i}}{Z_i^2} \right) \left[-C_{v,i} \left(\frac{\rho_{f,i}}{\rho_g} \right) \frac{dT_{f,i}}{d\Psi_i} + \frac{C_{v,i}}{2} \left(\frac{\rho_{f,i}}{\rho_g} - 1 \right) \frac{dT_{w,i}}{d\Psi_i} \right] - F(\alpha_i) \frac{dT_{f,i}}{d\Psi_i}, \quad (4.8.51)$$

where

$$Z_i = h_{fg} + C_{v,i} (T_s - T_{f,i}) \left(\frac{\rho_{f,i}}{\rho_g} \right) + \frac{C_{v,i}}{2} (T_{w,i} - T_s) \left(\frac{\rho_{f,i}}{\rho_g} - 1 \right), \quad (4.8.52)$$

$$F(\alpha_i) = \left(\frac{\Delta V_i}{h_{fg}} \right) [C_{11} + C_{12} \alpha_i (1 - \alpha_i)] (1 + C_{13}), \quad (4.8.53)$$

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for $T_i \geq T_s$, and

$$F(\alpha_i) = \left(\frac{\Delta V_i}{h_{fg}} \right) [C_{11} + C_{12} \alpha_i (1 - \alpha_i)] (1 - C_{13}), \quad (4.8.54)$$

for $T_i < T_s$.

The coolant temperature T_{fi} is related to the vapor generation rate at steady state by the following expression:

$$T_{f,i} = \frac{K_1 - K_2 \psi_i}{K_3 - C_{v,i} \psi_i}, \quad (4.8.55)$$

where

$$K_1 = w_{f,i-1} e_{f,i-1} + \delta_2(i) w_{fw} e_{f,fw} + [w_{f,i-1} + \delta_2(i) w_{fw}] C_{v,i} T_s^0 - w_{g,i-1} e_{g,i} - P \left[\frac{w_{f,i-1}}{\rho_{f,i}} + \frac{w_{g,i-1}}{\rho_g} + \delta_2(i) w_{fw} \right] + Q_i + g_j, \quad (4.8.56)$$

$$K_2 = C_{v,i} T_s^0 + h_{fg}, \quad (4.8.57)$$

$$K_3 = C_{v,i} (w_{f,i-1} + \delta_2(i) w_{fw}) + f_j, \quad (4.8.58)$$

and the two new quantities, f_j and g_j , are defined as

1) Outside the core:

$$\begin{aligned} f_j &= 0, \\ g_j &= 0. \end{aligned} \quad (4.8.59)$$

2) Coolant Channel j:

$$\begin{aligned} f_j &= P_{M,j} \Delta Z_j A_j, \\ g_j &= f_j T_{i,NPC}. \end{aligned} \quad (4.8.60)$$

3) Bypass Channel (j=NPC):

$$\begin{aligned} f_{NPC} &= \sum_{j=1}^{NCC} f_j, \\ g_{NPC} &= \sum_{j=1}^{NCC} f_j T_{i,j}. \end{aligned} \quad (4.8.61)$$

Thus,

$$\frac{dT_{f,i}}{d\psi_i} = \frac{1}{(K_3 - C_{v,i} \psi_i)} \left(-K_2 + \frac{C_{v,i} (K_1 - K_2 \psi_i)}{(K_3 - C_{v,i} \psi_i)} \right). \quad (4.8.62)$$

The clad wall temperature $T_{w,i}$ is determined as follows:

$$T_{w,i} = \min(T_{w,i}^1, T_{w,i}^2), \quad (4.8.63)$$

$$T_{w,i}^1 = T_{f,i} + \frac{Q_{w,i}}{h_{DB} A_i}, \quad (4.8.64)$$

$$T_{w,i}^2 = T_s + \frac{Q_{w,i}}{h_{JL} A_i}. \quad (4.8.65)$$

Therefore, we have

$$\frac{dT_{w,i}}{d\psi_i} = \frac{dT_{f,i}}{d\psi_i} \quad (4.8.66)$$

for single-phase flow, and

$$\frac{dT_{w,i}}{d\psi_i} = 0 \quad (4.8.67)$$

for the two-phase flow condition.

Using the marching technique starting from the core inlet, the Newton-Raphson iteration proceeds as follows:

Step 1. Determine core inlet conditions as is done in RAMONA-3B.

Step 2. Make guess of void fraction and coolant temperature:

$$\begin{aligned} \alpha_i &= \alpha_{i-1}, \\ T_{\xi i} &= T_{\xi i-1}. \end{aligned}$$

Compute initial estimate of the vapor generation rate:

$$\psi_i = \frac{K_1 (\psi_{S,i}^{(n)} + F(\alpha_i)) - K_3 (\psi_{S,i}^{(n)} T_{f,i}^{(n)} - \psi_{S,i}^{(n)} + F(\alpha_i) T_s)}{K_3 + (\psi_{S,i}^{(n)} + F(\alpha_i)) (K_2 - C_{v,i} T_{f,i}^{(n)})} \quad (4.8.68)$$

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with the surface terms zeroed out. This is what was done originally in RAMONA-3B, and retained as an initial guess of the vapor generation rate.

Set the iteration counter $npsi=0$.

Step 3. Update iteration counter $npsi=npsi+1$.

Compute the liquid density ρ_{fi} by Eq.(4.8.29).

Step 4. Save old value of vapor generation rate Ψ_i .

Compute new vapor generation rate ψ_i from Eq.(4.8.49) by the Newton-Raphson (N-R) iteration.

Step 5. Compute vapor mass flow rate $w_{g,i}$ by Eq.(4.8.22).

Compute liquid mass flow rate $w_{f,i}$ by Eq.(4.8.30).

Compute mixture volumetric flow rate W_i by Eq.(4.8.28).

Step 6. Save old value of $\alpha_i^{old}=\alpha_i$.

Compute new α_i from Eq.(4.8.41) by N-R iteration.

Step 7. Check convergence on α_i and Ψ_i :

Compute the residuals,

$$\Delta\alpha_i = \left| 1 - \frac{\alpha_i^{(n-1)}}{\alpha_i^{(n)}} \right|, \quad (4.8.69)$$

$$\Delta\Psi_i = \left| 1 - \frac{\psi_i^{(n-1)}}{\psi_i^{(n)}} \right|. \quad (4.8.70)$$

If $(\Delta\alpha_i \leq 0.00005$ and $\Delta\Psi_i \leq 0.0002)$, go to Step 8; else if $npsi \geq 50$, go to Step 8; otherwise, go to Step 3.

Step 8. Stop the iteration (converged).

It should be pointed out that the liquid density should be updated after the new liquid temperature is computed during the iteration, as shown in the above iteration schemes. This is particularly important in the reactor core where the spatial variation of liquid density could be significant.

Test results indicate that the above Newton-Raphson iteration has improved the convergence rate on the vapor generation rate by a factor of 2 as compared to the old method.

4.9 MECHANISTIC STEAM SEPARATOR MODEL

The RAMONA-4B code employed a simple model to simulate steam separators. This model considers only the effective L/A ratio for the riser for flow calculations and is based on an empirical correlation for the effective L/A developed by GE [Linford, 1973]. The carry-under fraction required for flow calculations is estimated from an input specified core inlet subcooling at steady state. Furthermore, the pressure drop in the riser is calculated from input form loss coefficients for the riser. Both the carry-under fraction and the riser form loss coefficients are kept constant during a transient. These approximations will impact the results of flow calculations and hence the transient system responses. Clearly, there is a need for a validated mechanistic steam separator model for predicting the effective L/A, carry-under fraction, and riser form loss coefficient.

GE has developed a validated mechanistic steam separator model for the joint USNRC-EPRI-GE sponsored BWR Refill-Reflood Program. The model is non-proprietary and ideal for use in RAMONA-4B. This model is used by the other NRC codes, RELAP5/MOD3.2 and TRAC/BF1.

Steam separators in a BWR are made up of either two or three stages connected in series. The separator model determines the separating efficiency of a single stage, and feeding the output of one stage to the next stage. The primary measures of separator performance are carryunder of steam in the liquid leaving the discharge passage of the separator, carryover of entrained liquid in the steam leaving the separator, and the separator pressure drop. The following description of the mechanistic separator model is adapted from the RELAP5/MOD3.2 code manual [NUREG/CR-5535, Vol. 1].

A steam separator consists of three regions, a standpipe, the separator barrel, and the discharge passage. Figure 4.7 shows a schematic of the first stage of a separator. A two-phase mixture enters the separator barrel from the standpipe, passing through a set of stationary swirl vanes attached to the separator hub which acts as a bladed nozzle. These vanes produce a high rotational velocity component in the fluid flowing through the separator barrel. The resultant centrifugal force separates the steam-water mixture into a water film on the inner wall of the separating barrel and a steam vortex core. Separation is accomplished by directing the water layer on the wall into the discharge passage through pick-off rings at the exit of the separator barrel.

4.9.1 Model Assumptions

The following assumptions are made for the axial location of the pick-off ring:

1. There is one uniform axial velocity in each of the flow regions, i.e.

$$V_a = V_{ag}, \text{ uniform within vapor core } (0 \leq r \leq r_f) \quad (4.9.1)$$

$$V_a = V_{af}, \text{ uniform within water layer } (r_f \leq r \leq r_w) \quad (4.9.2)$$

where r_f is the inner radius of the water layer and r_w is the wall radius of the separator barrel.

2. The tangential velocity in each region is proportional to a parameter C which is related to the vortex strength or angular momentum, and is a function of r as follows:

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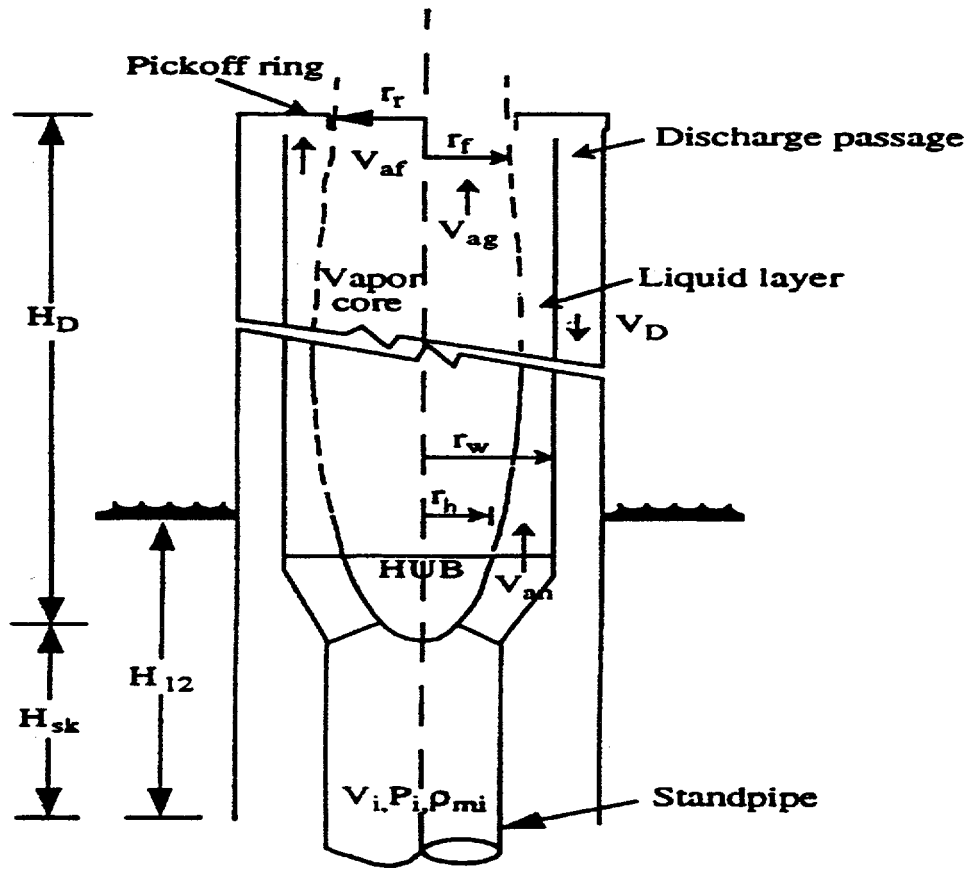


Figure 4.7 Schematic of First Stage of the Mechanistic Steam Separator.

$$V_t = \frac{rC}{r_f^{3/2}} \quad \text{for } 0 \leq r \leq r_f, \quad (4.9.3)$$

$$V_t = \frac{C}{\sqrt{r}} \quad \text{for } r_f \leq r \leq r_w. \quad (4.9.4)$$

3. The radial distributions of void fraction in each region depend on the vortex strength and inlet quality. The void profiles are assumed as follows:

$$\alpha = 1 - b \left(\frac{r}{r_w} \right) \quad \text{for } 0 \leq r \leq r_f, \quad (4.9.5)$$

$$\alpha = a \left(\frac{r_w}{r} - 1 \right) \quad \text{for } r_f \leq r \leq r_w, \quad (4.9.6)$$

where

$$\begin{aligned} a &= AA \left(\frac{\sqrt{g r_w^2}}{C} \right)^{0.5} x_i^2 \quad \text{for } x_i < 0.15, \\ &= AA \left(\frac{\sqrt{g r_w^2}}{C} \right)^{0.5} (0.09335) x_i^{0.75} \quad \text{for } x_i > 0.15, \end{aligned} \quad (4.9.7)$$

$$b = BB \left(\frac{\sqrt{g r_w^2}}{C} \right)^{0.5} (1 - x_i)^3, \quad (4.9.8)$$

x_i is the inlet quality, AA and BB are parameters to be determined from data.

4. The pressure in the vapor core (P_v) is assumed to be uniform radially and axially, and the pressure at the separator wall (P_w) is related to P_v by centrifugal force across the water layer (See Eq. 27).

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4.9.2 Model Equations

For the first stage of the separator, a total of six unknowns are introduced in this model. These are V_{ag} , V_{af} , C , r_f , P_o , and P_w . The required model equations are formulated from conservation of water mass, vapor mass, axial momentum, and angular momentum entering and leaving the separating barrel, from centrifugal pressure drop across the water layer, and from pressure drop in the discharge passage. The above unknowns can now be solved from given inlet conditions of pressure P_i , total flow rate W_i , and flow quality x_i .

4.9.2.1 Formulation of Separator Barrel

The mass and momentum conservation equations for flows entering and leaving the separating barrel are:

Water Mass

$$\begin{aligned} (1-x_i) W_i &= 2\pi \int_0^{r_w} (1-\alpha) \rho_f V_a r dr \\ &= 2\pi \rho_f V_{ag} \left(\frac{b r_f}{3 r_w} \right) r_f^2 + 2\pi \rho_f V_{af} \left[\left(\frac{1+a}{2} \right) (r_w^2 - r_f^2) - a r_w (r_w - r_f) \right]. \end{aligned} \quad (4.9.9)$$

Vapor Mass

$$\begin{aligned} x_i W_i &= 2\pi \int_0^{r_w} \alpha \rho_g V_a r dr \\ &= 2\pi \rho_g V_{ag} \left(\frac{1}{2} - \frac{b}{3} \frac{r_f}{r_w} \right) r_f^2 + 2\pi \rho_g V_{af} \frac{a}{2} (r_w - r_f)^2. \end{aligned} \quad (4.9.10)$$

Angular Momentum

$$\int_{r_h}^{r_w} V_{in} r (2\pi r \rho_{mi} V_{an}) dr = \int_0^{r_w} V_t r (2\pi r \rho V_a) dr + F_t r_w, \quad (4.9.11)$$

which leads to

$$\begin{aligned}
\frac{2}{3} \pi \rho_{mi} V_{tn} V_{an} (r_w^3 - r_h^3) &= 2\pi V_{ag} C \left[\frac{1}{4} \rho_g r_f^{5/2} + \frac{1}{5} \frac{b}{r_w} (\rho_f - \rho_g) r_f^{7/2} \right] \\
&+ 2\pi V_{af} C [\rho_f + a(\rho_f - \rho_g)] \frac{2}{5} (r_w^{5/2} - r_f^{5/2}) \\
&- 2\pi V_{af} C r_w a (\rho_f - \rho_g) \frac{2}{3} (r_w^{3/2} - r_f^{3/2}) + F_t r_w.
\end{aligned} \tag{4.9.12}$$

Radial Momentum

$$\int_{P_o}^{P_w} dP = \int_{r_f}^{r_w} \rho \left(\frac{V_t^2}{r} \right) dr \tag{4.9.13}$$

Axial Momentum

$$\pi r_h^2 P_o + 2\pi \int_{r_h}^{r_w} (\rho_{mi} V_{an}^2 + P_n) r dr = 2\pi \int_0^{r_w} (\rho V_a^2 + P) r dr + F_a, \tag{4.9.14}$$

which leads to

$$\begin{aligned}
\pi r_h^2 P_o + (\rho_{mi} V_{an}^2 + P_n) \pi (r_w^2 - r_h^2) &= (\rho_g V_{ag}^2 + P_o) \pi r_f^2 + \frac{2}{3} \pi b (\rho_f - \rho_g) V_{ag}^2 \frac{r_f^3}{r_w} \\
&+ 2\pi V_{af}^2 \left[(\rho_f + a(\rho_f - \rho_g)) \frac{1}{2} (r_w^2 - r_f^2) - a(\rho_f - \rho_g) r_w (r_w - r_f) \right] + F_a \\
&+ 2\pi \left[\frac{1}{2} P_w (r_w^2 - r_f^2) - \frac{1}{2 r_w} (\rho_f + a(\rho_f - \rho_g)) C^2 (r_w - r_f)^2 \right] \\
&+ 2\pi \left[\frac{1}{4} a(\rho_f - \rho_g) C^2 \left(2 r_w \log \left(\frac{r_w}{r_f} \right) - \frac{1}{r_w} (r_w^2 - r_f^2) \right) \right].
\end{aligned} \tag{4.9.15}$$

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4.9.2.2 Flow in Swirl Vane Passage

Assuming that the flow through the swirl vane passage is homogeneous and that the swirl vane passage acts as a nozzle, the pressure and velocity entering the separating barrel are related to the conditions at the standpipe as follows:

$$W_i = \rho_{mi} V_i A_i = \rho_{mi} V_{an} A_n, \quad (4.9.16)$$

$$P_i + \frac{1}{2} \rho_{mi} V_i^2 = P_n + \frac{1}{2} \rho_{mi} (V_{an}^2 + V_{tn}^2) (1 + C_{NOZ}), \quad (4.9.17)$$

$$\frac{V_{an}}{V_{tn}} = \tan \theta, \quad (4.9.18)$$

where θ is the angle between the swirl vane and the horizontal plane, ρ_{mi} is the inlet mixture density, and C_{NOZ} is the contraction loss coefficient defined as:

$$C_{NOZ} = 0.5 \left(1 - \frac{A_n}{A_i} \right) \quad (4.9.19)$$

where A_n is the swirl vane flow area and A_i is the standpipe flow area.

F_a and F_t in Equations (4.9.12) and (4.9.14) are the axial and tangential components of the frictional force on the swirling water layer given by

$$F_a = F_f \left(\frac{V_{af}}{V_w} \right), \quad (4.9.20)$$

$$F_t = F_f \left(\frac{V_{tw}}{V_w} \right), \quad (4.9.21)$$

where V_{tw} is the tangential velocity on the wall,

$$V_{tw} = \frac{C}{\sqrt{r_w}}, \quad (4.9.22)$$

V_w is the resultant swirling velocity on the wall,

$$V_w = \sqrt{V_{tw}^2 + V_{af}^2}, \quad (4.9.23)$$

and F_f is the resultant frictional force,

$$F_f = \frac{1}{2} \rho_f C_F V_w^2 (2\pi r_w) H_D. \quad (4.9.24)$$

Assuming that the liquid film is thin with respect to the radius of curvature of the separator barrel, the flow can be considered to be film flow over a flat plate. For turbulent flow over a flat plate,

$$C_F = \frac{0.455}{(\log R_{eL})^{2.58}}, \quad (4.9.25)$$

where R_{eL} is defined as

$$R_{eL} = \frac{\rho V_w}{\mu} \left(\frac{H_D V_w}{V_{af}} \right) \quad (4.9.26)$$

with μ being the dynamic viscosity. The term $(H_D V_w / V_{af})$ represents the linear distance that a water particle will travel inside the separator barrel.

The radial pressure drop across the water layer due to centrifugal force is obtained from Eq. (4.9.13):

$$P_o = P_w - [\rho_f + a(\rho_f - \rho_g)] C^2 \left(\frac{1}{r_f} - \frac{1}{r_w} \right) + \frac{1}{2} a(\rho_f - \rho_g) C^2 \left(\frac{r_w}{r_f} \frac{1}{r_f} - \frac{1}{r_w} \right). \quad (4.9.27)$$

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4.9.2.3 Discharge Passage

The discharge passage consists of the region between pickoff ring and separator barrel wall as well as the region between the separator barrel wall and outer wall of the separator. The pressure drop in the discharge passage is

$$\frac{\int_{r_r}^{r_w} \left(\frac{1}{2} \rho V_a^2 + P \right) dA}{\int_{r_r}^{r_w} dA} = \frac{1}{2} \rho_{M,D} V_D^2 \left[1 + 4 C_F \left(\frac{H_D}{D_D} + \left(\frac{L}{D} \right)_{\text{eff}} \right) + C_K \right] \quad (4.9.28)$$

$$+ [P_o + \rho_f H_{12} g - \rho_D (H_D + H_{sk}) g],$$

where $\rho_{M,D}$ and V_D are the mean mixture density and velocity in the discharge passage, H_D and D_D are the length of the separator barrel and hydraulic diameter of the discharge passage, $(L/D)_{\text{eff}}$ is the effective L/D coefficient at the pick-off ring, C_K is the total loss coefficient in the discharge passage, H_{12} is the height of the liquid pool surrounding the separator relative to the bottom of the discharge passage, and H_{sk} is the axial distance between the separator hub and the bottom of the discharge passage.

The left-hand side (LHS) of Equation (4.9.28) can be integrated. The results are given as follows:

For $r_f < r_r$:

$$\begin{aligned} LHS = & \frac{1}{(r_w^2 - r_r^2)} \left\{ \frac{1}{2} V_{af}^2 \left[(\rho_f + a(\rho_f - \rho_g))(r_w^2 - r_r^2) - 2a(\rho_f - \rho_g)r_w(r_w - r_r) \right] \right\} \\ & + \frac{1}{(r_w^2 - r_r^2)} \left\{ P_w(r_w^2 - r_r^2) - [\rho_f + a(\rho_f - \rho_g)] C^2 \frac{(r_w - r_r)^2}{r_w} \right\} \\ & + \frac{1}{(r_w^2 - r_r^2)} \left\{ \frac{1}{2} a(\rho_f - \rho_g) C^2 \left[2r_w \log \left(\frac{r_w}{r_r} \right) - \frac{(r_w^2 - r_r^2)}{r_w} \right] \right\}. \end{aligned} \quad (4.9.29)$$

For $r_f \geq r_r$:

$$\begin{aligned}
 LHS = & \frac{1}{(r_w^2 - r_r^2)} \left\{ \left(\frac{1}{2} \rho_g V_{ag}^2 + P_o \right) (r_f^2 - r_r^2) + \frac{b}{3 r_w} (\rho_f - \rho_g) V_{ag}^2 (r_f^3 - r_r^3) \right\} \\
 & + \frac{1}{(r_w^2 - r_r^2)} \left\{ V_{ag}^2 \left[\frac{1}{2} (\rho_g + a(\rho_f - \rho_g)) (r_w^2 - r_f^2) - a(\rho_f - \rho_g) r_w (r_w - r_f) \right] \right\} \\
 & + \frac{1}{(r_w^2 - r_r^2)} \left\{ V_{af}^2 \left[\frac{1}{2} (\rho_f + a(\rho_f - \rho_g)) (r_w^2 - r_f^2) - a(\rho_f - \rho_g) r_w (r_w - r_f) \right] \right\} \quad (4.9.30) \\
 & + \frac{1}{(r_w^2 - r_r^2)} \left\{ P_w (r_w^2 - r_f^2) - [\rho_f + a(\rho_f - \rho_g)] C^2 \frac{(r_w - r_f)^2}{r_w} \right\} \\
 & + \frac{1}{(r_w^2 - r_r^2)} \left\{ \frac{1}{2} a(\rho_f - \rho_g) C^2 \left[2 r_w \log \left(\frac{r_w}{r_f} \right) - \frac{(r_w^2 - r_f^2)}{r_w} \right] \right\}.
 \end{aligned}$$

For the discharge passage, the single-phase friction factor is given by

$$C_F = \frac{0.079}{R_e^{0.25}}. \quad (4.9.31)$$

The steam and water flows discharged through the discharge passage are given by

$$W_{g,cu} = 2\pi \int_{r_r}^{r_w} \alpha \rho_g V_a r dr, \quad (4.9.32)$$

$$W_{f,cu} = 2\pi \int_{r_r}^{r_w} (1 - \alpha) \rho_f V_a r dr. \quad (4.9.33)$$

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For a water layer thicker than the gap between the pick-off ring and the separator barrel, i.e., $r_t > r_f$, the steam and water flows through the discharge passage are

$$W_{g,cu} = \pi \rho_g V_{af} a (r_w - r_r)^2, \quad (4.9.34)$$

and

$$W_{f,cu} = \pi \rho_f V_{af} \left[(r_w^2 - r_r^2) - a (r_w - r_r)^2 \right]. \quad (4.9.35)$$

For a water layer thinner than the gap between the pick-off ring and the separator barrel wall, i.e., $r_t < r_f$, the steam and water flows through the discharge passage are

$$W_{g,cu} = x_i W_i - 2\pi \rho_g V_{ag} \left(\frac{1}{2} - \frac{b}{3} \frac{r_r}{r_w} \right) r_r^2, \quad (4.9.36)$$

and

$$W_{f,cu} = (1 - x_i) W_i - 2\pi \rho_f V_{ag} \left(\frac{b}{3} \frac{r_r}{r_w} \right) r_r^2. \quad (4.9.37)$$

The steam and water flows leaving the present stage and entering the next stage are

$$W_{g,co} = (\text{steam flow})_{in} - W_{g,cu}, \quad (4.9.38)$$

and

$$W_{f,co} = (\text{water flow})_{in} - W_{f,cu}. \quad (4.9.39)$$

The total discharge flow is

$$W_D = W_{g,cu} + W_{f,cu}. \quad (4.9.40)$$

Assuming homogeneous flow in the discharge passage, the mean void fraction is

$$\alpha_{cu} = \frac{W_{g,cu}}{W_{g,cu} + W_{f,cu} \left(\frac{\rho_g}{\rho_f} \right)}, \quad (4.9.41)$$

the mean mixture density in the discharge passage is

$$\rho_{m,D} = \alpha_{cu} \rho_g + (1 - \alpha_{cu}) \rho_f, \quad (4.9.42)$$

and the mean discharge velocity is

$$V_D = \frac{W_D}{\rho_{m,D} A_D}. \quad (4.9.43)$$

In summary, for given swirl vane inlet conditions, P_i , X_i , and W_i , the unknowns V_{ag} , V_{af} , C , r_p , P_o , and P_w are calculated by solving Eqs (4.9.9), (4.9.10), (4.9.12), (4.9.15), (4.9.27), and 4.9.(28) simultaneously.

Similar equations can be written for the second and third stages. Since it is assumed that P_o is uniform axially, i.e., the vapor core pressure drop in the axial direction is small, the axial momentum equation can be neglected in the solution for the upper stages. For these stages, the unknowns are reduced to V_{ag} , V_{af} , C , r_f , and P_w , and the equations are the conservation of water mass (Eq. 4.9.9), vapor mass (Eq. 4.9.10), angular momentum (Eq. 4.9.12), the pressure drop across the water layer (Eq. 4.9.27), and the pressure drop in the discharge passage (Eq. 4.9.28).

The right-hand sides of Equations (4.9.9), (4.9.10), and (4.9.11) represent the water flow, vapor flow, and angular momentum entering the separator barrel. For the second and third stages, these terms are modified as follows:

$$(Water\ flow)_{in} = (W_{f,co})_{previous\ stage}, \quad (4.9.44)$$

$$(Steam\ flow)_{in} = (W_{g,co})_{previous\ stage}, \quad (4.9.45)$$

and

$$(Angular\ momentum)_{in} = \left[\int_0^{r_r} V_t r (2\pi \rho V_a) r dr \right]_{previous\ stage} \quad (4.9.46)$$

4.9.2.4 Carryunder and Carryover

The total vapor flow that is carried under consists of two parts. The first part is the steam flow through the first discharge passage which discharges its fluid below the surface of the water pool surrounding the separators. The second part is the steam entrained by the water discharged from higher stages. The exit of the discharge passages of the higher stages are located above the liquid level. It is assumed that the entrainment is proportional to the square of the total water flow discharged from the higher stages [4, 5, 6], i.e.,

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$$(W_{g,cu})_{total} = (W_{g,cu})_1 + CC \left[\sum_{i=2}^N (W_{f,cu})_i \right]^2, \quad (4.9.47)$$

where

N = 2 for a two-stage separator,

N = 3 for a three-stage separator,

and CC is a constant to be determined from test data.

The total water flow that is carried over consists of two parts. The first part is the water flow through the last stage. The second part is the water flow entrained by the steam discharged from the higher stages through their discharge passages. Similarly, the second part is assumed to be proportional to the square of the total steam flow discharged from the higher stages, i.e.,

$$(W_{f,co})_{total} = (W_{f,co})_N + DD \left[\sum_{i=2}^N (W_{g,co})_i \right]^2, \quad (4.9.48)$$

where DD is a constant to be determined from test data.

The carryunder and carryover are defined as

$$x_{cu} = \frac{(W_{g,cu})_{total}}{\text{Total downward water flow}}, \quad (4.9.49)$$

and

$$x_{co} = \frac{(W_{f,co})_{total}}{\text{Total upward steam flow}}. \quad (4.9.50)$$

The parameters AA, BB, CC, and DD were determined by fitting the available test data for two-stage and three-stage separators [Moen 1969, Burley 1969, Wolf 1973]. Table 4.1 summarizes the values for these parameters.

Table 4.1 Summary of Fitted Parameters in the Mechanistic Separator Model

	2-Stage Separator		3-Stage Separator		
Parameter	1st Stage	2nd Stage	1st Stage	2nd Stage	3rd Stage
AA	110.	20.	110.	20.	20.
BB	0.5	0.25	0.5	0.25	0.55
CC	0.0004	---	0.0004	---	---
DD	0.009	---	0.11	---	---

4.9.3 Implementation Strategy

The flow calculation of the RAMONA-4B code requires three parameters related to the steam separator model: (1) carryunder fraction, (2) effective L/A ratio for the riser, and (3) form loss coefficient associated with the separator pressure drop. The carryunder fraction is given by Eq. (4.9.49).

4.9.3.1 Effective L/A Ratio for Riser

The effective L/A ratio accounts for the separator inertia which represents the effect of spiral flow path through the separator vanes. Since steam separators are located in the riser, the separator inertia must be taken into account in the flow calculation in the riser via the flow inertia L/A.

The separator inertia is calculated as follows:

$$\left(\frac{L}{A}\right)_{\text{eff}} = \frac{(1 - X_i)}{A_{\text{rsr}}} \sum_{i=1}^N \left[H_{D,i} + \left(\frac{L}{D}\right)_{\text{eff},i} D_{h,i} \right], \quad (4.9.51)$$

where $H_{D,i}$ is the length of the separator barrel of i^{th} stage, $(L/D)_{\text{eff}}$ is the effective L/D coefficient at the pick-off ring, $D_{h,i}$ is the hydraulic diameter of the discharge passage, X_i is the separator inlet flow quality, and A_{rsr} is the riser flow area. All the parameters on the right-hand side of the above equation are input data, except for X_i which is calculated in RAMONA-4B. For a BWR, Eq.(4.9.51) produces the same result as the original GE correlation [2] which was used by RAMONA-4B prior to the implementation of the present mechanistic separator model. The old simple model is still retained as an option.

4.9.3.2 Form Loss Coefficient for Separator Pressure Drop

The separator pressure drop is the major part of the total pressure drop in the riser, and must be taken into account in the riser flow calculation. RAMONA-4B uses the riser inlet and riser exit form loss coefficients to calculate the pressure drop in the riser. The two form loss coefficients in the riser are treated as input at present, and assumed to be constant during a transient. The new mechanistic separator model relieves this simplistic assumption and allows one to calculate a time-dependent form loss coefficient to represent the separator pressure drop.

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The total separator pressure drop is simply

$$(\Delta P)_{sep} = P_o - P_i, \quad (4.9.52)$$

where P_o is the separator outlet pressure obtained from Eq. (4.9.27) and P_i is the separator inlet pressure assumed to be the average RPV pressure, which is one of the state variables calculated in RAMONA-4B.

Assuming that the riser pressure drop is primarily due to the separator pressure drop and that it occurs at the riser inlet, we can determine the riser inlet form loss coefficient by equating the separator pressure drop to the pressure drop at the riser inlet:

$$(\Delta P)_{sep} = \frac{K_{rsr,i} \phi_{rsr,i}^2 W_{rsr,i} |W_{rsr,i}|}{2 \rho_{l,rsr,i} A_{min}^2}, \quad (4.9.53)$$

where $K_{rsr,i}$ is the riser inlet form loss coefficient, $W_{rsr,i}$ is the mixture mass flow rate at the riser inlet, $\rho_{l,rsr,i}$ is the liquid density at the riser inlet, A_{min} is the minimum flow area at the riser inlet, and $\phi_{rsr,i}^2$ is the two-phase multiplier for form loss. The two-phase multiplier for form loss employed by RAMONA-4B is the correlation developed by EPRI [Gitznick, 1981]:

$$\phi_{rsr,i}^2 = 1 + \beta x_i \left(\frac{\rho_{l,rsr,i}}{\rho_{g,rsr,i}} - 1 \right), \quad (4.9.54)$$

where β is an input parameter (typically, 1.2) and $\rho_{g,rsr,i}$ is the vapor density at the riser inlet.

Note that the pressure drops in various flow segments are calculated from inlet and outlet loss coefficients of each flow segment in a DO loop in RAMONA-4B. Therefore, it is easier to replace the input riser inlet loss coefficient by that defined in Eq. (4.9.53). Solving Eq. (4.9.53) for $K_{rsr,i}$, we obtain

$$K_{rsr,i} = \left| \frac{2 \rho_{l,rsr,i} A_{min}^2 (\Delta P)_{sep}}{\phi_{rsr,i}^2 W_{rsr,i} |W_{rsr,i}|} \right|. \quad (4.9.55)$$

4.9.3.3 Implementation

The carryunder fraction, effective L/A ratio, and riser inlet form loss coefficient are calculated in the new subroutine STMSEP which, in turn, calls the GE subroutine GESUB (taken from RELAP5/MOD3.2). The GE subroutine GESUB coded the new mechanistic separator model described in Section 2. The subroutine STMSEP is called by subroutine HYDRO2 prior to the flow calculations. The carryunder fraction is used in HYDRO2 to calculate the carryunder vapor mass flow rate, which is required for vapor mass balance. The effective L/A ratio is used in the subroutine RISERM to calculate the drift-flux model parameters in the riser. Finally, the riser inlet form loss coefficient is used in HYDRO2

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(transient) and FUNCSS (steady state) to calculate the pressure drop in the riser, which is required for loop momentum balance. Appendix A lists the Fortran code for the new subroutines STMSEP and GESUB.

The input data required for the new separator model are read in by subroutine PRINP on the input Cards 624000, 624100, 624200, 624300, 624400, 624500, and 624600 as shown in Appendix B. If these input cards are not present in the input data file, the default values as specified in the RELAP5/MOD3.2 input manual are used as default. The input parameter ISTAGE on Card 624000 is used to select either the two-stage separator (ISTAGE=2) or the three-stage separator (ISTAGE=3). If ISTAGE=0, the original simplistic separator model is used.

Four output variables are saved on the plot file fort.49 for the new separator model. They are the carryunder fraction, the effective L/A ratio, the riser inlet form loss coefficient, and the separator pressure drop. The main program RAMONA and subroutine PRINT were modified to define and save these plot variables.

Subroutine FPTOT was also modified to take care of the wrapup/restart capability for the new separator model in RAMONA-4B.

4.9.4 Test Results

The calculational model used for testings is illustrated Figures 1.1 and 1.4. It includes the reactor pressure vessel (RPV) with all important internal components, steam lines, balance of plant (BOP), control systems, and the SLCS for boron injection.

The reactor core was modeled with 101 neutronic channels and 25 hydraulic channels in a 1/8-core symmetry. Twenty four axial cells were used in each of the neutronic and hydraulic channels. Seven evenly distributed spacers were located along the axial height of each hydraulic channel (except for the bypass channel) to account for the local form losses due to the spacer grids.

The nuclear parameters for the 3D neutron kinetics were based on the Browns Ferry Unit 1 at the end of cycle 5. The two-group cross sections and their feedback coefficients were generated from the lattice physics data given by TVA to BNL in early 1980.

The testing was done for a Recirculation Pump Trip (RPT) event with density wave oscillations. In this testing, the BOP, motor/generator set dynamics and control systems options in RAMONA-4B were all activated in order to produce realistic simulation results.

The transient selected for the test case is a dual recirculation pump trip (RPT) event initiated from 100% core power and 75% core flow (minimum permissible) with postulated scram failure (ATWS). The initial condition was the hot full power but reduced flow condition. The key parameters calculated by RAMONA-4B are given below:

Core Thermal Power	3293 Mwt
System Pressure	6.962 Mpa
Feedwater Flow Rate	1685 kg/s
Core Flow Rate	9,780 kg/s
Core Inlet Subcooling	10.65 °C

The transient was run for 600 seconds for this testing. The event began with a trip of both recirculation pumps at time zero. The RPT reduced the core flow to natural circulation (about 29% of rated) within 60 seconds as shown in Figure 4.8. As a result, core power was reduced from its initial rated value to approximately 40% of rated power due to

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increased vapor fraction as shown in Figure 4.9. The evidence of instability is visible within 100 seconds of the transient in the oscillatory behavior of both core flow and core power when core inlet subcooling has increased above 18°C as shown in Figure 4.10. Figure 4.11 presents the oscillatory behavior of core average void fraction, which demonstrates clearly that this is a density wave oscillation.

The system pressure response is presented in Figure 4.12 and the steam flow and feedwater flow behavior are shown in Figure 4.13. Due to the rapid reduction of feedwater flow at the beginning of the transient, the system pressure decreases initially and settles at about 6.62 MPa after 50 seconds. The density wave oscillation also appears in the pressure response through the vapor generation, which in turn produces the oscillatory behavior of steam flow and feedwater flow. The oscillatory water level behaviors are shown in Figure 4.14. The water level remained in the steam dome region.

Figure 4.15 shows the transient response of the carryunder for this case, and Figure 4.16 shows that of the effective L/A ratio. The separator form loss coefficient is shown in Figure 4.17. The form loss coefficient increases rapidly from its initial value of about 10 and oscillates between 30 and 70 after about 60 seconds. It is well known that the greater the loss coefficient is in a high void region such as riser, the more profound a density wave oscillation is. Therefore, this increase in loss coefficient in the riser is responsible for the enhanced power oscillations after 60 seconds as compared to the corresponding power oscillations (Figure 4.18) predicted by the old simple separator model which assumes that the riser loss coefficient is constant (input). We see that the simple separator model is non-conservative for this event (density wave oscillation).

4.9.5 Summary And Conclusions

The new mechanistic separator model has been implemented in RAMONA-4B and successfully tested with an input deck based on the Browns Ferry 1 cross sections for a MSIV closure ATWS event and a recirculation pump trip event with density wave oscillations. The following conclusions can be drawn from this work:

1. The key parameters, carryunder fraction, effective L/A, and riser loss coefficient, all vary significantly during the transients tested. It is, therefore, an oversimplification to assume that they are constant independent of time. This clearly demonstrates the need for a mechanistic separator model for a realistic simulation of the BWR plant response.
2. The riser form loss coefficient increases considerably after a recirculation pump trip. This enhances the density wave oscillation of the first test case, hence can impact the severity of a large-amplitude density wave oscillation.
3. Since the larger the riser loss coefficient, the more profound the density wave oscillation is, the amplitude of the power oscillation predicted by the present model is considerably larger than that predicted by the simple separator model which assumes that the riser loss coefficient is constant via input. Thus, assuming a constant riser loss coefficient is non-conservative for density wave oscillations.

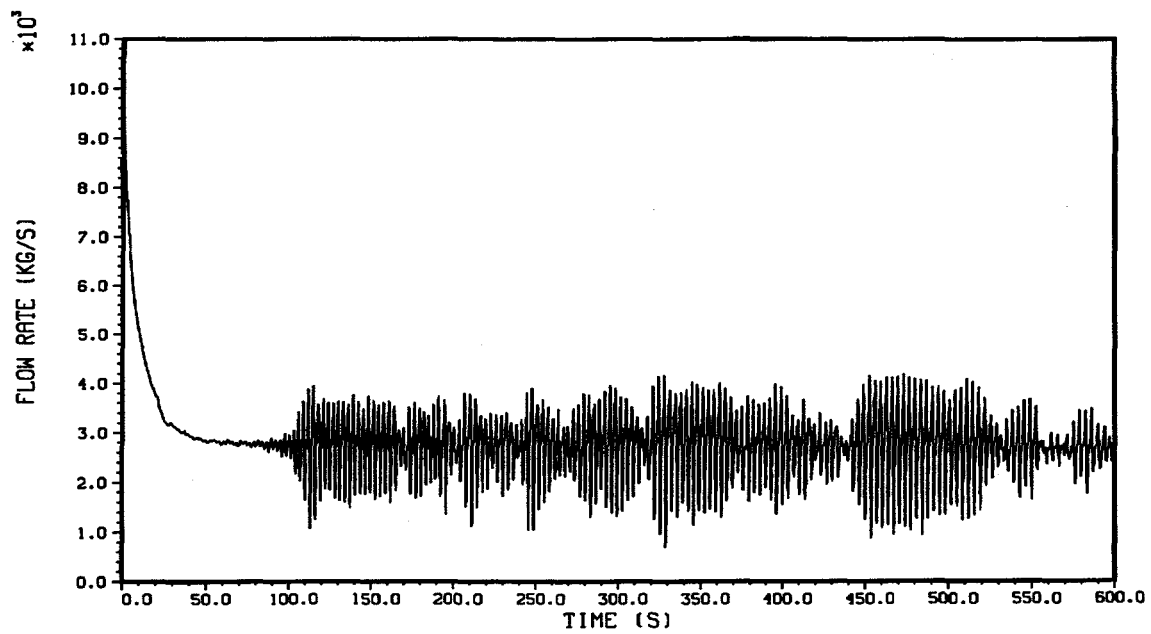


Figure 4.8 Transient Response of Core Inlet Flow During the RPT Event

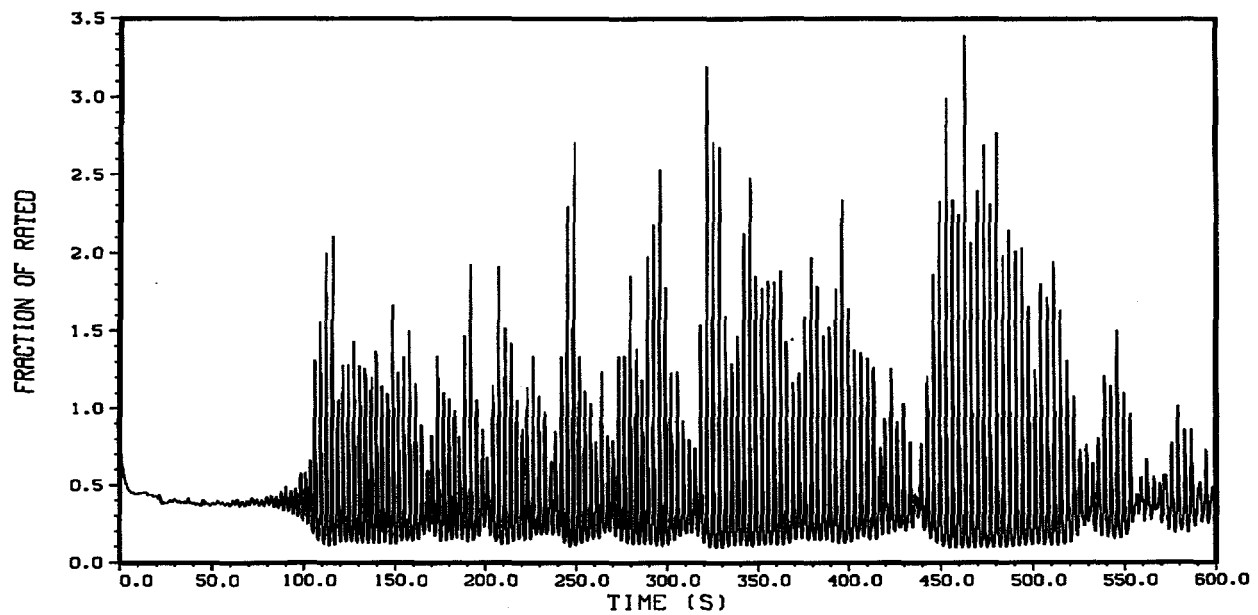


Figure 4.9 Reactor Power Response During the RPT Event as Predicted by Present Model

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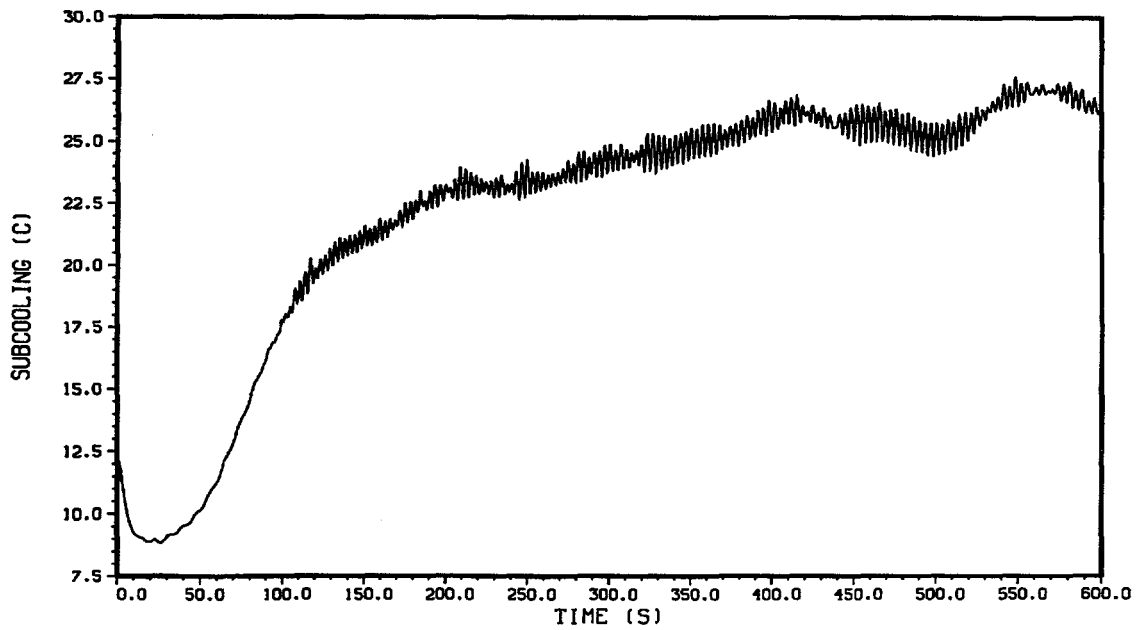


Figure 4.10 Transient Response of Core Inlet Subcooling During the RPT Event

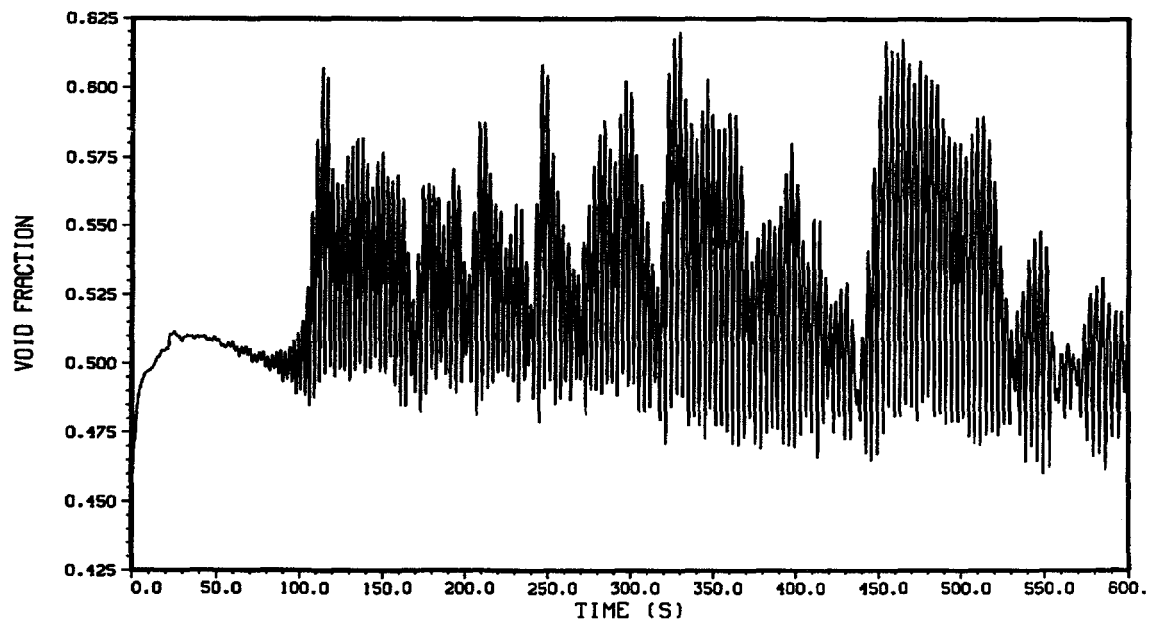


Figure 4.11 Transient Response of Core Average Void Fraction During the RPT Event

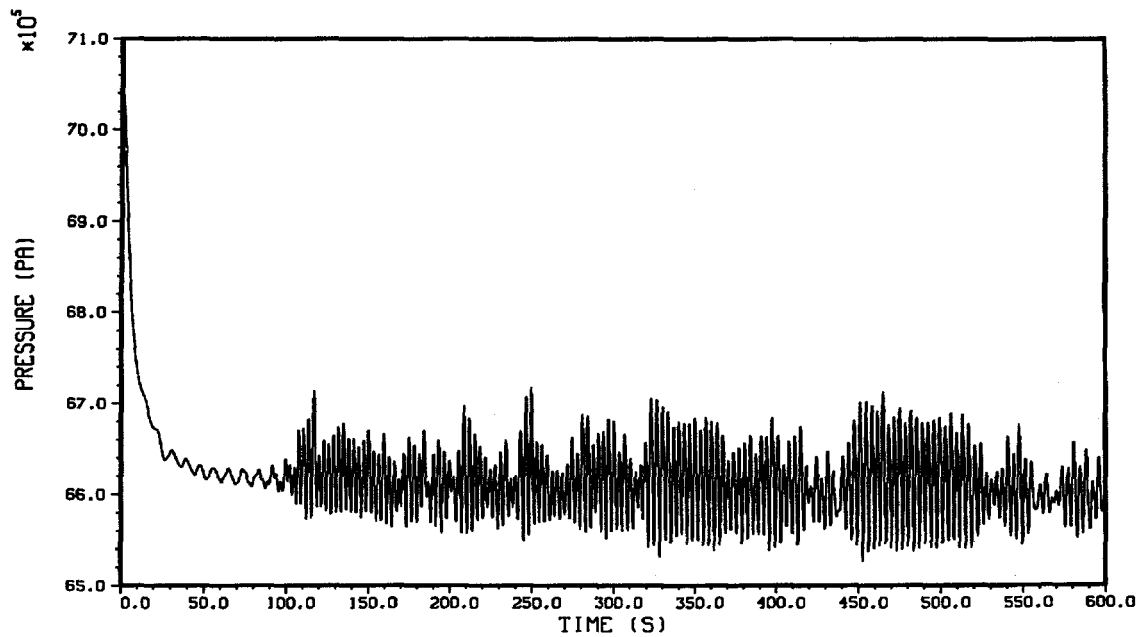


Figure 4.12 Transient Response of Average RPV Pressure During the RPT Event

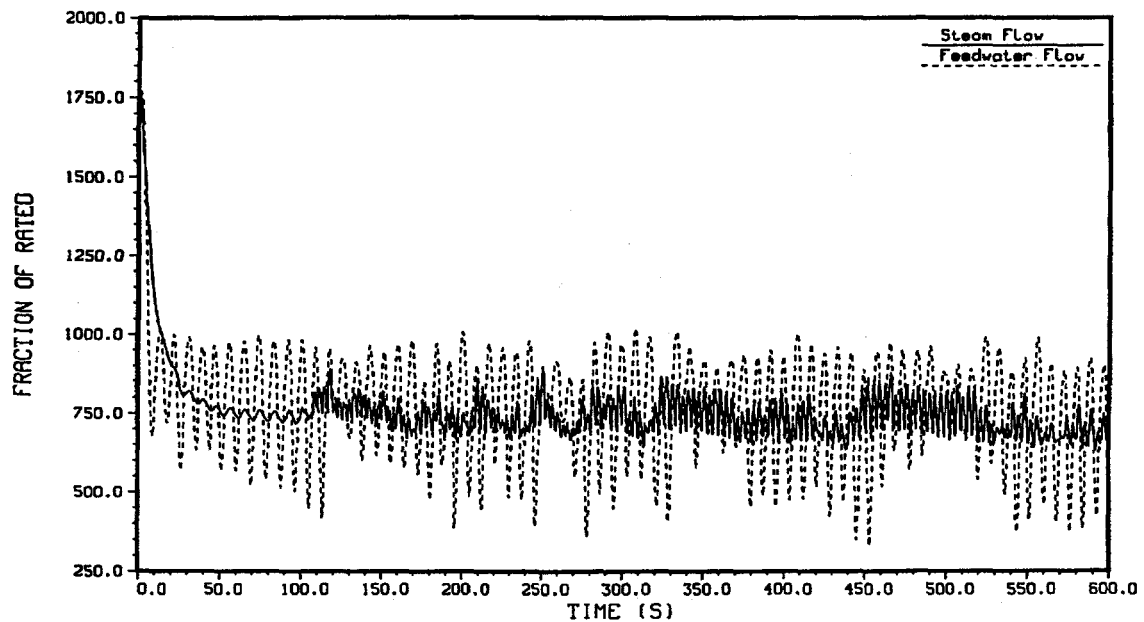


Figure 4.13 Transient Response of Steam and Feedwater Flow During the RPT Event

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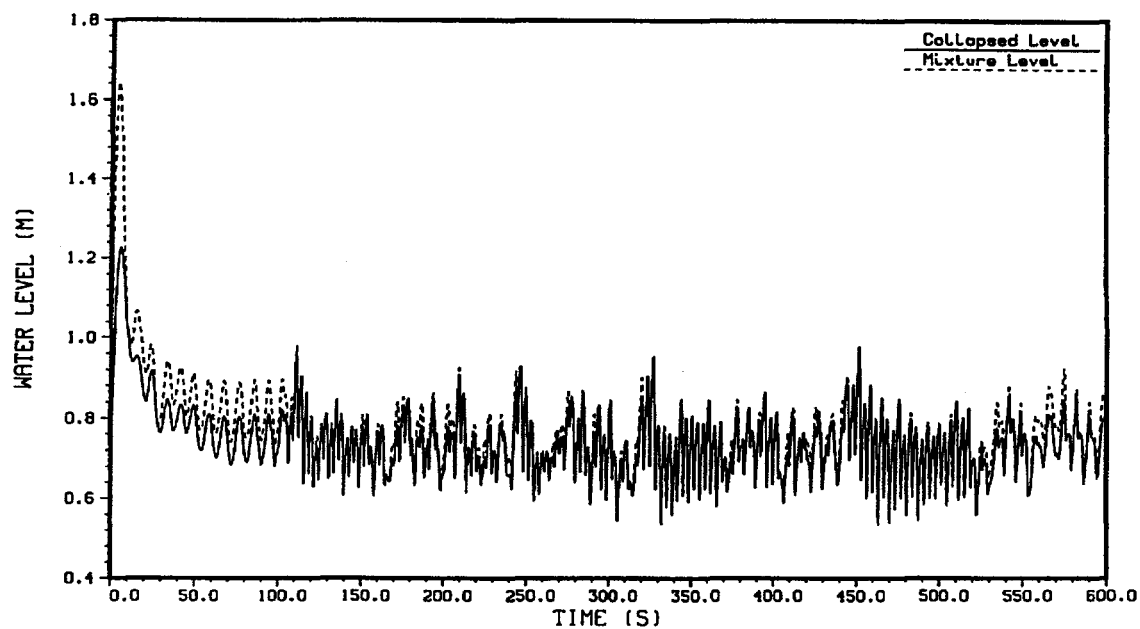


Figure 4.14 Transient Response of Downcomer Water Level During the RPT Event

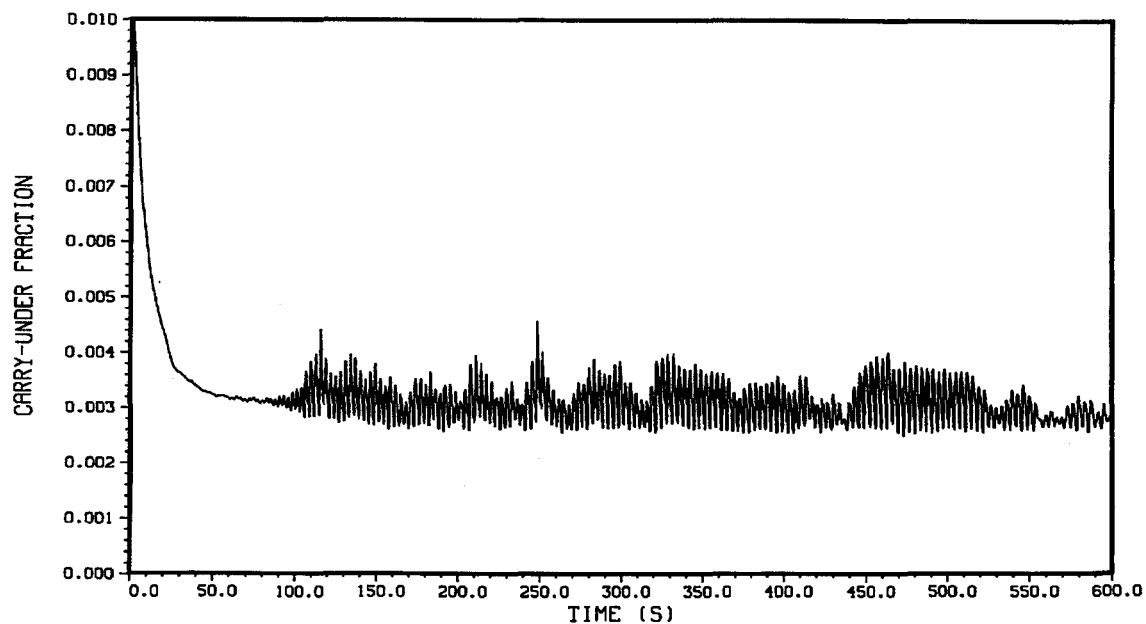


Figure 4.15 Transient Response of Steam Carryunder During the RPT Event

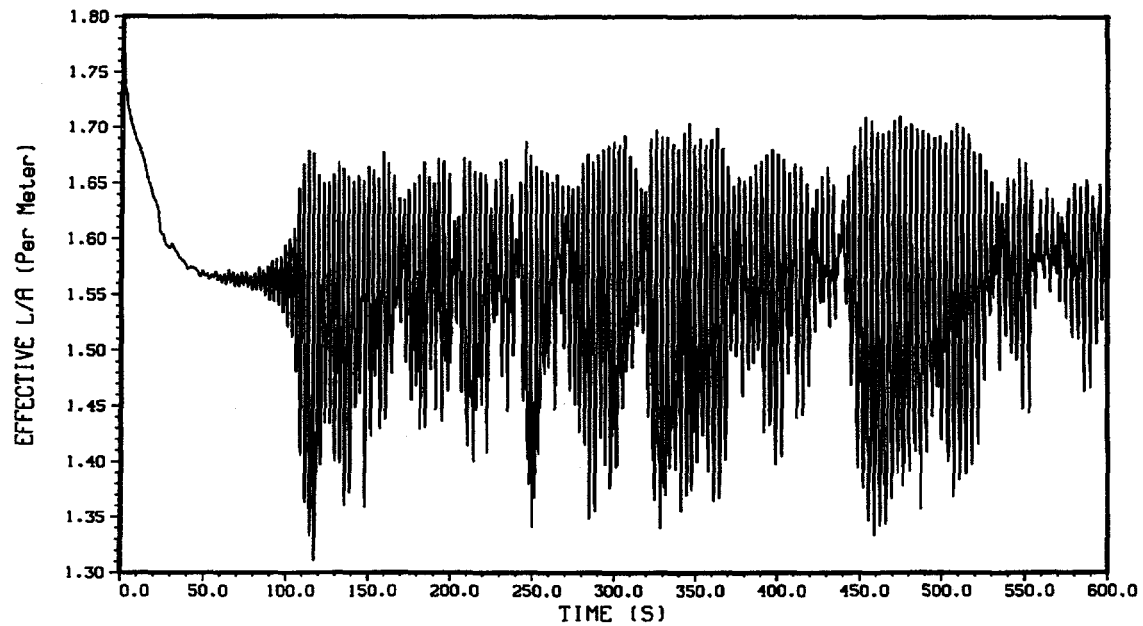


Figure 4.16 Transient Response of Effective L/A Ratio During the RPT Event

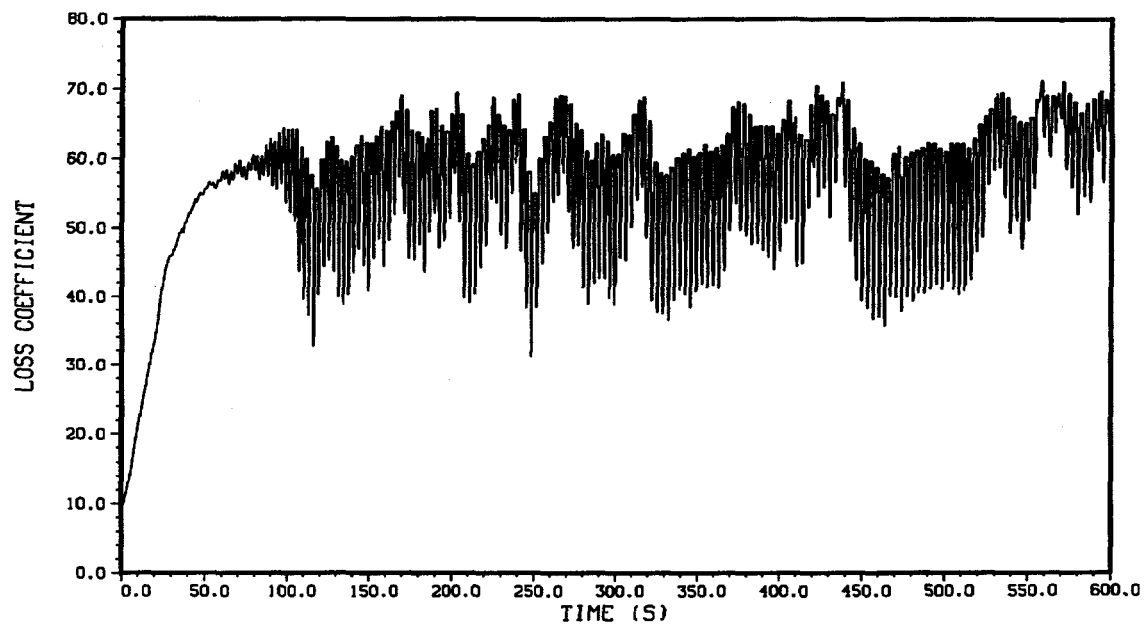


Figure 4.17 Transient Response of Riser Loss Coefficient During the RPT Event

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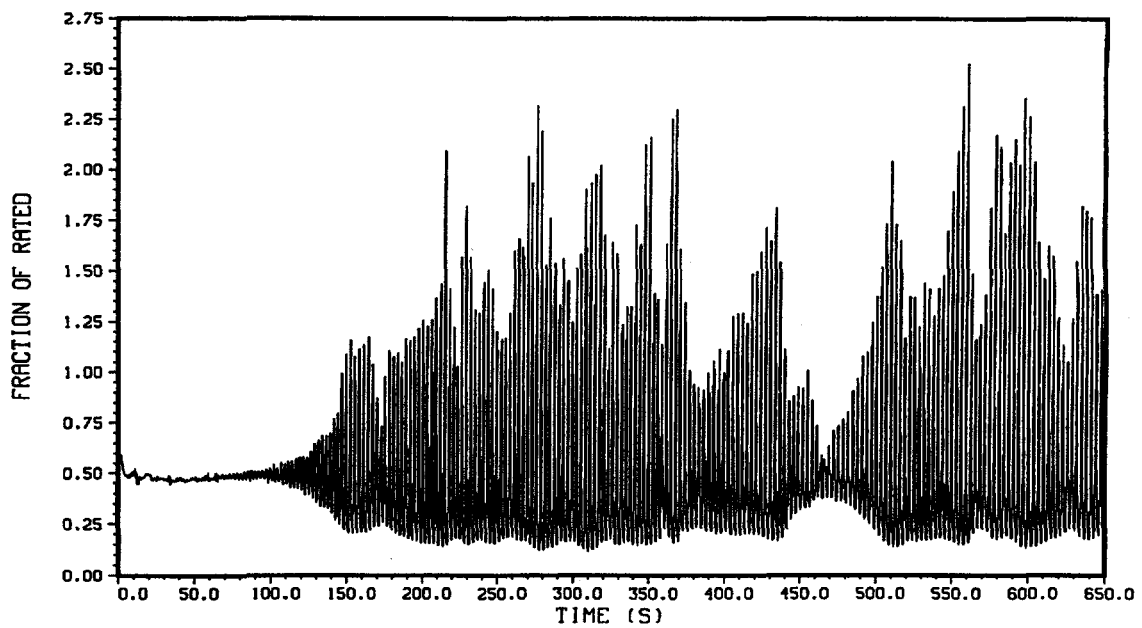


Figure 4.18 Reactor Power Response During the RPT Event as Predicted by the Old separator Model

4.10 Time-Step Control

4.10.1 Introduction

RAMONA-4B controls the time integration step size via an automatic time-step control algorithm based on accuracy criteria. There are three automatic time-step control algorithms in the code for four different dynamic processes: (1) neutron kinetics and thermal conduction, (2) hydraulics, and (3) steam line dynamics. Each one controls its time step in its own way, but they are synchronized to match in time at each time point during integration.

The 3D neutron kinetics and transient thermal conduction use fully implicit integration scheme which is unconditionally stable, hence the time-step control is based on input accuracy criteria.

The other dynamic processes employ explicit integration methods which must be monitored against numerical instability. The remedy is to apply the stability limit estimated from the maximum eigenvalue of the Jacobian matrix of the state equations being solved by explicit Euler integration. This is done by subroutine LAMBDA.

The steam line dynamics solves the local momentum equations using the RK4-Simpson integration method. It is prone to the instability problem because of the stiffness of the local momentum equations and the very lenient accuracy criteria being used. The time-step control algorithm used does not check against the stability limit. The problem is further complicated by the need for synchronizing with the vessel hydraulic integration. We have remedied the problems by using fixed time steps determined by dividing the hydraulic time steps into 5 substeps. In this way, the steam line dynamic integration is always synchronized with the hydraulic integration.

The time step control algorithms used by RAMONA-4B are documented in the earlier report [NUREG/CR-3664, 1984]. In this section, we describe the theory and implementation of the stability limit in the subroutine LAMBDA which has recently been restored in RAMONA-4B for the drift-flux two-phase flow model.

4.10.2 Stability Limit of Explicit Euler Integration

The hydraulic state equations in RAMONA-4B are of the form:

$$\frac{dy}{dt} = f(x, y, t) \quad (4.10.1)$$

where f is a general function, x is the spatial variable, y is the state variable, and t is time.

The explicit Euler integration of Eq. (4.10.1) over a time interval from t_n to $t_n + \Delta t_n$ yields the following time-advancing algorithm:

$$y_k^n = y_k^{n-1} + \Delta t_n f_k^{n-1}, \quad (4.10.2)$$

where the superscript n stands for time step index and the subscript k represents spatial index.

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The explicit Euler integration in Eq. (4.10.2) is stable if the time step size Δt_n is less than the stability limit [Gear, 1971]:

$$(\Delta t_n)_{\max} \leq \frac{2}{|\lambda_{\max}|} \quad (4.10.3)$$

where $\lambda_{\max}$ is the largest eigenvalue of the Jacobian matrix of the state equation (4.10.1); namely

$$\left[\frac{\partial f_i}{\partial y_j} \right] = \begin{bmatrix} \frac{\partial f_1}{\partial y_1} & \frac{\partial f_1}{\partial y_2} & \dots & \frac{\partial f_1}{\partial y_n} \\ \frac{\partial f_2}{\partial y_1} & \frac{\partial f_2}{\partial y_2} & \dots & \frac{\partial f_2}{\partial y_n} \\ \vdots & & & \vdots \\ \frac{\partial f_n}{\partial y_1} & \frac{\partial f_n}{\partial y_2} & \dots & \frac{\partial f_n}{\partial y_n} \end{bmatrix} \quad (4.10.4)$$

with i and j ranging from 1 to $k_{\max}$, the maximum value of the k index.

4.10.3 Equivalence Between Drift-Flux and Slip Model

The original LAMBDA subroutine was written for the slip model. In order to make use of the existing coding of LAMBDA, it is necessary to derive the equivalence between the drift-flux and slip model for two-phase flow.

The drift-flux model in RAMONA-4B correlates the phasic mass flow rates in terms of drift-flux parameters as follows:

$$w_{g,k} = \rho_g (C_{0,k} \alpha_k W_k + A_k \alpha_k V_{gj,k}), \quad (4.10.5)$$

and

$$w_{l,k} = \rho_{l,k} [(1 - C_{0,k} \alpha_k) W_k - A_k \alpha_k V_{gj,k}], \quad (4.10.6)$$

where

- $w_{g,k}$ = vapor mass flow rate at exit of cell k,
- $w_{l,k}$ = liquid mass flow rate at exit of cell k,
- α_k = average void fraction in cell k,
- $\rho_{l,k}$ = average liquid density in cell k,
- ρ_g = average vapor density independent of k,
- $C_{0,k}$ = void distribution parameter in cell k,
- $V_{gj,k}$ = drift velocity in cell k,

W_k = mixture volumetric flow rate at exit of cell k,
 A_k = flow area of cell k.

The slip model in the earlier version of the code correlates the phasic mass flow rates in terms of a slip ratio S_k :

$$V_{g,k} = S_k V_{l,k} + V_k^0, \quad (4.10.7)$$

$$w_{g,k} = \alpha_k \rho_g A_k (S_k V_{l,k} + V_k^0), \quad (4.10.8)$$

$$w_{l,k} = (1 - \alpha_k) \rho_l V_{l,k} A_k, \quad (4.10.9)$$

where $V_{g,k}$ is the vapor velocity at exit of cell k, $V_{l,k}$ is liquid velocity at exit of cell k, V_k^0 is the average bubble rise velocity in a stationary fluid, and A_k is the flow area of cell k.

Since the liquid mass flow rate can also be expressed as

$$w_{l,k} = \rho_l A_k \left(W_k - \frac{w_{g,k}}{\rho_g} \right), \quad (4.10.10)$$

we can derive the liquid velocity by equating Eqs. (4.10.9) and (4.10.10):

$$V_{l,k} = \frac{\alpha_k}{1 + (S_k - 1) \alpha_k} \left(\frac{W_k}{\alpha_k A_k} - V_k^0 \right). \quad (4.10.11)$$

Substituting the above equation into Eq. (4.10.8), we obtain for the slip model

$$w_{g,k} = \rho_g \left\{ \frac{\alpha_k S_k}{1 + (S_k - 1) \alpha_k} W_k + A_k V_k^0 \frac{\alpha_k (1 - \alpha_k)}{1 + (S_k - 1) \alpha_k} \right\}. \quad (4.10.12)$$

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Comparing Eq. (4.10.5) with Eq. (4.10.12), we obtain the following equivalence relations:

$$C_{0,k} = \frac{S_k}{1 + (S_k - 1) \alpha_k}, \quad (4.10.13)$$

and

$$V_{gj,k} = \frac{(1 - \alpha_k) V_k^0}{1 + (S_k - 1) \alpha_k}. \quad (4.10.14)$$

Solving the above two equations for S_k and V_k^0 , we get the alternative equivalence relations:

$$S_k = \frac{C_{0,k}(1 - \alpha_k)}{1 - C_{0,k} \alpha_k}, \quad (4.10.15)$$

and

$$V_k^0 = \frac{V_{gj,k}}{1 - C_{0,k} \alpha_k}. \quad (4.10.16)$$

The LAMBDA subroutine also uses the function,

$$AAUX(k) = \frac{1}{1 + (S_k - 1) \alpha_k}, \quad (4.10.17)$$

which can be expressed in terms of the drift-flux parameter $C_{0,k}$:

$$AAUX(k) = \frac{1 - C_{0,k} \alpha_k}{1 - \alpha_k}. \quad (4.10.18)$$

4.10.4 Jacobian Matrix of Hydraulic State Equations

4.10.4.1 Jacobian Matrix of Loop Momentum Equations

The loop momentum equations in the RAMONA-4B code are given by

$$\frac{dI_j}{dt} = D_j - F_j - L_j; \quad j=1,2,3,\dots \quad (4.10.19)$$

where the subscript j is the loop index, I_j is the loop momentum of loop j , D_j is driving potential of loop j , F_j is friction loss of loop j , and L_j is form loss of loop j , all of which are integrated over the entire loop space.

The driving head of loop j is given by the equation,

$$D_j = \sum_{k=1}^{k=k_{\max}} \left[\left((1 - \alpha_k) \rho_{l,k} + \alpha_k \rho_g \right) g_k \Delta Z_k + \delta_{np,k} \Delta P_{\text{pump}} \right], \quad (4.10.20)$$

the friction loss of loop j is given by

$$F_j = \sum_{k=1}^{k=k_{\max}} f_k R_k \frac{w_k |w_k| \Delta Z_k}{2 \rho_{l,k} A_k^2 D_{h,k}}, \quad (4.10.21)$$

and the form loss of loop j is given by

$$L_j = \sum_{k=1}^{k=k_{\max}} \left(\left[\frac{K_k \phi_{l,k}^2 w_k |w_k|}{2 \rho_{l,k} A_{k,\min}^2} \right]_{\text{inlet}} + \left[\frac{K_k \phi_{l,k}^2 w_k |w_k|}{2 \rho_{l,k} A_{k,\min}^2} \right]_{\text{exit}} \right), \quad (4.10.22)$$

where

- α_k = average void fraction in cell k ,
- $\rho_{l,k}$ = average liquid density in cell k ,
- ρ_g = average vapor density independent of k ,
- ΔZ_k = spacing of cell k ,
- Δp_{pump} = pump head,
- g_k = gravitational constant,
- f_k = single-phase friction factor in cell k ,
- R_k = two-phase multiplier for friction in cell k ,
- w_k = mixture mass flow rate at exit of cell k ,
- A_k = flow area of cell k ,

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- $A_{k,min}$ = minimum flow area at inlet or exit of cell k ,
 $D_{h,k}$ = hydraulic diameter of cell k ,
 K_k = loss coefficient at inlet or exit of cell k ,
 $\phi_{l,k}^2$ = two-phase multiplier for form loss in cell k ,
 $\delta_{np,k}$ = Kronecker delta function (=1 if $k=np$, =0 otherwise).

Let the right-hand side of the loop momentum equation (4.10.19) be the function:

$$f_I = D_j - F_j - L_j \quad (4.10.23)$$

then the Jacobian is simply

$$\frac{\partial f_I}{\partial I_j} = \frac{\partial D_j}{\partial I_j} - \frac{\partial F_j}{\partial I_j} - \frac{\partial L_j}{\partial I_j} \quad (4.10.24)$$

Since D_j is independent of I_j , the Jacobian for D_j is zero. The Jacobian for friction loss F_j is given by the equation,

$$\frac{\partial F_j}{\partial I_j} = \sum_{k=1}^{k=k_{max}} \left(\frac{f_k R_k w_k |w_k| \Delta Z_k}{2 \rho_{l,k} A_k^2 D_{h,k}} \right) \left[\left(\frac{1}{f_k} \frac{\partial f_k}{\partial I_j} \right) + \left(\frac{1}{R_k} \frac{\partial R_k}{\partial I_j} \right) + \frac{1}{w_k} \left(\frac{\partial w_k}{\partial I_j} \right) + \frac{1}{|w_k|} \left(\frac{\partial |w_k|}{\partial I_j} \right) \right] \quad (4.10.25)$$

The Jacobian for form loss L_j is given by

$$\begin{aligned} \frac{\partial L_j}{\partial I_j} = & \sum_{k=1}^{k=k_{max}} \left(\frac{K_k w_k |w_k|}{2 \rho_{l,k} A_{k,min}^2} \right) \left[\frac{\partial \phi_{l,k}^2}{\partial I_j} + \frac{\phi_{l,k}^2}{w_k} \left(\frac{\partial w_k}{\partial I_j} \right) + \frac{\phi_{l,k}^2}{|w_k|} \left(\frac{\partial |w_k|}{\partial I_j} \right) \right]_{inlet} \\ & + \sum_{k=1}^{k=k_{max}} \left(\frac{K_k w_k |w_k|}{2 \rho_{l,k} A_{k,min}^2} \right) \left[\frac{\partial \phi_{l,k}^2}{\partial I_j} + \frac{\phi_{l,k}^2}{w_k} \left(\frac{\partial w_k}{\partial I_j} \right) + \frac{\phi_{l,k}^2}{|w_k|} \left(\frac{\partial |w_k|}{\partial I_j} \right) \right]_{exit} \end{aligned} \quad (4.10.26)$$

In order to evaluate the partial derivatives on the right-hand side of Eqs. (4.10.25) and (4.10.26), we use the drift-flux formulations for various flow rates. First, the mixture mass flow rate w_k is the sum of vapor mass flow rate $w_{g,k}$ and liquid mass flow rate $w_{l,k}$:

$$w_k = w_{g,k} + w_{l,k} \quad (4.10.27)$$

where $w_{g,k}$ is given by Eq. (4.10.5) and $w_{\ell,k}$ is given by Eq. (4.10.6) or (4.10.10).

The volumetric flow rate W_k is calculated as follows:

$$W_k = W_{di} + \sum_{i=1}^{i=k} B_i \quad \text{for downcomer, lower plenum,} \quad (4.10.28)$$

$$W_k = W_{ri} + \sum_{i=ri}^{i=k} B_i \quad \text{for riser,} \quad (4.10.29)$$

$$W_{k,j} = W_{ci,j} + \sum_{i=ci}^{i=k} B_{i,j} \quad \text{for core channel } j, \quad (4.10.30)$$

where W_{di} is the volumetric flow rate at downcomer entrance given by

$$W_{di} = \mu \left(\sum_{j=1}^{j=j_{\max}} \beta_j - \Sigma_d \right), \quad (4.10.31)$$

$W_{ci,j}$ is the volumetric flow rate at core inlet of channel j given by

$$W_{ci,j} = \beta_j - (a_0 + a_r) \frac{W_{di}}{a_j}, \quad (4.10.32)$$

and W_{ri} is the volumetric flow rate at the riser inlet given by

$$W_{ri} = W_{di} + \Sigma_d + \Sigma_c. \quad (4.10.33)$$

The variables on the right-hand side of the above equations are defined as follows:

$$\mu = \frac{1}{1 + (a_0 + a_r) \sum_{j=1}^{j=j_{\max}} \frac{1}{a_j}}, \quad (4.10.34)$$

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$$\beta_j = \left(\frac{1}{a_j} \right) \left(I_j - [b_j + b_0 + b_r + a_r(\Sigma_d + \Sigma_c)] \right), \quad (4.10.35)$$

$$a_0 = \sum_{k=di}^{k=tpo} C_k \Delta Z_k, \quad (4.10.36)$$

$$a_r = \sum_{k=ri}^{k=ro} C_k \Delta Z_k, \quad (4.10.37)$$

$$a_j = \sum_{k=ci}^{k=co} C_{k,j} \Delta Z_k, \quad (4.10.38)$$

$$b_0 = \sum_{k=di}^{k=tpo} \left[C_k \sum_{i=di}^{i=k} B_i + \delta_{n0,k} (W_{fw} + W_{ECC}) - V_k^0 D_k \right] \Delta Z_k, \quad (4.10.39)$$

$$b_r = \sum_{k=ri}^{k=ro} \left(C_k \sum_{i=ri}^{i=k} B_i - V_k^0 D_k \right) \Delta Z_k, \quad (4.10.40)$$

$$b_j = \sum_{k=ci}^{k=co} \left(C_k \sum_{i=ci}^{i=k} B_{i,j} - V_{k,j}^0 D_{k,j} \right) \Delta Z_k, \quad (4.10.41)$$

$$B_i = \left(\frac{dp_s}{dt} \right) \phi_i + \gamma_i \psi_i + \delta_{n0,i} (W_{fw} + W_{ECC}), \quad (4.10.42)$$

$$B_{i,j} = \left(\frac{dp_s}{dt} \right) \phi_{i,j} + \gamma_{i,j} \psi_{i,j}, \quad (4.10.43)$$

$$C_k = \left(\frac{1}{A_k} \right) [\rho_{l,k} - (\rho_{l,k} - \rho_g) C_{0,k} \alpha_k], \quad (4.10.44)$$

$$D_k = \alpha_k (\rho_{l,k} - \rho_g) (1 - C_{0,k} \alpha_k), \quad (4.10.45)$$

$$\Sigma_d = \sum_{i=di}^{i=tpo} B_i + \delta_{n0,i} (W_{fw} + W_{ECC}), \quad (4.10.46)$$

$$\Sigma_c = \sum_{j=1}^{j=j_{\max}} \sum_{i=ci}^{i=co} B_{i,j}. \quad (4.10.47)$$

In the above equations, p_s is system pressure, ϕ_i is compressibility in cell i , W_{fw} is volumetric feedwater flow rate, W_{ECC} is the volumetric Emergency Core Cooling (ECC) flow rate, ψ_i is the vapor generation rate in cell i , γ_i is given by

$$\gamma_i = \frac{1}{\rho_g} - \frac{1}{\rho_{l,i}}, \quad (4.10.48)$$

$C_{0,k}$ is the void distribution parameter in cell k , $V_{gi,k}$ is the drift velocity in cell k , and V_k^0 is the average bubble rise velocity in a stationary fluid given by Eq. (4.10.16).

The partial derivatives can now be evaluated and the results are given below:

$$\begin{aligned} \frac{\partial W_k}{\partial I_j} &= \frac{\partial W_{di}}{\partial I_j} && \text{for downcomer, lower plenum, riser,} \\ &= \frac{\partial W_{ci,i}}{\partial I_j} && \text{for core channel } i, \end{aligned} \quad (4.10.49)$$

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$$\frac{\partial W_{di}}{\partial I_j} = \mu \left(\frac{1}{a_j} \right), \quad (4.10.50)$$

$$\begin{aligned} \frac{\partial W_{ci,i}}{\partial I_j} &= \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i=j \text{ (diagonal element),} \\ &= - \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i \neq j \text{ (off-diagonal element).} \end{aligned} \quad (4.10.51)$$

$$\frac{\partial w_k}{\partial I_j} = \frac{\partial w_{g,k}}{\partial I_j} + \frac{\partial w_{l,k}}{\partial I_j}, \quad (4.10.52)$$

$$\begin{aligned} \frac{\partial w_{g,k}}{\partial I_j} &= \frac{\rho_g C_{0,k} \alpha_k}{a_j} \mu = \frac{m_{g,k} S_k AAUX(k)}{\Delta V_k} \left(\frac{\mu}{a_j} \right) \quad \text{for downcomer, lower plenum, riser,} \\ &= \frac{\rho_g C_{0,k} \alpha_k}{a_j} \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] = \frac{m_{g,k} S_k AAUX(k)}{\Delta V_k} \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for channel } j, \end{aligned} \quad (4.10.53)$$

$$\begin{aligned} \frac{\partial w_{l,k}}{\partial I_j} &= \frac{\rho_{l,k} (1 - C_{0,k} \alpha_k)}{a_j} \mu = \frac{m_{l,k} AAUX(k)}{\Delta V_k} \left(\frac{\mu}{a_j} \right) \quad \text{for downcomer, lower plenum, riser,} \\ &= \frac{\rho_{l,k} (1 - C_{0,k} \alpha_k)}{a_j} \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] = \frac{m_{l,k} AAUX(k)}{\Delta V_k} \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for channel } j. \end{aligned} \quad (4.10.54)$$

Thus, for downcomer, lower plenum, and riser:

$$\begin{aligned} \frac{\partial |w_k|}{\partial I_j} &= \left| \left[\rho_g C_{0,k} \alpha_k + \rho_{l,k} (1 - C_{0,k} \alpha_k) \right] \left(\frac{\mu}{a_j} \right) \right|, \\ &= \left| \frac{(m_{l,k} + m_{g,k} S_k) AAUX(k)}{\Delta V_k} \left(\frac{\mu}{a_j} \right) \right|, \end{aligned} \quad (4.10.56)$$

For core channels:

$$\begin{aligned}\frac{\partial w_k}{\partial I_j} &= [\rho_g C_{0,k} \alpha_k + \rho_{l,k} (1 - C_{0,k} \alpha_k)] \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i=j, \\ &= -[\rho_g C_{0,k} \alpha_k + \rho_{l,k} (1 - C_{0,k} \alpha_k)] \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i \neq j,\end{aligned}\quad (4.10.57)$$

or

$$\begin{aligned}\frac{\partial w_k}{\partial I_j} &= \frac{(m_{l,k} + m_{g,k} S_k) AAUX(k)}{\Delta V_k} \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i=j, \\ &= - \left[\frac{(m_{l,k} + m_{g,k} S_k) AAUX(k)}{\Delta V_k} \right] \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \quad \text{for } i \neq j,\end{aligned}\quad (4.10.58)$$

and

$$\begin{aligned}\frac{\partial |w_k|}{\partial I_j} &= \left| [\rho_g C_{0,k} \alpha_k + \rho_{l,k} (1 - C_{0,k} \alpha_k)] \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \right| \quad \text{for } i=j, \\ &= \left| -[\rho_g C_{0,k} \alpha_k + \rho_{l,k} (1 - C_{0,k} \alpha_k)] \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \right| \quad \text{for } i \neq j,\end{aligned}\quad (4.10.59)$$

or

$$\begin{aligned}\frac{\partial |w_k|}{\partial I_j} &= \left| \frac{(m_{l,k} + m_{g,k} S_k) AAUX(k)}{\Delta V_k} \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right] \right| \quad \text{for } i=j, \\ &= \left| - \left[\frac{(m_{l,k} + m_{g,k} S_k) AAUX(k)}{\Delta V_k} \right] \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \right| \quad \text{for } i \neq j.\end{aligned}\quad (4.10.60)$$

The single-phase friction factor used in RAMONA-4B is given by

$$f_k = G_1 N_{Re}^{-G2}, \quad (4.10.61)$$

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where N_{Re} is the Reynolds number defined as

$$N_{Re} = \frac{w_k D_{h,k}}{A_k \mu_f}, \quad (4.10.62)$$

with μ_f being the dynamic viscosity.

The partial derivative for f_k is then given by

$$\frac{1}{f_k} \frac{\partial f_k}{\partial I_j} = -G_2 \left(\frac{1}{w_k} \frac{\partial w_k}{\partial I_j} \right). \quad (4.10.63)$$

Using Eqs. (4.10.55) and (4.10.58), we obtain

$$\begin{aligned} \frac{1}{f_k} \frac{\partial f_k}{\partial I_j} &= - \left[\frac{G_2 (m_{l,k} + m_{g,k} S_k) AAUX(k)}{w_k \Delta V_k} \right] \left(\frac{\mu}{a_j} \right) \text{ for downcomer, lower plenum, riser,} \\ &= - \left[\frac{G_2 (m_{l,k} + m_{g,k} S_k) AAUX(k)}{w_k \Delta V_k} \right] \left(\frac{1 - \frac{\mu}{a_j} (a_0 + a_r)}{a_j} \right) \text{ for core channel } i=j, \\ &= + \left[\frac{G_2 (m_{l,k} + m_{g,k} S_k) AAUX(k)}{w_k \Delta V_k} \right] \left(\frac{1}{a_j} \right) \left[\frac{\mu}{a_j} (a_0 + a_r) \right] \text{ for core channel } i \neq j. \end{aligned} \quad (4.10.64)$$

The two-phase multiplier for friction in RAMONA-4B is the formula,

$$R_k = 1 + A_F \left(\frac{x_k}{p_s} \right)^{0.96}, \quad (4.10.65)$$

where x_k is the flow quality defined as

$$x_k = \frac{w_{g,k}}{w_{g,k} + w_{l,k}}. \quad (4.10.66)$$

The partial derivative of x_k with respect to I_j is then given by the equation,

$$\frac{\partial x_k}{\partial I_j} = \frac{(1 - x_k)}{w_k} \left(\frac{\partial w_{g,k}}{\partial I_j} \right) - \frac{x_k}{w_k} \left(\frac{\partial w_{l,k}}{\partial I_j} \right). \quad (4.10.67)$$

Thus, using Eqs. (4.10.53) and (4.10.54), we get for downcomer, lower plenum, and riser:

$$\begin{aligned} \frac{\partial x_k}{\partial I_j} &= [\rho_g C_{0,k} \alpha_k (1 - x_k) - \rho_{l,k} x_k (1 - C_{0,k} \alpha_k)] \left(\frac{\mu}{a_j w_k} \right), \\ &= \left(\frac{AAUX(k)}{w_k \Delta V_k} \right) [m_{g,k} S_k (1 - x_k) - m_{l,k} x_k] \left(\frac{\mu}{a_j} \right), \end{aligned} \quad (4.10.68)$$

$$\frac{\partial R_k}{\partial I_j} = - \frac{0.96 (R_k - 1)}{w_k \Delta V_k} \left[m_{l,k} + m_{g,k} S_k \left(1 - \frac{1}{x_k} \right) \right] AAUX(k) \left(\frac{\mu}{a_j} \right). \quad (4.10.69)$$

For diagonal elements of core channels ($i=j$):

$$\begin{aligned} \frac{\partial x_k}{\partial I_j} &= [\rho_g C_{0,k} \alpha_k (1 - x_k) - \rho_{l,k} x_k (1 - C_{0,k} \alpha_k)] \frac{\left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right]}{a_j w_k}, \\ &= \left(\frac{AAUX(k)}{w_k \Delta V_k} \right) [m_{g,k} S_k (1 - x_k) - m_{l,k} x_k] \frac{\left[1 - \frac{\mu}{a_j} (a_0 + a_r) \right]}{a_j}, \end{aligned} \quad (4.10.70)$$

For off-diagonal elements of core channels ($i \neq j$):

$$\begin{aligned} \frac{\partial x_k}{\partial I_j} &= - [\rho_g C_{0,k} \alpha_k (1 - x_k) - \rho_{l,k} x_k (1 - C_{0,k} \alpha_k)] \frac{\left[\frac{\mu}{a_j} (a_0 + a_r) \right]}{a_j w_k}, \\ &= - \left(\frac{AAUX(k)}{w_k \Delta V_k} \right) [m_{g,k} S_k (1 - x_k) - m_{l,k} x_k] \frac{\left[\frac{\mu}{a_j} (a_0 + a_r) \right]}{a_j}. \end{aligned} \quad (4.10.71)$$

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Thus, we have

$$\begin{aligned} \frac{\partial R_k}{\partial I_j} = & - \frac{0.96(R_k - 1)}{w_k \Delta V_k} \left[m_{l,k} + m_{g,k} S_k \left(1 - \frac{1}{x_k} \right) \right] AAUX(k) \left[\frac{1 - \frac{\mu}{a_j}(a_0 + a_r)}{a_j} \right] \text{ for } i=j, \\ & + \frac{0.96(R_k - 1)}{w_k \Delta V_k} \left[m_{l,k} + m_{g,k} S_k \left(1 - \frac{1}{x_k} \right) \right] AAUX(k) \left[\frac{\frac{\mu}{a_j}(a_0 + a_r)}{a_j} \right] \text{ for } i \neq j. \end{aligned} \quad (4.10.72)$$

$$\begin{aligned} \frac{\partial F_j}{\partial I_j} = & \sum_{k=1}^{k=k_{\max}} \left(\frac{f_k AAUX(k) \xi_j}{2 \rho_{l,k} A_k^3 D_{h,k}} \right) [(w_k + |w_k|) R_k - 0.96(R_k - 1) |w_k|] \left[m_{l,k} + m_{g,k} S_k \left(1 - \frac{1}{x_k} \right) \right] \\ & - \sum_{k=1}^{k=k_{\max}} \left(\frac{f_k AAUX(k) \xi_j}{2 \rho_{l,k} A_k^3 D_{h,k}} \right) G_2 |w_k| (m_{l,k} + m_{g,k} S_k), \end{aligned} \quad (4.10.73)$$

where

$$\begin{aligned} \xi_j = & \left(\frac{\mu}{a_j} \right) \text{ for downcomer, lower plenum, riser,} \\ = & \left(\frac{1}{a_j} \right) \left[1 - \frac{\mu}{a_j}(a_0 + a_r) \right] \text{ for core channel } i=j, \\ = & \left(\frac{1}{a_j} \right) \left[- \frac{\mu}{a_j}(a_0 + a_r) \right] \text{ for core channel } i \neq j. \end{aligned} \quad (4.10.74)$$

The two-phase multiplier for form loss in RAMONA-4B is the correlation,

$$\Phi_{l,k}^2 = 1 + \beta x_k \left(\frac{\rho_{l,k}}{\rho_g} - 1 \right), \quad (4.10.75)$$

where β is a correlation constant (typically, 1.2).

The partial derivative of the two-phase multiplier (4.10.75) can be obtained by taking derivative with respect to the loop momentum I_j . The results are the following:

$$\frac{\partial \phi_{l,k}^2}{\partial I_j} = \beta \left(\frac{\rho_{l,k}}{\rho_g} - 1 \right) \left[\rho_g C_{0,k} \alpha_k (1 - x_k) - \rho_{l,k} x_k (1 - C_{0,k} \alpha_k) \right] \left(\frac{\xi_j}{w_k} \right), \quad (4.10.76)$$

or

$$\frac{\partial \phi_{l,k}^2}{\partial I_j} = \beta \left(\frac{\rho_{l,k}}{\rho_g} - 1 \right) \left(\frac{AAUX(k)}{\Delta V_k} \right) \left[m_{g,k} S_k (1 - x_k) - m_{l,k} x_k \right] \left(\frac{\xi_j}{w_k} \right), \quad (4.10.77)$$

where ξ_j is given by Eq. (4.10.74).

Thus, the partial derivative of form loss becomes

$$\begin{aligned} \frac{\partial L_j}{\partial I_j} = & \sum_{k=1}^{k=k_{\max}} \left(\frac{K_k AAUX(k) \xi_j}{2 \rho_{l,k} A_{k,\min}^2 \Delta V_k} \right) \beta \left(\frac{\rho_{l,k}}{\rho_g} - 1 \right) \left[m_{g,k} S_k (1 - x_k) - m_{l,k} x_k \right] |w_k| \\ & + \sum_{k=1}^{k=k_{\max}} \left(\frac{K_k AAUX(k) \xi_j}{2 \rho_{l,k} A_{k,\min}^2 \Delta V_k} \right) \phi_{l,k}^2 (w_k + |w_k|) (m_{l,k} + m_{g,k} S_k). \end{aligned} \quad (4.10.78)$$

The loop momentum Jacobian is then given by

$$\frac{\partial f_I}{\partial I_j} = - \frac{\partial F_j}{\partial I_j} - \frac{\partial L_j}{\partial I_j}. \quad (4.10.79)$$

4.10.4.2 Jacobian Matrix of Vapor Mass Balance Equations

The vapor mass balance equations in RAMONA-4B are given by

$$\frac{dm_{g,k}}{dt} = w_{g,k-1} - w_{g,k} + \psi_k, \quad (4.10.80)$$

where the subscript k is spatial index, $m_{g,k}$ is the vapor mass in cell k , $w_{g,k-1}$ is vapor mass flow rate at entrance of cell k , $w_{g,k}$ is vapor mass flow rate at exit of cell k , and ψ_k is vapor generation rate in cell k .

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Let the right-hand side of Eq. (80) be the function:

$$f_{M,k} = w_{g,k-1} - w_{g,k} + \psi_k. \quad (4.10.81)$$

The Jacobian of vapor mass is given by

$$\frac{\partial f_{M,k}}{\partial m_{g,k}} = \frac{\partial w_{g,k-1}}{\partial m_{g,k}} - \frac{\partial w_{g,k}}{\partial m_{g,k}} + \frac{\partial \psi_k}{\partial m_{g,k}}. \quad (4.10.82)$$

The first two terms on RHS of the above equation can be evaluated from Eq. (4.10.5), and the last term requires an expression for the vapor generation rate:

$$\psi_k = \psi_{B,k} + \psi_{S,k}, \quad (4.10.83)$$

where $\psi_{B,k}$ is the vapor generation due to bulk boiling and $\psi_{S,k}$ is that due to wall heating.

The bulk boiling vapor generation rate is given by the correlation

$$\psi_{B,k} = \frac{\Delta V_k}{h_{fg}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] [(T_{l,k} - T_s) + C_{13} |T_{l,k} - T_s|], \quad (4.10.84)$$

where h_{fg} is latent heat of evaporation, α_k is void fraction in cell k, $T_{l,k}$ is the liquid temperature in cell k, T_s is the saturation temperature, and C_{11} , C_{12} , C_{13} are correlation constants.

The vapor generation rate due to wall heating is prescribed by the expression

$$\psi_{S,k} = \frac{q_{w,k}}{h_{fg} + C_{p,k}(T_s - T_{l,k}) \frac{\rho_{l,k}}{\rho_g} + \frac{1}{2} C_{p,k}(T_{w,k} - T_s) \gamma_k \rho_{l,k}}, \quad (4.10.85)$$

where $q_{w,k}$ is the wall heating rate, $C_{p,k}$ is specific heat at constant pressure, $T_{w,k}$ is clad wall temperature in cell k, and γ_k is defined by Eq. (4.10.48).

Using the definition of void fraction,

$$\alpha_k = \frac{m_{g,k}}{\rho_g \Delta V_k}, \quad (4.10.86)$$

we can express Eq. (4.10.5) as follows:

$$w_{g,k} = \frac{m_{g,k}}{\Delta V_k} (C_{0,k} W_k + A_k V_{gj,k}), \quad (4.10.87)$$

then its partial derivative with respect to $m_{g,k}$ is

$$\begin{aligned} \frac{\partial w_{g,k}}{\partial m_{g,k}} = & \left(\frac{1}{\Delta V_k} \right) (C_{0,k} W_k + A_k V_{gj,k}) \\ & + \left(\frac{m_{g,k}}{\Delta V_k} \right) \left[W_k \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) + C_{0,k} \left(\frac{\partial W_k}{\partial m_{g,k}} \right) + A_k \left(\frac{\partial V_{gj,k}}{\partial m_{g,k}} \right) \right]. \end{aligned} \quad (4.10.88)$$

Note that

$$\frac{\partial w_{g,k-1}}{\partial m_{g,k}} = 0. \quad (4.10.89)$$

The drift-flux parameters are given by the following correlations [Rohatgi, 1994]:

$$\begin{aligned} C_{0,k} = & \left(1.2 - 0.2 \sqrt{\frac{\rho_g}{\rho_{l,k}}} \right) (1 - e^{-18 \alpha_k}) \quad \text{for churn-turbulent flow,} \\ = & 1 + \frac{1 - \alpha_k}{\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}}} \quad \text{for annular flow,} \end{aligned} \quad (4.10.90)$$

and

$$\begin{aligned} V_{gj,k} = & 1.54 \left[\frac{(\rho_{l,k} - \rho_g) g \sigma}{\rho_{l,k}^2} \right]^{0.25} \quad \text{for churn-turbulent flow,} \\ = & \frac{(1 - \alpha_k) \sqrt{\frac{g D_{h,k} (\rho_{l,k} - \rho_g) (1 - \alpha_k)}{0.015 \rho_{l,k}}}}{\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}}} \quad \text{for annular flow.} \end{aligned} \quad (4.10.91)$$

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The partial derivatives of the drift-flux parameters are then

$$\begin{aligned} \frac{\partial C_{0,k}}{\partial m_{g,k}} &= \left(\frac{18}{\rho_g \Delta V_k} \right) \left(1.2 - 0.2 \sqrt{\frac{\rho_g}{\rho_{l,k}}} \right) e^{-18 \alpha_k} \quad \text{for churn-turbulent flow,} \\ &= - \left[\frac{1}{\rho_g \Delta V_k \left(\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}} \right)} \right] \left(1 + \frac{1 - \alpha_k}{\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}}} \right) \quad \text{for annular flow,} \end{aligned} \quad (4.10.92)$$

and

$$\begin{aligned} \frac{\partial V_{gj,k}}{\partial m_{g,k}} &= 0.0 \quad \text{for churn-turbulent flow,} \\ &= - \left(\frac{1}{\rho_g \Delta V_k} \right) \frac{\sqrt{\frac{g D_{h,k} (\rho_{l,k} - \rho_g) (1 - \alpha_k)}{0.015 \rho_{l,k}}}}{\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}}} \left[1.5 + \frac{1 - \alpha_k}{\alpha_k + 4 \sqrt{\frac{\rho_g}{\rho_{l,k}}}} \right] \quad \text{for annular flow} \end{aligned} \quad (4.10.93)$$

The partial derivative of W_k with respect to vapor mass can be evaluated from Eqs. (4.10.28), (4.10.29) and (4.10.30). The results are given below:

$$\begin{aligned} \frac{\partial W_k}{\partial m_{g,k}} &= \frac{\partial W_{di}}{\partial m_{g,k}} + \frac{\partial B_k}{\partial m_{g,k}} \quad \text{for downcomer, lower plenum,} \\ &= \frac{\partial W_{ri}}{\partial m_{g,k}} + \frac{\partial B_k}{\partial m_{g,k}} \quad \text{for riser,} \\ &= \frac{\partial W_{ci,j}}{\partial m_{g,k}} + \frac{\partial B_{k,j}}{\partial m_{g,k}} \quad \text{for core channel } j, \end{aligned} \quad (4.10.94)$$

where

$$\begin{aligned} \frac{\partial B_k}{\partial m_{g,k}} &= \gamma_k \left(\frac{\partial \psi_k}{\partial m_{g,k}} \right) \quad \text{for downcomer, lower plenum, riser,} \\ &= \gamma_{k,j} \left(\frac{\partial \psi_{k,j}}{\partial m_{g,k}} \right) \quad \text{for core channel } j, \end{aligned} \quad (4.10.95)$$

$$\frac{\partial W_{di}}{\partial m_{g,k}} = \left(\sum_{j=1}^{j=j_{\max}} \beta_j - \Sigma_d \right) \left(\frac{\partial \mu}{\partial m_{g,k}} \right) + \mu \left(\sum_{j=1}^{j=j_{\max}} \frac{\partial \beta_j}{\partial m_{g,k}} - \frac{\partial \Sigma_d}{\partial m_{g,k}} \right), \quad (4.10.96)$$

$$\frac{\partial W_{ri}}{m_{g,k}} = \frac{\partial W_{di}}{m_{g,k}} + \frac{\partial \Sigma_d}{m_{g,k}} + \frac{\partial \Sigma_c}{m_{g,k}}, \quad (4.10.97)$$

$$\frac{\partial W_{ci,j}}{\partial m_{g,k}} = \frac{\partial \beta_j}{\partial m_{g,k}} - \frac{W_{di}}{a_j} \left(\frac{\partial a_0}{\partial m_{g,k}} + \frac{\partial a_r}{\partial m_{g,k}} \right) + \frac{(a_0 + a_r)}{a_j} \left[\frac{W_{di}}{a_j} \left(\frac{\partial a_j}{\partial m_{g,k}} \right) + \frac{\partial W_{di}}{\partial m_{g,k}} \right], \quad (4.10.98)$$

$$\frac{\partial C_k}{\partial m_{g,k}} = - \frac{(\rho_{l,k} - \rho_g)}{A_k} \left[\frac{C_{0,k}}{\rho_g \Delta V_k} + \alpha_k \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) \right], \quad (4.10.99)$$

$$\frac{\partial D_k}{\partial m_{g,k}} = (\rho_{l,k} - \rho_g) \left[\frac{1 - 2 C_{0,k}}{\rho_g \Delta V_k} - \alpha_k^2 \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) \right], \quad (4.10.100)$$

$$\frac{\partial \mu}{\partial m_{g,k}} = - \frac{\sum_{j=1}^{j=j_{\max}} \frac{1}{a_j}}{\left[1 + (a_0 + a_r) \sum_{j=1}^{j=j_{\max}} \frac{1}{a_j} \right]^2} \left(\frac{\partial a_0}{\partial m_{g,k}} + \frac{\partial a_r}{\partial m_{g,k}} \right) - (a_0 + a_r) \sum_{j=1}^{j=j_{\max}} \frac{1}{a_j^2} \left(\frac{\partial a_j}{\partial m_{g,k}} \right), \quad (4.10.101)$$

$$\begin{aligned} \frac{\partial \beta_j}{\partial m_{g,k}} = & - \left(\frac{1}{a_j} \right) \left[\frac{\partial b_j}{\partial m_{g,k}} + \frac{\partial b_0}{\partial m_{g,k}} + \frac{\partial b_r}{\partial m_{g,k}} + (\Sigma_d + \Sigma_c) \left(\frac{\partial a_r}{\partial m_{g,k}} \right) + a_r \left(\frac{\partial \Sigma_d}{\partial m_{g,k}} + \frac{\partial \Sigma_c}{\partial m_{g,k}} \right) \right] \\ & - \{ I_j - [b_j + b_0 + b_r + a_r (\Sigma_d + \Sigma_c)] \} \left(\frac{1}{a_j^2} \right) \left(\frac{\partial a_j}{\partial m_{g,k}} \right), \end{aligned} \quad (4.10.102)$$

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$$\begin{aligned} \frac{\partial a_0}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_k}{\partial m_{g,k}} \right) && \text{for } k \in \text{downcomer, lower plenum,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.103)$$

$$\begin{aligned} \frac{\partial a_r}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_k}{\partial m_{g,k}} \right) && \text{for } k \in \text{riser,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.104)$$

$$\begin{aligned} \frac{\partial a_j}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_{k,j}}{\partial m_{g,k}} \right) && \text{for } k \in \text{core channel } j, \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.105)$$

$$\begin{aligned} \frac{\partial b_0}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_k}{\partial m_{g,k}} \sum_{i=di}^{i=k} B_i + C_k \frac{\partial B_k}{\partial m_{g,k}} - V_k^0 \frac{\partial D_k}{\partial m_{g,k}} - D_k \frac{\partial V_k^0}{\partial m_{g,k}} \right) && \text{for } k \in \text{DCLP,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.106)$$

$$\begin{aligned} \frac{\partial b_r}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_k}{\partial m_{g,k}} \sum_{i=ri}^{i=k} B_i + C_k \frac{\partial B_k}{\partial m_{g,k}} - V_k^0 \frac{\partial D_k}{\partial m_{g,k}} - D_k \frac{\partial V_k^0}{\partial m_{g,k}} \right) && \text{for } k \in \text{riser,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.107)$$

$$\begin{aligned} \frac{\partial b_j}{\partial m_{g,k}} &= \Delta Z_k \left(\frac{\partial C_{k,j}}{\partial m_{g,k}} \sum_{i=ci}^{i=k} B_{i,j} + C_{k,j} \frac{\partial B_{k,j}}{\partial m_{g,k}} - V_{k,j}^0 \frac{\partial D_{k,j}}{\partial m_{g,k}} - D_{k,j} \frac{\partial V_{k,j}^0}{\partial m_{g,k}} \right) && \text{for } k \in \text{core,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.108)$$

$$\begin{aligned} \frac{\partial \Sigma_d}{\partial m_{g,k}} &= \frac{\partial B_k}{\partial m_{g,k}} && \text{for } k \in \text{downcomer, lower plenum,} \\ &= 0, && \text{otherwise,} \end{aligned} \quad (4.10.109)$$

$$\begin{aligned} \frac{\partial \Sigma_c}{\partial m_{g,k}} &= \frac{\partial B_{k,j}}{\partial m_{g,k}} && \text{for } k \in \text{core channel } j, \\ &= 0, && \text{otherwise.} \end{aligned} \quad (4.10.110)$$

The partial derivative of $\Psi_{B,k}$ with respect to vapor mass can be obtained from Eq. (4.10.84) using the definition (4.10.86) for α_k :

$$\begin{aligned} \frac{\partial \Psi_{B,k}}{\partial m_{g,k}} &= \frac{C_{12} [(T_{l,k} - T_s) + C_{13} |(T_{l,k} - T_s)|]}{h_{fg} \rho_g} (1 - 2\alpha_k) \\ &+ \frac{\Delta V_k}{h_{fg}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] \left(\frac{\partial T_{l,k}}{\partial m_{g,k}} + C_{13} \frac{\partial |T_{l,k}|}{\partial m_{g,k}} \right). \end{aligned} \quad (4.10.111)$$

The liquid temperature in cell k is calculated from

$$T_{l,k} = T_s^0 + \frac{e_{l,k}}{C_{p,k}}, \quad (4.10.112)$$

where $e_{l,k}$ is the specific internal energy of liquid given by

$$e_{l,k} = \frac{E_k - m_{g,k} e_g}{m_{l,k}}. \quad (4.10.113)$$

The partial derivative of $T_{l,k}$ with respect to vapor mass is then

$$\frac{\partial T_{l,k}}{\partial m_{g,k}} = - \left(\frac{1}{C_{p,k} m_{l,k}} \right) \left[e_g + \frac{e_{l,k}}{m_{l,k}} \left(\frac{\partial m_{l,k}}{\partial m_{g,k}} \right) \right]. \quad (4.10.114)$$

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The liquid mass in cell k is given by

$$m_{l,k} = \left(1 - \frac{m_{g,k}}{\rho_g \Delta V_k} \right) \rho_{l,k} \Delta V_k, \quad (4.10.115)$$

thus, we have

$$\frac{\partial m_{l,k}}{\partial m_{g,k}} = - \frac{\rho_{l,k}}{\rho_g}. \quad (4.10.116)$$

Eq. (114) then becomes

$$\frac{\partial T_{l,k}}{\partial m_{g,k}} = \left(\frac{1}{C_{p,k} m_{l,k}} \right) \left[-e_g + \left(\frac{e_{l,k}}{m_{l,k}} \right) \left(\frac{\rho_{l,k}}{\rho_g} \right) \right]. \quad (4.10.117)$$

Similarly, we obtain

$$\frac{\partial |T_{l,k}|}{\partial m_{g,k}} = \left(\frac{1}{C_{p,k} m_{l,k}} \right) \left| -e_g + \left(\frac{e_{l,k}}{m_{l,k}} \right) \left(\frac{\rho_{l,k}}{\rho_g} \right) \right|. \quad (4.10.118)$$

Thus, Eq. (4.10.111) becomes

$$\begin{aligned} \frac{\partial \Psi_{B,k}}{\partial m_{g,k}} = & \frac{C_{12} [(T_{l,k} - T_s) + C_{13} |T_{l,k} - T_s|]}{h_{fg} \rho_g} (1 - 2\alpha_k) + \frac{\Delta V_k}{h_{fg}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] \\ & \cdot \left(\frac{1}{C_{p,k} m_{l,k}} \right) \left[-e_g + \left(\frac{e_{l,k}}{m_{l,k}} \right) \left(\frac{\rho_{l,k}}{\rho_g} \right) + C_{13} \left| -e_g + \left(\frac{e_{l,k}}{m_{l,k}} \right) \left(\frac{\rho_{l,k}}{\rho_g} \right) \right| \right]. \end{aligned} \quad (4.10.119)$$

Let the denominator of Eq. (4.10.85) be

$$D_{\Psi,k} = h_{fg} + C_{p,k} (T_s - T_{l,k}) \frac{\rho_{l,k}}{\rho_g} + \frac{1}{2} C_{p,k} (T_{w,k} - T_s) \gamma_k \rho_{l,k}, \quad (4.10.120)$$

then the partial derivative of $\psi_{s,k}$ with respect to vapor mass is given by

$$\frac{\partial \psi_{s,k}}{\partial m_{g,k}} = \frac{1}{D_{\psi,k}} \left(\frac{\partial q_{w,k}}{\partial m_{g,k}} \right) - \frac{\psi_{s,k}}{D_{\psi,k}} \left(\frac{\partial D_{\psi,k}}{\partial m_{g,k}} \right). \quad (4.10.121)$$

From Eq. (4.10.120) we get

$$\frac{\partial D_{\psi,k}}{\partial m_{g,k}} = - \frac{C_{p,k} \rho_{l,k}}{\rho_g} \left(\frac{\partial T_{l,k}}{\partial m_{g,k}} \right) + \frac{1}{2} C_{p,k} \gamma_k \rho_{l,k} \left(\frac{\partial T_{w,k}}{\partial m_{g,k}} \right). \quad (4.10.122)$$

The wall heating rate depends on fission power and heat transfer regime. For two-phase heat transfer, the wall heating rate is prescribed by the Jens-Lottes correlation:

$$q_{w,k} = A_{h,k} [h_B (T_{w,k} - T_s)]^4, \quad (4.10.123)$$

where $A_{h,k}$ is the heat transfer area of cell k , and h_B is given by

$$h_B = 2.555 e^{6.45 \times 10^{-7} p_s}, \quad (4.10.124)$$

with p_s being the system pressure.

The partial derivative of the wall heating rate with respect to vapor mass is then

$$\frac{\partial q_{w,k}}{\partial m_{g,k}} = \frac{4 q_{w,k}}{(T_{w,k} - T_s)} \left(\frac{\partial T_{w,k}}{\partial m_{g,k}} \right). \quad (4.10.125)$$

Therefore, for two-phase heat transfer,

$$\frac{\partial \psi_{s,k}}{\partial m_{g,k}} = C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{q_{w,k}} \right) \left(\frac{\partial T_{l,k}}{\partial m_{g,k}} \right) + \left[\frac{4 \psi_{s,k}}{(T_{w,k} - T_s)} - \frac{1}{2} C_{p,k} \gamma_k \rho_{l,k} \left(\frac{\psi_{s,k}^2}{q_{w,k}} \right) \right] \left(\frac{\partial T_{w,k}}{\partial m_{g,k}} \right). \quad (4.10.126)$$

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It is a good approximation that

$$\frac{\partial T_{w,k}}{\partial m_{g,k}} \approx \frac{\partial T_{l,k}}{\partial m_{g,k}}, \quad (4.10.127)$$

and that

$$\gamma_k = \frac{1}{\rho_g} - \frac{1}{\rho_{l,k}} \approx \frac{1}{\rho_g}, \quad \text{since } \rho_{l,k} \gg \rho_g. \quad (4.10.128)$$

Thus, we have for two-phase heat transfer,

$$\frac{\partial \psi_{s,k}}{\partial m_{g,k}} = \left[C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{2 q_{w,k}} \right) + \frac{4 \psi_{s,k}}{(T_{w,k} - T_s)} \right] \left(\frac{\partial T_{l,k}}{\partial m_{g,k}} \right). \quad (4.10.129)$$

For single-phase heat transfer, the wall heating rate is given by

$$q_{w,k} = A_{h,k} h_{NB,k} (T_{w,k} - T_{l,k}), \quad (4.10.130)$$

where $h_{NB,k}$ is the non-boiling Dittus-Boelter heat transfer coefficient in cell k:

$$h_{NB,k} = 0.023 \frac{|G_{l,k}|^{0.8} C_{p,k}^{0.4} k_f^{0.6}}{\mu_f^{0.4} D_{h,k}^{0.2}}. \quad (4.10.131)$$

In the above equation, k_f is the liquid thermal conductivity, μ_f is the dynamic viscosity, and $G_{l,k}$ is the liquid mass flux defined by

$$G_{l,k} = \frac{w_{l,k}}{A_k}. \quad (4.10.132)$$

Thus, using Eqs. (4.10.130) and (4.10.131), we have for single-phase heat transfer,

$$\frac{\partial q_{w,k}}{\partial m_{g,k}} \approx 0, \quad (4.10.133)$$

and

$$\frac{\partial \psi_{s,k}}{\partial m_{g,k}} = C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{2 q_{w,k}} \right) \left(\frac{\partial T_{l,k}}{\partial m_{g,k}} \right). \quad (4.10.134)$$

From Eq. (83) we obtain

$$\frac{\partial \psi_k}{\partial m_{g,k}} = \frac{\partial \psi_{B,k}}{\partial m_{g,k}} + \frac{\partial \psi_{s,k}}{\partial m_{g,k}}, \quad (4.10.135)$$

where the first term on RHS is given by Eq. (4.10.119), the second term by Eqs. (4.10.129) for two-phase heat transfer, and by Eq. (4.10.134) for single-phase heat transfer, respectively.

The Jacobian for vapor mass balance equation can now be obtained from Eqs. (4.10.82) and (4.10.88):

$$\begin{aligned} \frac{\partial f_{M,k}}{\partial m_{g,k}} = & \left(\frac{1}{\Delta V_k} \right) (C_{0,k} W_k + A_k V_{gj,k}) \\ & + \left(\frac{m_{g,k}}{\Delta V_k} \right) \left[W_k \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) + C_{0,k} \left(\frac{\partial W_k}{\partial m_{g,k}} \right) + A_k \left(\frac{\partial V_{gj,k}}{\partial m_{g,k}} \right) \right] + \frac{\partial \psi_k}{\partial m_{g,k}}, \end{aligned} \quad (4.10.136)$$

where the last term is given by Eq. (4.10.135).

4.10.4.3 Jacobian Matrix of Mixture Energy Balance Equations

The mixture energy balance equations solved in RAMONA-4B are given by

$$\begin{aligned} \frac{dE_k}{dt} = & (w_{l,k-1} e_{l,k-1} + w_{g,k-1} e_g) - (w_{l,k} e_{l,k} + w_{g,k} e_g) \\ & + p_s (W_{k-1} - W_k) + Q_k + \delta_{n0,k} (w_{e,fw} + w_{e,ECC}), \end{aligned} \quad (4.10.137)$$

where E_k is the mixture energy in cell k , p_s is system pressure, Q_k is heating rate in cell k , $w_{l,k}$ is the liquid mass flow rate, $w_{g,k}$ is the vapor mass flow rate both at exit of cell k , W_k is the volumetric flow rate in cell k , $w_{e,fw}$ is the feedwater energy flow rate, and $w_{e,ECC}$ is that of the Emergency Core Cooling (ECC) water, e_g is specific vapor internal energy, $e_{l,k}$ is specific liquid internal energy given by Eq. (4.10.113).

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Let the RHS of Eq. (4.10.137) be the function:

$$f_{E,k} = (w_{l,k-1} e_{l,k-1} + w_{g,k-1} e_g) - (w_{l,k} e_{l,k} + w_{g,k} e_g) + p_s (W_{k-1} - W_k) + Q_k + \delta_{n0,k} (w_{e,fw} + w_{e,ECC}). \quad (4.10.138)$$

The Jacobian of mixture energy with respect to E_k is given by

$$\frac{\partial f_{E,k}}{\partial E_k} = -w_{l,k} \left(\frac{\partial e_{l,k}}{\partial E_k} \right) - e_{l,k} \left(\frac{\partial w_{l,k}}{\partial E_k} \right) - e_g \left(\frac{\partial w_{g,k}}{\partial E_k} \right) - p_s \left(\frac{\partial W_k}{\partial E_k} \right). \quad (4.10.139)$$

From Eq. (4.10.113) we get

$$\frac{\partial e_{l,k}}{\partial E_k} = \frac{1}{m_{l,k}}, \quad (4.10.140)$$

and from Eqs. (4.10.28), (4.10.29), (4.10.30) we obtain

$$\begin{aligned} \frac{\partial W_k}{\partial E_k} &= \gamma_k \left(\frac{\partial \psi_k}{\partial E_k} \right) \quad \text{for downcomer, lower plenum, riser,} \\ &= \gamma_{k,j} \left(\frac{\partial \psi_{k,j}}{\partial E_k} \right) \quad \text{for core channel } j, \end{aligned} \quad (4.10.141)$$

where

$$\frac{\partial \psi_k}{\partial E_k} = \frac{\partial \psi_{B,k}}{\partial E_k} + \frac{\partial \psi_{S,k}}{\partial E_k}. \quad (4.10.142)$$

From Eq. (4.10.84), we get

$$\frac{\partial \psi_{B,k}}{\partial E_k} = \frac{\Delta V_k}{h_{fg}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] \left(\frac{\partial T_{l,k}}{\partial E_k} + C_{13} \frac{\partial |T_{l,k}|}{\partial E_k} \right), \quad (4.10.143)$$

where the partial derivative of liquid temperature can be evaluated from Eqs. (4.10.95) and (4.10.96):

$$\frac{\partial T_{l,k}}{\partial E_k} = \frac{1}{C_{p,k} m_{l,k}} = \frac{\partial |T_{l,k}|}{\partial E_k}. \quad (4.10.144)$$

Thus, Eq. (4.10.143) becomes

$$\frac{\partial \Psi_{B,k}}{\partial E_k} = \frac{\Delta V_k}{h_{fg} C_{p,k} m_{l,k}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] (1 + C_{13}). \quad (4.10.145)$$

From Eqs. (4.10.85) and (4.10.120), we have

$$\frac{\partial \Psi_{S,k}}{\partial E_k} = \frac{1}{D_{\Psi,k}} \left(\frac{\partial q_{w,k}}{\partial E_k} \right) - \frac{\Psi_{S,k}}{D_{\Psi,k}} \left(\frac{\partial D_{\Psi,k}}{\partial E_k} \right). \quad (4.10.146)$$

Following the same derivations as for the vapor mass in the previous section, we obtain from Eq. (126) the equation,

$$\frac{\partial \Psi_{S,k}}{\partial E_k} = C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\Psi_{S,k}^2}{q_{w,k}} \right) \left(\frac{\partial T_{l,k}}{\partial E_k} \right) + \left[\frac{4 \Psi_{S,k}}{(T_{w,k} - T_s)} - \frac{1}{2} C_{p,k} \gamma_k \rho_{l,k} \left(\frac{\Psi_{S,k}^2}{q_{w,k}} \right) \right] \left(\frac{\partial T_{w,k}}{\partial E_k} \right). \quad (4.10.147)$$

Using Eq. (4.10.144) and the approximation,

$$\frac{\partial T_{w,k}}{\partial E_k} \approx \frac{\partial T_{l,k}}{\partial E_k}, \quad (4.10.148)$$

Eq. (4.10.147) becomes, for two-phase heat transfer:

$$\frac{\partial \Psi_{S,k}}{\partial E_k} = \left[C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\Psi_{S,k}^2}{2 q_{w,k}} \right) + \frac{4 \Psi_{S,k}}{(T_{w,k} - T_s)} \right] \left(\frac{1}{C_{p,k} m_{l,k}} \right), \quad (4.10.149)$$

and for single-phase heat transfer:

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$$\frac{\partial \psi_{s,k}}{\partial E_k} = C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{2 q_{w,k}} \right) \left(\frac{1}{C_{p,k} m_{l,k}} \right). \quad (4.10.150)$$

Thus, from Eq. (4.10.141) we obtain for downcomer, lower plenum, and riser:

$$\frac{\partial W_k}{\partial E_k} = \frac{\gamma_k \Delta V_k}{h_{fg} C_{p,k} m_{l,k}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] (1 + C_{13}), \quad (4.10.151)$$

and for the core channel j:

$$\begin{aligned} \frac{\partial W_k}{\partial E_k} = & \frac{\gamma_{kj} \Delta V_k}{h_{fg} C_{p,k} m_{l,k}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] (1 + C_{13}) \\ & + \gamma_{kj} \left[C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{2 q_{w,k}} \right) + \frac{4 \psi_{s,k}}{(T_{w,k} - T_s)} \right] \left(\frac{1}{C_{p,k} m_{l,k}} \right) \end{aligned} \quad \text{for 2-phase heat transfer} \quad (4.10.152)$$

and

$$\begin{aligned} \frac{\partial W_k}{\partial E_k} = & \frac{\gamma_{kj} \Delta V_k}{h_{fg} C_{p,k} m_{l,k}} [C_{11} + C_{12} \alpha_k (1 - \alpha_k)] (1 + C_{13}) \\ & + \gamma_{kj} C_{p,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \left(\frac{\psi_{s,k}^2}{2 q_{w,k}} \right) \left(\frac{1}{C_{p,k} m_{l,k}} \right) \end{aligned} \quad \text{for single-phase heat transfer.} \quad (4.10.153)$$

From Eq. (4.10.5) we get the partial derivative of vapor mass flow rate:

$$\frac{\partial w_{g,k}}{\partial E_k} = \rho_g C_{0,k} \alpha_k \left(\frac{\partial W_k}{\partial E_k} \right), \quad (4.10.154)$$

and from Eq. (4.10.6) we get the partial derivative of liquid mass flow rate:

$$\frac{\partial w_{l,k}}{\partial E_k} = \rho_{l,k} (1 - C_{0,k} \alpha_k) \left(\frac{\partial W_k}{\partial E_k} \right). \quad (4.10.155)$$

The Jacobian for mixture energy is then obtained from Eq. (4.10.139):

$$\frac{\partial f_{E,k}}{\partial E_k} = - \left\{ \frac{w_{l,k}}{m_{l,k}} + [e_{l,k} \rho_{l,k} (1 - C_{0,k} \alpha_k) + e_g \rho_g C_{0,k} \alpha_k + p_s] \left(\frac{\partial W_k}{\partial E_k} \right) \right\}. \quad (4.10.156)$$

4.10.5 Implementation of LAMBDA Subroutine in RAMONA-4B

4.10.5.1 Combined Vapor Mass and Mixture Energy Jacobian Matrix

The original LAMBDA routine combined the Jacobian matrix of vapor mass and that of mixture energy into a joint matrix of the form:

$$\left[\frac{\partial f_i}{\partial y_j} \right]_{ME} = \begin{bmatrix} \frac{\partial f_{M,i}}{\partial m_{g,k}} & \frac{\partial f_{M,i}}{\partial E_k} \\ \frac{\partial f_{E,i}}{\partial m_{g,k}} & \frac{\partial f_{E,i}}{\partial E_k} \end{bmatrix}, \quad (4.10.157)$$

where the matrix elements can be obtained from Eqs. (4.10.81) and (4.10.138) as follows:

$$\frac{\partial f_{M,i}}{\partial m_{g,k}} = - \frac{\partial w_{g,k}}{\partial \alpha_k} \left(\frac{\partial \alpha_k}{\partial m_{g,k}} \right) - \frac{\partial w_{g,k}}{\partial V_{g,k}} \left(\frac{\partial V_{g,k}}{\partial m_{g,k}} \right) + \frac{\partial \Psi_k}{\partial m_{g,k}}, \quad (4.10.158)$$

$$\frac{\partial f_{M,i}}{\partial E_k} = - \left(\frac{\partial w_{g,k}}{\partial V_{g,k}} \right) \left(\frac{\partial V_{g,k}}{\partial V_{l,k}} \right) \left(\frac{\partial V_{l,k}}{\partial E_k} \right) + \frac{\partial \Psi_k}{\partial E_k}, \quad (4.10.159)$$

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$$\begin{aligned} \frac{\partial f_{E,i}}{\partial m_{g,k}} = & - \left[e_{l,k} \left(\frac{\partial w_{l,k}}{\partial \alpha_k} \right) + e_g \left(\frac{\partial w_{g,k}}{\partial \alpha_k} \right) \right] \left(\frac{\partial \alpha_k}{m_{g,k}} \right) - e_{l,k} \left(\frac{\partial w_{l,k}}{\partial V_{l,k}} \right) \left(\frac{\partial V_{l,k}}{\partial m_{g,k}} \right) \\ & - e_g \left(\frac{\partial w_{g,k}}{\partial V_{g,k}} \right) \left(\frac{\partial V_{g,k}}{\partial m_{g,k}} \right) - w_{l,k} \left(\frac{\partial e_{l,k}}{\partial m_{g,k}} \right) - p_s \left(\frac{\partial W_k}{\partial m_{g,k}} \right), \end{aligned} \quad (4.10.160)$$

$$\frac{\partial f_{E,i}}{\partial E_k} = - e_{l,k} \left(\frac{\partial w_{l,k}}{\partial V_{l,k}} \right) \left(\frac{\partial V_{l,k}}{\partial E_k} \right) - e_g \left(\frac{\partial w_{g,k}}{\partial V_{g,k}} \right) \left(\frac{\partial V_{g,k}}{\partial E_k} \right) - w_{l,k} \left(\frac{\partial e_{l,k}}{\partial E_k} \right) - p_s \left(\frac{\partial W_k}{\partial E_k} \right). \quad (4.10.161)$$

The partial derivatives on the right-hand side of the above equations (4.10.158) through (4.10.161) can be obtained from Section 3. Since $V_{l,k}$ is a function of α_k , W_k , $C_{0,k}$, and $V_{gj,k}$,

$$V_{l,k} = \left(\frac{1 - C_{0,k} \alpha_k}{1 - \alpha_k} \right) \left(\frac{W_k}{A_k} \right) - \left(\frac{\alpha_k}{1 - \alpha_k} \right) V_{gj,k}, \quad (4.10.162)$$

we may write

$$\frac{\partial V_{l,k}}{\partial m_{g,k}} = \frac{\partial V_{l,k}}{\partial \alpha_k} \left(\frac{\partial \alpha_k}{\partial m_{g,k}} \right) + \frac{\partial V_{l,k}}{\partial W_k} \left(\frac{\partial W_k}{\partial m_{g,k}} \right) + \frac{\partial V_{l,k}}{\partial C_{0,k}} \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) + \frac{\partial V_{l,k}}{\partial V_{gj,k}} \left(\frac{\partial V_{gj,k}}{\partial m_{g,k}} \right), \quad (4.10.163)$$

where

$$\begin{aligned} \frac{\partial V_{l,k}}{\partial \alpha_k} &= \frac{1 - C_{0,k}}{(1 - \alpha_k)^2} \left(\frac{W_k}{A_k} \right) - \frac{V_{gj,k}}{(1 - \alpha_k)^2} \\ &= \frac{V_{l,k} - V_{gj,k}}{1 - \alpha_k}, \end{aligned} \quad (4.10.164)$$

$$\frac{\partial V_{l,k}}{\partial W_k} = \frac{1 - C_{0,k} \alpha_k}{A_k (1 - \alpha_k)} = \frac{AAUX(k)}{A_k}, \quad (4.10.165)$$

$$\frac{\partial V_{l,k}}{\partial C_{l,k}} = - \left(\frac{\alpha_k}{1 - \alpha_k} \right) \left(\frac{W_k}{A_k} \right), \quad (4.10.166)$$

$$\frac{\partial V_{l,k}}{\partial V_{gj,k}} = - \left(\frac{\alpha_k}{1 - \alpha_k} \right), \quad (4.10.167)$$

$$\frac{\partial V_{l,k}}{\partial E_k} = \frac{1 - C_{0,k} \alpha_k}{A_k (1 - \alpha_k)} \left(\frac{\partial W_k}{\partial E_k} \right) = \left(\frac{\partial V_{l,k}}{\partial W_k} \right) \left(\frac{\partial W_k}{\partial E_k} \right), \quad (4.10.168)$$

$$\frac{\partial \alpha_k}{\partial m_{g,k}} = \frac{1}{\rho_g \Delta V_k}. \quad (4.10.169)$$

The partial derivative $\partial W_k / \partial m_{g,k}$ is given by Eq. (4.10.94), $\partial C_{0,k} / \partial m_{g,k}$ is given by Eq. (4.10.92), $\partial V_{gj,k} / \partial m_{g,k}$ is given by Eq. (4.10.93), $\partial W_k / \partial E_k$ is given by Eqs. (4.10.141), $\partial e_{l,k} / \partial E_k$ is given by Eq. (4.10.140), $\partial \psi_k / \partial m_{g,k}$ is given by Eq. (4.10.135), and $\partial \psi_k / \partial E_k$ is given by Eqs. (4.10.142), (4.10.145) and (4.10.146), respectively.

The vapor velocity in cell k is given by

$$V_{g,k} = C_{0,k} \left(\frac{W_k}{A_k} \right) + V_{gj,k}, \quad (4.10.170)$$

therefore, we have

$$\frac{\partial V_{g,k}}{\partial m_{g,k}} = \frac{\partial V_{g,k}}{\partial W_k} \left(\frac{\partial W_k}{\partial m_{g,k}} \right) + \frac{\partial V_{g,k}}{\partial C_{0,k}} \left(\frac{\partial C_{0,k}}{\partial m_{g,k}} \right) + \frac{\partial V_{g,k}}{\partial V_{gj,k}} \left(\frac{\partial V_{gj,k}}{\partial m_{g,k}} \right), \quad (4.10.171)$$

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where

$$\frac{\partial V_{g,k}}{\partial W_k} = \frac{C_{0,k}}{A_k}, \quad (4.10.172)$$

$$\frac{\partial V_{g,k}}{\partial C_{0,k}} = \frac{W_k}{A_k}, \quad (4.10.173)$$

$$\frac{\partial V_{g,k}}{\partial V_{g',k}} = 1. \quad (4.10.174)$$

$$\frac{\partial V_{g,k}}{\partial V_{l,k}} = \frac{C_{0,k}(1 - \alpha_k)}{1 - C_{0,k}\alpha_k} = S_k. \quad (4.10.175)$$

The vapor mass flow rate is defined as

$$w_{g,k} = \rho_g \alpha_k V_{g,k} A_k. \quad (4.10.176)$$

Thus, we have

$$\frac{\partial w_{g,k}}{\partial V_{g,k}} = \rho_g \alpha_k A_k = \frac{\rho_g \alpha_k A_k \Delta Z_k}{\Delta Z_k} = \frac{m_{g,k}}{\Delta Z_k}, \quad (4.10.177)$$

$$\frac{\partial w_{g,k}}{\partial \alpha_k} = \rho_g V_{g,k} A_k. \quad (4.10.178)$$

The liquid mass flow rate is defined as

$$w_{l,k} = (1 - \alpha_k) \rho_{l,k} V_{l,k} A_k. \quad (4.10.179)$$

Thus, we have

$$\frac{\partial w_{l,k}}{\partial V_{l,k}} = (1 - \alpha_k) \rho_{l,k} A_k = \frac{(1 - \alpha_k) \rho_{l,k} A_k \Delta Z_k}{\Delta Z_k} = \frac{m_{l,k}}{\Delta Z_k}, \quad (4.10.180)$$

$$\frac{\partial w_{l,k}}{\partial \alpha_k} = -\rho_{l,k} V_{l,k} A_k. \quad (4.10.181)$$

From Eq. (4.10.113) we obtain

$$\frac{\partial e_{l,k}}{\partial m_{g,k}} = -\left(\frac{1}{m_{l,k}}\right) \left[e_g - e_{l,k} \left(\frac{\rho_{l,k}}{\rho_g} \right) \right]. \quad (4.10.182)$$

The subroutine LAMBDA calculates the matrix elements given by Eqs. (4.10.158) through (4.10.161) and stores the results in the array DFYC(J,I) for closed flow loop J, where I ranges from 1 to 4, corresponding to the four matrix elements. The maximum eigenvalue of the combined Jacobian matrix is determined as follows.

Consider a 2x2 matrix of the form:

$$[A] = \begin{bmatrix} A_{11} & A_{12} \\ A_{21} & A_{22} \end{bmatrix}, \quad (4.10.183)$$

where

$$A_{11} = \frac{\partial f_{M,j}}{\partial m_{g,k}} = DFYC(J,1), \quad (4.10.184)$$

$$A_{12} = \frac{\partial f_{M,j}}{\partial E_k} = DFYC(J,2), \quad (4.10.185)$$

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$$A_{21} = \frac{\partial f_{E,J}}{\partial m_{g,k}} = DFYC(J,3), \quad (4.10.186)$$

$$A_{22} = \frac{\partial f_{E,J}}{\partial E_k} = DFYC(J,4). \quad (4.10.187)$$

The eigen values of the matrix [A] are the roots of the characteristic equation,

$$|[A] - \lambda [I]| = 0, \quad (4.10.188)$$

which gives rise to the quadratic equation,

$$\lambda^2 - (A_{11} + A_{22})\lambda + (A_{11}A_{22} - A_{12}A_{21}) = 0. \quad (4.10.189)$$

The solutions of the quadratic equation are given by

$$\lambda = \frac{(A_{11} + A_{22}) \pm \sqrt{(A_{11} - A_{22})^2 + 4A_{12}A_{21}}}{2}. \quad (4.10.190)$$

The maximum eigenvalue is the larger of the magnitude of the two roots above. If the argument of the square root is negative, the eigenvalue is conservatively estimated as

$$\lambda = \frac{\sqrt{(A_{11} + A_{22})^2 - [(A_{11} - A_{22})^2 + 4A_{12}A_{21}]}}{2}. \quad (4.10.191)$$

4.10.5.2 Implementation Strategy

The old subroutine LAMBDA for the slip model was modified, using the equivalence relations (4.10.15) and (4.10.18) in Section 4.10.3, the derivations in Section 4.10.4, and the formulations in Section 4.10.5.1 to create a new LAMBDA subroutine in RAMONA-4B. It should be noted that the old LAMBDA subroutine did mix up the single-phase and two-phase heat transfer for the mixture energy and vapor mass Jacobian. This has been corrected in new LAMBDA subroutine.

To account for the uncertainties due to approximations, a conservative factor of 0.95 is applied to the theoretical stability limit (4.10.3):

$$(\Delta t_n)_{stb} \leq 0.95 \left(\frac{2}{|\lambda_{\max}|} \right). \quad (4.10.192)$$

In order to obtain safe and practical time step size, it is imposed that $|\lambda_{\max}|$ be no greater than 1900 nor less than 10. Appendix A lists the FORTRAN code of the new LAMBDA subroutine.

The subroutine LAMBDA obtains the maximum eigenvalue and the stability limit by either a major (full-blown) $\lambda_{\max}$ calculation or a simplified calculation with a reduced Jacobian matrix. It employs several parameters to determine which mode of calculation to take as described below.

The parameters used in the $\lambda_{\max}$ calculational procedures are

- VX1: set initially as 'TRUE', then set as 'FALSE' after a major $\lambda_{\max}$ calculation and if the momentum equation is critical.
- NOSTEP: set initially as zero to do a major $\lambda_{\max}$ calculation, then set to non-zero after the vapor mass, mixture energy, and pressure $\lambda_{\max}$ calculations are done.
- KRITEQ: the critical equation parameter, if KRITEQ < 0, the critical equation is loop momentum, if KRITEQ = 0, the critical equation is vapor mass and mixture energy, if KRITEQ > 0, the critical equation is pressure. The value of KRITEQ is determined by the dominant eigenvalue of the three maximum eigenvalues of loop momentum, vapor mass and mixture energy, and system pressure.
- MODI: the momentum $\lambda_{\max}$ calculation parameter, set initially as zero to force a major momentum $\lambda_{\max}$ calculation, then set as non-zero if MODI ≤ 0 and the momentum equation is not critical, and updated as MODI=MODI-1 if the momentum equation is critical (MODI>0).
- MODE: the vapor mass and mixture energy $\lambda_{\max}$ calculation parameter, set initially as zero to make sure a major vapor mass and mixture energy $\lambda_{\max}$ calculation, then set as non-zero if MODE ≤ 0 and the vapor mass and mixture energy equations are not critical, and updated as MODE=MODE-1 if the vapor mass and mixture energy equations are critical (MODE>0).
- MODP: the pressure $\lambda_{\max}$ calculation parameter, set initially as zero to make sure a pressure $\lambda_{\max}$ calculation, then set as non-zero if MODP ≤ 0 and the pressure equation is not critical, and updated as MODP=MODP-1 if the pressure equation is critical (MODP>0).

The $\lambda_{\max}$ calculational procedures in the subroutine LAMBDA are summarized as follows:

I. Simplified $\lambda_{\max}$ Calculation (NOSTEP > 0)

If the parameter NOSTEP ≤ 0, go to step II to do a major calculation to determine $\lambda_{\max}$; otherwise, do a simplified calculation as follows:

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- (1) If the parameter $KRITEQ < 0$, the critical equation is loop momentum.

If $(MODE*MODP) \leq 0$, vapor mass, mixture energy, or pressure is out of date, go to step II to do a major λ_{max} calculation.

Otherwise, do a simplified calculation with a reduced momentum matrix using an equivalent loop $J=NEQ$ which was determined from previous major calculation.

- (2) If $KRITEQ = 0$, the critical equation is vapor mass and mixture energy.

If $(MODI*MODP) \leq 0$, loop momentum or pressure is out of date, go to step II to do a major λ_{max} calculation.

Otherwise, do a simplified calculation with a reduced vapor mass and energy matrix using the critical cell $KKRIT$, the critical flow segment $IKRIT$, and the critical loop $JKRIT$, which were determined from previous major calculation.

- (3) If $KRITEQ > 0$, the critical equation is pressure.

If $(MODE*MODI) \leq 0$, vapor mass, mixture energy, or loop momentum is out of date, go to step II to do a major λ_{max} calculation.

Otherwise, set the following parameters and return:

MODI = MODI - 1
MODE = MODE - 1
NOSTEP = NOSTEP - 1

II. Major λ_{max} Calculation ($NOSTEP \leq 0$)

- (1) If $MODI > 0$, the momentum matrix is up to date and go straight to vapor mass and mixture energy matrix calculation in step (2). Otherwise, the loop momentum matrix is out of date and do a major loop momentum λ_{max} calculation as described in Section 4.1 and also do step (2) below.
- (2) If $MODE > 0$, the vapor mass and mixture energy matrix is up to date and go to pressure matrix calculation. Otherwise, the vapor mass and mixture energy matrix is out of date and do a major vapor mass and mixture energy λ_{max} calculation as described in Section 4.10.5.1 and also do step (3) below.
- (3) If $MODP > 0$, the pressure equation matrix is up to date and go to step (4) below. Otherwise, the pressure matrix is out of date and calculate the pressure λ_{max} by the equation,

$$(\lambda_{max})_p = - \left| \frac{f_p^{(n)} - f_p^{(n-1)}}{f_p^{(n-1)} \Delta t_n} \right|. \quad (4.10.193)$$

Here n is the time step index, $f_p^{(n)}$ is the current pressure derivative at time step n , $f_p^{(n-1)}$ is that at previous time step $n-1$, and Δt_n is the current time step size. Also, do step (4) below.

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- (4) Set the parameters NOSTEP, MODI, MODE, MODP, KRITQ, KKRT, LKRT, AUXDFI, RLAM0 ($\lambda_{\max}$), etc. for the next time step. In particular, determine the value of KRITQ by the dominant eigenvalue.

In RAMONA-4B, the time step control is done in the subroutine NUMINT which calls the new subroutine LAMBDA after the truncation error has met the accuracy criterion from input. In this case, the time step taken is the smaller of the stability limit $(\Delta t_n)_{stb}$ and the accuracy limit $(\Delta t_n)_{acc}$:

$$\Delta t_n = \text{Min} \left[(\Delta t_n)_{stb}, (\Delta t_n)_{acc} \right]. \quad (4.10.194)$$

Otherwise, if the truncation error is greater than the accuracy criterion, the time step is reduced in accordance with the square root of the ratio of the truncation error and the accuracy criterion. In this case, the LAMBDA subroutine needs not be called. The original input parameter SDTMAX (on Card 500082) used for guarding against numerical instability is still retained. A value of 0.03 sec has been used for SDTMAX in the past with success. This value should be relaxed (say 0.06 sec or higher) for the new LAMBDA subroutine to work effectively.

4.10.6 Accuracy Limits

The time-step control procedures for the calculation of neutron kinetics have not been changed since the RAMONA-3B description report [Wulff et al., 1984] was published. However, some modifications were made to the time-step control strategy for the hydraulic calculation which uses the Euler-Cauchy method to solve the state equations.

4.10.6.1 Transient Neutronic Calculation

The time-step control strategy for the neutronic calculation is discussed in Chapter 6.7 of the RAMONA-3B report [Wulff et al., 1984]. For the sake of completeness, the diagram showing the time-step selection logic is shown in Figure 4.19.

The neutronic time-step control is executed in subroutine DYN. The time-step control algorithm is based on the input neutronic accuracy criterion ACCF on Card 720000. To help the reader understand the diagram, Figure 4.19, a brief description of the subroutines and key variables is given in Table 4.2.

4.10.6.2 Transient Hydraulic Calculation

A description of the procedure for hydraulic time-step selection based on the accuracy limit that is currently implemented in RAMONA-4B follows. The hydraulic time step is limited from above according to the RAMONA-4B method of coupling the transient neutronic and hydraulic calculations: the hydraulic time step can not exceed the neutronic time step which is the master time step, $\Delta t_n^{\text{hydr}} \leq \Delta t_N$, n being the hydraulic time step index and N the neutronic time step index. At the same time, the step size can not exceed a maximum value specified by the user via the input variable SDTMAX on Card 500082.

The procedure for time-step selection starts from a search for the state equation which incurs the largest truncation error. Equations for the following variables are tested in this search:

- System pressure,
- vapor mass in the feedwater junction cell,

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- integrated loop momenta for NPC hydraulic loops, where NPC is the total number of hydraulic channels representing the core,
- specific energy at the exit cells of NPC hydraulic channels.

Table 4.2 List of Subroutines Called by Subroutine DYN for Transient Neutronic Calculation

Subroutine Name	Subroutine Description
AVGF	A variable defined as corrected core-average fast flux by subroutine F1DYN.
AVGP	A variable defined as predicted core-average fast flux by subroutine PREDYN.
DYNLST	Calculates the reactor power distribution and other output variables, prints them and saves them on a plot file.
F1DYN	Calculates the fast flux distribution in the core.
F2DYN	Calculates the thermal flux and precursor distributions in the core.
MATDYN	Calculates the coefficient matrix associated with the fast flux equations.
PREDYN	Predicts the fluxes, fuel temperatures, nodal power and decay heat based on the past derivations and time step.
SUMDF	A variable defined as core-average fast flux residual by subroutine F1DYN.
TFDYN	Calculates the average fuel temperature, temperature distribution and derivative.
TIME	Neutronic transient time.
XEDYN	Calculates cross sections and other parameters for transient neutronic calculation.

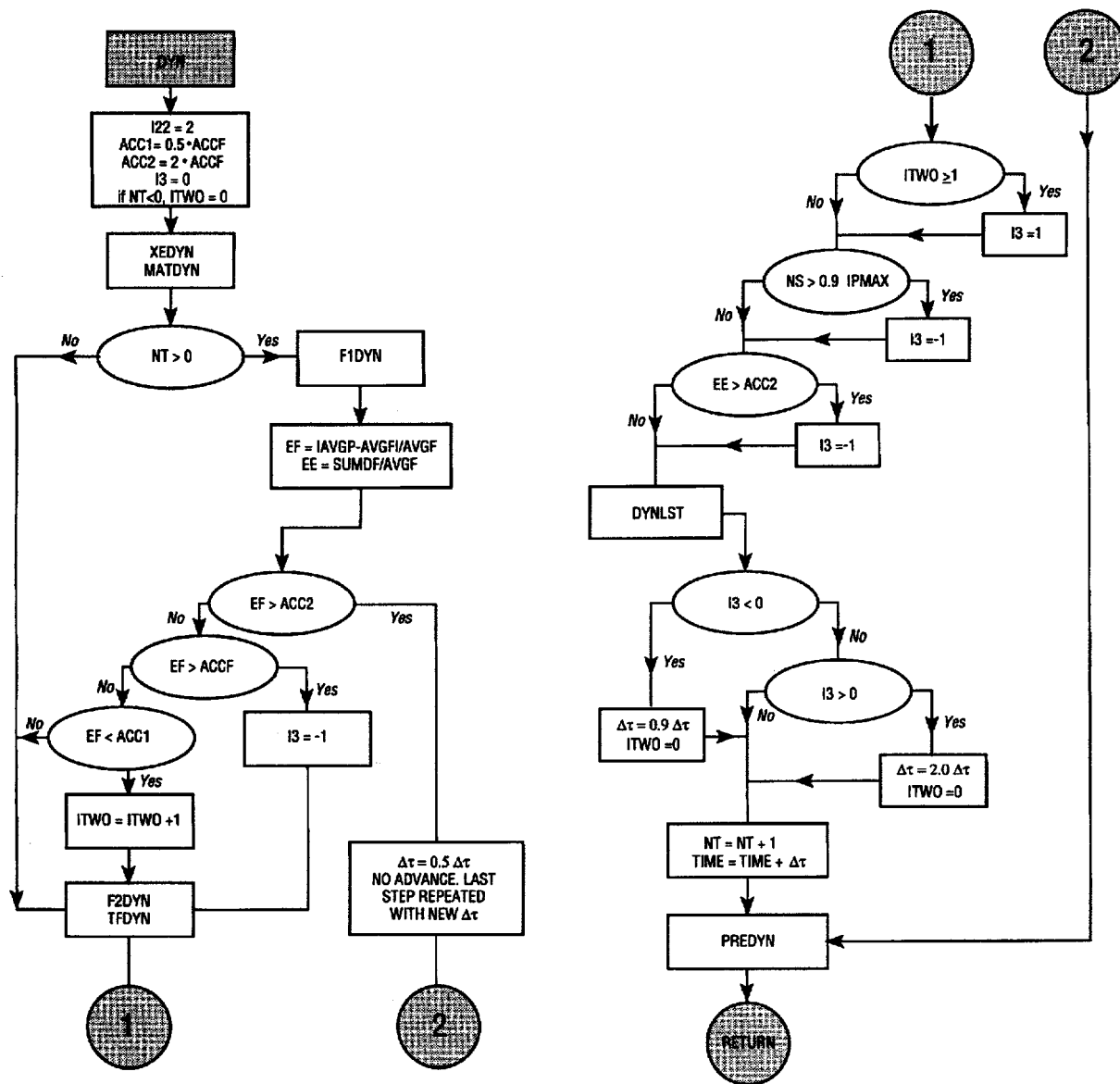


Figure 4.19 Neutronic Time Step Control Logic Diagram.

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First, determine the maximum relative truncation error, TRUNC, from the 2nd-order term of a Taylor series expansion:

$$TRUNC = \frac{\Delta t_n^2 y_n''}{2y_n} \quad (4.10.195)$$

where y_n is the state variable of interest and y_n'' is the second derivative of y_n . The following time step control strategy is used:

- (1) If TRUNC is less than the hydraulic accuracy criterium ACCH (input on Card 500082),

$$\Delta t_{aux} = 0.8 \Delta t_n \sqrt{\frac{ACCH}{TRUNC}}, \quad (4.10.196)$$

$$\Delta t_{n+1}^* = \text{Min} (SDTMAX, \Delta t_{aux}), \quad (4.10.197)$$

$$\Delta t_{n+1} = \text{Max} (\Delta t_{n+1}^*, HMIN), \quad (4.10.198)$$

where HMIN is the minimum hydraulic time step defined as $0.01 * SDTMAX$, and SDTMAX is the maximum allowable hydraulic time-step specified via the input on Card 500082.

- (2) If TRUNC is greater than or equal to ACCH, perform a back-stepping: Integrate the hydraulic state equations with a negative time step, $-\Delta t_n$. Calculate a cut-back time step multiplier:

$$\gamma_{CB} = 0.8 \sqrt{\frac{ACCH}{TRUNC}}, \quad (4.10.199)$$

$$\Delta t_n = \gamma_{CB} \Delta t_n, \quad (4.10.200)$$

subject to the minimum hydraulic time step HMIN:

$$\Delta t_n = \text{Max} (\Delta t_n, HMIN), \quad (4.10.201)$$

In either case, the hydraulic time step is further limited by the condition,

$$\Delta t_n = \text{Min}(\Delta t_n, \text{TIME} - \text{TSEC}), \quad (4.10.202)$$

in order to synchronize with the neutronic time step. Here TIME is the neutronic time and TSEC is the hydraulic time. Note that the neutronic time is always ahead of the hydraulic time (TIME > TSEC).

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4.11 Boron Transport Model

The original boron transport model in RAMONA-3B (Wulff and others, 1984a) is inadequate for accurate tracking of boron in the reactor core because of the very crude nodalization scheme used. There are only ten nodes used for the reactor pressure vessel (RPV) of which three are used for the core. Furthermore, the multiple core channels are lumped into a single channel for boron tracking. The strong nuclear-thermal-hydraulic coupling in a Simplified Boiling Water Reactor (SBWR), due to the natural circulation used for the coolant flow, requires an accurate calculation of soluble boron in the multiple parallel channels of the core. To this end, a new detailed boron transport model has been implemented in the upgraded version RAMONA-4B.

The following documents the model formulation, the numerical solution, and the model implementation.

4.11.1 Model Formulation

The following assumptions are made for the present model:

1. Boron is transported in the liquid phase only,
2. Boron velocity is the liquid velocity degraded by boron mixing,
3. Boron mixing can be represented by an empirical correlation,
4. Liquid properties are not altered by the presence of boron.

4.11.1.1 Boron Transport Equation

The conservation of boron mass in a control volume (V) with surface area (A) gives rise to the boron transport equation:

$$\int_0^V \frac{\partial}{\partial t} [(1 - \alpha) \rho_l C_b] dV + \oint_0^A [(1 - \alpha) \rho_l C_b] \bar{v}_b \cdot d\bar{A} = \int_0^V S_b dV, \quad (4.11.1)$$

where C_b is the boron concentration, v_b is the boron transport velocity, ρ_l is the liquid density, α is the void fraction, and S_b is the source of boron. By definition, the boron mass M_b is given by

$$M_b = (1 - \alpha) \rho_l C_b V. \quad (4.11.2)$$

Using the above definition and carrying out the spatial integration in Eq.(4.11.1) over the control volume of cell j, we obtain the following boron mass balance equation:

$$\frac{dM_{bj}}{dt} = W_{bj-1} - W_{bj} + \delta_{j,k} S_b, \quad (4.11.3)$$

where S_b is the boron injection rate from the standby liquid control system (SLCS) which is present only in the cells where the boron injection takes place as indicated by the Kronecker delta function,

$$\begin{aligned}\delta_{j,k} &= 1, \quad \text{for } j=k, \\ &= 0, \quad \text{for } j \neq k,\end{aligned}\tag{4.11.4}$$

where the index k represents the cell in which boron is injected.

The boron mass flow rate $W_{b,j}$ in cell j is related to the liquid mass flow rate $W_{t,j}$ in cell j as

$$W_{b,j} = C_{b,j} W_{t,j},\tag{4.11.5}$$

where $C_{b,j}$ is the boron concentration in cell j .

It should be noted that the formulation in Eq.(4.11.5) has tacitly assumed that the boron is well mixed with the liquid so that the boron transport velocity is equal to the liquid velocity. When a boron solution is injected into the liquid, the injected solution will drift in the direction of gravity due to its higher density until it is perfectly mixed with the surrounding liquid. Also, when the boron concentration exceeds its solubility, it will plate out. In order to account for the realistic non-perfect mixing, an empirical boron mixing efficiency function is introduced in the definition of the boron mass flow rate in Eq.(4.11.5):

$$W_{b,j} = \eta_{b,j} C_{b,j} W_{t,j},\tag{4.11.6}$$

where $\eta_{b,j}$ is the boron mixing efficiency which is a function of the liquid flow rate, and treated as an input tabular function. The above equation can be thought of as the boron velocity is the liquid velocity that is degraded by the imperfect mixing, especially at very low flow rates, in accordance with the assumption 2.

Thus, using the new definition of boron mass flow rate in Eq.(4.11.6), the boron mass balance equation for cell j becomes

$$\frac{dM_{b,j}}{dt} = \eta_{b,j-1} C_{b,j-1} W_{t,j-1} - \eta_{b,j} C_{b,j} W_{t,j} + \delta_{j,k} S_b.\tag{4.11.7}$$

4.11.1.2 Boron Concentration

Once the boron mass is calculated from Eq.(4.11.7), the boron concentration in cell j can then be computed from Eq.(4.11.2):

$$C_{b,j} = \frac{M_{b,j}}{(1 - \alpha_j) \rho_{t,j} V_j},\tag{4.11.8}$$

where V_j is the volume of cell j .

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4.11.1.3 Boron Mixing Efficiency

The boron mixing efficiency is a function of the liquid mass flow rate. In general, the higher the liquid flow rate, the better the boron will be mixed. At a sufficiently high liquid flow rate, the boron will be perfectly mixed. It also depends on the direction of the liquid flow because of the gravity effect. The downward flow will have a better mixing than the upward flow, especially at the low flow condition. For RAMONA-4B, the boron mixing efficiency is treated as an input tabular function. Three separate boron mixing efficiency functions are used in RAMONA-4B: one for downflow, one for upflow, and the other for horizontal flow. Figure 4.20 shows a typical example of these three functions.

4.11.1.4 Boron Flow Reversal

The boron flow reversal is taken into account by the following logic for the boron mass flow rate at cell j:

$$W_{b,j} = \frac{1}{2} \left(W_{b,j}^+ + |W_{b,j}^+| + W_{b,j}^- - |W_{b,j}^-| \right), \quad (4.11.9)$$

where

$$W_{b,j}^+ = \eta_{b,j}^+ C_{b,j} W_{t,j}^+, \quad (4.11.10)$$

$$W_{b,j}^- = \eta_{b,j}^- C_{b,j+1} W_{t,j}^-, \quad (4.11.11)$$

and

$W_{t,j}^+$ = positive liquid mass flow rate at the exit of cell j,
 $W_{t,j}^-$ = negative liquid mass flow rate at the exit of cell j.

Note that this logic yields a forward boron flow if its sign is positive, and leads to a reversed boron flow if its sign is negative. This is the standard donor cell differencing scheme for accommodating the flow reversal. That is, the inlet interface concentration of each cell is set to the boron concentration of the cell above for positive flow and to the boron concentration of the cell below for negative flow. It is assumed that the exit boron concentration of each cell is equal to the cell-average value.

At the entrance to and exit from the core, the boron tracking must take into account that there are multiple core channels. The core inlet boron concentrations of the multiple parallel channels must be determined with consideration for the flow reversal. To this end, consider the continuity of boron flow between the exit of lower plenum 2 (LP2) and the inlet of the parallel coolant channels:

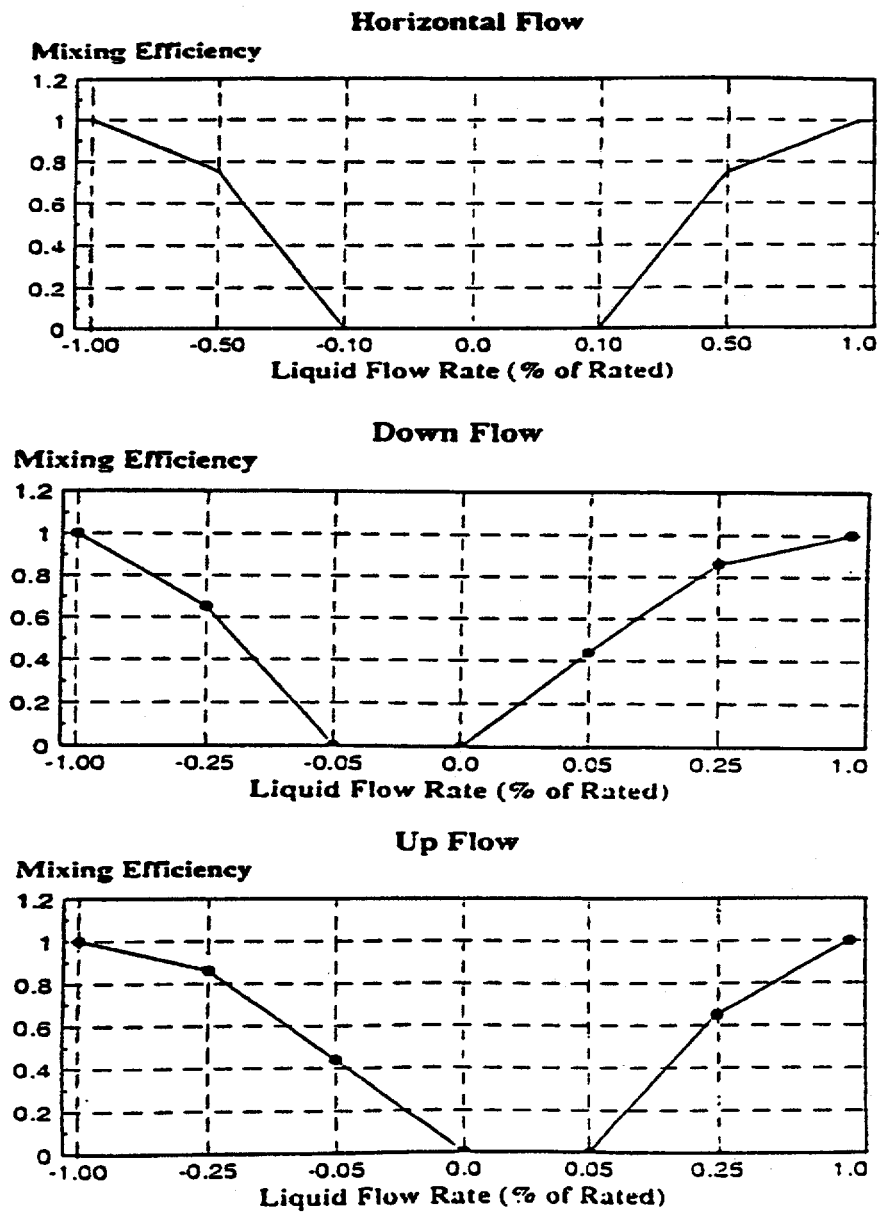


Figure 4.20 Boron Mixing Efficiency Functions.

4. Modeling of Thermal Hydraulics

$$\begin{aligned} & \frac{1}{2}C_{b,LP2}(W_{t,LP2} + |W_{t,LP2}|) + \frac{1}{2}C_{b,ci}(W_{t,LP2} - |W_{t,LP2}|) \\ &= \sum_{k=1}^{NPC} \left[\frac{1}{2}C_{b,ci}(W_{t,k}^i + |W_{t,k}^i|) + \frac{1}{2}C_{b,k}^1(W_{t,k}^i - |W_{t,k}^i|) \right] F_{sym,k}, \end{aligned} \quad (4.11.12)$$

where

- $C_{b,ci}$ = boron concentration at core inlet,
- $C_{b,LP2}$ = boron concentration at LP2 exit,
- $C_{b,k}^1$ = boron concentration in 1st cell of core channel k,
- $W_{t,k}^i$ = liquid mass flow rate at inlet of core channel k,
- $W_{t,LP2}$ = liquid mass flow rate at exit of LP2,
- $F_{sym,k}$ = core symmetry factor for core channel k (1 for full core, 2 for half-core symmetry, and so on).

Solving Eq.(4.11.12) for the core inlet boron concentration, we obtain

$$C_{b,ci} = \frac{-\frac{1}{2} \sum_{k=1}^{NPC} C_{b,k}^1 (W_{t,k}^i - |W_{t,k}^i|) F_{sym,k} + \frac{1}{2} C_{b,LP2} (W_{t,LP2} + |W_{t,LP2}|)}{\frac{1}{2} \sum_{k=1}^{NPC} (W_{t,k}^i + |W_{t,k}^i|) F_{sym,k} - \frac{1}{2} (W_{t,LP2} - |W_{t,LP2}|)}. \quad (4.11.13)$$

Similarly, the continuity of boron flow between the exit of core channels and the inlet of the riser requires that

$$\begin{aligned} & \frac{1}{2}C_{b,ri}(W_{t,ri} + |W_{t,ri}|) + \frac{1}{2}C_{b,RSR}(W_{t,ri} - |W_{t,ri}|) \\ &= \sum_{k=1}^{NPC} \left[\frac{1}{2}C_{b,k}^{ex}(W_{t,k}^{ex} + |W_{t,k}^{ex}|) + \frac{1}{2}C_{b,ri}(W_{t,k}^{ex} - |W_{t,k}^{ex}|) \right] F_{sym,k}, \end{aligned} \quad (4.11.14)$$

where

- $C_{b,ri}$ = boron concentration at riser inlet,
- $C_{b,RSR}$ = boron concentration at 1st cell of riser,
- $C_{b,k}^{ex}$ = boron concentration at exit of core channel k,
- $W_{t,k}^{ex}$ = liquid mass flow rate at exit of core channel k,
- $W_{t,ri}$ = liquid mass flow rate at inlet of riser,
- $F_{sym,k}$ = core symmetry factor for core channel k (1 for full core, 2 for half-core symmetry, and so on).

Solving Eq. (4.11.14) for the riser inlet boron concentration, we obtain

$$C_{b,ri} = \frac{\frac{1}{2} \sum_{k=1}^{NPC} C_{b,k}^{ex} (W_{\ell,k}^{ex} + |W_{\ell,k}^{ex}|) F_{sym,k} - \frac{1}{2} C_{b,RSR} (W_{\ell,ri} - |W_{\ell,ri}|)}{-\frac{1}{2} \sum_{k=1}^{NPC} (W_{\ell,k}^{ex} - |W_{\ell,k}^{ex}|) F_{sym,k} + \frac{1}{2} (W_{\ell,ri} + |W_{\ell,ri}|)} \quad (4.11.15)$$

4.11.2 Numerical Solutions

4.11.2.1 Boron Mass Balance Equation

The boron transport equation (4.11.7) is solved via explicit Euler integration using the same time step as the hydraulic integration. This is sufficiently accurate and efficient. The potential problem with numerical instability is taken care of by the automatic time step control algorithm used for the hydraulic integration subject to a maximum permissible time step size specified by an input.

The boron mass in cell j at the n th time step is given by

$$M_{b,j}^{(n)} = M_{b,j}^{(n-1)} + \Delta t_n \left(\eta_{b,j-1}^{(n)} C_{b,j-1}^{(n-1)} W_{\ell,j-1}^{(n)} - \eta_{b,j}^{(n)} C_{b,j}^{(n-1)} W_{\ell,j}^{(n)} + \delta_{j,k} S_b^{(n)} \right), \quad (4.11.16)$$

where the superscript (n) stands for the time step index. The boron injection rate is computed from

$$S_b^{(n)} = C_{b,inj} W_{b,inj}^{(n)}, \quad (4.11.17)$$

where $C_{b,inj}$ is the boron concentration (in ppm) of the pentaborate solution used for the boron injection and $W_{b,inj}$ is the mass flow rate (in kg/s) of the pentaborate solution. Here $C_{b,inj}$ is treated as an input parameter and $W_{b,inj}$ is either an input for the regular BWR (constant injection rate) or calculated by the SLCS model (see Section 8.2) for the SBWR (variable injection rate).

4.11.2.2 Boron Concentration

The n th-time-step boron concentration in cell j is computed from Eq.(4.11.8) as follows:

$$C_{b,j}^{(n)} = \frac{M_{b,j}^{(n)}}{(1 - \alpha_j^{(n)}) \rho_{\ell,j}^{(n)} V_j}, \quad (4.11.18)$$

where α_j and $\rho_{\ell,j}$ are calculated by the coolant dynamics.

4.11.2.3 Boron Mixing Efficiency

The boron mixing efficiency at the n th time step is computed from the input tabular functions via linear interpolation:

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$$\eta_{b,j}^{(n)} = F_1(W_{l,j}^{(n)}) \quad (4.11.19)$$

for the downcomer,

$$\eta_{b,j}^{(n)} = F_2(W_{l,j}^{(n)}) \quad (4.11.20)$$

for the lower plenum 1, and

$$\eta_{b,j}^{(n)} = F_3(W_{l,j}^{(n)}) \quad (4.11.21)$$

for the lower plenum 2, reactor core and riser.

Here F_1 , F_2 , and F_3 are the input tabular functions.

4.11.2.4 Boron Flow Reversal

In the donor cell differencing scheme used for the boron flow reversal, the core inlet and riser inlet boron concentrations are required. They are computed from Eqs.(4.11.13) and (4.11.15), respectively at the n th time step as follows:

$$C_{b,ci}^{(n)} = \frac{-\frac{1}{2} \sum_{k=1}^{NPC} (C_{b,k}^{(n)}) \left[(W_{l,k}^{(n)}) - |(W_{l,k}^{(n)})| \right] F_{sym,k} + \frac{1}{2} C_{b,LP2}^{(n)} (W_{l,LP2}^{(n)} + |W_{l,LP2}^{(n)}|)}{\frac{1}{2} \sum_{k=1}^{NPC} \left[(W_{l,k}^{(n)}) + |(W_{l,k}^{(n)})| \right] F_{sym,k} - \frac{1}{2} (W_{l,LP2}^{(n)} - |W_{l,LP2}^{(n)}|)}}, \quad (4.11.22)$$

$$C_{b,ri}^{(n)} = \frac{\frac{1}{2} \sum_{k=1}^{NPC} (C_{b,k}^{ex(n)}) \left[(W_{l,k}^{ex(n)}) + |(W_{l,k}^{ex(n)})| \right] F_{sym,k} - \frac{1}{2} C_{b,RSR} (W_{l,ri}^{(n)} - |W_{l,ri}^{(n)}|)}{-\frac{1}{2} \sum_{k=1}^{NPC} \left[(W_{l,k}^{ex(n)}) - |(W_{l,k}^{ex(n)})| \right] F_{sym,k} + \frac{1}{2} (W_{l,ri}^{(n)} + |W_{l,ri}^{(n)}|)}}, \quad (4.11.23)$$

4.11.3 Implementation in RAMONA-4B

RAMONA-4B employs integrated loop momentum equations to calculate the coolant flow rate [Wulff and others, 1984a]. The boron tracking is done using the same nodalization scheme as the hydraulic calculation. The boron mass and concentration are computed in every hydraulic cell of each flow loop after the hydraulic calculation has been accepted by the automatic time step control subject to a maximum time step specified by an input (to guard against potential numerical instability if the time step becomes too large). The boron transport model is implemented in RAMONA-4B as the subroutine BORON. This subroutine is called several times in the subroutine NUMINT (the transient driver) according to its function defined by the literal argument MODE.

4.11.3.1 Indexing Scheme

There are a total of six flow segments in each of the multiple coolant flow loops used by RAMONA-4B. They consist of the upper downcomer (DC1), the lower downcomer (DC2), the lower plenum 1 (LP1), the lower plenum 2 (LP2) which represents the core entrance region, the reactor core, and the riser region which includes the upper plenum. The thermal-hydraulic cells and interfaces between the flow segments are numbered consecutively starting from the two-phase mixture cell in the steam dome numbered as 1, the downcomer entrance as 2, the first cell of DC1 as 3, and so on.

MAXCELL is the total number of hydraulic cells and interfaces in the nodalization. The variables **NIN**, and **NOUT** are defined for each flow segment and refer to the cell number of the entrance interface and the last cell of the flow segment, respectively. The first cell index refers to the two-phase region in the steam dome. The second cell index refers to the interface between the steam dome and upper downcomer (DC1). The indexing of the cells then continues with consecutive integers. **NIN(1)** is the index of DC1 entrance interface, **NOUT(1)** is the index of the last cell of DC1, **NOUT(1)+1** refers to the interface between DC1 and DC2, and **NOUT(1)+2** refers to the first cell of DC2. Likewise, **NIN(2)** refers to the interface between DC1 and DC2. This indexing scheme remains the same for LP1 and LP2.

At the entrance of the core, the indexing method changes slightly to accommodate multiple coolant channels. The core has **NPC** parallel channels (**NPC** is the bypass channel). The index of the interface between the first core channel and Lower Plenum 2 is defined by **NOUT(4)+1**, that of the second channel by **NOUT(4)+2**, etc. The interface can also be referred to by **NIN**. In this case the same interfaces would be referenced by **NIN(5)**, **NIN(5+1)**, **NIN(5+2)**, and so on. The indexing for the interior core cells proceeds horizontally across the multiple coolant channels. Thus, the first cell of channel one is indexed by **NOUT(4)+1+NPC**, the second cell by **NOUT(4)+1+NPC+NPC**, etc.

The interface between the core and the riser is indexed by the integer **NIN(5+NPC)**. The interface between the riser and the steam dome has the index **NOUT(5+NPC)**.

4.11.3.2 Boron Tracking

The concentration and mass of boron in each cell are kept by the arrays **CBOR** and **YB**. The standard donor cell differencing scheme as described in Section 4.11.2.4 is used to account for the flow reversal. That is, the inlet interface concentration of each cell is set to the boron concentration of the cell above for positive flow and to the boron concentration of the cell below for negative flow. It is assumed that the exit boron concentration of each cell is equal to the cell-average value.

Starting from DC1 successively to the riser, the boron mass is computed from Eq.(4.11.16) for each cell by using Euler's explicit method. The boron concentration is then calculated from the boron mass, void fraction and liquid density of the cell using Eq.(4.11.18). This process of boron tracking is continued, starting from the first cell of each flow segment, until all the cells in the vessel are tracked.

At the entrance to and exit from the core, the boron tracking must take into account that there are multiple core channels. The core inlet boron concentration is computed from Eq.(4.11.22), and the riser inlet boron concentration is computed from Eq.(4.11.23).

Boron tracking is also done in the steam dome. The inlet concentration is set to the concentration of the last cell of the riser if the flow is positive and to that in the steam dome if the flow is negative. Similarly, the outlet concentration is set to the boron concentration in the steam dome for positive flow and to the boron concentration of the first cell of DC1 for negative flow.

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4.11.3.3 Output Printing and Plotting

The output from RAMONA-4B is piped into an output file through UNIX. The standard boron output prints the average boron concentration in the core, the core inlet boron concentration, and the riser inlet boron concentration. On option (IPRNT $\geq$ 2), the local boron masses and boron concentrations in all cells of the RPV are also printed.

The graphic output of RAMONA-4B is generated on a plot file **fort.49**. This file must be renamed to **RAMONA1** for the plotting program **PAD** (based on the proprietary program **DISSPLA**) to produce the plots on a postscript or Talaris laser printer. If a long transient is run with multiple restarts, one must rename the first **fort.49** file as **RAMONA1**, the second **fort.49** file as **RAMONA2**, the third as **RAMONA3**, and so on. The program **PAD** will combine them together to produce the plots for the entire transient. At present, only the core-average boron concentration and the average boron concentrations of the multiple coolant channels in the core are saved on the plot file. These plot variables are defined as follows:

<u>Variable</u>	<u>Dimension</u>	<u>Description</u>
CBAVE	ZERO	Core-Average Boron Concentration, ppm
BORON	NPC	Channel-Average Boron Concentration, ppm

4.11.3.4 Wrapup/Restart

RAMONA-4B has a wrapup/restart capability. During a transient run, a wrapup file **fort.33** is generated, which can be used to restart the transient from where it was terminated. However, RAMONA-4B uses **fort.39** as the restart file. Thus, the wrapup file **fort.33** from the previous run must be renamed to **fort.39** prior to a restart run. This restart capability is very useful in calculating a long transient because the transient can be run in several phases with a shorter duration to get a better turn-around.

The restart procedures for RAMONA-4B are summarized below:

1. After a transient or steady-state run has been completed, rename the wrapup file **fort.33** to **fort.39** as the restart file.
2. The first number on Card 140000 needs to be changed to 3 for restarting from a steady state, or to 0 for restarting from a transient.
3. Finally, the transient termination time on Card 870000 must be increased to a higher value for the next phase. For example, to run a transient from 200 sec to 300 sec, the time on Card 870000 must be changed from 200 to 300.

5. MODELING OF CONTROL AND PROTECTION SYSTEMS

5.1 Overview

The plant control and protection systems are important component systems of the BWR. This chapter provides a description of the modeling for these systems.

There are three control systems in a BWR; namely, the pressure regulator for controlling the RPV pressure, the feedwater control system for controlling the water level in the RPV as well as the mismatch between the steam and feedwater flow rates, and the recirculation control system for controlling the core flow and power level. For the natural circulation SBWR, the recirculation pumps have been eliminated, thus there is no recirculation controller. RAMONA-4B models the control systems in terms of a lag, lead/lag, proportional integrator, second-order system, and time delay. The model equations are solved by explicit integration.

The plant protection systems are designed to provide protective actions to shut down the reactor, isolate the reactor containment, or initiate emergency core cooling systems if an unsafe condition exists. Such protective actions are initiated via various setpoints or safety limits imposed on important process variables such as the reactor power, system pressure, water level, feedwater flow rate, and steam flow rate. The plant protection systems consist of (a) the reactor protection system (RPS), (b) the emergency core cooling system (ECCS), and (c) the nuclear steam supply system (NSSS) isolation system. The RPS protects against conditions that could threaten the fuel barriers and reactor coolant pressure boundary by rapidly inserting all control rods (called "scram") upon detection of a violation of various scram setpoints. The ECCS is made up of several subsystems to provide emergency core cooling capability including the engineered safety features. The NSSS isolation system initiates automatic closure of valves within the steam lines penetrating the NSSS in the event that process conditions require isolation (e.g., MSIV closure). RAMONA-4B models the plant protection systems by means of a manual operation (via time specified initiation) or an automatic actuation initiated by various setpoints. In either case, a delay time is allowed via input.

5.2 Pressure Control System Model

The original pressure-regulator model of RAMONA-3B [Wulff et al., 1984a] does not pertain to the pressure control system in a BWR. The GE's pressure-regulator model as used in the Engineering Plant Analyzer [Wulff et al., 1984b] has therefore been implemented in RAMONA-4B replacing the old model. The new pressure-regulator model is illustrated in the block diagram as shown in Figure 5.1. The input to the pressure regulator is the turbine inlet pressure calculated by the steam line dynamics and the outputs are the turbine control valve (TCV) stem position, bypass valve-stem position, and the demand error required by the recirculation flow controller.

5.2.1 Model Formulation

5.2.1.1 Turbine-Control-Valve Model

5.2.1.1.1 Sensed Turbine Inlet Pressure Signal

The turbine inlet pressure is processed by a first-order LAG as a sensed pressure signal:

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$$\frac{dP_{ps}}{dt} = \frac{1}{\tau_{ps}} (G_{ps} P_{TCV} - P_{ps}), \quad (5.2.1)$$

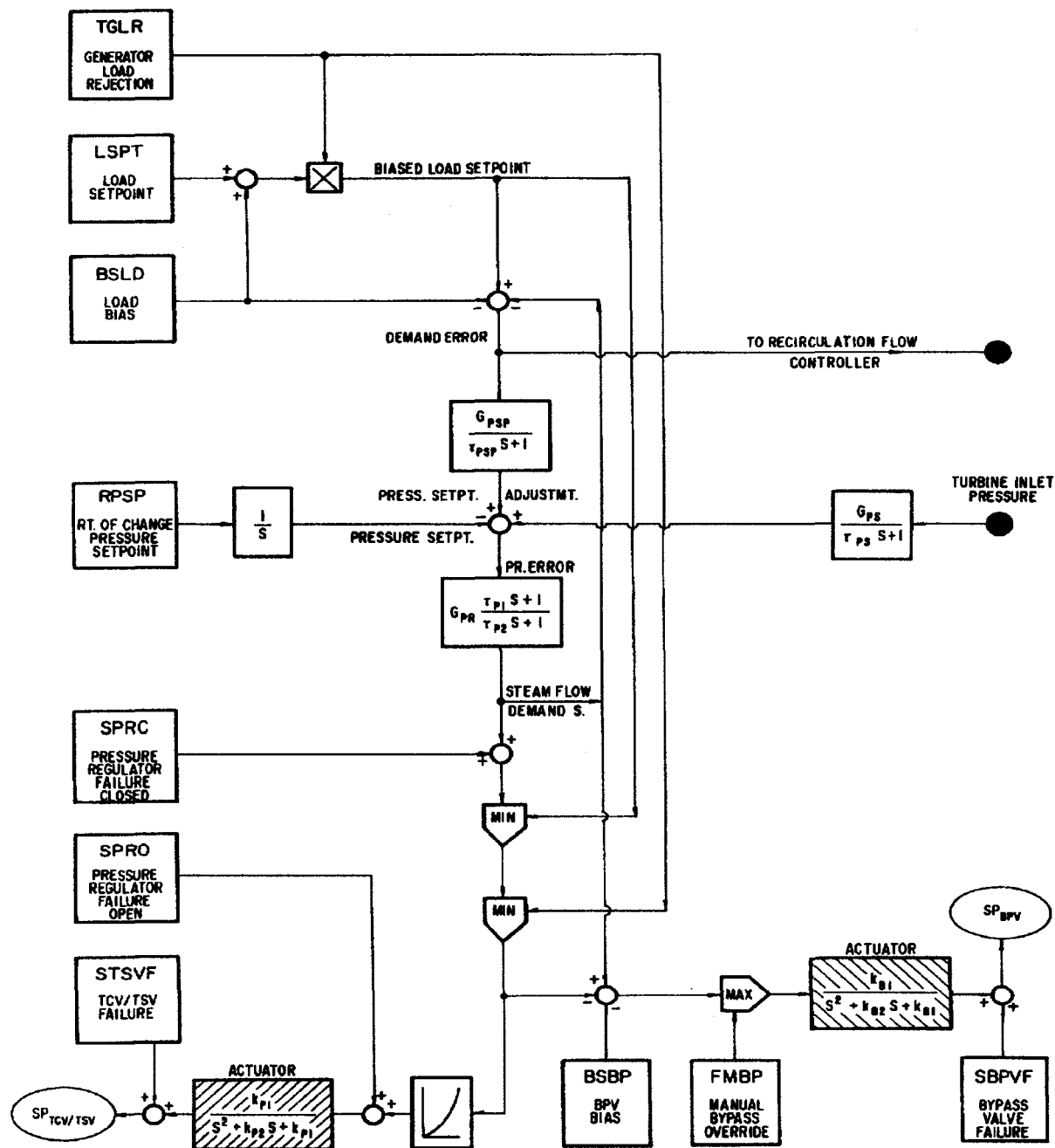


Figure 5.1 Block Diagram of BWR Pressure Regulator.

where

- P_{ps} = Sensed pressure signal,
 P_{TCV} = Turbine inlet pressure,
 G_{ps} = Gain factor for the LAG,
 τ_{ps} = Time constant for the LAG.

5.2.1.1.2 Demand Error

The demand error is the input to a first-order LAG for the pressure setpoint adjustment and also the output to the recirculation flow controller. It is defined as follows:

$$D_E = (L_{SPT} + B_L)T_{GLR} - B_L - X_{p0}, \quad (5.2.2)$$

where

- D_E = Demand error,
 L_{SPT} = Load setpoint,
 B_L = Load bias,
 T_{GLR} = Generator load rejection signal (0=tripped, 1=no trip),
 X_{p0} = Steam flow demand signal from a lead/lag compensator.

5.2.1.1.3 Pressure Setpoint Adjustment

The pressure setpoint adjustment is the output of a LAG compensator with the demand error as input:

$$\frac{dA_{PSP}}{dt} = \frac{1}{\tau_{PSP}} (G_{PSP} D_E - A_{PSP}), \quad (5.2.3)$$

where

- A_{PSP} = Pressure setpoint adjustment,
 G_{PSP} = Gain factor for the LAG,
 τ_{PSP} = Time constant for the LAG.

5.2.1.1.4 Pressure Setpoint

The pressure setpoint is treated as the output of an integrator with the rate of change of the pressure setpoint as an input parameter:

$$\frac{dP_{SP}}{dt} = R_{PSP}, \quad (5.2.4)$$

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where

P_{SP} = Pressure setpoint,
 R_{PSP} = Rate of change of the pressure setpoint (input).

5.2.1.1.5 Pressure Error

The pressure error is defined as follows:

$$P_E = P_{TCV} - P_{SP} + A_{PSP}, \quad (5.2.5)$$

where P_E is the pressure error.

5.2.1.1.6 Pressure Regulator (LLG)

The pressure error is input to a lead/lag compensator with the steam flow demand signal X_{p0} as the output:

$$\tau_{p2} \frac{dX_{p0}}{dt} + X_{p0} = G_{PR} \left(\tau_{p1} \frac{dP_E}{dt} + P_E \right), \quad (5.2.6)$$

where

X_{p0} = Steam flow demand signal,
 G_{PR} = Gain factor for the pressure regulator,
 τ_{p1} = Lead time for the LLG,
 τ_{p2} = Lag time for the LLG.

5.2.1.1.7 Pressure-Regulator Failure at Minimum Demand

The pressure-regulator failure (closed) signal S_{PRC} is added to the steam flow demand signal to simulate the pressure-regulator failure at minimum demand:

$$X_{p1} = X_{p0} + S_{PRC}, \quad (5.2.7)$$

where

S_{PRC} = Pressure regulator failure signal at minimum demand (closed)
= A large negative number (say, -10.0) for failure,
= 0 for no failure.

5.2.1.1.8 Minimum Gates

Two minimum gates are used to simulate load following and generator load rejection:

$$X_{p2} = \text{Min}(X_{p1}, Z_1), \quad (5.2.8)$$

$$X_{p3} = \text{Min}(X_{p2}, T_{GLR}), \quad (5.2.9)$$

where

$$Z_1 = (L_{SPT} + B_L)T_{GLR}. \quad (5.2.10)$$

5.2.1.1.9 Turbine-Control-Valve Nonlinear Characteristics

The TCV nonlinear characteristics are represented by the function:

$$\begin{aligned} F_{TCV} &= \frac{aX_{p3}}{1 + a - X_{p3}} \quad \text{for } 0 \leq X_{p3} \leq 1, \\ &= 0 \quad \text{for } -1 \leq X_{p3} \leq 0. \end{aligned} \quad (5.2.11)$$

where a is an input parameter (typically, $a=0.271114$ for BWRs).

The input to the TCV actuator is the output of the TCV function:

$$X_{p4} = F_{TCV}(X_{p3}). \quad (5.2.12)$$

5.2.1.1.10 Pressure-Regulator Failure at Maximum Demand

The pressure-regulator failure at maximum demand signal S_{PRO} is added to the output of the TCV nonlinear function to simulate the pressure-regulator failure at maximum demand:

$$X_{p5} = X_{p4} + S_{PRO}, \quad (5.2.13)$$

where

S_{PRO} = Pressure regulator failure signal at maximum demand (open)
 = A large positive number (say, +10.0) for failure,
 = 0 for no failure.

5.2.1.1.11 Turbine-Control-Valve Actuator

The T.V. actuator is modeled as a second-order system:

$$\frac{d^2 S_{TCV}}{dt^2} + 2\zeta_{TCV}\omega_{TCV}\frac{dS_{TCV}}{dt} + \omega_{TCV}^2(S_{TCV} - X_{p5}) = 0, \quad (5.2.14)$$

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where

- S_{TCV} = Output of the TCV actuator,
- X_{p5} = Input to the TCV actuator,
- ζ_{TCV} = Damping coefficient of the TCV actuator,
- ω_{TCV} = Natural frequency of the TCV actuator.

5.2.1.1.12 Turbine-Control-Valve-Failure Signal

The TCV-failure signal is used to simulate the failure of the turbine control valve either stuck open or stuck closed by adding it to the output of the TCV actuator:

$$SP_{TCV} = S_{TCV} + S_{TCVF}, \quad (5.2.15)$$

where

- SP_{TCV} = TCV stem position,
- S_{TCV} = Output of the TCV actuator,
- S_{TCVF} = TCV failure signal,
 - = A large negative number (say, -10.0) for TCV stuck closed,
 - = A large positive number (say, +10.0) for TCV stuck open.

5.2.1.2 Turbine-Bypass-Valve Model

5.2.1.2.1 Turbine-Bypass-Valve Bias

The BPV bias is taken into account in the input to a maximum gate:

$$Z_2 = X_{p0} - X_{p3} - B_{BPV}, \quad (5.2.16)$$

where B_{BPV} is the BPV bias treated as an input parameter.

5.2.1.2.2 Input Signal to Turbine-Bypass-Valve Actuator

The input signal to the turbine-bypass-valve actuator is the output of a maximum gate:

$$X_{p6} = \text{Max}(Z_2, S_{MBO}), \quad (5.2.17)$$

where S_{MBO} is the manual bypass override signal (0=No, 1=Yes) specified by an input.

5.2.1.2.3 Turbine-Bypass-Valve Actuator

The BPV actuator is represented by a second-order system as follows:

$$\frac{d^2 S_{BPV}}{dt^2} + 2\zeta_{BPV}\omega_{BPV}\frac{dS_{BPV}}{dt} + \omega_{BPV}^2(S_{BPV} - X_{p6}) = 0, \quad (5.2.18)$$

where

- S_{BPV} = Output of the BPV actuator,
- X_{ps} = Input signal to the BPV actuator,
- ζ_{BPV} = Damping coefficient of the BPV actuator,
- ω_{BPV} = Natural frequency of the BPV actuator.

5.2.1.2.4 Turbine-Bypass-Valve Failure Signal

The BPV failure signal is used to simulate the failure of the turbine bypass valve either stuck open or stuck closed by adding it to the output of the BPV actuator:

$$SP_{BPV} = S_{BPV} + S_{BPVF}, \quad (5.2.19)$$

where

- SP_{BPV} = BPV stem position,
- S_{BPV} = Output of the BPV actuator,
- S_{BPVF} = BPV failure signal,
- = A large negative number (say, -10.0) for BPV stuck closed,
- = A large positive number (say, +10.0) for BPV stuck open.

5.2.2 Numerical Solutions

The pressure-regulator-model equations are solved by means of explicit Euler integration using the same time step as the hydraulic integration. This is sufficiently accurate and efficient. A potential problem with numerical instability is taken care of by the automatic time-step control algorithm used for the hydraulic integration subject to a maximum permissible time step size specified by an input.

5.2.2.1 Turbine-Control-Valve Model

5.2.2.1.1 Sensed Turbine Inlet Pressure

Applying the first-order explicit integration to Eq.(5.2.1) yields the following numerical algorithm for the sensed turbine inlet pressure:

$$P_{ps}^{(n)} = P_{ps}^{(n-1)} + \frac{\Delta t_n}{\tau_{ps}} \left(G_{ps} P_{TCV}^{(n-1)} - P_{ps}^{(n-1)} \right), \quad (5.2.20)$$

where (n) stands for the time step index, and Δt_n is the nth time step size.

5.2.2.1.2 Demand Error

The demand error at the nth time step is given by

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$$D_E^{(n)} = (L_{SPT} + B_L)T_{GLR} - B_L - X_{p0}^{(n-1)}. \quad (5.2.21)$$

5.2.2.1.3 Pressure Setpoint Adjustment

The numerical algorithm for the pressure setpoint adjustment via the explicit Euler integration is

$$A_{PSP}^{(n)} = A_{PSP}^{(n-1)} + \frac{\Delta t_n}{\tau_{PSP}} (G_{PSP} D_E^{(n)} - A_{PSP}^{(n-1)}). \quad (5.2.22)$$

5.2.2.1.4 Pressure Setpoint

The nth-time-step pressure setpoint is the result of the explicit integration:

$$P_{SP}^{(n)} = P_{SP}^{(n-1)} + \Delta t_n R_{PSP}, \quad (5.2.23)$$

where R_{PSP} is a constant specified by an input.

5.2.2.1.5 Pressure Error

The pressure error at nth time step is simply

$$P_E^{(n)} = P_{TCV}^{(n)} - P_{SP}^{(n)} + A_{PSP}^{(n)}. \quad (5.2.24)$$

5.2.2.1.6 Pressure Regulator

The lead/lag compensator, Eq.(5.2.6), is solved by a method developed by Hortal [Hortal, 1987] as follows:

First, split the output variable X_{p0} into two parts:

$$X_{p0} = X_{p0,1} + X_{p0,2}, \quad (5.2.25)$$

where $X_{p0,1}$ is defined as

$$X_{p0,1} = G_{PR} \frac{\tau_{p1}}{\tau_{p2}} P_E. \quad (5.2.26)$$

Substituting Eqs.(5.2.25) and (5.2.26) into Eq.(5.2.6), we obtain

$$X_{p0,2} + \tau_{p2} \frac{dX_{p0,2}}{dt} = G_{PR} \left(1 - \frac{\tau_{p1}}{\tau_{p2}} \right) P_E. \quad (5.2.27)$$

Thus, the solution of Eqs. (5.2.25), (5.2.26), and (5.2.27) is the solution of the original Eq.(5.2.6).

Applying the explicit Euler integration to Eqs. (5.2.25), (5.2.26), and (5.2.27) yields the following numerical algorithms:

$$X_{p0,1}^{(n)} = G_{PR} \frac{\tau_{p1}}{\tau_{p2}} P_E^{(n)}, \quad (5.2.28)$$

$$X_{p0,2}^{(n)} = \frac{\left(\frac{\tau_{p2}}{\Delta t_n} X_{p0,2}^{(n-1)} + G_{PR} \left(1 - \frac{\tau_{p1}}{\tau_{p2}} \right) P_E^{(n)} \right)}{\left(1 + \frac{\tau_{p2}}{\Delta t_n} \right)}, \quad (5.2.29)$$

$$X_{p0}^{(n)} = X_{p0,1}^{(n)} + X_{p0,2}^{(n)}. \quad (5.2.30)$$

5.2.2.1.7 Pressure-Regulator Failure at Minimum Demand

The nth-step pressure-regulator failure at minimum demand signal is given by

$$X_{p1}^{(n)} = X_{p0}^{(n)} + S_{PRC}. \quad (5.2.31)$$

5.2.2.1.8 Minimum Gates

The nth-step output signals of the minimum gates are given by

$$X_{p2}^{(n)} = \text{Min} \left(X_{p1}^{(n)}, Z_1 \right), \quad (5.2.32)$$

$$X_{p3}^{(n)} = \text{Min} \left(X_{p2}^{(n)}, T_{GLR} \right). \quad (5.2.33)$$

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5.2.2.1.9 Turbine-Control-Valve Nonlinear Characteristics

The nth-step TCV nonlinear characteristic function is given by

$$\begin{aligned} F_{TCV}^{(n)} &= \frac{a X_{p3}^{(n)}}{1 + a - X_{p3}^{(n)}} \quad \text{for } 0 \leq X_{p3}^{(n)} \leq 1, \\ &= 0 \quad \text{for } -1 \leq X_{p3}^{(n)} \leq 0. \end{aligned} \quad (5.2.34)$$

The output of the TCV nonlinear function is simply

$$X_{p4}^{(n)} = F_{TCV}(X_{p3}^{(n)}). \quad (5.2.35)$$

5.2.2.1.10 Pressure-Regulator Failure at Maximum Demand

The nth-step pressure-regulator failure at maximum demand signal is given by

$$X_{p5}^{(n)} = X_{p4}^{(n)} + S_{PRO}. \quad (5.2.36)$$

5.2.2.1.11 Turbine-Control-Valve Actuator

We wish to solve Eq.(5.2.14) via a pair of ordinary differential equations. To this end, let

$$\frac{dS_{TCV}}{dt} = Y_{TCV}. \quad (5.2.37)$$

Then, Eq.(5.2.14) reduces to

$$\frac{dY_{TCV}}{dt} = -2\zeta_{TCV}\omega_{TCV}Y_{TCV} + \omega_{TCV}^2(X_{p4} - S_{TCV}). \quad (5.2.38)$$

Thus, the solution of Eqs.(5.2.37) and (5.2.38) is the solution of Eq.(5.2.14).

We now apply the explicit Euler integration to Eqs.(5.2.38) and (5.2.37), respectively, to obtain

$$\frac{Y_{TCV}^{(n)} - Y_{TCV}^{(n-1)}}{\Delta t_n} = -2\zeta_{TCV}\omega_{TCV}Y_{TCV}^{(n-1)} + \omega_{TCV}^2(X_{p4}^{(n-1)} - S_{TCV}^{(n-1)}), \quad (5.2.39)$$

$$\frac{S_{TCV}^{(n)} - S_{TCV}^{(n-1)}}{\Delta t_n} = Y_{TCV}^{(n)}, \quad (5.2.40)$$

whence

$$Y_{TCV}^{(n)} = (1 - 2\zeta_{TCV}\omega_{TCV}\Delta t_n)Y_{TCV}^{(n-1)} + \omega_{TCV}^2\Delta t_n(X_{p4}^{(n-1)} - S_{TCV}^{(n-1)}), \quad (5.2.41)$$

$$S_{TCV}^{(n)} = S_{TCV}^{(n-1)} + \Delta t_n Y_{TCV}^{(n)}. \quad (5.2.42)$$

5.2.2.2 Turbine-Bypass-Valve Model

5.2.2.2.1 Turbine-Bypass-Valve Bias

The BPV bias at the nth time step is given by

$$Z_2^{(n)} = X_{p0}^{(n)} - X_{p3}^{(n)} - B_{BPV}. \quad (5.2.43)$$

5.2.2.2.2 Input Signal to Turbine-Bypass-Valve Actuator

The nth-step input signal to the BPV actuator is simply

$$X_{p6}^{(n)} = \text{Max}(Z_2^{(n)}, S_{MBO}). \quad (5.2.44)$$

5.2.2.2.3 Turbine-Bypass-Valve Actuator

Using the same technique as applied to the TCV actuator, we obtain the following numerical solution algorithms for the BPV actuator:

$$Y_{BPV}^{(n)} = (1 - 2\zeta_{BPV}\omega_{BPV}\Delta t_n)Y_{BPV}^{(n-1)} + \omega_{BPV}^2\Delta t_n(X_{p6}^{(n-1)} - S_{BPV}^{(n-1)}), \quad (5.2.45)$$

$$S_{BPV}^{(n)} = S_{BPV}^{(n-1)} + \Delta t_n Y_{BPV}^{(n)}. \quad (5.2.46)$$

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5.2.3 Implementation in RAMONA-4B

The numerical algorithms presented above have been implemented in RAMONA-4B as the subroutine VALPOS. This subroutine is called by the subroutine CNTRL in the steam line dynamics.

5.3 Feedwater Control System Model

The feedwater-control-system model as used in the Engineering Plant Analyzer [Wulff et al., 1984b] has been implemented in RAMONA-4B. The feedwater-control-system model is illustrated in the block diagram as shown in Figure 5.2. The inputs to the feedwater controller are the steam flow rate calculated by the steam line dynamics, the feedwater flow rate from the BOP dynamics, and the collapsed-water level in the downcomer from the coolant dynamics. The output is the feedwater turbine valve (FTV) stem position which regulates the mass flow rate through the FTV controlling the feedwater pump speed.

5.3.1 Model Formulation

5.3.1.1 Sensed-Steam Flow-Rate Signal

The input steam flow rate is processed by a first-order LAG as a sensed-steam flow-rate signal:

$$\frac{dW_{sl,s}}{dt} = \frac{1}{\tau_{sl}} (G_{sl} W_{sl} - W_{sl,s}), \quad (5.3.1)$$

where

- $W_{sl,s}$ = Sensed-steam flow rate,
- W_{sl} = Input steam flow rate from steam line dynamics,
- G_{sl} = Gain factor for the LAG,
- τ_{sl} = Time constant for the LAG.

5.3.1.2 Sensed-Feedwater Flow-Rate Signal

The input feedwater flow rate is also processed by a first-order LAG as a sensed-feedwater flow-rate signal:

$$\frac{dW_{fw,s}}{dt} = \frac{1}{\tau_{fw}} (G_{fw} W_{fw} - W_{fw,s}), \quad (5.3.2)$$

where

- $W_{fw,s}$ = Sensed-feedwater flow rate,
- W_{fw} = Input feedwater flow rate from BOP dynamics,
- G_{fw} = Gain factor for the LAG,
- τ_{fw} = Time constant for the LAG.

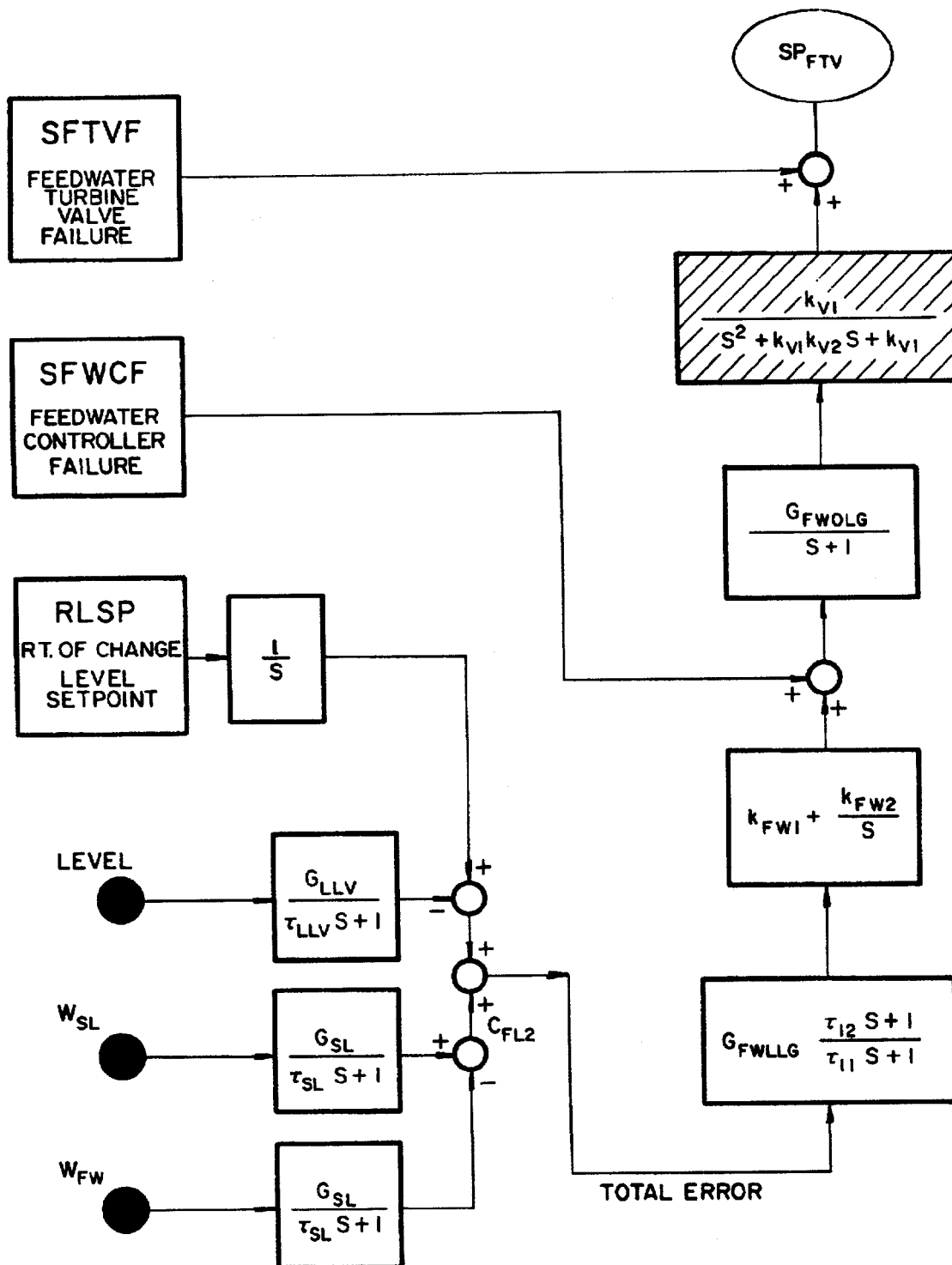


Figure 5.2 Block Diagram of Feedwater Control System.

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5.3.1.3 Sensed-Water-Level Signal

The sensed-water-level signal is the output of a LAG compensator with the collapsed-liquid level in the downcomer as input:

$$\frac{d\bar{L}_s}{dt} = \frac{1}{\tau_{lv}} (G_{lv} \bar{L}_{lv} - \bar{L}_s). \quad (5.3.3)$$

The bar over the levels L_s and L_{lv} indicates that they are the normalized levels defined as follows:

$$\bar{L}_s = \frac{L_s - L_{\min}}{L_{\max} - L_{\min}} \quad (5.3.4)$$

and

$$\bar{L}_{lv} = \frac{L_{lv} - L_{\min}}{L_{\max} - L_{\min}}, \quad (5.3.5)$$

where

- L_s = Sensed-water-level signal,
- L_{lv} = Input collapsed-liquid level from coolant dynamics,
- $L_{\max}$ = Maximum level of the narrow-range level sensor,
- $L_{\min}$ = minimum level of the narrow-range level sensor,
- G_{lv} = Gain factor for the LAG,
- τ_{lv} = Time constant for the LAG.

5.3.1.4 Total Error for Feedwater Controller

The total error for the feedwater controller is defined as follows:

$$E_{FWC} = (\bar{L}_{set} - \bar{L}_s)(L_{\max} - L_{\min}) + C_{FWC}(W_{sl,s} - W_{fw,s}), \quad (5.3.6)$$

where

$$C_{FWC} = \frac{0.0254 C_{f12} \cdot 100}{W_{sl,r}}, \quad (5.3.7)$$

$$\bar{L}_{set} = \frac{L_{set} - L_{\min}}{L_{\max} - L_{\min}}, \quad (5.3.8)$$

and

- C_{fl2} = Input parameter in inch per % flow (typically, 0.36),
 $W_{sl,r}$ = Rated steam mass flow rate in kg/s,
 L_{set} = Level setpoint as an input parameter.

5.3.1.5 Lead/Lag Compensator

The total feedwater controller error is fed to a lead/lag compensator:

$$\tau_{11} \frac{dX_1}{dt} + X_1 = G_{FWLLG} \left(\tau_{12} \frac{dE_{FWC}}{dt} + E_{FWC} \right), \quad (5.3.9)$$

where

- X_1 = Output signal of the lead/lag (LLG) compensator,
 G_{FWLLG} = Gain factor for the LLG,
 τ_{11} = Lag time for the LLG,
 τ_{12} = Lead time for the LLG.

5.3.1.6 PI Controller

The output signal from the LLG is input to a PI controller:

$$X_2 = K_{FW1} X_1 + K_{FW2} \int_0^t X_1 d\tau + X_2^{(0)}, \quad (5.3.10)$$

where

- X_2 = Output signal of the PI controller,
 $X_2^{(0)}$ = Initial value of X_2 ,
 K_{FW1} = P-constant for the PI controller,
 K_{FW2} = I-constant for the PI controller.

5.3.1.7 Feedwater-Controller-Failure Signal

The feedwater-controller-failure signal is added to the output signal of the PI controller to simulate the feedwater-controller failure at zero or maximum demand:

$$X_3 = X_2 + S_{FWCF}, \quad (5.3.11)$$

where

- S_{FWCF} = Feedwater-controller-failure signal,
 = A large negative number (-10) for failure at zero demand,

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= 0 for no failure,

= A large positive number (+10) for failure at max demand.

5.3.1.8 Lag Compensator

The combined signal from the PI controller is fed into a lag compensator:

$$\frac{dX_4}{dt} = \frac{1}{\tau_{21}} (G_{FWOLG} X_3 - X_4), \quad (5.3.12)$$

where

X_4 = Output signal of the LAG,

X_3 = Input signal to the LAG,

G_{FWOLG} = Gain factor for the LAG,

τ_{21} = Time constant for the LAG.

5.3.1.9 Feedwater-Turbine-Valve Actuator

The feedwater turbine valve (FTV) actuator is modeled as a second-order system:

$$\frac{d^2 S_{FTV}}{dt^2} + 2\zeta_{FTV} \omega_{FTV} \frac{dS_{FTV}}{dt} + \omega_{FTV}^2 (S_{FTV} - X_4) = 0, \quad (5.3.13)$$

where

S_{FTV} = Output of the FTV actuator,

X_4 = Input to the FTV actuator,

ζ_{FTV} = Damping coefficient of the FTV actuator,

ω_{FTV} = Natural frequency of the FTV actuator.

5.3.1.10 Feedwater-Turbine-Valve-Failure Signal

The FTV-failure signal is used to simulate the failure of the feedwater turbine valve either stuck open or stuck closed by adding it to the output of the FTV actuator:

$$SP_{FTV} = S_{FTV} + S_{FTVF}, \quad (5.3.14)$$

where

SP_{FTV} = FTV stem position,

S_{FTV} = Output of the FTV actuator,

S_{FTVF} = FTV failure signal,

= A large negative number (say, -10) for FTV stuck closed,

= A large positive number (say, +10) for FTV stuck open.

5.3.2 Numerical Solutions

The feedwater-control-system model equations are solved by means of the explicit Euler integration using the same time step as the hydraulic integration. This is sufficiently accurate and efficient. A potential problem with numerical instability is taken care of by the automatic time-step control algorithm used for the hydraulic integration subject to a maximum permissible time step size specified by an input.

5.3.2.1 Sensed-Steam-Flow Rate Signal

Applying the first-order explicit integration to Eq.(5.3.1) yields the following numerical algorithm for the sensed-steam flow-rate signal:

$$W_{sl,s}^{(n)} = W_{sl,s}^{(n-1)} + \frac{\Delta t_n}{\tau_{sl}} (G_{sl} W_{sl}^{(n-1)} - W_{sl,s}^{(n-1)}), \quad (5.3.15)$$

where (n) stands for the time-step index, and Δt_n is the nth-time-step size.

5.3.2.2 Sensed-Feedwater Flow-Rate Signal

Applying the first-order explicit integration to Eq.(5.3.2) yields the following numerical algorithm for the sensed-feedwater flow-rate signal:

$$W_{fw,s}^{(n)} = W_{fw,s}^{(n-1)} + \frac{\Delta t_n}{\tau_{fw}} (G_{fw} W_{fw}^{(n-1)} - W_{fw,s}^{(n-1)}). \quad (5.3.16)$$

5.3.2.3 Sensed-Water-Level Signal

Similarly, applying the explicit Euler integration to Eq.(5.3.3) yields the following numerical algorithm for the sensed-water-level signal:

$$\bar{L}_s^{(n)} = \bar{L}_s^{(n-1)} + \frac{\Delta t_n}{\tau_{lv}} [G_{lv} \bar{L}_{lv}^{(n-1)} - \bar{L}_s^{(n-1)}]. \quad (5.3.17)$$

5.3.2.4 Total Error for Feedwater Controller

The total feedwater controller error at the nth time step is simply

$$E_{FWC}^{(n)} = (\bar{L}_{set} - \bar{L}_s^{(n)}) (L_{max} - L_{min}) + C_{FWC} (W_{sl,s}^{(n)} - W_{fw,s}^{(n)}). \quad (5.3.18)$$

5.3.2.5 Lead/Lag Compensator

The lead/lag compensator, Eq.(5.3.9), is solved by means of the method due to Hortal [Hortal, 1987] as follows:

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First, split the output variable X_1 into two parts:

$$X_1 = X_{1,1} + X_{1,2}, \quad (5.3.19)$$

where $X_{1,1}$ is defined as

$$X_{1,1} = G_{FWLLG} \frac{\tau_{12}}{\tau_{11}} E_{FWC}. \quad (5.3.20)$$

Substituting Eqs.(5.3.19) and (5.3.20) into Eq.(5.3.9), we obtain

$$X_{1,2} + \tau_{11} \frac{dX_{1,2}}{dt} = G_{FWLLG} \left(1 - \frac{\tau_{12}}{\tau_{11}} \right) E_{FWC}. \quad (5.3.21)$$

Thus, the solution of Eqs. (5.3.19), (5.3.20), and (5.3.21) is the solution of the original Eq.(5.3.9).

Applying the explicit Euler integration to Eqs. (5.3.19), (5.3.20), and (5.3.21) yields the following numerical algorithms:

$$X_{1,1}^{(n)} = G_{FWLLG} \frac{\tau_{12}}{\tau_{11}} E_{FWC}^{(n)}, \quad (5.3.22)$$

$$X_{1,2}^{(n)} = \frac{\left(\frac{\tau_{11}}{\Delta t_n} \right) X_{1,2}^{(n-1)} + G_{FWLLG} \left(1 - \frac{\tau_{12}}{\tau_{11}} \right) E_{FWC}^{(n)}}{\left(1 + \frac{\tau_{11}}{\Delta t_n} \right)}, \quad (5.3.23)$$

$$X_1^{(n)} = X_{1,1}^{(n)} + X_{1,2}^{(n)}. \quad (5.3.24)$$

5.3.2.6 PI Controller

The nth-time-step PI controller output signal is given by

$$X_{2,i}^{(n)} = X_{2,i}^{(n-1)} + K_{FW2} X_1^{(n)} \Delta t_n, \quad (5.3.25)$$

$$X_2^{(n)} = K_{FW1} X_1^{(n)} + X_{2,i}^{(n)}. \quad (5.3.26)$$

5.3.2.7 Feedwater-Controller Failure Signal

The feedwater-controller failure signal is added to the nth-time-step output signal of the PI controller:

$$X_3^{(n)} = X_2^{(n)} + S_{FWCF}. \quad (5.3.27)$$

5.3.2.8 Lag Compensator

The nth-time-step LAG output signal is given by

$$X_4^{(n)} = X_4^{(n-1)} + \frac{\Delta t_n}{\tau_{21}} (G_{FWOLG} X_3^{(n)} - X_4^{(n-1)}). \quad (5.3.28)$$

5.3.2.9 Feedwater-Turbine Valve Actuator

We wish to solve Eq.(5.3.13) via a pair of ordinary differential equations. To this end, let

$$\frac{dS_{FTV}}{dt} = Y_{FTV}. \quad (5.3.29)$$

Then, Eq.(5.3.13) reduces to

$$\frac{dY_{FTV}}{dt} = -2\zeta_{FTV}\omega_{FTV}Y_{FTV} + \omega_{FTV}^2(X_4 - S_{FTV}). \quad (5.3.30)$$

Thus, the solution of Eqs.(5.3.29) and (5.3.30) is the solution of Eq.(5.3.13).

We now apply the explicit Euler integration to Eqs.(5.3.30) and (5.3.29), respectively, to obtain

$$\frac{Y_{FTV}^{(n)} - Y_{FTV}^{(n-1)}}{\Delta t_n} = -2\zeta_{FTV}\omega_{FTV}Y_{FTV}^{(n-1)} + \omega_{FTV}^2(X_4^{(n-1)} - S_{FTV}^{(n-1)}), \quad (5.3.31)$$

$$\frac{S_{FTV}^{(n)} - S_{FTV}^{(n-1)}}{\Delta t_n} = Y_{FTV}^{(n)}, \quad (5.3.32)$$

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whence

$$Y_{FTV}^{(n)} = [1 - 2\zeta_{FTV}\omega_{FTV}\Delta t_n]Y_{FTV}^{(n-1)} + \omega_{FTV}^2\Delta t_n[X_4^{(n-1)} - S_{FTV}^{(n-1)}], \quad (5.3.33)$$

$$S_{FTV}^{(n)} = S_{FTV}^{(n-1)} + \Delta t_n Y_{FTV}^{(n)}. \quad (5.3.34)$$

5.3.3 Implementation in RAMONA-4B

The numerical algorithms presented above have been implemented in RAMONA-4B as the subroutine FWCTRL. This subroutine is called twice by the subroutine NUMINT, the transient driver.

5.4 Recirculation Flow Control System

The recirculation-flow-control-system model as used in the Engineering Plant Analyzer [Wulff and others, 1984] has been implemented in RAMONA-4B. The recirculation flow control system model is illustrated in the block diagram as shown in Figure 5.3. The inputs to the recirculation flow controller are the steam demand error calculated by the pressure regulator and the generator angular speed calculated by the motor/generator set dynamics. The output is the scoop tube position for controlling the recirculation pump speed.

5.4.1 Model Formulation

5.4.1.1 Limiter for Steam Demand Error Signal

The steam demand error signal from the pressure regulator is limited by a maximum steam demand error $(D_E)_{\max}$:

$$Y_0 = -(D_E)_{\max} \leq D_E \leq (D_E)_{\max}. \quad (5.4.1)$$

5.4.1.2 PI Controller for Steam Demand Error

The limited steam demand error signal is input to a PI controller:

$$Y_1 = K_{d1}Y_0 + K_{d2} \int_0^t Y_0 d\tau + Y_1^{(0)}, \quad (5.4.2)$$

where

- Y_1 = Output signal of the PI controller,
- $Y_1^{(0)}$ = Initial value of Y_1 ,
- K_{d1} = P-constant for the PI controller,
- K_{d2} = I-constant for the PI controller.

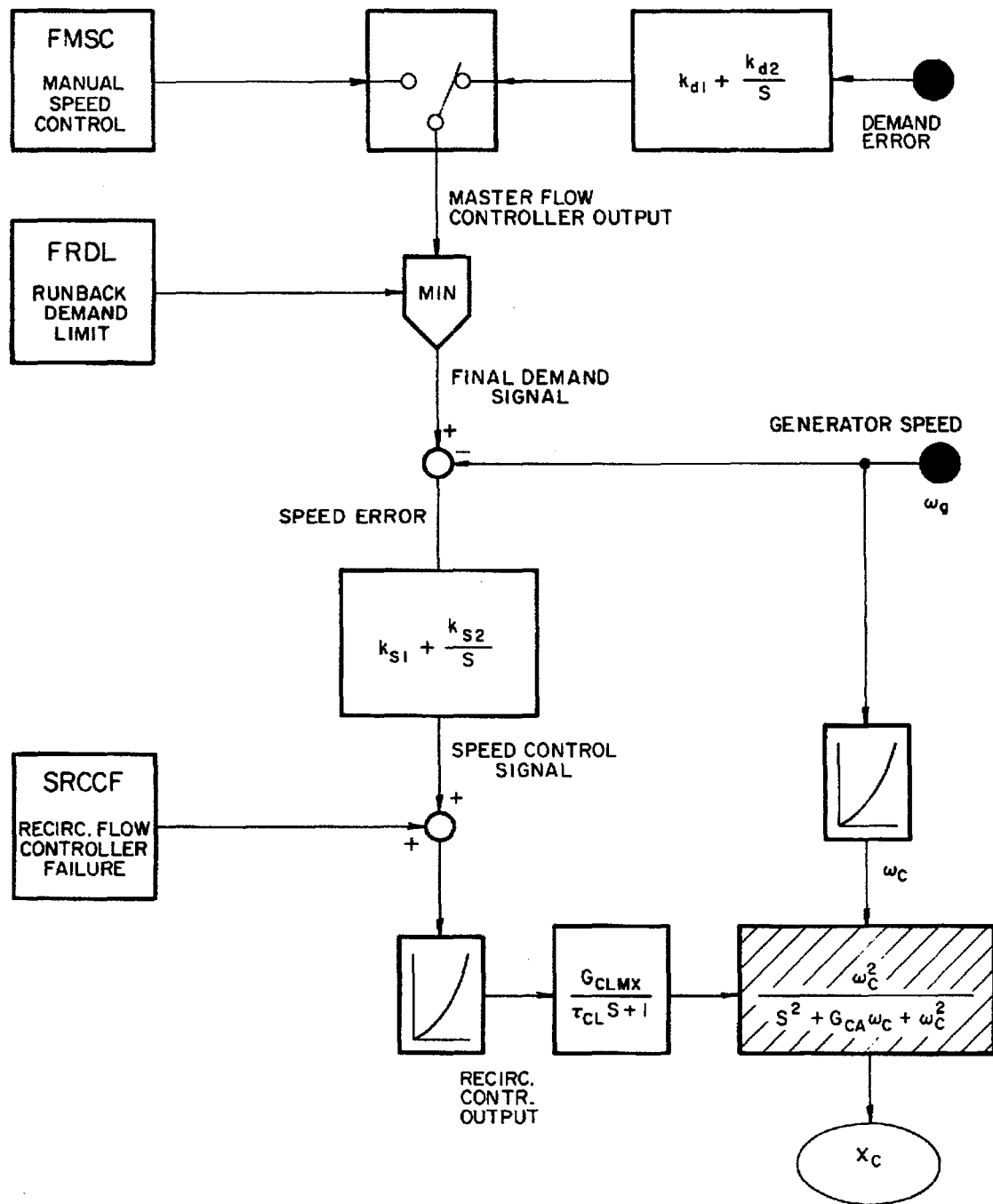


Figure 5.3 Block Diagram of Recirculation Flow Control System.

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5.4.1.3 Manual Speed Control Switch

Normally, the recirculation speed is controlled automatically. However, a manual speed control switch is provided as follows:

$$\begin{aligned} Y_2 &= Y_1 \quad \text{if } F_{msc} < 0, \text{ Automatic Mode,} \\ &= F_{msc} \quad \text{if } F_{msc} \geq 0, \text{ Manual Mode.} \end{aligned} \quad (5.4.3)$$

where F_{msc} is the manual speed control switch.

5.4.1.4 Runback Demand Limit

A runback demand limit is imposed on the processed steam demand error signal:

$$Y_3 = \min(Y_2, F_{rdl}), \quad (5.4.4)$$

where F_{rdl} is the runback demand limit.

5.4.1.5 Recirculation Controller Speed Error

The recirculation controller speed error is defined as

$$E_s = Y_3 - \frac{\omega_g}{\omega_{g0}}, \quad (5.4.5)$$

where

ω_g = generator angular speed of the motor/generator set,
 ω_{g0} = Initial generator angular speed at $t=0$.

5.4.1.6 PI Controller for Speed Error

The speed error signal is input to a PI controller to produce the speed control signal:

$$Y_4 = K_{s1} E_s + K_{s2} \int_0^t E_s d\tau + Y_4^{(0)}, \quad (5.4.6)$$

where

Y_4 = Output signal of the PI controller,
 $Y_4^{(0)}$ = Initial value of Y_4 ,
 K_{s1} = P-constant for the PI controller,
 K_{s2} = I-constant for the PI controller.

5.4.1.7 Recirculation Controller Failure Signal

The recirculation controller failure signal is added to the output signal of the PI controller to simulate the recirculation controller failure:

$$Y_5 = Y_4 + S_{RCCF}, \quad (5.4.7)$$

where

S_{RCCF} = Recirculation controller failure signal,
 = A large negative number (say, -10.0) for failure,
 = 0 for no failure.

5.4.1.8 Nonlinear Function for Speed Control Signal

The combined speed control signal is fed into a nonlinear function:

$$Y_6 = \sqrt{\frac{a Y_5}{b - Y_5}}, \quad (5.4.8)$$

where Y_6 is the recirculation control signal, $a=0.0543298$, and $b=1.021223$.

5.4.1.9 Lag Compensator for Recirculation Control Signal

The recirculation control signal is processed by a lag compensator:

$$\frac{dY_7}{dt} = \frac{1}{\tau_{cl}} (G_{clmx} Y_6 - Y_7), \quad (5.4.9)$$

where

Y_7 = Sensed recirculation control signal,
 G_{clmx} = Gain factor for the LAG,
 τ_{cl} = Time constant for the LAG.

5.4.1.10 Fluid Coupler Natural Frequency

The input generator angular speed is fed into a nonlinear function to obtain the natural frequency of the fluid coupler of the motor/generator set:

$$\omega_c = F_{fc}(\omega_g), \quad (5.4.10)$$

where F_{fc} is the nonlinear function for the fluid coupler, treated as an input function.

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5.4.1.11 Fluid Coupler Scoop Tube Actuator

The fluid coupler scoop tube actuator is modeled as a second order system:

$$\frac{d^2 X_c}{dt^2} + 2\zeta_c \omega_c \frac{dX_c}{dt} + \omega_c^2 (X_c - Y_7) = 0, \quad (5.4.11)$$

where

- X_c = Output of the scoop tube actuator,
- Y_7 = Input to the scoop tube actuator,
- ζ_c = Damping coefficient of the scoop tube actuator,
- ω_c = Natural frequency of the scoop tube actuator.

5.4.2 Numerical Solutions

The recirculation flow control system model equations are solved by means of the explicit Euler integration using the same time step as the hydraulic integration. This is sufficiently accurate and efficient. The potential problem with numerical instability is taken care of by the automatic time step control algorithm used for the hydraulic integration subject to a maximum permissible time step size specified by an input.

5.4.2.1 Limiter for Steam Demand Error Signal

Applying the first-order explicit integration to Eq.(1) yields the following numerical algorithm for the sensed steam demand error signal:

$$Y_0^{(n)} = -(D_E)_{\max} \leq D_E^{(n)} \leq (D_E)_{\max}, \quad (5.4.12)$$

where (n) stands for the time step index, and Δt_n is the nth time step size.

5.4.2.2 PI Controller for Steam Demand Error

Applying the first-order explicit integration to Eq.(5.4.2) yields the following numerical algorithm for the processed steam demand error signal:

$$Y_{1,i}^{(n)} = Y_{1,i}^{(n-1)} + K_{d2} Y_0^{(n)} \Delta t_n, \quad (5.4.13)$$

$$Y_1^{(n)} = K_{d1} Y_0^{(n)} + Y_{1,i}^{(n)}. \quad (5.4.14)$$

5.4.2.3 Manual Speed Control Switch

Similarly, applying the explicit Euler integration to Eq.(3) yields the following numerical algorithm for the master flow controller output:

$$\begin{aligned}
 Y_2^{(n)} &= Y_1^{(n)} \text{ if } F_{msc} < 0, \text{ Automatic Mode,} \\
 &= F_{msc} \text{ if } F_{msc} \geq 0, \text{ Manual Mode.}
 \end{aligned}
 \tag{5.4.15}$$

5.4.2.4 Runback Demand Limit

The final steam demand error signal at the nth-time step is simply

$$Y_3^{(n)} = \min(Y_2^{(n)}, F_{rdl}). \tag{5.4.16}$$

5.4.2.5 Recirculation Controller Speed Error

The recirculation speed error at the nth-time step is given by

$$E_s^{(n)} = Y_3^{(n)} - \frac{\omega_g^{(n)}}{\omega_{g0}}. \tag{5.4.17}$$

5.4.2.6 PI Controller for Speed Error

The nth-time-step PI controller output signal for speed error is given by

$$Y_{4,i}^{(n)} = Y_{4,i}^{(n-1)} + K_{s2} E_s^{(n)} \Delta t_n, \tag{5.4.18}$$

$$Y_4^{(n)} = K_{s1} E_s^{(n)} + Y_{4,i}^{(n)}. \tag{5.4.19}$$

5.4.2.7 Recirculation Controller Failure Signal

The recirculation controller failure signal is added to the nth-time step output signal of the PI controller:

$$Y_5^{(n)} = Y_4^{(n)} + S_{RCCF}. \tag{5.4.20}$$

5.4.2.8 Nonlinear Function for Speed Control Signal

The nth-time-step output signal from the nonlinear function is give by

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$$Y_6^{(n)} = \sqrt{\frac{a Y_5^{(n)}}{b - Y_5^{(n)}}}. \quad (5.4.21)$$

5.4.2.9 Lag Compensator for Recirculation Control Signal

$$Y_7^{(n)} = Y_7^{(n-1)} + \frac{\Delta t_n}{\tau_{cl}} (G_{clmx} Y_6^{(n)} - Y_7^{(n-1)}). \quad (5.4.22)$$

5.4.2.10 Fluid Coupler Natural Frequency

$$\omega_c^{(n)} = F_{fc}(\omega_g^{(n)}). \quad (5.4.23)$$

5.4.2.11 Fluid Coupler Scoop Tube Actuator

We wish to solve Eq.(5.4.11) via a pair of ordinary differential equations. To this end, let

$$\frac{dX_c}{dt} = Y_c. \quad (5.4.24)$$

Then, Eq.(5.4.11) reduces to

$$\frac{dY_c}{dt} = -2\zeta_c \omega_c Y_c + \omega_c^2 (Y_7 - X_c). \quad (5.4.25)$$

Thus, the solution of Eqs.(5.4.24) and (5.4.25) is the solution of Eq.(5.4.11).

We now apply the explicit Euler integration to Eqs.(5.4.25) and (5.4.24), respectively, to obtain

$$\frac{Y_c^{(n)} - Y_c^{(n-1)}}{\Delta t_n} = -2\zeta_c \omega_c Y_c^{(n-1)} + \omega_c^2 (Y_7^{(n-1)} - X_c^{(n-1)}), \quad (5.4.26)$$

$$\frac{X_c^{(n)} - X_c^{(n-1)}}{\Delta t_n} = Y_c^{(n)}, \quad (5.4.27)$$

whence

$$Y_c^{(n)} = [1 - 2\zeta_c \omega_c \Delta t_n] Y_c^{(n-1)} + \omega_c^2 \Delta t_n [Y_7^{(n-1)} - X_c^{(n-1)}], \quad (5.4.28)$$

$$X_c^{(n)} = X_c^{(n-1)} + \Delta t_n Y_c^{(n)}. \quad (5.4.29)$$

5.4.3 Implementation in RAMONA-4B

The numerical algorithms presented above have been implemented in RAMONA-4B as the subroutine RFCTRL. This subroutine is called twice by the subroutine NUMINT, the transient driver.

5.5 EPG Level Control Model

One of the major concern for an Anticipated Transient Without Scram (ATWS) in a BWR is the pressure suppression pool (PSP) heatup due to the long period of cycling of the safety/relief valves (SRV). The Technical Specification for some BWR containments is that the pool temperature be below 79.4 °C (175 °F) during an ATWS event. In order to meet this requirement, the BWR owners' group developed Emergency Procedure Guidelines (EPG), one of which is to keep the reactor power as low as practicable in the early phase of the ATWS by lowering the downcomer water level to the top of active fuel (TAF) until the Standby Liquid Control System (SLCS) is activated to inject boron into the reactor core. For the current BWR, this is done via the High Pressure Coolant Injection (HPCI) system.

The downcomer level control per EPG is not a trivial task since it may lead to partial uncovering of the core, if it is not done very carefully. Upon a request from NRC, we have developed an automatic level control model based on a level controller, which along with a time-dependent level setpoint can keep the water level at TAF in the early phase of an ATWS event and then raise the level by increasing the flow, after the SLCS has been activated, to speed up the boron circulation in the RPV. This will help shut down the reactor sooner while keeping the suppression pool temperature below the Tech Spec.

The level control per EPG objectives is modeled by means of a general-purpose level/flow controller as illustrated in Figure 5.4. It consists of two Proportional-Integral-Derivative (PID) control elements: one for a level error signal S_L and the other for a combined level and flow error signal T_r . The final combined error signal then determines the appropriate flow rate (in this case the HPCI flow) to maintain the level around a level setpoint L_R .

The level error signal is given by

$$E_L = L_R - L_m, \quad (5.5.1)$$

where L_m is the sensed water level and L_R is the requested water level setpoint (e.g., TAF).

The flow error signal is the mass inventory error due to the HPCI flow:

$$E_W = W_{HR,n} - W_{HC,n}, \quad (5.5.2)$$

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where $W_{HR,n}$ is the reference HPCI flow rate and $W_{HC,n}$ is the sensed HPCI flow rate.

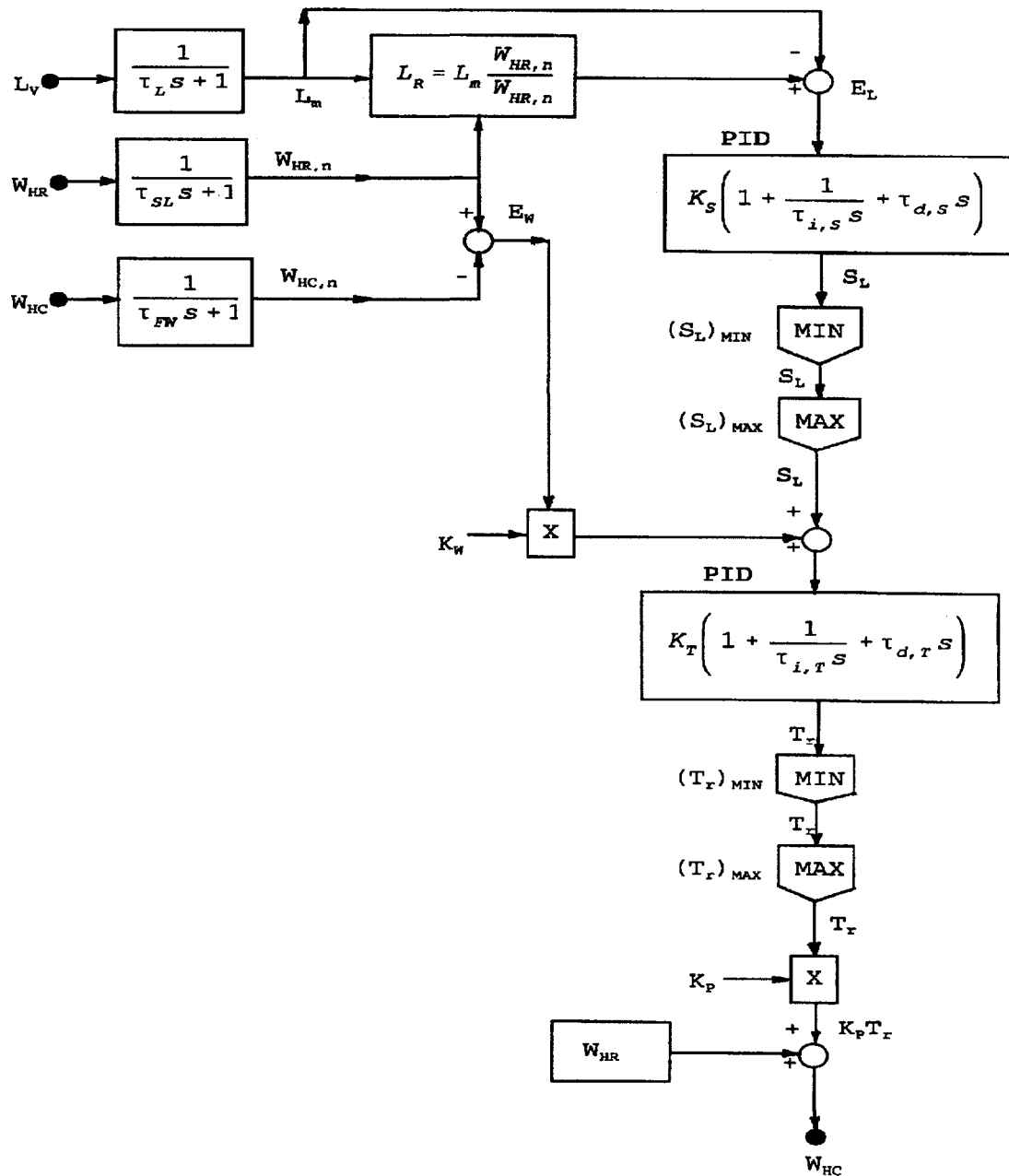


Figure 5.4 Block Diagram of the EPG Level Control Model.

The level and flow signals are sensed by a first-order LAG:

$$\frac{dL_m}{dt} = \frac{L_v - L_m}{\tau_L}, \quad (5.5.3)$$

$$\frac{dW_{HR,n}}{dt} = \frac{W_{HR} - W_{HR,n}}{\tau_{HR}}, \quad (5.5.4)$$

where L_v is the calculated water level and the τ 's are the time constants of the sensors.

The requested (or desired) level setpoint L_R is treated as a time-dependent function, a tubular input of L_R vs. time.

The level error signal E_L is fed into a PID, which generates an intermediate signal:

$$S_L = K_S \left(E_L + \frac{1}{\tau_{I,S}} \int_0^t E_L dt + \tau_{D,S} \frac{dE_L}{dt} \right). \quad (5.5.5)$$

The intermediate level signal is limited by a limiter to prevent an excessively strong response:

$$\begin{aligned} S_L &= S_{L,MAX} \quad \text{if } S_L > S_{L,MAX}, \\ S_L &= S_{L,MIN} \quad \text{if } S_L < S_{L,MIN}. \end{aligned} \quad (5.5.6)$$

The trimmed level signal is then combined with the flow error signal to form a total error signal:

$$E_T = S_L + K_W E_W. \quad (5.5.7)$$

Finally, the combined signal is supplied to another PID controller:

$$T_r = K_T \left(E_T + \frac{1}{\tau_{I,T}} \int_0^t E_T dt + \tau_{D,T} \frac{dE_T}{dt} \right). \quad (5.5.8)$$

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The intermediate combined signal T_r is also limited as follows:

$$T_r = T_{r,MAX} \quad \text{if } T_r > T_{r,MAX}, \quad (5.5.9)$$

$$T_r = T_{r,MIN} \quad \text{if } T_r < T_{r,MIN},$$

where $T_{r,MAX}$ is given by

$$T_{r,MAX} = f_{RWC} |T_{r,MIN}|. \quad (5.5.10)$$

The regulated HPCI flow rate to meet the requested level setpoint is given by the correlation:

$$W_{HC} = W_{HR} + K_P T_r, \quad (5.5.11)$$

where W_{HR} is the reference HPCI flow rate and K_P is given by

$$K_P = \frac{W_{HR}}{|T_{r,MIN}|}. \quad (5.5.12)$$

This model has been used to test the revised Emergency Procedure Guidelines as proposed by BWR owners' group with good success [Cheng and Rohatgi, 1996].

5.6 Plant Protection Systems

5.6.1 Introduction

The plant protection systems are designed to provide protective actions to shut down the reactor, isolate the reactor containment, or initiate emergency core cooling systems if an unsafe condition exists. Such protective actions are initiated via various set points or safety limits imposed upon important process variables such as the reactor power, system pressure, vessel water level, feedwater flow rate, and steam flow rate. The protection systems consist of (a) the reactor protection system (RPS), (b) the emergency core cooling system (ECCS), and (c) the nuclear steam supply system (NSSS) isolation system. The RPS protects against conditions that could threaten the fuel barriers and reactor coolant pressure boundary by rapidly inserting all control rods (called "scram") upon detection of a violation of various scram set points. The ECCS is made up of several subsystems to provide emergency core cooling capability including the engineered safety features. The NSSS isolation system initiates automatic closure of valves within the steam lines penetrating the NSSS in the event that process conditions require isolation (e.g., MSIV closure and turbine stop valve (TSV) closure).

RAMONA-4B has simplified but adequate models of the plant protection systems which directly affect the reactor operation. Specifically, RAMONA-4B simulates actions of the pressure regulator, Safety and Relief Valves (SRVs), and Main Steam Isolation Valve (MSIV); the code also models the responses of the plant protection systems on signals provided by the neutron flux sensors, water level and system pressure measuring devices, reactor trip logic, and the steam line valves

actuation. In addition, a feedwater trip can be activated on the transient time or too high downcomer water level signals. The High Pressure Coolant Injection (HPCI) and the Reactor Core Isolation Cooling (RCIC) systems also are modeled.

As shown in Figure 5.5, both manual and automatic modes of operation (including a time delay) are available for the Plant Protection System (PPS).

5.6.2 Reactor Protection System

The reactor protection system (RPS) consists of the reactor trip (scram) and the recirculation pump trip (RPT), both of which can be activated manually or automatically by monitoring various trip signals against their corresponding setpoints.

5.6.2.1 Reactor Trip (Scram)

The reactor trip scrams all the control rods to protect the reactor core (a) when any of four scram signals exceeds their setpoints, (b) whenever an NSSS isolation occurs (e.g., MSIV or TSV closure), and (c) by operator action. Thus, the reactor trip consists of eight scram trips as shown in Table 5.1.

Table 5.1 Summary of Reactor Trips

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. High Power Scram	High reactor power level	120% of rated	0.27
2. High Pressure Scram	High system pressure	105% of rated	0.27
3. Low Water Level Scram	Low downcomer water level	-0.6028 m	0.27
4. High Water Level Scram	High downcomer water level	1.6045 m	0.27
5. MSIV Closure Scram	MSIV closure	90% closed	0.27
6. TSV/TCV Closure Scram	Turbine stop/control valve closure	90% closed	0.27
7. Generator Load Rejection Scram	Loss of generator load	TGLR=-1	0.27
8. Manual Scram	Operator Action	Specify by time	0.27

5.6.2.2 Recirculation Pump Trip

The recirculation pump trip (RPT) is an important protective function in an Anticipated Transient Without Scram (ATWS) event and is considered as part of the reactor protection system. The RPT can be initiated manually by the operator in the control room. Modern BWRs are equipped with an automatic RPT logic which will trip the recirculation pumps if either one of the two RPT setpoints is exceeded. The automatic RPT can be initiated by either high system pressure or low downcomer water level. The manual RPT can be activated by either tripping directly the recirculation pump or tripping the motor-generator set. Thus, there are four RPT logics implemented in RAMONA-4B as shown in Table 5.2.

5. Modeling of Control and Protection Systems

Table 5.2 Summary of Recirculation Pump Trips

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. High Pressure RPT	High system (RPV) pressure	79.25 bar	0.53
2. Low Water Level RPT	Low downcomer water level	-1.8474 m	0.53
3. Direct Manual RPT	Operator tripping of recirculation pump	TRCP=-1	0.53
4. Indirect Manual RPT	Operator tripping of motor-generator set	TDMG=-1	0.53

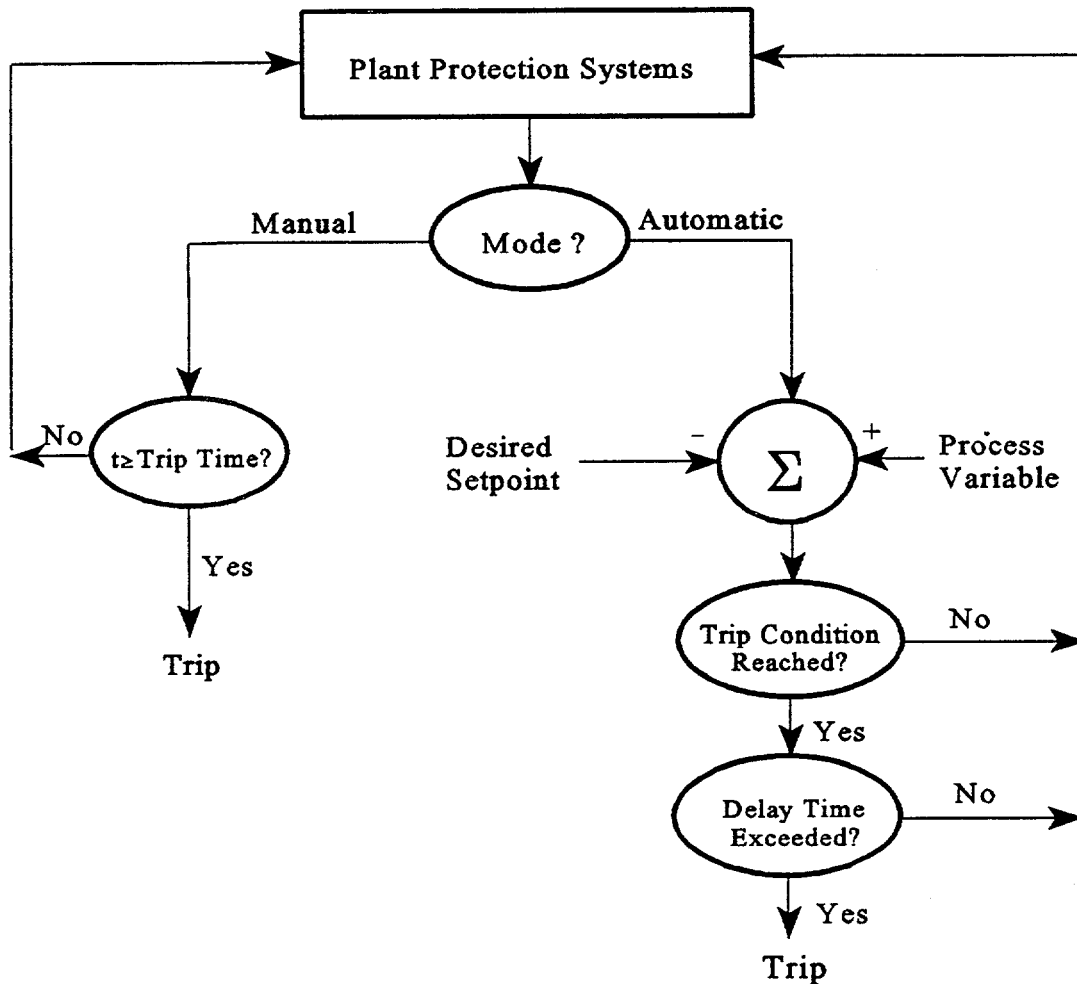


Figure 5.5 Manual and Automatic Trips of Plant Protection Systems.

5.6.3 Nuclear Steam Supply System (NSSS) Isolation

The NSSS isolation system initiates automatic closure of valves on steam lines downstream of containment penetration when needed. In RAMONA-4B, we have modeled the closures of main steam isolation valves (MSIVs), turbine stop valves (TSVs) or turbine control valves (TCVs), and turbine bypass valves (BPVs) as well as the feedwater turbine trip.

5.6.3.1 MSIV Closure

The MSIVs are designed to close on the following six signals:

- (1) Main steam line low pressure,
- (2) Main steam line high flow,
- (3) Reactor vessel low water level,
- (4) Operator action,
- (5) Main steam line break,
- (6) Main steam line high radiation.

In RAMONA-4B we implemented the MSIV closure on the first four of the trip signals listed above. Table 5.3 summarizes the MSIV closure trips as implemented in RAMONA-4B.

Table 5.3 Summary of MSIV Closure Trips

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. Low Pressure Closure	Main steam line low pressure	57.84 bar	1.0
2. Low Water Level Closure	Reactor vessel low water level	-4.667 m	1.0
3. High Steam Flow Closure	Main steam line high flow rate	3359 kg/s	1.0
4. Manual Closure	Operator action	Specified by time via input	1.0

5.6.3.2 Turbine Trip and Generator Trip

A sudden loss of condenser vacuum initiates a turbine trip *without* bypass. In this case, the turbines are shut off through fast-acting turbine stop valves within 0.1 s. This trip is accompanied by automatic scram unless scram is manually disabled. A high reactor vessel water level (Level 8) will also automatically initiate a turbine trip with bypass.

A loss of generator electrical load, often referred to as generator load rejection, will result in a generator trip which produces the following sequence of events:

- (1) Turbine-generator acceleration protection devices initiate turbine control valve (TCV) fast closure in about 0.2 s.

5. Modeling of Control and Protection Systems

- (2) The TCV fast closure is sensed by the reactor protection system which initiates a scram (for initial power level above 30% of the rated power).
- (3) The turbine bypass valves are opened simultaneously with the TCV fast closure, and used to control the system pressure during the trip.
- (4) Reactor vessel pressure rises to the relief valve setpoints, causing them to open and to discharge steam into the suppression pool.

Table 5.4 summarizes the turbine and generator trips implemented in RAMONA-4B.

Table 5.4 Summary of Turbine Trip and Generator Trip

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. Automatic Turbine Trip	High reactor vessel water level (Level 8)	1.605 m	0.1
2. Automatic Turbine Trip	Loss of condenser vacuum	Not implemented at present	0.1
3. Manual Turbine Trip	Operator action	Specified by time or on signal	0.1
4. Automatic Generator Trip	Loss of generator electrical load	STSVF=-1	0.2
5. Manual Generator Trip	Operator action	On signal TGLR=-1 Set TGLR=-1	0.2

5.6.3.3 Feedwater Trip

In RAMONA-4B, we implemented four feedwater trips: (a) feedwater check valve closure, (b) feedwater turbine valve closure, (c) feedwater pump trip, and (d) feedwater control system failure trip. The feedwater check valve (FCV) closure is activated through the signal SFCVF by input. The feedwater turbine valve closure is modeled as part of the feedwater control system presented in Section 5.3 via a feedwater turbine valve failure signal (SFTVF). A feedwater pump trip will occur whenever the reactor vessel water level reaches the high-level setpoint (Level 8). The feedwater pump trip is modeled in RAMONA-4B by the trip signal RPT=-1 specified by time via input. The feedwater control system failure trip is modeled as part of the feedwater control system presented in Section 5.3 via a feedwater controller failure signal (SFWCF) specified by input. Table 5.5 summarizes the various feedwater trips implemented in RAMONA-4B.

Table 5.5 Summary of Feedwater Trips

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. Feedwater Check Valve Closure	Operator action	SFCVF=-1	0.5
2. Feedwater Pump Trip	High vessel water level	1.605 m (Level 8)	0.5
3. Feedwater Turbine Valve Closure	Operator action	SFTVF=-1	0.5
4. Feedwater Controller Failure	Operator action	SFWCF=-1	0.5

5.6.4 Emergency Core Cooling Systems

The emergency core cooling system (ECCS) is made up of several subsystems to provide emergency core cooling capability to remove decay heat from the reactor core whenever the normal feedwater-steam cycle heat removal mechanism is incapable of performing its functions for any reason.

Core cooling is accomplished by pumping water into the reactor vessel from the condensate storage tank or from the suppression pool or both. For small breaks, the turbine driven high pressure coolant injection (HPCI) system takes its suction from the condensate storage tank to augment the normal function of the reactor core isolation cooling (RCIC) system. In case of large breaks, the condensate storage tank inventory may be depleted, in which case the system suction is valved into the suppression pool and the HPCI turbine continues to be driven by reactor steam until the reactor vessel is depressurized to a point where the core spray system and the low pressure coolant injection (LPCI) system can provide adequate cooling because of their high capacity at low pressures.

In the event that the HPCI system can not function because of equipment failure, the depressurization is accomplished by the automatic depressurization system (ADS) which relieves reactor steam directly to the suppression pool through dual purpose automatic relief valves.

The core spray system takes water from the suppression pool and pumps it into the reactor vessel through circular spray spargers above the reactor core by means of pumps driven by a-c electric motors. The LPCI system also takes water from the suppression pool and pumps it into the reactor vessel via the recirculation loops piping to accomplish a core flooding action. The core spray and LPCI functions are only part of several functions performed by the residual heat removal (RHR) system described in Chapter 7, Sections 7.2.3.4 and 7.2.3.5. The RHR system also performs the long-term shutdown cooling and containment spray cooling. Table 5.6 summarizes the ECCS Trips implemented in RAMONA-4B.

Table 5.6 Summary of ECCS Trips

Trip	Initiating Mechanism	Setpoint	Delay Time (s)
1. Automatic HPCI Turn-on and Turn-off	Low and high water level	-1.847 and 1.605 m	25 ~ 30
2. Automatic RCIC Turn-on	Low vessel water level	-1.847 m	25 ~ 30
3. Manual HPCI Turn-on and Turn-off	Operator action	Specified by times	25 ~ 30
4. Manual RCIC Turn-on and Turn-off	Operator action	Specified by times	25 ~ 30
5. Core Spray Turn-on	Low RPV pressure	8.412 bar	25 ~ 30
6. Core Spray Turn-off	Operator action	Specified by time	25 ~ 30
7. LPCI Turn-on	Low RPV pressure	1.379 bar	25 ~ 30
8. LPCI Turn-off	Operator action	Specified by time	25 ~ 30

6. MODELING OF BALANCE OF PLANT (BOP)

A schematic diagram of the BWR plant layout is shown in Figure 6.1. The balance of plant (BOP) models as used in the Engineering Plant Analyzer [Wulff et al., 1984b] have been implemented in RAMONA-4B. The inputs to the BOP are the steam flow rate, turbine control valve inlet pressure and turbine bypass valve inlet pressure calculated by the steamline dynamics, and the initial value of condenser pressure. The outputs are the feedwater flow rate, feedwater temperature, feedwater pump speed, condenser mixture enthalpy, and condenser pressure.

6.1 Model Formulation

6.1.1 Turbine Dynamics

6.1.1.1 Assumptions

1. The high-pressure and low-pressure turbines are represented by a two-stage turbine, an impulse stage and reaction stage.
2. The flow is quasi-steady with negligible flow inertia.

6.1.1.2 Mass Flow Rates

The mass flow rate through the turbine control valve (TCV) is derived from a quasi-static momentum balance in terms of the state variables P_{TCV} and P_c :

$$W_{TCV} = Z_{TCV} \sqrt{P_{TCV} - P_c} \quad (6.1.1)$$

where W_{TCV} is the TCV mass flow rate, P_{TCV} is the TCV inlet pressure, P_c is the condenser pressure, and Z_{TCV} is the TCV characteristic function given by

$$Z_{TCV} = \sqrt{\frac{b_0}{a_0 + \frac{1}{SP_{TCV}}}} \quad (6.1.2)$$

Here $a_0 = 1.20222$, $b_0 = 1.17717$ are typical values for BWRs, and SP_{TCV} is the TCV stem position.

The mass flow rate through the low-pressure turbine (LPT) is also derived from a simple mechanical energy balance:

$$W_{L2} = W_{TCV} \frac{1}{1 + \sqrt{\frac{K_{LPT}}{K_{FWH}}}} \quad (6.1.3)$$

6. Modeling of Balance of Plant (BOP)

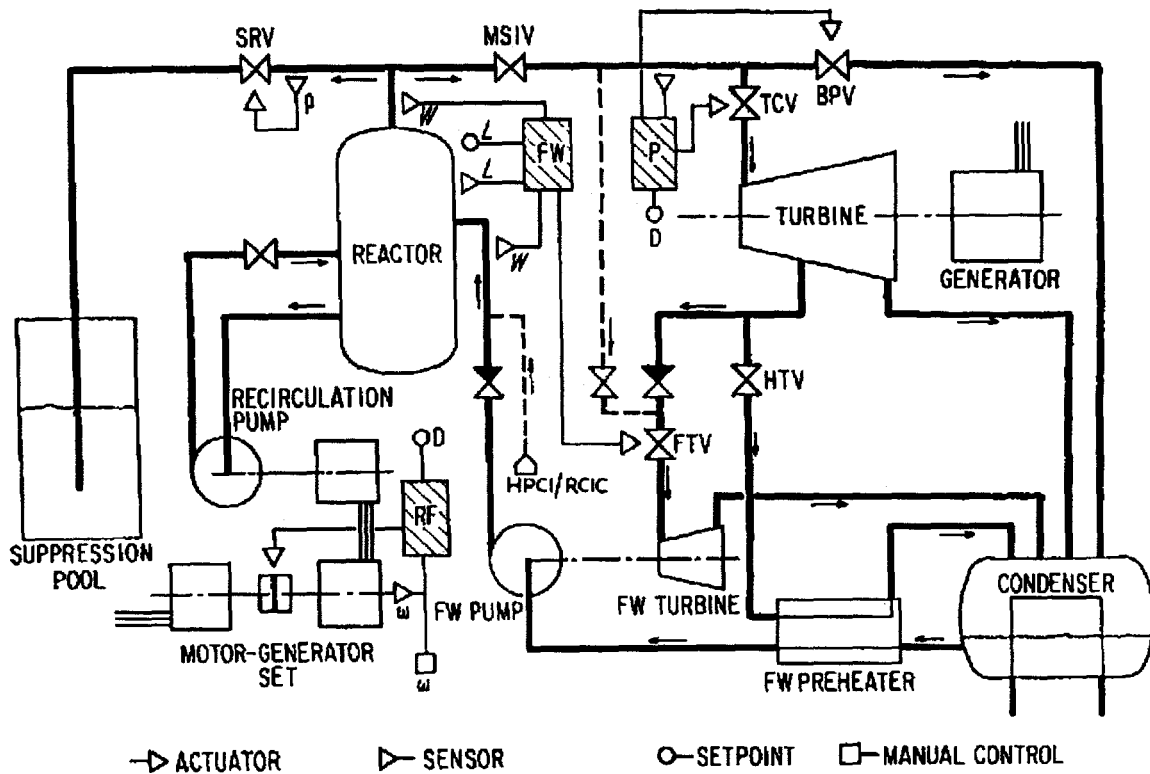


Figure 6.1 Schematic of the BWR4 Plant Layout

where

- W_{t2} = Mass flow rate through the LPT,
- K_{LPT} = Pressure loss coefficient for the LPT,
- K_{FWH} = Pressure loss coefficient for the feedwater heaters.

The mass flow rate of the extraction steam from the high-pressure turbine is simply

$$W_{t3} = W_{TCV} - W_{t2}, \quad (6.1.4)$$

where W_{t3} is the mass flow rate of the extraction steam to the feedwater heaters.

Again, applying the quasi-static momentum balance yields the mass flow rate through the feedwater turbine valve (FTV):

$$W_{FTV} = Z_{FTV} \sqrt{P_{l3} - P_c} \quad (6.1.5)$$

where P_{l3} is the upstream pressure of the LPT and Z_{FTV} is the FTV characteristic function prescribed by

$$Z_{FTV} = (Z_{FTV})_{max} \left[\frac{SP_{FTV} (C_{FTV1} + C_{FTV2} SP_{FTV})}{1 + C_{FTV3} SP_{FTV}} \right]^{C_{FTV4}}, \quad (6.1.6)$$

with

$$(Z_{FTV})_{max} = \frac{(W_{FTV})_{max}}{\sqrt{P_{l3}^{(0)} - P_c^{(0)}}}. \quad (6.1.7)$$

Here the coefficients C_{FTV1} , C_{FTV2} , C_{FTV3} , C_{FTV4} are input constants, $(W_{FTV})_{max}$ is the maximum value of the FTV mass flow rate treated as an input, W_{FTV} is the FTV mass flow rate, SP_{FTV} is the FTV stem position, and the superscript (0) stands for the initial condition.

6.1.1.3 Turbine Inlet and Exit Pressures

The upstream pressure of the high-pressure turbine (HPT) is given by a mechanical energy balance as follows:

$$P_{l1} = P_{TCV} - W_{TCV}^2 \left(K_{TCV} + \frac{1}{SP_{TCV}} - \frac{1}{SP_{TCV}^{(0)}} \right), \quad (6.1.8)$$

where K_{TCV} is the pressure loss coefficient of TCV.

The upstream pressure of the low-pressure turbine (LPT) is related to the upstream pressure of the HPT by

$$P_{l3,x} = P_{l1} - K_{HPT} W_{TCV}^2, \quad (6.1.9)$$

where K_{HPT} is the pressure loss coefficient of HPT.

The LPT upstream pressure is limited by a cross-over pressure setpoint P_{cro} specified as an input:

$$P_{l3,y} = \text{Min}(P_{l3,x}, P_{cro}). \quad (6.1.10)$$

Furthermore, when the main steamline isolation valve (MSIV) closes, the pressure $P_{l3,x}$ reduces instantaneously to the condenser pressure P_c due to the quasi-static-flow approximation. To smooth the transition, a first-order LAG is employed to filter the LPT upstream pressure:

6. Modeling of Balance of Plant (BOP)

$$\frac{dP_{\ell 3}}{dt} = \frac{1}{\tau_{p\ell 3}} (P_{\ell 3,y} - P_{\ell 3}), \quad (6.1.11)$$

where $\tau_{p\ell 3}$ is an input time constant for the LAG.

6.1.1.4 Isentropic Expansion in Turbines

The enthalpy loss in a turbine due to isentropic expansion can be represented by

$$\Delta h_{is} = (h_m)_i f_1(P_i, P_e) + f_2(P_i, P_e), \quad (6.1.12)$$

where P_i is the inlet pressure, P_e is the exit pressure, and $(h_m)_i$ is the inlet mixture enthalpy. The two functions f_1 and f_2 are defined as

$$f_1(P_i, P_e) = \frac{T_s(P_e)}{T_s(P_i)} - 1, \quad (6.1.13)$$

$$f_2(P_i, P_e) = T_s(P_e) \left[\int_{P_i}^{P_e} \frac{v_f(P)}{T_s(P)} dP - \int_{T_s(P_i)}^{T_s(P_e)} h_f(T_s) \frac{dT_s}{T_s^2} \right], \quad (6.1.14)$$

where T_s is the saturation temperature, v_f is the specific volume of saturated liquid, and h_f is the specific enthalpy of the saturated liquid.

Applying to the HPT and LPT as shown in Figure 6.2, we obtain

$$(\Delta h_{is})_{HPT} = h_g(P_s) f_1(P_{\ell 1}, P_{\ell 3}) + f_2(P_{\ell 1}, P_{\ell 3}), \quad (6.1.15)$$

$$(\Delta h_{is})_{LPT} = h_g(P_{\ell 3}) f_1(P_{\ell 3}, P_c) + f_2(P_{\ell 3}, P_c), \quad (6.1.16)$$

where h_g is the specific enthalpy of saturated steam.

Note that the enthalpy loss in the feedwater turbine is the same as that in the LPT; namely,

$$(\Delta h_{is})_{FWT} = (\Delta h_{is})_{LPT}. \quad (6.1.17)$$

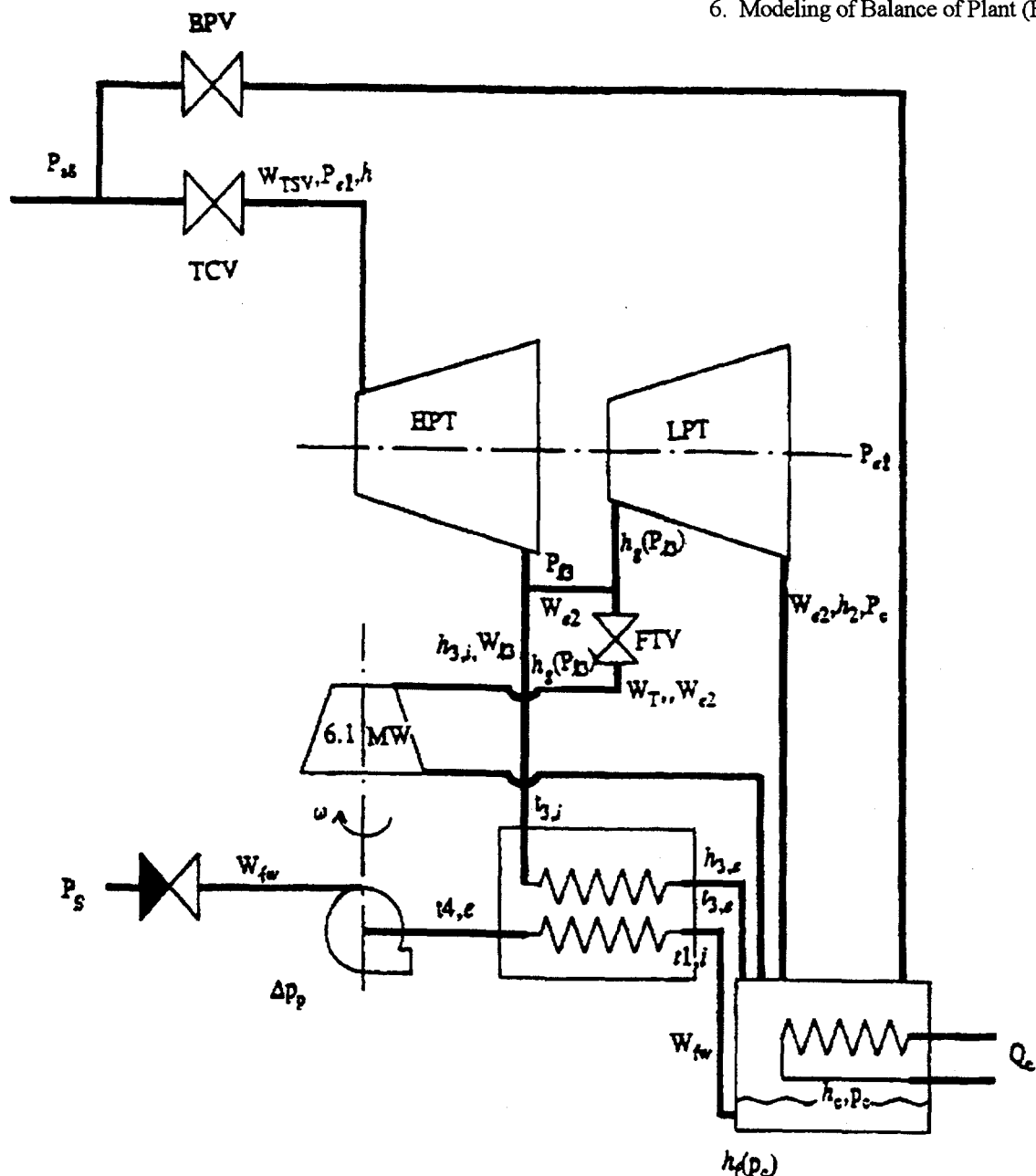


Figure 6.2 Schematic of the BOP of a BWR

6.1.1.5 Turbine Efficiencies

The turbine stage efficiency is computed from

$$\eta_s = \frac{\eta_i}{1 - \phi_E \left[(1 - r^*) \left(\varphi(\cos \alpha_1) \frac{V_{m2}}{V_{m1}} \right)^2 + \left(v \frac{R_2}{R_1} - \psi \sqrt{E} \right)^2 \right]}, \quad (6.1.18)$$

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where the inner efficiency is given by Euler as

$$\eta_i = 2v^2 \left[\sin \alpha_1 \frac{\phi}{v} \sqrt{1-r^*} - \frac{R_2}{R_1} \left(\frac{R_2}{R_1} - \frac{\psi}{v} \sqrt{E} \right) \right], \quad (6.1.19)$$

with

$$v = \frac{R_1 \omega}{\sqrt{2 \Delta h_{is}}}, \quad (6.1.20)$$

and

$$\begin{aligned} E = (1+f)r^* + \phi^2 \left[\phi_R - \cos^2 \alpha_1 \left(\frac{V_{m2}}{V_{m1} \psi} \right)^2 \right] (1-r^*) \\ - 2\phi v \phi_R \sin \alpha_1 \sqrt{1-r^*} + v^2 \left[\phi_R + \left(\frac{R_2}{R_1} \right)^2 - 1 \right]. \end{aligned} \quad (6.1.21)$$

The various turbine stage parameters in the above equations are defined in Table 6.1 along with the typical values for a BWR4. These parameters are treated as input in RAMONA-4B.

Table 6.1 Typical Turbine Stage Parameters

<u>Definition</u>	<u>Symbol</u>	<u>HPT</u>	<u>LPT</u>
Theoretical degree of reaction	r^*	0.40	0.50
Rotor entrance flow angle	α_1	75°	75°
Mean radius ratio	R_2/R_1	1.00	1.033
Velocity coefficient (stator loss)	ϕ	0.97	0.96
Carry-over coefficient	ϕ_R	0.98	0.95
Velocity coefficient (rotor loss)	ψ	0.95	0.948
Reheat factor	f	0.089	0.025
Recovery of exit kinetic energy	ϕ_E	1.00	0.95
Meridian velocity ratio	V_{m2}/V_{m1}	1.20	1.20

Using the typical turbine stage parameters listed in Table 5.1, we obtain the following turbine efficiencies for both HPT and LPT:

$$\eta_{HPT} = \frac{v(1.451514 - 2v + 1.9\sqrt{E_{HPT}})}{0.945544 - (v - 0.95\sqrt{E_{HPT}})^2}, \quad (6.1.22)$$

where

$$E_{HPT} = 0.92843 - 1.422483v + 0.98v^2, \quad (6.1.23)$$

and

$$\eta_{LPT} = \frac{v(1.311384 - 2.132113v + 1.95762\sqrt{E_{LPT}})}{0.957773 - (1.006357v - 0.923996\sqrt{E_{LPT}})^2}, \quad (6.1.24)$$

where

$$E_{LPT} = 0.90080 - 1.245815v + 1.016056v^2. \quad (6.1.25)$$

The feedwater turbine efficiency is given by

$$\eta_{FWT} = 0.81667\eta_{LPT}(v_{FWT}). \quad (6.1.26)$$

6.1.1.6 Turbine Exit Enthalpies

The extraction steam enthalpy (exit enthalpy of the HPT) is given by

$$h_{EXT} = \left(1 + \sqrt{\frac{K_{FWH}}{K_{LPT}}} \right) [(h_i)_{HPT} - \eta_{HPT}(\Delta h_{is})_{HPT}] - \sqrt{\frac{K_{FWH}}{K_{LPT}}} (h_i)_{LPT}, \quad (6.1.27)$$

where

$$(h_i)_{HPT} = h_g(P_s), \quad (6.1.28)$$

$$(h_i)_{LPT} = h_g(P_{t3}), \quad (6.1.29)$$

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and the exit enthalpy of the LPT is computed from

$$(h_e)_{LPT} = (h_i)_{LPT} - \eta_{LPT}(\Delta h_{is})_{LPT}. \quad (6.1.30)$$

The exit enthalpy of the FTV is given by

$$(h_e)_{FWT} = h_{EXT} - \eta_{FWT}(\Delta h_{is})_{FWT}. \quad (6.1.31)$$

6.1.1.7 Turbine Power and Turbine Torque

The turbine power is given by

$$P_T = \eta_{HPT}(\Delta h_{is})_{HPT} W_{TCV} + \eta_{LPT}(\Delta h_{is})_{LPT} W_{LPT}, \quad (6.1.32)$$

and the turbine torque is computed from

$$M_{FWT} = \frac{\eta_{FWT}}{\omega} (\Delta h_{is})_{FWT} W_{FTV}, \quad (6.1.33)$$

where ω is the feedwater pump speed.

6.2 Feedwater Train Dynamics

6.2.1 Assumptions

1. Feedwater is single-phase liquid and incompressible.
2. Feedwater pump is a standard centrifugal pump.
3. Form loss coefficients and friction factors are constant.

6.2.2 Feedwater Pump Head

The feedwater pump head characteristics is prescribed by the following dimensionless function:

$$\psi(\varphi) = a\varphi^2 + b\varphi + c, \quad (6.2.1)$$

where the coefficients a , b , and c are input parameters (e.g., $a=-0.508159$, $b=-0.191841$, $c=1.70$), φ is the standard flow coefficient defined as

$$\varphi = \left(\frac{W_{fw}}{W_{fw}^{(0)}} \right) \left(\frac{\omega^{(0)}}{\omega} \right), \quad (6.2.2)$$

and ψ is the pressure coefficient defined as

$$\psi = \frac{\Delta P_{FWP}}{\rho_l d^2 \omega^2}, \quad (6.2.3)$$

where d is the impeller tip diameter, W_{fw} is feedwater flow rate, ΔP_{FWP} is the feedwater pump head, ω is the angular speed of the feedwater pump/turbine assembly, and ρ_l is the feedwater density. Eliminating d and ρ_l from Eq.(5.5.36) based on the initial condition, we obtain the transient feedwater pump head as follows:

$$\Delta P_{FWP} = \Delta P_{FWP}^{(0)} \psi(\varphi) \frac{\omega^2}{(\omega^{(0)})^2}, \quad (6.2.4)$$

where the superscript (0) stands for the initial condition.

6.2.3 Pressure Drop in the Feedwater Train

The pressure drop in the feedwater train is related to the feedwater flow rate W_{fw} as follows:

$$\Delta P_{FWL} = K_{FWL} (W_{fw})^2, \quad (6.2.5)$$

where the loss coefficient K_{FWL} (assumed constant during a transient) is determined from the initial condition,

$$K_{FWL} = \frac{\Delta P_{FWP}^{(0)} + P_c^{(0)} - P_s^{(0)}}{(W_{fw}^{(0)})^2}. \quad (6.2.6)$$

Here ΔP_{FWP} is the feedwater pump head, P_s is the RPV system pressure, and P_c is the condenser pressure.

6.2.4 Feedwater Flow Equation

By matching the feedwater pump head and the feedwater line characteristics, one finds the feedwater flow rate as follows:

$$W_{fw} = (A + \sqrt{A^2 - B}) S_{FCV} \geq 0, \quad (6.2.7)$$

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where

$$A = \frac{b \omega^{(0)} \omega A_{FW2}}{2 W_{fw}^{(0)}}, \quad (6.2.8)$$

$$B = [(P_s - P_c) A_{FW1} - c \omega^2] A_{FW2}, \quad (6.2.9)$$

$$A_{FW1} = \frac{(\omega^{(0)})^2}{\Delta P_{fw}^{(0)}}, \quad (6.2.10)$$

$$A_{FW2} = \frac{1}{\left[K_{FWL} A_{FW1} - a \frac{(\omega^{(0)})^2}{(W_{fw}^{(0)})^2} \right]}, \quad (6.2.11)$$

and S_{PCV} is the feedwater check valve failure signal (0=failure, 1=no failure).

6.2.5 Feedwater-Pump Hydraulic Torque

The feedwater-pump torque characteristics are represented by the following dimensionless function:

$$\mu(\varphi) = c_1 \varphi^3 + c_2 \varphi^2 + c_3 \varphi + c_4, \quad (6.2.12)$$

where C_1 , C_2 , C_3 , and C_4 are input constants (typically, $C_1=-0.28866$, $C_2=0.265979$, $C_3=0.22268$, $C_4=0.8$), and μ is the torque coefficient of the feedwater pump defined as

$$\mu = \frac{M_{FWP}}{\rho_l d^5 \omega^2}. \quad (6.2.13)$$

To obtain the feedwater-pump torque M_{FWP} at any speed and flow rate, we compute φ from Eq.(6.2.2) and μ from Eq.(6.2.12), then the torque from Eq.(6.2.14):

$$M_{FWP} = \frac{M_{FWP}^{(0)}}{(\omega^{(0)})^2} \mu(\varphi) \omega^2, \quad (6.2.14)$$

where the superscript (0) stands for the initial condition.

Figure 6.3 shows the normalized feedwater pump characteristics.

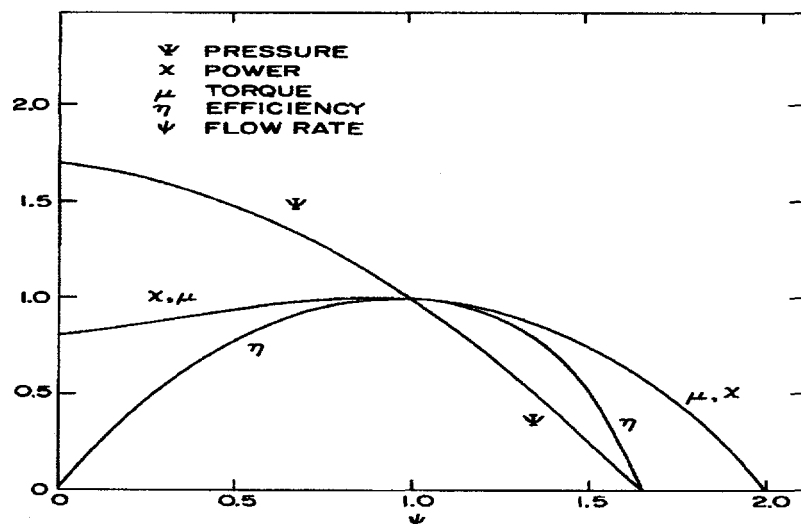


Figure 6.3 Feedwater Pump Characteristics.

6.2.6 Feedwater Pump Speed

The feedwater pump speed is given by the equation of rotation

$$\frac{d\omega}{dt} = \frac{M_{FWT} - M_{FWP}}{I_{FWT} + I_{FWP}}, \quad (6.2.15)$$

where the moments of inertia, $I_{FWT} + I_{FWP}$, are specified by an input parameter, and M_{FWT} is the feedwater turbine torque given by Eq.(6.2.1) for the current BWRs. For the SBWR, an electric motor driven pump is used instead of the turbine driven pump. Thus, M_{FWT} is the electric torque of the motor given by:

$$M_{FWT} = F_{trip} \left(\frac{C_{motor}}{f} \right) \frac{S_m S_n V_m^2}{S_m^2 + S_n^2}, \quad (6.2.16)$$

where

$$S_n = 1 - \left(\frac{\omega}{\omega^{(0)}} \right) \left(\frac{f_0}{f} \right), \quad (6.2.17)$$

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$$V_m = V_e \left(\frac{S_{FTV}}{S_{FTV}^{(0)}} \right), \quad (6.2.18)$$

F_{trip} is the pump trip signal, f is frequency (typically, 60 Hz), f_0 is 50 Hz, S_m is 0.1, V_e is 110 volts, and the constant C_{motor} is determined from the rated feedwater pump torque, $M_{FWP,0}$:

$$C_{motor} = M_{FWP,0} \left(\frac{f(S_m^2 + S_{n,0}^2)}{S_m S_n V_e^2} \right), \quad (6.2.19)$$

$$S_{n,0} = 1 - \frac{f_0}{f}.$$

6.3 Feedwater Preheater Model

6.3.1 Assumptions

1. Flow through the preheaters is quasi-steady.
2. All the preheaters in three feedwater trains can be lumped into a combination of a drain cooler and a main heat exchanger as illustrated in Figure 6.4.
3. Counter-current flow prevails in drain cooler and main heat exchanger.
4. Overall heat-transfer coefficient is constant.

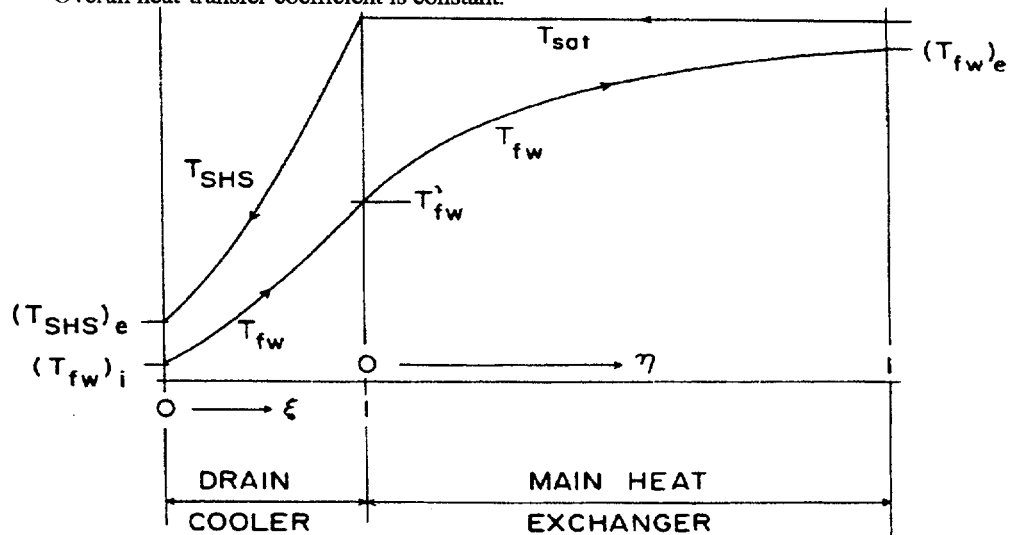


Figure 6.4 Schematic of Feedwater Heater (Counter-Current Heat Exchanger).

6.3.2 Drain Cooler Equations

The local energy balance equation for each side of the drain cooler is given by

$$W_{SHS} c_p \frac{dT_{SHS}}{L_D d\xi} = W_{fw} c_p \frac{dT_{fw}}{L_D d\xi} = \frac{(AU)_D}{L_D} (T_{SHS} - T_{fw}). \quad (6.3.1)$$

The solution of this equation yields the feedwater temperature rise in the drain cooler:

$$T'_{fw} - (T_{fw})_i = [T_s - (T_{fw})_i] \frac{1 - e^{-m}}{1 - v e^{-m}}, \quad (6.3.2)$$

where the variables are defined in Figure 6.4, m and v are given by

$$m = \frac{(AU)_D}{c_p} \left(\frac{1}{W_{SHS}} - \frac{1}{W_{fw}} \right), \quad (6.3.3)$$

$$v = \frac{W_{fw}}{W_{SHS}}, \quad (6.3.4)$$

W_{SHS} is the mass flow rate at the shell side of the feedwater heater, $(AU)_D$ is the overall heat-transfer coefficient of the drain cooler, and c_p is the specific heat at constant pressure.

6.3.3 Main Heat Exchanger Equations

The local energy balance of the main heat exchanger is given by

$$W_{fw} c_p \frac{dT_{fw}}{L_M d\eta} = \frac{(AU)_M}{L_M} (T_s - T_{fw}), \quad (6.3.5)$$

where T_s is the saturation temperature at the extraction line pressure P_{E3} .

The solution of this equation gives the feedwater temperature rise in the main heat exchanger:

$$(T_{fw})_e - T'_{fw} = (T_s - T'_{fw})(1 - e^{-n}), \quad (6.3.6)$$

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where

$$n = \frac{(AU)_M}{c_p W_{fw}}, \quad (6.3.7)$$

and $(AU)_M$ is the overall heat-transfer coefficient of the heat exchanger.

6.3.4 Lumped Preheater Equations

The overall feedwater temperature rise is obtained by eliminating the intermediate temperature T_{fw}' from Eqs.(6.3.2) and (6.3.6):

$$(T_{fw})_e - (T_{fw})_i = [T_s - (T_{fw})_i] f_{FWH}, \quad (6.3.8)$$

where

$$f_{FWH} = 1 - \frac{e^{m-n}(v-1)}{ve^m - 1}. \quad (6.3.9)$$

6.3.5 Feedwater Enthalpy

There are typically five preheaters in each of three feedwater trains in a BWR4. The total feedwater temperature rise in the preheater is the sum of that in each preheater:

$$\begin{aligned} \sum_{j=1}^{N_h} [(T_{fw})_e - (T_{fw})_i]_j &= \sum_{j=1}^{N_h} [T_s - (T_{fw})_i]_j f_{FWH} \\ &= [T_s(P_{t3}) - T_s(P_c)] \bar{f}_{FWH}, \end{aligned} \quad (6.3.10)$$

where N_h is the number of preheaters and f_{FWH} is evaluated from Eq.(6.3.9).

The feedwater enthalpy is then computed from the total feedwater temperature rise:

$$h_{fwcs} = h_f(P_c) + c_{pl}(P_{avg}) [T_s(P_{t3}) - T_s(P_c)] \bar{f}_{FWH} \lambda_{HTF}, \quad (6.3.11)$$

where λ_{HTF} is related to the feedwater heater failure signal as follows:

$$\lambda_{HTF} = \frac{N_{HTF}}{N_{HTR}} f_{HTF} + \left(1 - \frac{N_{HTF}}{N_{HTR}} \right) \quad (6.3.12)$$

Here N_{HTF} is the number of failed heaters, N_{HTR} is the total number of feedwater heaters (both are input), and the heater failure delay function f_{HTF} satisfies the equation

$$\frac{df_{HTF}}{dt} = -\frac{f_{HTF}}{\tau_{HTF}} S_{HTF}, \quad (6.3.13)$$

where τ_{HTF} is the time constant for the heater failure specified by an input, and S_{HTF} is the heater failure signal (0=no failure, 1=failure).

The specific heat c_p in Eq.(6.3.11) is evaluated at an average pressure of the feedwater preheater defined as

$$P_{avg} = P_{l3} (1 - F_{pavg}) + P_c F_{pavg}, \quad (6.3.14)$$

where the fraction F_{pavg} is determined at steady state ($t=0$) by iteration until the calculated initial feedwater enthalpy yields the feedwater temperature specified as an input.

A first-order LAG is used to account for the inertia associated with the feedwater preheater system:

$$\frac{dh_{fw}}{dt} = \frac{1}{\tau_{hfw}} (h_{fwcs} - h_{fw}), \quad (6.3.15)$$

where τ_{hfw} is the time constant for the LAG.

The enthalpy of the extraction steam as it enters the condenser is given by the equation,

$$(h_e)_{FWH} = h_{EXT} - v [h_{fw} - h_f(P_c)], \quad (6.3.16)$$

where v is defined by Eq.(6.3.4).

6.4 Condenser Dynamics

6.4.1 Assumptions

1. The condenser is composed of an equilibrium mixture of vapor and liquid water.
2. The vapor/water mixture is stagnant.
3. The overall heat-transfer coefficient is constant.

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6.4.2 Mass and Energy Balance Equations

The thermodynamic state of the condenser is defined by the two state variables, pressure P_c and mixture enthalpy h_c . From the transient mass and energy balance equations for the two-phase mixture in the condenser along with the thermal and caloric equations of state, we obtain

$$\frac{dP_c}{dt} = \frac{1}{V_c \left(1 + \rho_m \frac{\rho_p}{\rho_h} \right)} \left[Q_c - \sum_i (W_i h_i) + \left(h_c + \frac{\rho_m}{\rho_h} \right) \sum_i W_i \right], \quad (6.4.1)$$

and

$$\frac{dh_c}{dt} = \frac{1}{\rho_h V_c} \sum_i W_i - \frac{\rho_p}{\rho_h} \frac{dP_c}{dt}, \quad (6.4.2)$$

where V_c , ρ_m , and Q_c are the condenser volume, the mixture density, and the heat transfer rate from the shell side to the circulating water on the tube side,

$$Q_c = U_c A_c [T_s(P_c) - T_{cir}]. \quad (6.4.3)$$

Here the product $U_c A_c$ is the overall heat-transfer coefficient computed from the initial condition, and T_{cir} is the circulating water temperature specified by an input.

The net mass flow into the condenser is given by

$$\sum_i W_i = W_{TCV} + W_{BPV} - W_{fw}. \quad (6.4.4)$$

The net enthalpy flow into the condenser is computed from

$$\begin{aligned} \sum_i (W_i h_i) = & W_{LPT} (h_e)_{LPT} + W_{\ell 3} (h_e)_{FWH} + W_{BPV} h_g(P_{BPV}) \\ & + W_{FWT} (h_e)_{FWT} - W_{fw} h_f(P_c), \end{aligned} \quad (6.4.5)$$

where $(h_e)_{LPT}$, $(h_e)_{FWT}$, and $(h_e)_{FWH}$ are the low-pressure turbine, feedwater turbine and feedwater heater exit enthalpies given by Eqs.(6.2.30), (6.2.31), and (6.3.16), respectively.

6.4.3 Constitutive Equations

In Eqs.(6.4.1) and (6.4.2), the variables ρ_m , ρ_p , and ρ_h are defined by the constitutive equations:

$$\rho_m = \frac{1}{v_f + v_{fg} \frac{h_c - h_f}{h_{fg}}}, \quad (6.4.6)$$

$$\rho_p = \left(\frac{\partial \rho_m}{\partial P} \right)_h \quad (6.4.7)$$

$$= \frac{\rho_m^2}{h_{fg}} \left[(h_g - h_c) \left(\frac{v_{fg}}{h_{fg}} \frac{dh_f}{dP} - \frac{dv_f}{dP} \right) + (h_c - h_f) \left(\frac{v_{fg}}{h_{fg}} \frac{dh_g}{dP} - \frac{dv_g}{dP} \right) \right],$$

and

$$\rho_h = \left(\frac{\partial \rho_m}{\partial h_c} \right)_P = -\rho_m^2 \frac{v_{fg}}{h_{fg}}, \quad (6.4.8)$$

where

v_f	= Specific volume of saturated liquid at P_c ,
v_g	= Specific volume of saturated vapor at P_c ,
v_{fg}	= $v_g - v_f$,
h_f	= Specific enthalpy of saturated liquid at P_c ,
h_g	= Specific enthalpy of saturated vapor at P_c ,
h_{fg}	= $h_g - h_f$.

6.5 Numerical Solutions

The BOP model equations are solved by means of the explicit Euler integration using the same time step as the hydraulic integration. This is sufficiently accurate and efficient. The potential problem with numerical instability is taken care of by the automatic time step control algorithm used for the hydraulic integration subject to a maximum permissible time step size specified by an input.

6.5.1 Turbine Dynamics

6.5.1.1 Mass Flow Rates

The turbine mass flow rates at nth time step are given by

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$$W_{TCV}^{(n)} = Z_{TCV}^{(n)} \sqrt{P_{TCV}^{(n)} - P_c^{(n)}}, \quad (6.5.1)$$

$$Z_{TCV}^{(n)} = \sqrt{\frac{b_0}{a_0 + \frac{1}{SP_{TCV}^{(n)}}}}, \quad (6.5.2)$$

$$W_{\ell 2}^{(n)} = W_{TCV}^{(n)} \frac{1}{1 + \sqrt{\frac{K_{LPT}}{K_{FWH}}}}, \quad (6.5.3)$$

$$W_{\ell 3}^{(n)} = W_{TCV}^{(n)} - W_{\ell 2}^{(n)}, \quad (6.5.4)$$

$$W_{FTV}^{(n)} = Z_{FTV}^{(n)} \sqrt{P_{\ell 3}^{(n)} - P_c^{(n)}}, \quad (6.5.5)$$

$$Z_{FTV}^{(n)} = (Z_{FTV})_{max} \left(\frac{SP_{FTV}^{(n)} (C_{FTV1} + C_{FTV2} SP_{FTV}^{(n)})}{1 + C_{FTV3} SP_{FTV}^{(n)}} \right)^{C_{FTV4}}, \quad (6.5.6)$$

$$(Z_{FTV})_{max} = \frac{(W_{FTV})_{max}}{\sqrt{P_{\ell 3}^{(0)} - P_c^{(0)}}}, \quad (6.5.7)$$

where the superscript (n) stands for the time-step index.

6.5.1.2 Turbine Inlet and Exit Pressures

The turbine inlet and exit pressures at nth time step are

$$P_{\ell 1}^{(n)} = P_{TCV}^{(n)} - \left(W_{TCV}^{(n)} \right)^2 \left(K_{TCV} + \frac{1}{SP_{TCV}^{(n)}} - \frac{1}{SP_{TCV}^{(0)}} \right), \quad (6.5.8)$$

$$\left. \begin{aligned} P_{\ell 3, x}^{(n)} &= P_{\ell 1}^{(n)} - \left[W_{TCV}^{(n)} \right]^2 K_{HPT}, \\ P_{\ell 3, y}^{(n)} &= \text{Min} \left(P_{\ell 3, x}^{(n)}, P_{cro}^{(n)} \right), \\ P_{\ell 3}^{(n)} &= P_{\ell 3}^{(n-1)} + \frac{\Delta t_n}{\tau_{P\ell 3}} \left[P_{\ell 3, y}^{(n)} - P_{\ell 3}^{(n-1)} \right]. \end{aligned} \right\} \quad (6.5.9)$$

6.5.1.3 Isentropic Expansion in Turbines

The isentropic expansions in the high- and low-pressure turbines are

$$(\Delta h_{is})_{HPT}^{(n)} = h_g(P_s^{(n)}) f_1(P_{\ell 1}^{(n)}, P_{\ell 3}^{(n)}) + f_2(P_{\ell 1}^{(n)}, P_{\ell 3}^{(n)}), \quad (6.5.10)$$

$$(\Delta h_{is})_{LPT}^{(n)} = h_g(P_{\ell 3}^{(n)}) f_1(P_{\ell 3}^{(n)}, P_c^{(n)}) + f_2(P_{\ell 3}^{(n)}, P_c^{(n)}). \quad (6.5.11)$$

6.5.1.4 Turbine Efficiencies

The various turbine efficiencies at nth time step are computed as follows:

$$\eta_s^{(n)} = \frac{\eta_i^{(n)}}{1 - \phi_E \left[(1 - r^*) \left(\varphi(\cos \alpha_1) \frac{V_{m2}}{V_{m1}} \right)^2 + \left(v \frac{R_2}{R_1} - \psi \sqrt{E^{(n)}} \right)^2 \right]}, \quad (6.5.12)$$

$$\eta_i^{(n)} = 2(v^{(n)})^2 \left[\sin \alpha_1 \frac{\varphi}{v} \sqrt{1 - r^*} - \frac{R_2}{R_1} \left(\frac{R_2}{R_1} - \frac{\psi}{v} \sqrt{E^{(n)}} \right) \right], \quad (6.5.13)$$

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$$E^{(n)} = (1+f)r^* + \varphi^2 \left[\phi_R - \cos^2 \alpha_1 \left(\frac{V_{m2}}{V_{m1} \Psi} \right)^2 \right] (1-r^*) - 2\varphi v^{(n)} \phi_R \sin \alpha_1 \sqrt{1-r^*} + (v^{(n)})^2 \left[\phi_R + \left(\frac{R_2}{R_1} \right)^2 - 1 \right]. \quad (6.5.14)$$

The turbine stage parameters in the above equations are specified by input. The feedwater turbine is treated as a low-pressure turbine.

6.5.1.5 Turbine Exit Enthalpies

The turbine exit enthalpies at nth time step are computed from the following equations:

$$h_{EXT}^{(n)} = \left(1 + \sqrt{\frac{K_{FWH}}{K_{LPT}}} \right) \left[(h_i^{(n)})_{HPT} - \eta_{HPT}^{(n)} (\Delta h_{is}^{(n)})_{HPT} \right] - \sqrt{\frac{K_{FWH}}{K_{LPT}}} (h_i^{(n)})_{LPT}, \quad (6.5.15)$$

$$(h_e^{(n)})_{LPT} = (h_i^{(n)})_{LPT} - \eta_{LPT}^{(n)} (\Delta h_{is}^{(n)})_{LPT}, \quad (6.5.16)$$

$$(h_e^{(n)})_{FWT} = h_{EXT}^{(n)} - \eta_{FWT}^{(n)} (\Delta h_{is}^{(n)})_{FWT}, \quad (6.5.17)$$

where

$$(h_i^{(n)})_{HPT} = h_g(P_s^{(n)}), \quad (6.5.18)$$

$$(h_i^{(n)})_{LPT} = h_g(P_{\ell 3}^{(n)}). \quad (6.5.19)$$

6.5.1.6 Turbine Power and Turbine Torque

The turbine power and torque at nth time step are computed from

$$P_T^{(n)} = \eta_{HPT}^{(n)} (\Delta h_{is}^{(n)})_{HPT} W_{TCV}^{(n)} + \eta_{LPT}^{(n)} (\Delta h_{is}^{(n)})_{LPT} W_{LPT}^{(n)}, \quad (6.5.20)$$

and

$$M_{FWT}^{(n)} = \frac{\eta_{FWT}^{(n)}}{\omega_{FWT}^{(n)}} \left(\Delta h_{is}^{(n)} \right)_{FWT} W_{FWT}^{(n)}, \quad (6.5.21)$$

respectively.

6.5.2 Feedwater Train Dynamics

6.5.2.1 Feedwater Pump Head

The feedwater pump head at nth time step is computed from

$$\Delta P_{FWP}^{(n)} = \Delta P_{FWP}^{(0)} \Psi^{(n)} \frac{[\omega^{(n)}]^2}{[\omega^{(0)}]^2}. \quad (6.5.22)$$

6.5.2.2 Pressure Drop in Feedwater Train

The pressure drop in the feedwater train is computed from

$$\Delta P_{FWL}^{(n)} = K_{FWL} [W_{fw}^{(n)}]^2, \quad (6.5.23)$$

where the loss coefficient K_{FWL} (assumed constant during a transient) is determined from the initial condition,

$$K_{FWL} = \frac{\Delta P_{FWP}^{(0)} + P_c^{(0)} - P_s^{(0)}}{[W_{fw}^{(0)}]^2}. \quad (6.5.24)$$

6.5.2.3 Feedwater Flow Equation

The feedwater flow rate at nth time step is computed as follows:

$$A_{FW1} = \frac{[\omega^{(0)}]^2}{\Delta P_{fw}^{(0)}}, \quad (6.5.25)$$

$$A_{FW2} = \frac{1}{\left[K_{FWL} A_{FW1} - a \frac{(\omega^{(0)})^2}{(W_{fw}^{(0)})^2} \right]}, \quad (6.5.26)$$

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$$A^{(n)} = \frac{b \omega^{(0)} \omega^{(n)} A_{FW2}}{2 W_{fw}^{(0)}}, \quad (6.5.27)$$

$$B^{(n)} = \left[\left(P_s^{(n)} - P_c^{(n)} \right) A_{FW1} - c \left(\omega^{(n)} \right)^2 \right] A_{FW2}, \quad (6.5.28)$$

$$W_{fw}^{(n)} = \left(A^{(n)} + \sqrt{\left(A^{(n)} \right)^2 - B^{(n)}} \right) S_{FCV} \geq 0. \quad (6.5.29)$$

6.5.2.4 Feedwater Pump Hydraulic Torque

The feedwater pump hydraulic torque at nth time step is computed as follows:

$$\mu^{(n)} = c_1 \left(\varphi^{(n)} \right)^3 + c_2 \left(\varphi^{(n)} \right)^2 + c_3 \varphi^{(n)} + c_4, \quad (6.5.30)$$

$$M_{FWP}^{(n)} = \frac{M_{FWP}^{(0)}}{\left(\omega_{FWT}^{(0)} \right)^2} \mu^{(n)} \left(\omega_{FWT}^{(n)} \right)^2. \quad (6.5.31)$$

6.5.2.5 Feedwater Pump Speed

The feedwater pump speed at nth time step is computed from

$$\omega^{(n)} = \omega^{(n-1)} + \frac{\Delta t_n}{I_{FWT} + I_{FWP}} \left(M_{FWT}^{(n)} - M_{FWP}^{(n)} \right). \quad (6.5.32)$$

6.5.3 Feedwater Preheater Model

6.5.3.1 Feedwater Heater Characteristic Function

The feedwater heater characteristic function is computed by the following equations:

$$m^{(n)} = \frac{(A U)_D}{c_p^{(0)}} \left(\frac{1}{W_{SHS}^{(n)}} - \frac{1}{W_{fw}^{(n)}} \right), \quad (6.5.33)$$

$$n^{(n)} = \frac{(AU)_M}{c_P^{(0)} W_{fw}^{(n)}}, \quad (6.5.34)$$

$$\bar{f}_{FWH}^{(n)} = 1 - \frac{e^{m^{(n)} - n^{(n)}(v^{(n)} - 1)}}{v^{(n)} e^{m^{(n)}} - 1}. \quad (6.5.35)$$

6.5.3.2 Feedwater Heater Failure Signal

The feedwater heater failure signal at nth time step is computed by the equations:

$$f_{HTF}^{(n)} = f_{HTF}^{(n-1)} - \frac{\Delta t_n}{\tau_{HTF}} S_{HTF} f_{HTF}^{(n-1)}, \quad (6.5.36)$$

$$\lambda_{HTF}^{(n)} = \frac{N_{HTF}}{N_{HTR}} f_{HTF}^{(n)} + \left(1 - \frac{N_{HTF}}{N_{HTR}} \right). \quad (6.5.37)$$

6.5.3.3 Average Pressure of Feedwater Preheater

The average feedwater preheater pressure at nth time step is computed by

$$P_{avg}^{(n)} = P_{\ell 3}^{(n)} \left(1 - F_{pavg}^{(0)} \right) + P_c^{(n-1)} F_{pavg}^{(0)}. \quad (6.5.38)$$

6.5.3.4 Feedwater Enthalpy and Temperature

The nth-time-step feedwater enthalpy is computed as follows:

$$h_{fwcs}^{(n)} = h_f(P_c^{(n)}) + C_{pl}(P_{avg}^{(n)}) \left[T_s(P_{\ell 3}^{(n)}) - T_s(P_c^{(n)}) \right] \bar{f}_{FWH}^{(n)} \lambda_{HTF}^{(n)}, \quad (6.5.39)$$

$$h_{fw}^{(n)} = h_{fw}^{(n-1)} + \frac{\Delta t_n}{\tau_{hfw}} \left(h_{fwcs}^{(n)} - h_{fw}^{(n-1)} \right). \quad (6.5.40)$$

The corresponding feedwater temperature at the nth time step is obtained from the feedwater enthalpy via the property function:

6. Modeling of Balance of Plant (BOP)

$$T_{fw}^{(n)} = f_{T_{fw}}(h_{fw}^{(n)}, P_s^{(n)}), \quad (6.5.41)$$

where $f_{T_{fw}}$ is the property function for the liquid temperature (Function TL in RAMONA-4B).

6.5.3.5 Feedwater Preheater Exit Enthalpy

The preheater exit enthalpy at nth time step is computed by

$$h_{3e}^{(n)} = h_{3i}^{(n)} - v^{(n)}(h_{fw}^{(n)} - h_{fw,i}^{(n)}), \quad (6.5.42)$$

where

$$h_{fw,i}^{(n)} = h_f(P_c^{(n-1)}). \quad (6.5.43)$$

6.5.4 Condenser Dynamics

6.5.4.1 Constitutive Equations

The constitutive equations at nth time step are given by

$$\rho_m^{(n)} = \frac{1}{v_f^{(n-1)} + v_{fg}^{(n-1)} \frac{h_c^{(n-1)} - h_f^{(n-1)}}{h_{fg}^{(n-1)}}}, \quad (6.5.44)$$

$$\begin{aligned} \rho_p^{(n)} = & \frac{(\rho_m^{(n)})^2}{h_{fg}^{(n-1)}} \left[(h_g^{(n-1)} - h_c^{(n-1)}) \left(\frac{v_{fg}^{(n-1)}}{h_{fg}^{(n-1)}} \left(\frac{dh_f}{dP} \right)^{(n-1)} - \left(\frac{dv_f}{dP} \right)^{(n-1)} \right) \right] \\ & + \frac{(\rho_m^{(n)})^2}{h_{fg}^{(n-1)}} \left[(h_c^{(n-1)} - h_f^{(n-1)}) \left(\frac{v_{fg}^{(n-1)}}{h_{fg}^{(n-1)}} \left(\frac{dh_g}{dP} \right)^{(n-1)} - \left(\frac{dv_g}{dP} \right)^{(n-1)} \right) \right], \end{aligned} \quad (6.5.45)$$

and

$$\rho_h^{(n)} = -(\rho_m^{(n)})^2 \frac{v_{fg}^{(n-1)}}{h_{fg}^{(n-1)}}, \quad (6.5.46)$$

where

$$\left. \begin{aligned} v_f^{(n-1)} &= v_f(P_c^{(n-1)}), \\ v_g^{(n-1)} &= v_g(P_c^{(n-1)}), \\ h_f^{(n-1)} &= h_f(P_c^{(n-1)}), \\ h_g^{(n-1)} &= h_g(P_c^{(n-1)}), \\ v_{fg}^{(n-1)} &= v_g^{(n-1)} - v_f^{(n-1)}, \\ h_{fg}^{(n-1)} &= h_g^{(n-1)} - h_f^{(n-1)}. \end{aligned} \right\} \quad (6.5.47)$$

6.5.4.2 Mass and Energy balance Equations

The condenser pressure at the nth time step is computed from

$$P_c^{(n)} = P_c^{(n-1)} + \frac{\Delta t_n}{V_c \left(1 + \rho_m^{(n)} \frac{\rho_p^{(n)}}{\rho_h^{(n)}} \right)} \left[Q_c^{(n)} - \sum_i (W_i^{(n)} h_i^{(n)}) + \left(h_c^{(n-1)} + \frac{\rho_m^{(n)}}{\rho_h^{(n)}} \right) \sum_i W_i^{(n)} \right], \quad (6.5.48)$$

and the nth time step condenser mixture enthalpy is computed as follows:

$$h_c^{(n)} = h_c^{(n-1)} + \frac{\Delta t_n}{\rho_h^{(n)} V_c} \sum_i W_i^{(n)} - \Delta t_n \frac{\rho_p^{(n)}}{\rho_h^{(n)}} \left(\frac{dP_c}{dt} \right)^{(n)}. \quad (6.5.49)$$

6.6 Implementation in RAMONA-4B

The numerical algorithms presented above have been implemented in RAMONA-4B as the subroutine BOP. This subroutine is called twice by the subroutine NUMINT, the transient driver. The initial conditions, pressure loss coefficients, and other constants defined by the initial conditions are computed in subroutine BOPIC. The turbine enthalpy loss is computed by the function DHIS and the turbine efficiency is computed by the function ETAS.

6. Modeling of Balance of Plant (BOP)

6.7 Test Results

The BOP models developed here have been tested for a MSIV closure ATWS event in a BWR4. The calculational model used for testings is illustrated Figures 1.1 and 1.4. It includes the reactor pressure vessel (RPV) with all important internal components, steam lines, balance of plant (BOP), control systems, and the SLCS for boron injection.

The reactor core was modeled with 101 neutronic channels and 25 hydraulic channels in a 1/8-core symmetry. Twenty four axial cells were used in each of the neutronic and hydraulic channels. Seven evenly distributed spacers were located along the axial height of each hydraulic channel (except for the bypass channel) to account for the local form losses due to the spacer grids.

The nuclear parameters for the 3D neutron kinetics were based on the Browns Ferry Unit 1 at the end of cycle 5. The two-group cross sections and their feedback coefficients were generated from the lattice physics data given by TVA to BNL in early 1980.

The testing was done for a Recirculation Pump Trip (RPT) event with density wave oscillations. In this testing, the BOP, motor/generator set dynamics and control systems options in RAMONA-4B were all activated in order to produce realistic simulation results.

The transient selected for the test case is a dual recirculation pump trip (RPT) event initiated from 100% core power and 75% core flow (minimum permissible) with postulated scram failure (ATWS). The initial condition was the hot full power but reduced flow condition. The key parameters calculated by RAMONA-4B are given below:

Core Thermal Power	3293 Mwt
System Pressure	6.962 Mpa
Feedwater Flow Rate	1685 kg/s
Core Flow Rate	9,780 kg/s
Core Inlet Subcooling	10.65 °C

The transient was run for 600 seconds for this testing. The event began with a trip of both recirculation pumps at time zero. This transient is the same one as is presented in Section 4.9 for the new separator model. The RPT reduced the core flow to natural circulation (about 29% of rated) within 60 seconds as shown in Figure 4.7. As a result, core power was reduced from its initial rated value to approximately 40% of rated power due to increased vapor fraction as shown in Figure 4.8. The evidence of instability is visible within 100 seconds of the transient in the oscillatory behavior of both core flow and core power when core inlet subcooling has increased above 18°C as shown in Figure 4.9. Figure 4.10 presents the oscillatory behavior of core average void fraction, which demonstrates clearly that this is a density wave oscillation.

The system pressure response is presented in Figure 4.11 and the steam flow and feedwater flow behavior are shown in Figure 4.12. Due to the rapid reduction of feedwater flow at the beginning of the transient, the system pressure decreases initially and settles at about 6.62 MPa after 50 seconds. The density wave oscillation also appears in the pressure response through the vapor generation, which in turn produces the oscillatory behavior of steam flow and feedwater flow. The oscillatory water level behaviors are shown in Figure 4.13. The water level remained in the steam dome region.

The transient responses of the BOP are presented in Figures 6.5 through 6.12. Figure 6.5 and 6.6 show the transient response of feedwater flow and temperature, respectively. Figure 6.7 shows the feedwater pump speed response. Figures 6.8 and 6.9 show the transient response of feedwater turbine flow and feedwater turbine valve stem position, respectively. The condenser responses are presented in Figure 6.10 for the average condenser pressure and in Figure 6.11 for the condenser mixture enthalpy. Figure 6.12 shows the transient response of the downstream pressure of the high-pressure turbine. Overall the BOP

6. Modeling of Balance of Plant (BOP)

responses demonstrated the expected reasonable trends. Furthermore, the feedback effect of the BOP on the reactor vessel responses can also be seen clearly, indicating the importance of the BOP modeling for this transient.

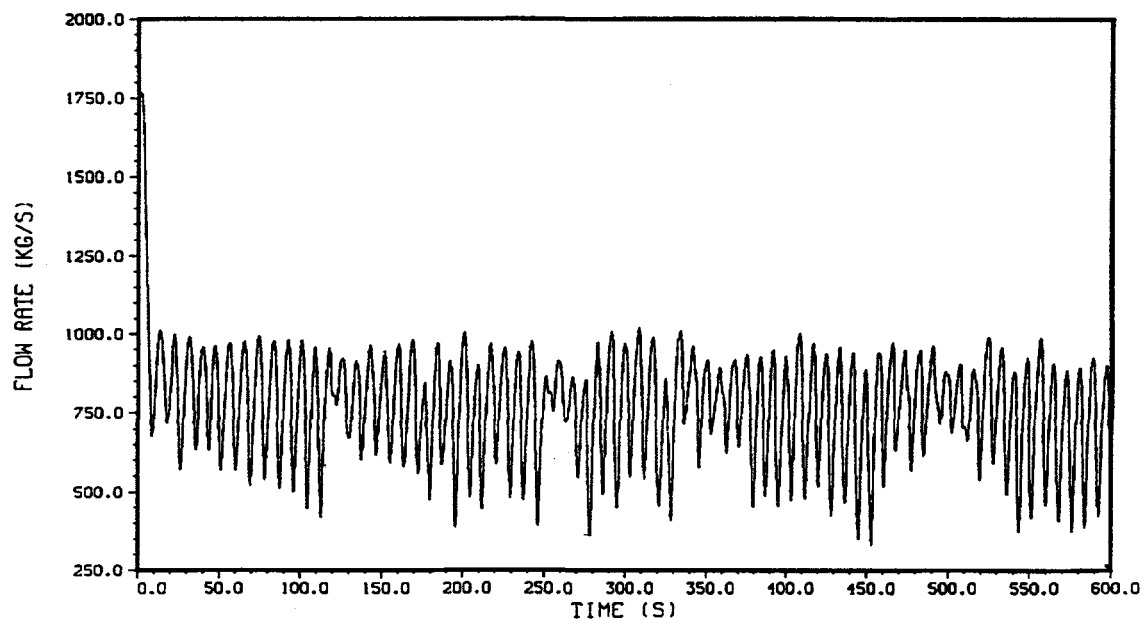


Figure 6.5 Transient Response of Feedwater Flow Rate During the RPT Event

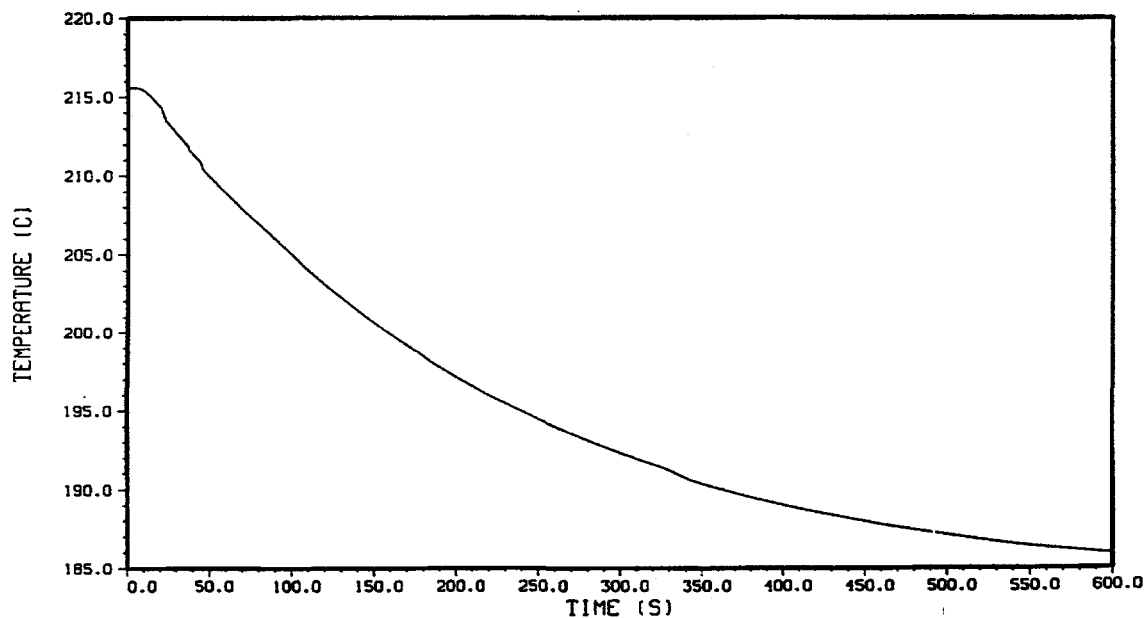


Figure 6.6 Transient Response of Feedwater Temperature During the RPT Event

6. Modeling of Balance of Plant (BOP)

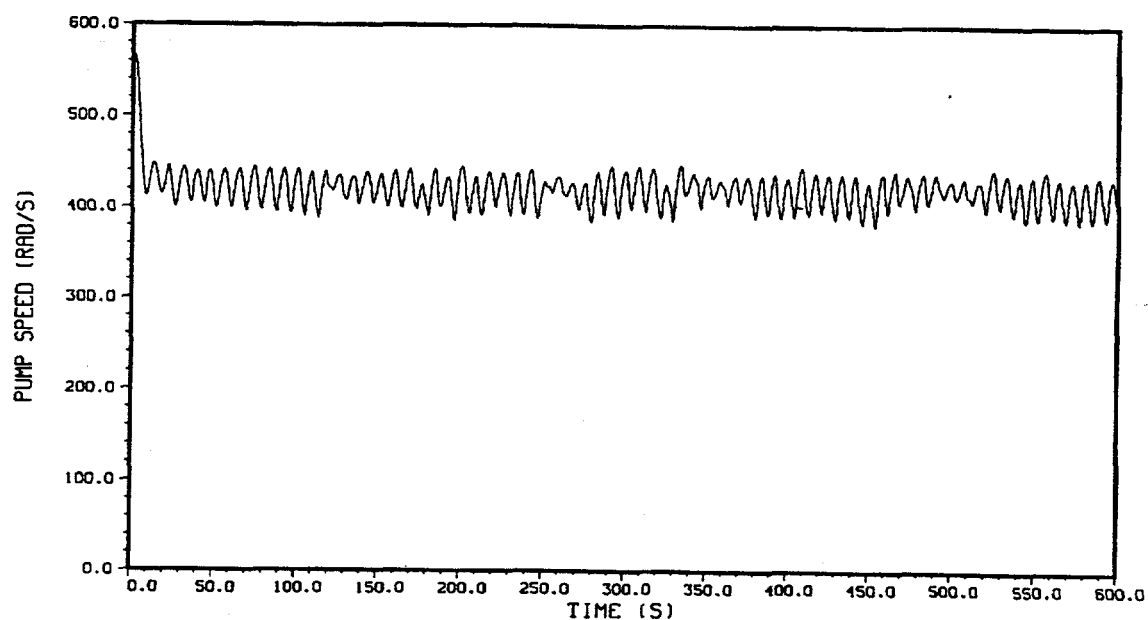


Figure 6.7 Transient Response of Feedwater Pump Speed During the RPT Event

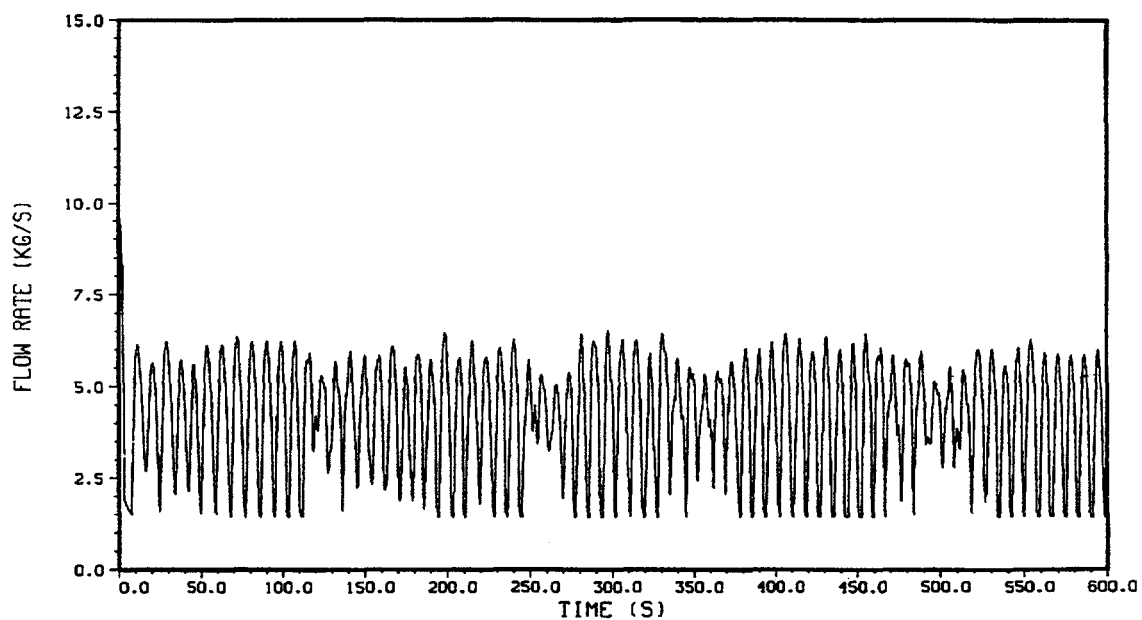


Figure 6.8 Transient Response of Feedwater Turbine Flow Rate During the RPT Event

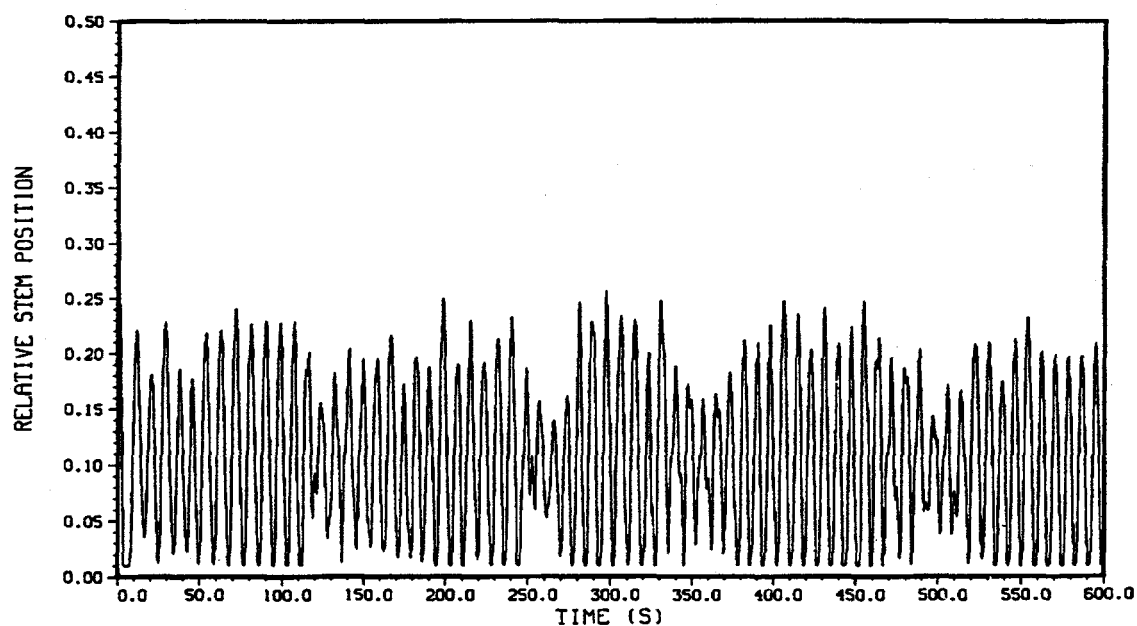


Figure 6.9 Transient Response of Feedwater Turbine Valve Stem Position During the RPT Event

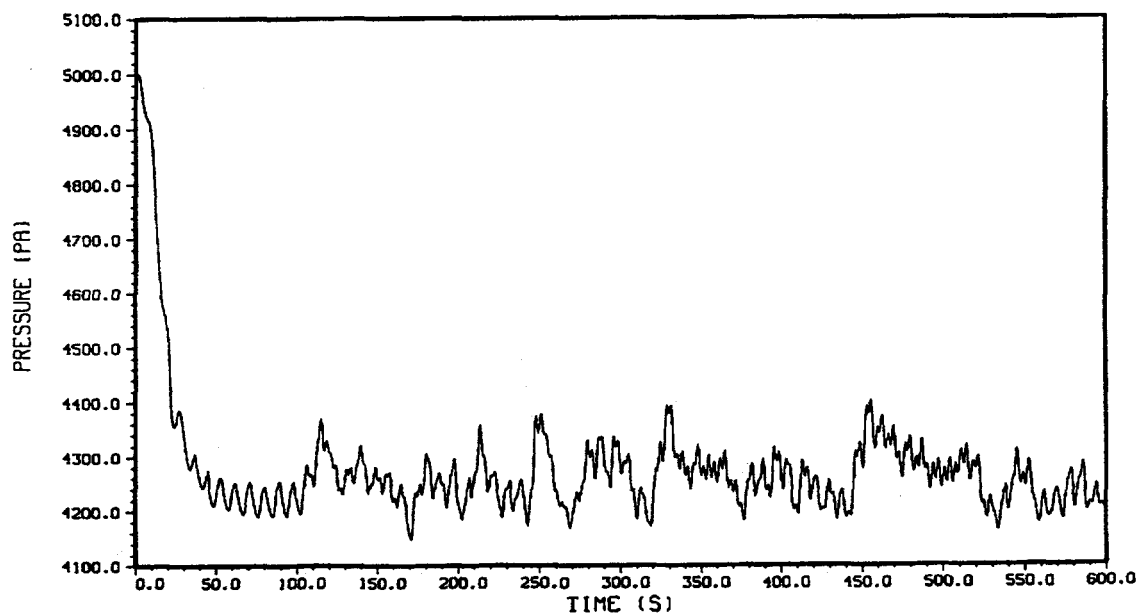


Figure 6.10 Transient Response of Condenser Pressure During the RPT Event

6. Modeling of Balance of Plant (BOP)

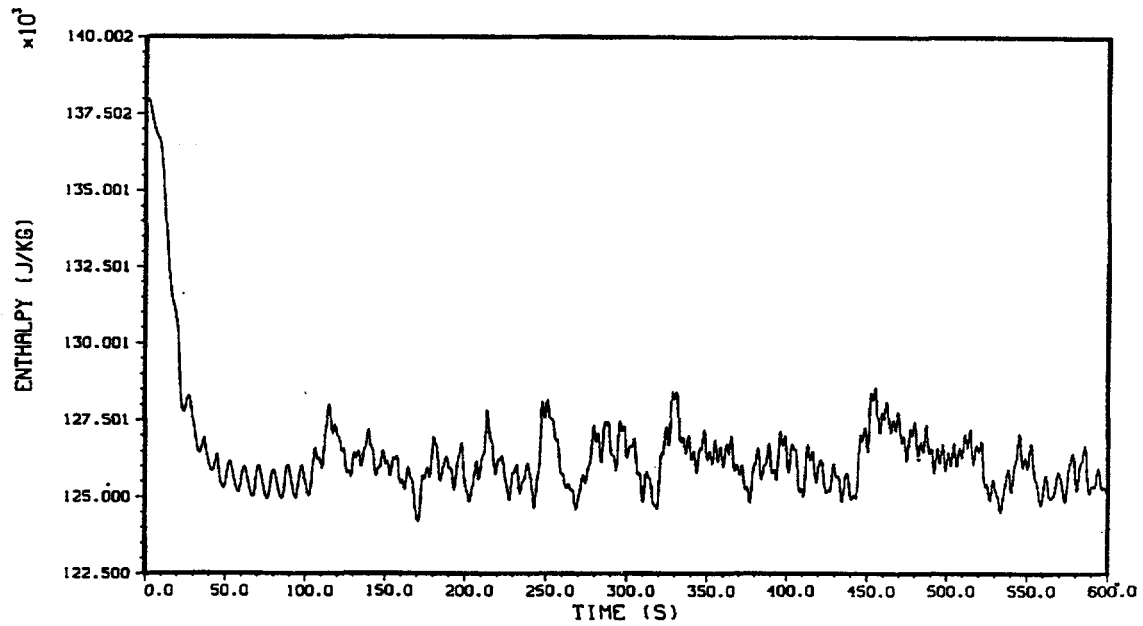


Figure 6.11 Transient Response of Condenser Mixture Enthalpy During the RPT Event

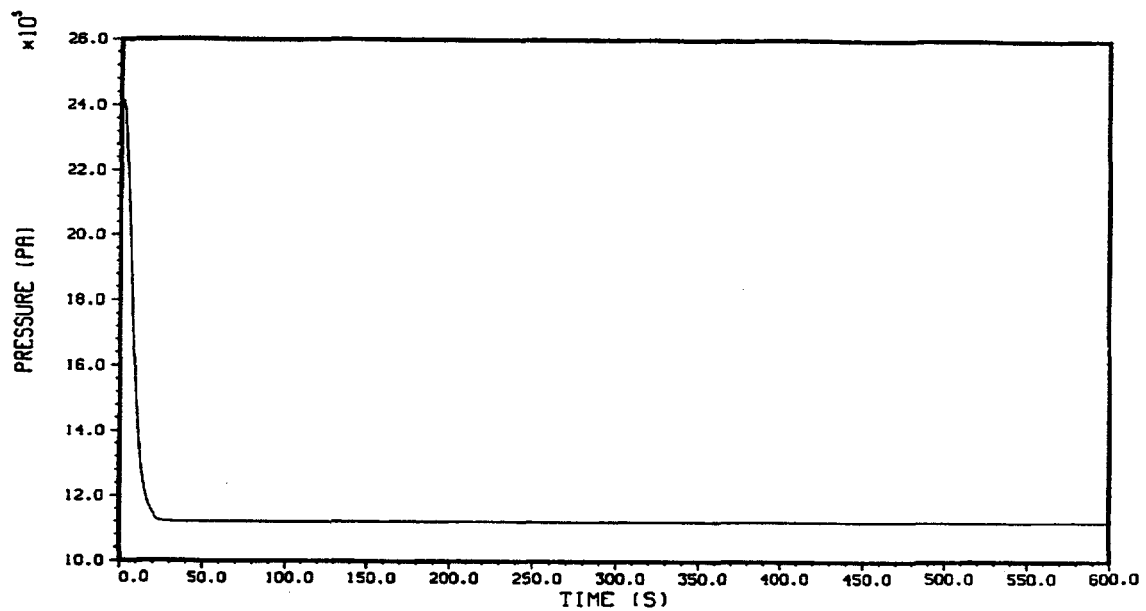


Figure 6.12 Transient Response of Downstream Pressure of High-Pressure Turbine During the RPT Event

7. MODELING OF BWR CONTAINMENT

7.1 Introduction

The RAMONA-4B code employed in the past a simple model to simulate the suppression pool dynamics as a post-processor. This model considers only the mass and energy balance for the suppression pool, and neglects the dynamics of the wetwell air space and the drywell. There is significant communication between the wetwell and drywell during a long-term transient involving steam discharge into the suppression pool of the containment. Thus, ignoring the presence of both the wetwell and drywell can impact the performance prediction for the containment. Clearly, there is a need for a more sophisticated mechanistic containment model for predicting the containment integrity during a long-term transient such as an ATWS event.

BNL has developed a mechanistic BWR containment model for the Engineering Plant Analyzer (EPA) [Wulff and others, 1984]. This model was developed by Dr. W. Wulff of BNL, and well tested for a variety of plant transients using the EPA. The model is non-proprietary and ideal for use in RAMONA-4B.

7.2 A Mechanistic Containment Model

The mechanistic containment model was developed primarily for BWR Mark1 containment. However, it is generic enough to be applicable to other BWR containments. A BWR containment is made up of a wetwell with a pressure suppression pool, a drywell, a wetwell-to-drywell vent system with vacuum breakers, and a residual heat removal (RHR) system as illustrated in the layout Figure 7.1 and the schematic diagram Figure 7.2. The following description of the mechanistic containment model is adapted from the personal notes of Dr. W. Wulff on the EPA containment model [Wulff, 1984].

7.2.1 Wetwell Dynamics

The wetwell consists of a pressure suppression pool, a gas space above the pool, and the wetwell wall. The wetwell dynamics is governed by the mass and energy balances of the pool, gas space, and wall.

7.2.1.1 State Equations

7.2.1.1.1 Pressure Suppression Pool (PSP) Mass and Energy Balances

A lumped-parameter model is assumed to be adequate for describing the PSP dynamics. That is, a single-region control volume is used to represent the mass and energy balances of the PSP. It is further assumed that the PSP is primarily composed of single-phase liquid.

7. Modeling of BWR Containment

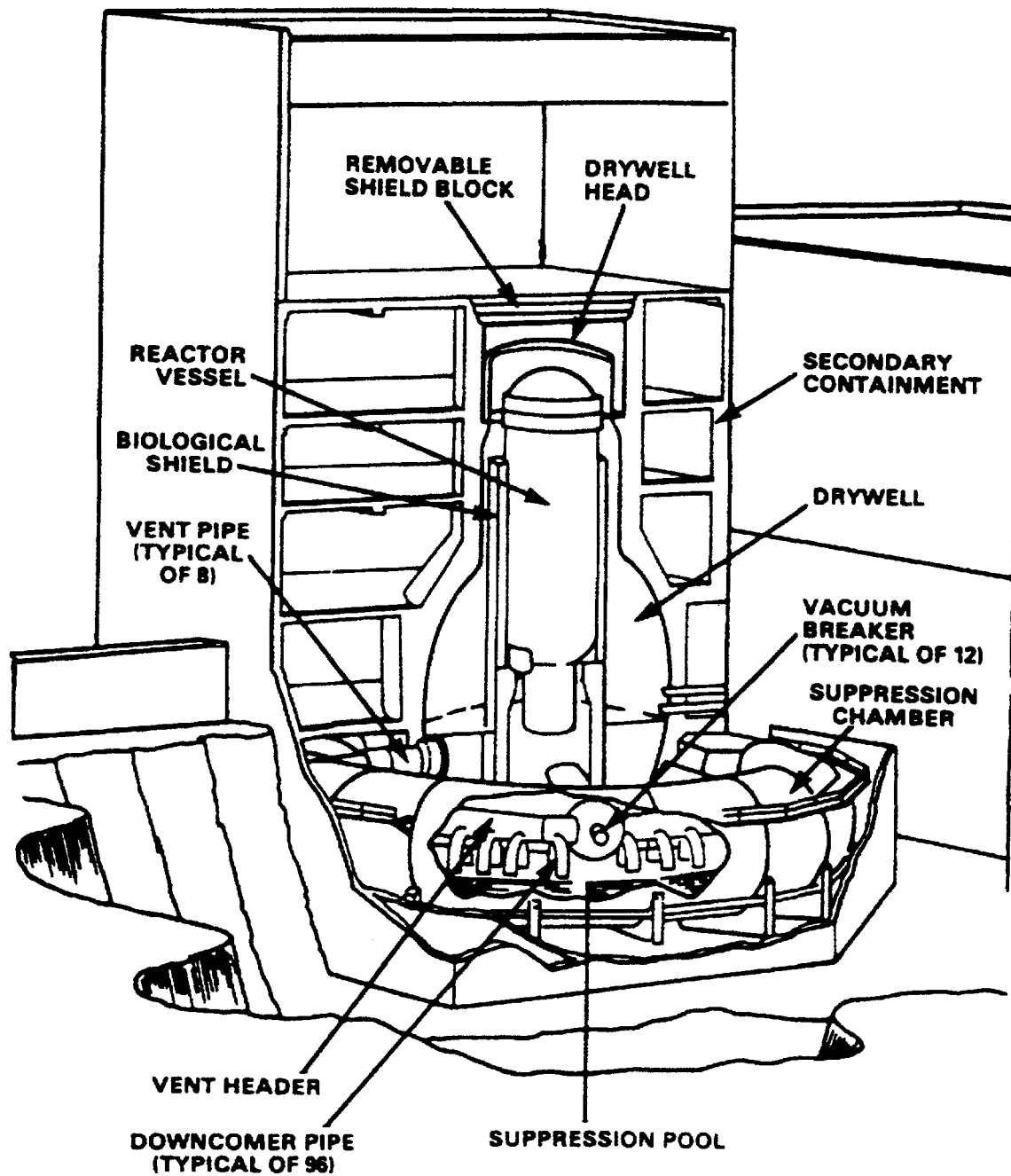


Figure 7.1 BWR Mark I Containment Layout.

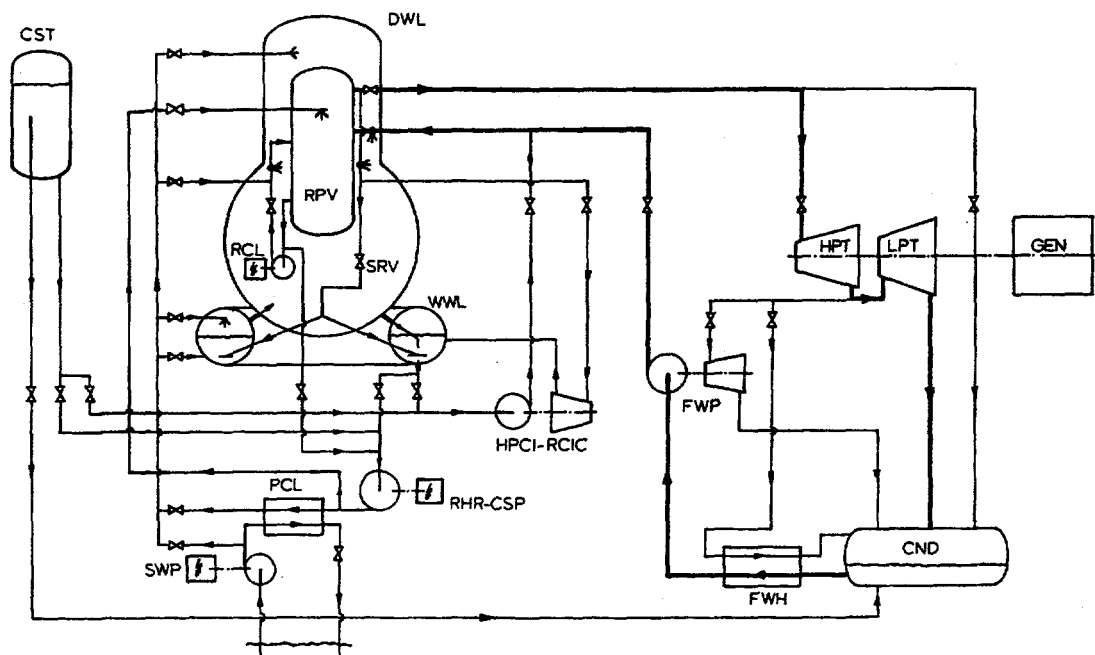


Figure 7.2 Schematic of BWR Mark I Containment and BOP.

7. Modeling of BWR Containment

The mass balance equation for the PSP liquid is:

$$\frac{dm_{L2}}{dt} = f_c W_{SRV} - W_{LG2} - A_i \Gamma_{ai} - V_{G2} \Gamma_{v2} - \sum_{j=1}^{j=N_{BCC}} W_j + (W_{cnd,w})_2 + W_{PSPC} + W_{12} (X_{v1} - X_{s,0}) + W_{IPT} + (1 - S_{SPR2}) W_{SPR2}, \quad (7.1)$$

where

$$\sum_{j=1}^{j=N_{BCC}} W_j = W_{RHR} + W_{CSS} - S_{SCST} (W_{RCIC} + W_{HPCI}), \quad (7.2)$$

and the rest of symbols are defined as follows:

m_{L2}	=	total liquid mass in the pressure suppression pool,
W_{SRV}	=	mass flow rate of the steam discharged by Safety/Relief Valves (SRV),
W_{LG2}	=	rate of evaporation from the PSP after the pool reaches saturation (zero if subcooled),
$A_i \Gamma_{ai}$	=	rate of surface evaporation from the PSP,
$V_{G2} \Gamma_{v2}$	=	rate of bulk condensation in the gas space (V_{g2}) of the PSP,
W_{cndw2}	=	rate of condensation from the wall of the PSP,
W_{PSPC}	=	mass flow rate from the RHR cooler,
W_{12}	=	mass flow rate from drywell (subscript 1) to wetwell (subscript 2),
X_{v1}	=	mass fraction of steam in drywell,
$X_{s,0}$	=	mass fraction corresponding to saturated condition in drywell,
W_{IPT}	=	mass flow rate from the pump drive turbine,
W_{RHR}	=	mass flow rate of the RHR system,
W_{CSS}	=	mass flow rate of the core spray system,
W_{RCIC}	=	mass flow rate of the reactor core isolation (RCIC) system,
W_{HPCI}	=	mass flow rate of the high pressure coolant injection (HPCI) system,
S_{SCST}	=	switch for taking the RCIC and HPCI flow from coolant storage tank (CST) (0=from CST, -1=from PSP).
f_c	=	fraction of condensation for the steam discharged by Safety/Relief Valves (SRV),
S_{SPR2}	=	switch for wetwell spray cooling (1.0 if wetwell relative humidity ≤ 1 , 0.0 otherwise).

Note that $W_{12}(X_{v1} - X_{s,0})$ represents the total condensate via venting from the drywell.

The energy balance equation for the PSP liquid is

$$\begin{aligned}
 \frac{d(mu)_{L2}}{dt} = & f_c h_g(P_s) W_{SRV} - h_g(T_{L2}) (W_{LG2} + A_i \Gamma_{a,i} + W_{12} X_{L2}) - h_f(T_{G2}) V_{G2} \Gamma_{v2} \\
 & - u_{L2} \sum_{j=1}^{j=N_{ECC}} W_j + C_{v,l} (T_{w22} - 273.15) W_{cndw2} + u_{RHR} W_{PSPC} \\
 & + W_{12} [(1 - X_{L2}) X_{vI} C_{v,v} + (1 - X_{vI}) C_{v,N}] (T_{GI} - T_{L2}) \\
 & + h_{IPT} W_{IPT} + A_{w2I} h_{c,21} (T_{w2I} - T_{L2}),
 \end{aligned} \tag{7.3}$$

where $h_{c,21}$ is the heat transfer coefficient between the pool wall and pool water given by the following correlation:

$$h_{c,21} = 0.129 k_l \left(\frac{g \beta_l N_{Pr}}{v_l^2} \right)^{1/6} (|T_{w2I} - T_{L2}|)^{1/6}, \tag{7.4}$$

and

$h_g(P_s)$	=	saturated vapor specific enthalpy at system pressure P_s ,
$h_g(T_{L2})$	=	saturated vapor specific enthalpy at pool liquid temperature T_{L2} ,
$h_f(T_{G2})$	=	saturated liquid specific enthalpy at wetwell gas temperature T_{G2} ,
u_{L2}	=	specific internal energy of the PSP liquid,
u_{RHR}	=	specific internal energy of the RHR system,
X_{L2}	=	liquid mass fraction of the total mass flow from drywell to wetwell,
$C_{v,l}$	=	liquid specific heat at constant volume,
$C_{v,N}$	=	specific heat at constant volume of nitrogen gas,
h_{IPT}	=	specific enthalpy at injection pump drive turbine discharge,
k_l	=	thermal conductivity of the pool water,
g	=	gravitational constant (9.81),

7. Modeling of BWR Containment

β_t	=	thermal expansion coefficient of the pool water,
N_{pr}	=	Prandtl number of the pool water,
ν_t	=	dynamic viscosity of the pool water,
T_{w21}	=	pressure suppression pool wall temperature,
A_{w21}	=	wall surface area in the liquid part of the pressure suppression pool [See Eq. (7.85)].

Note that the evaporation rate W_{LG2} is nonzero only if the PSP reaches saturation. To derive the expression for W_{LG2} under this condition, make use of the identity:

$$\frac{d(mu)_{L2}}{dt} = m_{L2} \left(\frac{du_{L2}}{dP_{G2}} \right) \left(\frac{dP_{G2}}{dt} \right) + u_{L2} \frac{dm_{L2}}{dt}, \quad (7.5)$$

where P_{G2} is the wetwell gas pressure.

When the PSP reaches saturation, we have

$$u_{L2} = u_f(P_{G2}) = u_{f2}, \quad (7.6)$$

$$h_g(T_{L2}) = h_g(P_{G2}) = h_{g2}, \quad (7.7)$$

and

$$T_{L2} = T_s(P_{G2}) = T_{s2}. \quad (7.8)$$

Substituting Eqs. (7.1) and (7.3) into Eq. (7.5) along with the above conditions and solving the resultant equation for W_{LG2} , we obtain the following results:

For subcooled pool ($u_{L2} < u_{f2}$),

$$W_{LG2} = 0. \quad (7.9)$$

For saturated or superheated pool ($u_{L2} \geq u_{f2}$),

$$\begin{aligned}
 W_{LG2} = & -A_i \Gamma_{ai} - X_{L2} W_{12} + A_{w21} h_{c,21} \frac{(T_{w21} - T_{s2})}{(h_{g2} - u_{f2})} \\
 & + W_{SRV} \frac{[h_g(P_s) - u_{f2}]}{(h_{g2} - u_{f2})} - V_{G2} \Gamma_{v2} \frac{[h_f(T_{G2}) - u_{f2}]}{(h_{g2} - u_{f2})} \\
 & + W_{cndw2} C_{v,l} \frac{(T_{w22} - T_{s2})}{(h_{g2} - u_{f2})} + W_{PSPC} C_{v,l} \frac{(T_{RHR} - T_{s2})}{(h_{g2} - u_{f2})} \\
 & + \left[X_{v1} (h_g(T_{G1}) - u_{f2}) + (1 - X_{v1}) C_{v,N} (T_{G1} - T_{s2}) \right] \frac{W_{12}}{(h_{g2} - u_{f2})} \\
 & + \frac{(h_{IPT} - u_{f2})}{(h_{g2} - u_{f2})} W_{IPT} - \frac{m_{L2}}{(h_{g2} - u_{f2})} \frac{du_{f2}}{dt},
 \end{aligned} \tag{7.10}$$

where X_{L2} is the mass fraction of vapor emerging from the pool, which is a secondary variable described in Section 7.2.1.2 and the time derivative du_{f2}/dt is approximated by finite difference; namely, $\Delta u_{f2}/\Delta t$.

7.2.1.1.2 Wetwell Gas Space Mass and Energy Balances

The wetwell gas space above the pool is composed of the nitrogen gas and water vapor (steam).

The mass balance equation for the gas mixture is given by

$$\begin{aligned}
 \frac{dm_{G2}}{dt} = & W_{LG2} - W_{21} + A_i \Gamma_{ai} + V_{G2} \Gamma_{v2} - W_{cndw2} + S_{SPR2} W_{SPR2} \\
 & + W_{12} (1 - X_{v1} + X_{L2}) + (1 - f_c) W_{SRV},
 \end{aligned} \tag{7.11}$$

where m_{G2} is the total mass of the gas mixture, W_{21} is the mass flow rate transferred from wetwell to drywell, W_{21} is that transferred from drywell to wetwell, and W_{SPR2} is the mass flow rate from wetwell spray cooling.

The energy balance equation for the gas mixture in the wetwell gas space is

7. Modeling of BWR Containment

$$\begin{aligned}
 \frac{d(mu)_{G2}}{dt} = & h_g(T_{L2}) (A_i \Gamma_{ai} + W_{LG2} + X_{L2} W_{12}) + V_{G2} \Gamma_{v2} h_f(T_{G2}) - h_{G2} W_{21} \\
 & + W_{SPR2} C_{p,l} (T_{SPR2} - T_{G2}) - W_{cndw2} C_{p,l} (T_{i2} + 273.15) \\
 & + (1 - X_{v1}) W_{12} C_{p,N} (T_{L2} - T_{G2}) - \dot{Q}_{w22} + \frac{P_{G2}}{\rho_{l2}} \left(W_{PSPC} - \sum_{j=1}^{j=N_{ECC}} W_j \right) \\
 & + (1 - f_c) h_g(P_s) W_{SRV},
 \end{aligned} \tag{7.12}$$

where $\dot{Q}_{w22}$ is the wetwell wall heat flux given by

$$\dot{Q}_{w22} = h_{fg}(T_{i2}) W_{cndw2} + A_{w22} \bar{h}_{e2} (T_{G2} - T_{i2}). \tag{7.13}$$

Here W_{cndw2} and A_{w22} will be described later in Section 7.2.1.3.3 [Eqs. (7.83) and (7.84)], and the heat transfer coefficient $\bar{h}_{e2}$ is determined as follows:

$$\bar{h}_{e2} = 0.129 k_g \left(\frac{g N_{Pr}}{v^2} \right)^{1/4} f_T, \tag{7.14}$$

where k_g is the thermal conductivity of the gas mixture, $g=9.81$ (gravitational constant), N_{Pr} is Prandtl number of the wetwell atmosphere, and f_T is given by

$$\begin{aligned}
 f_T &= \left(\frac{T_{G2}}{T_{i2}} - 1 \right)^{1/4} \quad \text{if } T_{i2} \leq T_{G2}, \\
 &= \left(\frac{T_{i2}}{T_{G2}} - 1 \right)^{1/4} \quad \text{if } T_{i2} > T_{G2}.
 \end{aligned} \tag{7.15}$$

It should be noted that, due to wall condensation, there exists a liquid film on the wall, hence an interface between the liquid film and the gas mixture in the wetwell gas space. T_{i2} in the above equations is the temperature at the interface, and T_{G2} is the temperature of the gas mixture. The calculation of the interface temperature T_{i2} is described later in Section 7.2.1.3.4, and that of T_{G2} is discussed in Section 7.2.1.2.

The water vapor mass in the wetwell gas space is tracked by the state equation

$$\begin{aligned} \frac{dm_{v2}}{dt} = & W_{LG2} - X_{v2} W_{21} + A_i \Gamma_{ai} + V_{G2} \Gamma_{v2} - W_{cndw2} + X_{L2} W_{12} \\ & + S_{SPR2} W_{SPR2} + (1 - f_c) W_{SRV}, \end{aligned} \quad (7.16)$$

where m_{v2} is the water vapor mass and all the other variables have been defined before.

7.2.1.1.3 Wetwell Wall Liner Heat Balances

The wetwell wall liner is in contact with the gas mixture via a liquid film in the upper part and the pool liquid in the lower part. We shall use the subscript w22 to represent the upper part and subscript w21 to denote the lower part. The heat balance equations for the wall liner are given by the following:

For the upper part wall liner,

$$\frac{dT_{w22}}{dt} = \frac{Q_{w22}}{(\rho C_p s_w)_{w22} A_{w22}}, \quad (7.17)$$

where ρC_p is the specific heat capacity of the upper liner, s_w is the liner wall thickness, and A_{w22} is the surface area of the upper liner.

For the lower part wall liner,

$$\frac{dT_{w21}}{dt} = \frac{h_{c,21} (T_{L2} - T_{w21})}{(\rho C_p s_w)_{w21}}, \quad (7.18)$$

where $h_{c,21}$ is the heat transfer coefficient for the lower liner given by Eq. (7.4).

7.2.1.2 Wetwell Secondary Variables

The wetwell secondary variables consist of the pressures, temperatures, specific internal energies, mass fractions, and densities. Once the state variables are obtained from integrating the state equations, the secondary variables can be calculated from the state variables.

The specific internal energy of the pool in wetwell is given by

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$$u_{L2} = \frac{(mu)_{L2}}{m_{L2}}. \quad (7.19)$$

The vapor mass fraction above the pool is given by definition

$$X_{v2} = \frac{m_{v2}}{m_{G2}}. \quad (7.20)$$

The specific internal energy of the gas mixture can be obtained from the internal energy and mass of the gas mixture:

$$u_{G2} = \frac{(mu)_{G2}}{m_{G2}}. \quad (7.21)$$

The temperature of the gas mixture is determined as follows:

$$\begin{aligned} u_{G2} &= X_{v2} [u_{fg}(T_3) + C_{v,v}(T_{G2} - T_3)] + (1 - X_{v2}) C_{v,N}(T_{G2} - T_3) \\ &= X_{v2} u_{fg}(T_3) + [X_{v2} C_{v,v} + (1 - X_{v2}) C_{v,N}] (T_{G2} - T_3), \end{aligned} \quad (7.22)$$

where $T_3 = 273.15$ °C. Define the composite specific heat of the gas mixture as

$$C_{v2} = X_{v2} C_{v,v} + (1 - X_{v2}) C_{v,N}. \quad (7.23)$$

Solving Eq. (7.22) for T_{G2} with the above definition, we obtain

$$T_{G2} = \frac{u_{G2} - u_{fg}(T_3)X_{v2}}{C_{v2}} + 273.15, \quad (7.24)$$

where T_{G2} is in degree Kelvin.

The total liquid volume of the wetwell is primarily the volume of the pool water:

$$V_{L2} = \frac{m_{L2}}{\rho_{L2}}, \quad (7.25)$$

where ρ_{L2} is the density of the pool water.

The volume occupied by the gas mixture is simply

$$V_{G2} = V_2 - V_{L2}, \quad (7.26)$$

where V_2 is the total volume of wetwell.

The gas mixture pressure and the partial component pressures in wetwell are obtained by assuming that the water vapor and nitrogen gas of the gas mixture behave as an *ideal gas*. Under the ideal gas law, we may write

$$P_{v2} V_{v2} = n_{v2} \bar{R} T_{v2}, \quad (7.27)$$

$$P_{N2} V_{N2} = n_{N2} \bar{R} T_{N2}, \quad (7.28)$$

where $\bar{R}$ is the universal gas constant ($8.3143 \text{ J Kmol}^{-1} \text{ K}^{-1}$), n 's are the mole numbers of the gas components given by the ratios of the component masses (m_{v2} and m_{N2}) and their molar masses or molecular weights (M_{v2} and M_{N2}):

$$n_{v2} = \frac{m_{v2}}{M_{v2}}, \quad (7.29)$$

$$n_{N2} = \frac{m_{N2}}{M_{N2}}, \quad (7.30)$$

and the component temperatures, T_{v2} and T_{N2} , are assumed to be that of the gas mixture T_{G2} .

The component masses are defined by their mass fractions:

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$$m_{v2} = X_{v2} m_{G2}, \quad (7.31)$$

$$m_{N2} = (1 - X_{v2}) m_{G2}. \quad (7.32)$$

Now, from the definition of vapor mass fraction, we have

$$X_{v2} = \frac{m_{v2}}{m_{N2} + m_{v2}}. \quad (7.33)$$

Using the component masses from Eqs. (7.27) and (7.28) in Eq. (7.33) along with the fact that $P_{G2} = P_{v2} + P_{N2}$, we obtain

$$X_{v2} = \frac{P_{v2} M_{v2}}{(P_{G2} - P_{v2}) M_{N2} + P_{v2} M_{v2}}. \quad (7.34)$$

Solving the above equation for P_{v2} , we obtain the partial vapor pressure:

$$P_{v2} = \frac{X_{v2} P_{G2}}{\frac{M_{v2}}{M_{N2}} + X_{v2} \left(1 - \frac{M_{v2}}{M_{N2}} \right)}. \quad (7.35)$$

Similarly from the definition of nitrogen mass fraction, we obtain the partial nitrogen pressure as follows:

$$P_{N2} = \frac{(1 - X_{v2}) \left(\frac{M_{v2}}{M_{N2}} \right) P_{G2}}{\frac{M_{v2}}{M_{N2}} + X_{v2} \left(1 - \frac{M_{v2}}{M_{N2}} \right)}. \quad (7.36)$$

Note that $P_{v2} + P_{N2} = P_{G2}$ as it should according to Dalton's law. Equations (7.35) and (7.36) require the knowledge of the gas mixture pressure P_{G2} , which can be determined from the ideal gas law for the gas mixture:

$$P_{G2} V_{G2} = \left[\frac{X_{v2} m_{G2}}{M_{v2}} + \frac{(1 - X_{v2}) m_{G2}}{M_{N2}} \right] \bar{R} T_{G2}, \quad (7.37)$$

whence

$$P_{G2} = \frac{T_{G2} m_{G2} \bar{R}}{V_{G2}} \left(\frac{X_{v2}}{M_{v2}} + \frac{1 - X_{v2}}{M_{N2}} \right). \quad (7.38)$$

The relative humidity in the wetwell is of practical interest and is defined as

$$\xi_2 = \frac{P_{v2}}{P_{sat}(T_{G2})}. \quad (7.39)$$

The suppression pool temperature T_{L2} is determined from the definition of the specific internal energy of the pool:

$$u_{L2} = C_{p,l2} (T_{L2} - T_3), \quad (7.40)$$

where $C_{p,l2}$ is the specific heat at constant pressure of the pool water.

Solving Eq. (7.40) for T_{L2} , we obtain

$$T_{L2} = \frac{u_{L2}}{C_{p,l2}} + T_3, \quad (7.41)$$

where $T_3 = 273.15^\circ \text{C}$ and T_{L2} is in degree Kelvin.

When the suppression pool reaches saturation, the mass fraction of vapor emerging from the pool is given by

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$$X_{L2} = \frac{P_{v,L2}}{\frac{M_{N2}}{M_{v2}} P_{G2} - \left(\frac{M_{N2}}{M_{v2}} - 1 \right) P_{v,L2}}, \quad (7.42)$$

where

$$\begin{aligned} P_{v,L2} &= P_{v2} && \text{if } P_{v2} < P_{s,L2}, \\ &= P_{s,L2} = P_s(T_{L2}) && \text{otherwise.} \end{aligned} \quad (7.43)$$

The gas mixture density is calculated from the gas mixture mass as follows:

$$\rho_{G2} = \frac{m_{G2}}{V_{G2}}. \quad (7.44)$$

7.2.1.3 Wetwell Mass and Heat Transfer

The wetwell mass and heat transfer deal with the evaporation at the pool surface, the bulk condensation in the gas space, and the wall condensation.

7.2.1.3.1 Evaporation at Pool Surface

The rate of evaporation per unit area at the pool surface is calculated from

$$\Gamma_{at} = \bar{h}_{G2} (P_{s1} - P_{v2}), \quad (7.45)$$

where $\bar{h}_{G2}$ is the mass transfer coefficient and P_{s1} is the pressure at interface between the pool and gas space. It is assumed that the interface pressure is the saturation pressure corresponding to the pool temperature T_{L2} . That is,

$$P_{s1} = P_s(T_{L2}). \quad (7.46)$$

The mass transfer coefficient is given by

$$\bar{h}_{G2} = \left(\frac{\bar{h}_{c,i2}}{\rho_{G2} C_{p,G2}} \right) \left(\frac{P_{G2} M_{v2}}{\bar{R} T_{G2} P_{Bm}} \right) \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67}, \quad (7.47)$$

where N_{Pr} is the Prandtl number of wetwell atmosphere ($N_{Pr} = 0.72$), N_{Sc} is the Schmidt number of wetwell atmosphere ($N_{Sc} = 0.58$),

$$P_{Bm} = \frac{(P_{si} - P_{v2})}{\ln \left(\frac{P_{G2} - P_{v2}}{P_{G2} - P_{si}} \right)}, \quad (7.48)$$

$$C_{p,G2} = \left(\frac{\gamma_{G2}}{\gamma_{G2} - 1} \right) \frac{\bar{R}}{M_{G2}}, \quad (7.49)$$

$$\rho_{G2} = \frac{P_{G2} M_{G2}}{T_{G2} \bar{R}}, \quad (7.50)$$

from the ideal gas law, and

$$\bar{h}_{c,i2} = 0.14 k_{G2} \left(\frac{g}{v^2} \right)^{1/3} F_{\Gamma i,2}, \quad (7.51)$$

$$\begin{aligned} F_{\Gamma i,2} &= \left(\frac{P_{si}}{P_{v2}} - 1 \right)^{1/3} \quad \text{if } P_{si} > P_{v2}, \\ &= \left(\frac{P_{v2}}{P_{si}} - 1 \right)^{1/3} \quad \text{if } P_{si} \leq P_{v2}. \end{aligned} \quad (7.52)$$

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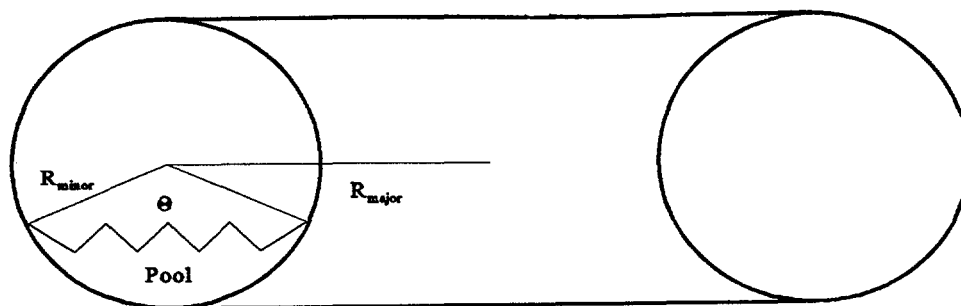


Figure 7.3 Wetwell Torus of Major Radius R_{major} and Minor Radius R_{minor} in a BWR

To calculate the rate of surface evaporation, we need to know the interfacial area between the pool and the gas space. The interfacial area depends on the wetwell geometry. We shall consider a Mark I containment of a BWR. The wetwell of a Mark I containment is a huge torus. The interfacial area for a torus with major radius R_{major} and minor radius R_{minor} as shown in Figure 7.3 is given by

$$A_{12} = 4\pi R_{major} R_{minor} \sin\left(\frac{\theta}{2}\right), \quad (7.53)$$

where θ is determined from the liquid volume of the pool V_{L2} .

Note that

$$V_{L2} = 2\pi R_{major} A_{L2}, \quad (7.54)$$

where A_{L2} is the area below the liquid level in the pool given by

$$\begin{aligned} A_{L2} &= \pi R_{minor}^2 \left(\frac{\theta}{2\pi}\right) - R_{minor} \sin\left(\frac{\theta}{2}\right) R_{minor} \cos\left(\frac{\theta}{2}\right) \\ &= \frac{R_{minor}^2}{2} (\theta - \sin \theta). \end{aligned} \quad (7.55)$$

Here we have used the identity,

$$2 \sin \left(\frac{\theta}{2} \right) \cos \left(\frac{\theta}{2} \right) = \sin \theta. \quad (7.56)$$

Substituting Eq. (7.55) into Eq. (7.54), we obtain the implicit equation,

$$\theta - \sin \theta = \frac{V_{L2}}{\pi R_{major} R_{minor}^2}. \quad (7.57)$$

The above implicit equation is solved by Newton-Raphson iteration to obtain the angle θ .

Thus, the rate of evaporation from the interface is given by

$$A_{i2} \Gamma_{ai} = \frac{0.14 A_{i2} k_{G2} M_{v2}}{\bar{R}} \left(\frac{g}{v^2} N_{Sc} \right)^{1/3} \left(\frac{\gamma_{G2} - 1}{\gamma_{G2}} \right) \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} F_{\Gamma i,2} \ln \left(\frac{P_{G2} - P_{v2}}{P_{G2} - P_{si}} \right), \quad (7.58)$$

where γ_{G2} is the polytropic expansion coefficient and $\gamma_{G2} = 1.4$ is used.

7.2.1.3.2 Bulk Condensation in Gas Space

The bulk condensation from the vapor in gas space is modeled as a first-order reaction with a relaxation time τ_0 treated as an input (typical value is 0.25 s):

$$\frac{dm_{v2}}{dt} = - \frac{m_{v2}}{\tau_0} \frac{P_{v2} - P_{s2}}{P_{v2}} = \frac{M_{v2} V_{v2}}{\bar{R} T_{G2} \tau_0} (P_{s2} - P_{v2}), \quad (7.59)$$

where P_{s2} is the saturation pressure at the gas temperature T_{G2} . That is,

$$P_{s2} = P_s(T_{G2}). \quad (7.60)$$

The bulk condensation rate per unit volume is defined as

$$\Gamma_{v2} = \frac{1}{V_{v2}} \frac{dm_{v2}}{dt} = \frac{M_{v2}}{\bar{R} T_{G2} \tau_0} (P_{s2} - P_{v2}) \leq 0. \quad (7.61)$$

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Note that Γ_{v2} is zero if $P_{s2} > P_{v2}$ because steam can only condense if subcooled. The total bulk condensation rate in the gas space is simply $V_{G2} \Gamma_{v2}$.

7.2.1.3.3 Wetwell Wall Condensation

The wetwell wall condensation involves heat transfer from the wall to the gas mixture in the wetwell. Consider a general case of convection plus condensation at the wall of thickness s_{w2} with a liquid film of thickness δ_2 adjacent to the wall. We shall use the subscript i to represent the interface between the gas mixture and the liquid film, the subscript 0 to denote the interface between the liquid film and the inner wall, and the subscript 2 to stand for the wetwell.

Continuity of heat flux at the interfaces requires that

$$\dot{Q}_{w2} = A_{w2} \frac{k_{i2}}{\delta_2} (T_{i2} - T_{02}) = A_{w2} \bar{h}_{c2} (T_{G2} - T_{i2}) + A_{w2} \bar{h}_{G2} [P_{v2} - P_s(T_{i2})] h_{fg}(T_{i2}), \quad (7.62)$$

where $\dot{Q}_{w2}$ is the wetwell wall heat flow, A_{w2} is the total wetwell wall surface area, and T_{i2} is the temperature at the interface between the gas mixture and the liquid film in wetwell gas space.

Estimation of T_{02}

Consider a quadratic temperature profile across the wall, that is

$$T_{w2} = a_2 x^2 + b_2 x + c_2, \quad (7.63)$$

subject to the boundary conditions:

$$T_{w2} = T_{02} \quad \text{at } x = 0 \text{ (inner wall)}, \quad (7.64)$$

$$\frac{dT_{w2}}{dx} = \frac{1}{k_{w2}} \frac{k_{i2}}{\delta_2} (T_{02} - T_{i2}) \quad \text{at } x = 0 \text{ (inner wall)}, \quad (7.65)$$

$$\frac{dT_{w2}}{dx} = 0 \quad \text{at } x = s_{w2} \text{ (outer wall)}. \quad (7.66)$$

From Eq. (7.63) we obtain

$$\frac{dT_{w2}}{dx} = 2 a_2 x + b_2, \quad (7.67)$$

and

$$\bar{T}_{w2} = \frac{\int_0^{s_{w2}} T_{w2} dx}{\int_0^{s_{w2}} dx} = \frac{a_2}{3} s_{w2}^2 + \frac{b_2}{2} s_{w2} + c_2. \quad (7.68)$$

Applying the boundary conditions (7.64), (7.65), and (7.66), we obtain the constants:

$$c_2 = T_{o2}, \quad (7.69)$$

$$b_2 = \frac{k_{\ell 2}}{\delta_2 k_{w2}} (T_{o2} - T_{i2}), \quad (7.70)$$

$$a_2 = \frac{k_{\ell 2}}{2 s_{w2} \delta_2 k_{w2}} (T_{i2} - T_{o2}). \quad (7.71)$$

Substituting Eqs. (7.69), (7.70), and (7.71) into Eq. (7.68), we obtain the relation

$$\bar{T}_{w2} - T_{i2} = (T_{o2} - T_{i2}) \left(1 + \frac{1}{3} \frac{s_{w2}}{\delta_2} \frac{k_{\ell 2}}{k_{w2}} \right). \quad (7.72)$$

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Solving the above equation for T_{02} , we get the expression,

$$T_{02} = T_{i2} + \frac{\bar{T}_{w2} - T_{i2}}{1 + \frac{1}{3} \frac{s_{w2}}{\delta_2} \frac{k_{t2}}{k_{w2}}}. \quad (7.73)$$

Now use Eq. (7.73) in Eq. (7.62) to obtain

$$\frac{\frac{\delta_2}{k_{t2}} + \frac{1}{3} \frac{s_{w2}}{k_{w2}}}{\frac{\delta_2}{k_{t2}} + \frac{1}{3} \frac{s_{w2}}{k_{w2}}} = \bar{h}_{c2}(T_{G2} - T_{i2}) + \bar{h}_{G2} h_{fg} [P_{v2} - P_s(T_{i2})]. \quad (7.74)$$

Thus, we can calculate T_{02} from the above two equations as follows:

$$T_{02} = T_{i2} - \left(\frac{\delta_2}{k_{t2}} \right) \left[\bar{h}_{c2}(T_{G2} - T_{i2}) + \bar{h}_{G2} h_{fg} (P_{v2} - P_s(T_{i2})) \right], \quad (7.75)$$

where $\bar{h}_{c2}$ is given by Eq. (7.14) and $\bar{h}_{G2}$ is given by Eq. (7.47).

Estimation of Liquid Film Thickness δ_2

Consider a liquid film of a condensing height H_{cnd} with z being the variable distance from the starting point of the condensing liquid film. From the classical laminar film analysis, we have

$$\delta = a z^{1/4}, \quad (7.76)$$

and

$$\delta_e = a H_{cnd}^{1/4}. \quad (7.77)$$

Define

$$\eta = \frac{z}{H_{cnd}}, \quad (7.78)$$

then

$$\delta = \delta_e \eta^{1/4}. \quad (7.79)$$

Since δ_2 is the *average* liquid film thickness, we have

$$\delta_2 = \delta_e \int_0^1 \eta^{1/4} d\eta = \frac{4}{5} \delta_e. \quad (7.80)$$

The wall condensate flow rate W_{cndw2} is defined as

$$W_{cndw2} = (\rho_l v_l A)_{cndw2} = \rho_l P_{cndw2} \frac{g \Delta \rho \delta_e^3}{3 \mu_l}, \quad (7.81)$$

where $P_{cndw2} = 4\pi R_{major}$ is the perimeter of both wetwell walls, and $\Delta \rho = \rho_l - \rho_g$.

Using δ_e from Eq. (7.81) in Eq. (7.80), we obtain

$$\delta_2 = \frac{4}{5} \left(\frac{3 \mu_l W_{cndw2}}{\rho_l \Delta \rho g P_{cndw2}} \right)^{1/3}. \quad (7.82)$$

Note that the wall condensation arises from the mass transfer; namely,

$$W_{cndw2} = A_{w22} \bar{h}_{G2} [P_{v2} - P_s(T_{i2})]. \quad (7.83)$$

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Using Eq. (7.47) for $\bar{h}_{G2}$ and rearranging, we obtain

$$W_{cndw2} = A_{w22} C_{cndw2} f_{nd21} f_{nd22}, \quad (7.84)$$

where

$$C_{cndw2} = 0.129 k_g \left(\frac{g N_{Sc}}{v^2} \right)^{1/6} \left(\frac{\gamma_{G2} - 1}{\gamma_{G2}} \right) \frac{M_{v2}}{\bar{R}} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67}, \quad (7.85)$$

$$f_{nd21} = \begin{cases} \left(\frac{P_{v2}}{P_{si,2}} - 1 \right)^{1/6} & \text{if } P_{v2} > P_{si,2}, \\ 0, & \text{otherwise.} \end{cases} \quad (7.86)$$

$$f_{nd22} = \ln \left(\frac{P_{G2} - P_{si,2}}{P_{G2} - P_{v2}} \right). \quad (7.87)$$

Here $P_{si,2}$ is the saturation pressure corresponding to the temperature T_{i2} .

Note that A_{w22} is the wall surface area in the gas space of the wetwell:

$$A_{w22} = 2\pi R_{major} R_{minor} (2\pi - \theta). \quad (7.88)$$

On the other hand, the wall surface area in the suppression pool is given by

$$A_{w21} = 2\pi R_{major} R_{minor} \theta. \quad (7.89)$$

The final expression for the wetwell wall heat flow rate is obtained from Eqs. (7.62) and (7.83):

$$\dot{Q}_{w2} = h_{fg}(T_{i2}) W_{cndw2} + A_{w22} \bar{h}_{c2} (T_{G2} - T_{i2}). \quad (7.90)$$

7.2.1.3.4 Interface Temperature T_{i2}

From Eq. (74), we have

$$T_{i2} - \bar{T}_{w2} = \left(\frac{\delta_2}{k_{l2}} + \frac{1}{3} \frac{s_{w2}}{k_{w2}} \right) \left\{ \bar{h}_{c2}(T_{G2} - T_{i2}) + \bar{h}_{G2} h_{fg} [P_{v2} - P_s(T_{i2})] \right\}. \quad (7.91)$$

Substituting Eqs. (7.82) and (7.84) into the above equation and rearranging, we obtain

$$T_{i2} = \bar{T}_{w2} + \left(1 + C_1 B_{1m}^{1/3} \right) \left[C_2 B_{2m} + C_3 B_{1m} \frac{h_{fg}(T_{i2})}{h_{fg}(T_3)} \right], \quad (7.92)$$

where

$$B_{1m} = \left[\frac{P_{v2}}{P_s(T_{i2})} - 1 \right]^{1/3} \ln \left(\frac{P_{G2} - P_s(T_{i2})}{P_{G2} - P_{v2}} \right) \quad \text{if } P_{v2} > P_s(T_{i2}),$$

$$= 0, \quad \text{otherwise,} \quad (7.93)$$

$$B_{2m} = \left(\frac{T_{G2}}{T_{i2}} - 1 \right)^{1/3} (T_{G2} - T_{i2}) \quad \text{if } T_{G2} \geq T_{i2},$$

$$= \left(\frac{T_{i2}}{T_{G2}} - 1 \right)^{1/3} (T_{G2} - T_{i2}) \quad \text{otherwise,} \quad (7.94)$$

$$C_1 = \frac{3 k_{w2} C_\delta A_{w22}^{1/3} C_{cndw2}^{1/3}}{k_{l2} s_{w2}}, \quad (7.95)$$

$$C_2 = \frac{0.129 k_g s_{w2}}{3 k_{w2}} \left(\frac{g N_{Pr}}{v^2} \right)^{1/3}, \quad (7.96)$$

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$$C_3 = \left(\frac{C_{cndw2} s_{w2}}{3 k_{w2}} \right) h_{fg}(T_3), \quad (7.97)$$

and

$$C_8 = \frac{4}{5} \left(\frac{3 \mu_t}{\rho_t \Delta \rho g P_{cndw2}} \right)^{1/6}. \quad (7.98)$$

Equation (7.92) is an implicit equation for T_{i2} and must be solved by iteration. Newton-Raphson method of iteration is used to obtain the interface temperature T_{i2} from Eq. (7.92).

7.2.1.3.5 Suppression Pool Level Tracking

The suppression pool level is defined as the distance from the center of the wetwell to the liquid surface in the pool. Referring to Figure 7.3, the pool level is given by

$$L_{pool} = -R_{minor} \cos\left(\frac{\theta}{2}\right). \quad (7.99)$$

Note that the pool level is defined to be zero at the center, negative below the center, and positive above it. Normally, the pool level is below the center.

7.2.2 Drywell Dynamics

The drywell is primarily an air space with a sump for collecting water and a drywell wall.

7.2.2.1 State Equations

7.2.2.1.1 Drywell Gas Space Mass and Energy Balances

As for the wetwell, the drywell gas space is assumed to consist of nitrogen gas and steam.

The mass balance equation for the drywell gas mixture is

$$\frac{dm_{GI}}{dt} = (\sum W)_1 + W_{21} - W_{12} - W_{LKI} + W_{LKV}, \quad (7.100)$$

where

$$(\sum W)_1 = W_{BRL} + W_{BSL} + W_{BFW} + V_{GI} \Gamma_{v1} - W_{cndwl} - W_{cndcl} + W_{SPR1}, \quad (7.101)$$

and

W_{21}	=	mass flow rate from wetwell to drywell,
W_{12}	=	mass flow rate from drywell to wetwell,
W_{LK1}	=	leakage flow rate from drywell to reactor building through drywell wall,
W_{LKV}	=	leakage flow rate from reactor pressure vessel to drywell,
W_{BRL}	=	break flow rate from recirculation line break if any,
W_{BSL}	=	break flow rate from steamline break if any,
W_{BFW}	=	break flow rate from feedwater line break if any,
W_{cndwl}	=	condensate flow rate from drywell wall,
W_{cndcl}	=	condensate flow rate from drywell coolers,
W_{SPR1}	=	drywell spray cooling flow rate if any,
$V_{GI} \Gamma_{v1}$	=	rate of bulk condensation in drywell gas space.

Note that $\Gamma_{v1} \leq 0$, $W_{cndwl} \geq 0$, and $W_{cndcl} \geq 0$.

The mass balance equation for the water vapor in drywell is

$$\frac{dm_{v1}}{dt} = (\sum W)_1 - X_{v1}(W_{LK1} + W_{12}) + X_{v2} W_{21} + W_{LKV}, \quad (7.102)$$

where X_{v1} is the mass fraction of the water vapor in drywell defined as

$$X_{v1} = \frac{m_{v1}}{m_{G1}}. \quad (7.103)$$

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The energy balance equation for the drywell gas mixture is

$$\begin{aligned} \frac{d(mu)_{G1}}{dt} = & W_{BRL} h_{RL} + W_{BFW} h_{FW} + (W_{BSL} + W_{LKV}) h_g(P_s) + V_{G1} \Gamma_{v1} h_f(T_{G1}) \\ & + W_{SPRI} C_{pt}(T_{RHR} + T_3) - W_{cndwl} C_{pt}(T_{i1} + T_3) + W_{21} h_{G2} \\ & - (W_{12} + W_{LKI}) h_{G1} - W_{cndcl} C_{pt}(T_{c1,e} + T_3) - S_{Qc1} \dot{Q}_{c1} - \dot{Q}_{w1} + \dot{Q}_{RPV}, \end{aligned} \quad (7.104)$$

where

$$h_{G1} = u_{G1} + \frac{P_{G1}}{\rho_{G1}}, \quad (7.105)$$

$$h_{G2} = u_{G2} + \frac{P_{G2}}{\rho_{G2}}, \quad (7.106)$$

and

- T_{RHR} = exit temperature of the RHR system,
- $T_{c1,e}$ = drywell cooler exit temperature,
- S_{qc1} = switch for drywell cooling (0=default, 1=turned on by temperature setpoint),
- $\dot{Q}_{c1}$ = heat loss from drywell coolers,
- $\dot{Q}_{w1}$ = heat loss from drywell gas space to reactor building through drywell wall,
- $\dot{Q}_{RPV}$ = heat gain from reactor pressure vessel.

7.2.2.1.2 Drywell Sump Liquid Mass Balance

The mass balance equation for the drywell sump liquid is

$$\frac{dm_{L1}}{dt} = -V_{G1} \Gamma_{v1} + W_{cndw1} + W_{cndc1} \quad (7.107)$$

7.2.2.1.3 Drywell Wall Liner Heat Balance

The heat balance equation for the drywell wall liner is given by

$$\frac{dT_{w1}}{dt} = \frac{\dot{Q}_{w1}}{(Mc)_{w1}}, \quad (7.108)$$

where $(Mc)_{w1}$ is the heat capacity of drywell wall specified via an input. It can also be defined as

$$(Mc)_{w1} = \rho_{w1} C_{p,w1} s_{w1} A_{w1} \quad (7.109)$$

where

ρ_{w1}	=	drywell wall density,
$C_{p,w1}$	=	specific heat capacity of the drywell wall,
s_{w1}	=	drywell wall thickness,
A_{w1}	=	drywell wall surface area.

7.2.2.2 Drywell Secondary Variables

The drywell secondary variables consist of the pressures, temperatures, specific internal energies, mass fractions, and densities. These secondary variables are obtained from the state variables.

The specific internal energy of the drywell gas mixture is given by

$$u_{G1} = \frac{(mu)_{G1}}{m_{G1}} \quad (7.110)$$

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The drywell vapor mass fraction is, by definition,

$$X_{vl} = \frac{m_{vl}}{m_{Gl}}. \quad (7.111)$$

The temperature of drywell gas mixture is determined as follows:

$$\begin{aligned} u_{Gl} &= X_{vl} [u_{fg}(T_3) + C_{v,v}(T_{Gl} - T_3)] + (1 - X_{vl}) C_{v,N}(T_{Gl} - T_3) \\ &= X_{vl} u_{fg}(T_3) + [X_{vl} C_{v,v} + (1 - X_{vl}) C_{v,N}] (T_{Gl} - T_3), \end{aligned} \quad (7.112)$$

where $T_3 = 273.15$ °C. Define the composite specific heat of the gas mixture as

$$C_{vl} = X_{vl} C_{v,v} + (1 - X_{vl}) C_{v,N}. \quad (7.113)$$

Solving Eq. (7.112) for T_{Gl} with the above definition, we obtain

$$T_{Gl} = \frac{u_{Gl} - u_{fg}(T_3)X_{vl}}{C_{vl}} + 273.15, \quad (7.114)$$

where T_{Gl} is in degree Kelvin.

The gas mixture pressure and the partial component pressures in drywell are obtained by assuming that the water vapor and nitrogen gas of the gas mixture behave as an *ideal gas*. Under the ideal gas law, we may write

$$P_{vl} V_{vl} = n_{vl} \bar{R} T_{vl}, \quad (7.115)$$

$$P_{Nl} V_{Nl} = n_{Nl} \bar{R} T_{Nl}, \quad (7.116)$$

where $\bar{R}$ is the universal gas constant ($8.3143 \text{ J Kmol}^{-1} \text{ K}^{-1}$), n 's are the mole numbers of the gas components given by the ratios of the component masses (m_{vl} and m_{Nl}) and their molar masses or molecular weights (M_{vl} and M_{Nl}):

$$n_{vl} = \frac{m_{vl}}{M_{vl}}, \quad (7.117)$$

$$n_{NI} = \frac{m_{NI}}{M_{NI}}, \quad (7.118)$$

and the component temperatures, T_{vl} and T_{NI} , are assumed to be that of the gas mixture T_{GI} .

The component masses are defined by their mass fractions:

$$m_{vl} = X_{vl} m_{GI}, \quad (7.119)$$

$$m_{NI} = (1 - X_{vl}) m_{GI}. \quad (7.120)$$

Now, from the definition of vapor mass fraction, we have

$$X_{vl} = \frac{m_{vl}}{m_{NI} + m_{vl}}. \quad (7.121)$$

Using the component masses from Eqs. (7.115) and (7.116) in Eq. (7.121) along with the fact that $P_{GI} = P_{vl} + P_{NI}$, we obtain

$$X_{vl} = \frac{P_{vl} M_{vl}}{(P_{GI} - P_{vl}) M_{NI} + P_{vl} M_{vl}}. \quad (7.122)$$

Solving the above equation for P_{vl} , we obtain the partial vapor pressure:

$$P_{vl} = \frac{X_{vl} P_{GI}}{\frac{M_{vl}}{M_{NI}} + X_{vl} \left(1 - \frac{M_{vl}}{M_{NI}} \right)}. \quad (7.123)$$

Similarly from the definition of nitrogen mass fraction, we obtain the partial nitrogen pressure as follows:

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$$P_{NI} = \frac{(1 - X_{vl}) \left(\frac{M_{vl}}{M_{NI}} \right) P_{GI}}{\frac{M_{vl}}{M_{NI}} + X_{vl} \left(1 - \frac{M_{vl}}{M_{NI}} \right)} \quad (7.124)$$

Note that $P_{vl} + P_{NI} = P_{GI}$ as it should according to Dalton's law. Equations (7.123) and (7.124) require the knowledge of the gas mixture pressure P_{GI} , which can be determined from the ideal gas law for the gas mixture:

$$P_{GI} V_{GI} = \left[\frac{X_{vl} m_{GI}}{M_{vl}} + \frac{(1 - X_{vl}) m_{GI}}{M_{NI}} \right] \bar{R} T_{GI}, \quad (7.125)$$

whence

$$P_{GI} = \frac{T_{GI} m_{GI} \bar{R}}{V_{GI}} \left(\frac{X_{vl}}{M_{vl}} + \frac{1 - X_{vl}}{M_{NI}} \right). \quad (7.126)$$

The relative humidity in the drywell is defined as

$$\xi_1 = \frac{P_{vl}}{P_{sat}(T_{GI})}. \quad (7.127)$$

The gas mixture density is calculated from the gas mixture mass as follows:

$$\rho_{GI} = \frac{m_{GI}}{V_{GI}}. \quad (7.128)$$

The specific enthalpy h_{RL} used for the recirculation line break flow in Eq. (7.104) is calculated from the liquid enthalpy as a function of system pressure P_s and recirculating coolant temperature $T_{l,RL}$:

$$h_{RL} = h_l(P_s, T_{l,RL}), \quad (7.129)$$

where h_l is a two-place property function in RAMONA-4B.

7.2.2.3 Drywell Cooler Dynamics

The following two assumptions are made:

1. The drywell cooler is a counter-current flow heat exchanger with the gas mixture (air and water vapor) on the shell side (index 2) and service water on the tube side (index 1).
2. The tube wall temperature is laterally uniform.

The energy balance of the cooler requires that

$$q_w' = W_1 \frac{dh_1}{dz} = W_2 \frac{dh_2}{dz}, \quad (7.130)$$

where q_w' is the linear heat flux on the tube wall and z is the spatial coordinate along the cooler.

The continuity of heat flux imposes that

$$q_w' = \xi \left[\bar{h}_{c2} (T_2 - T_w) + \bar{h}_G (P_v - P_{sat}) \right] = \xi \bar{h}_{c1} (T_w - T_1), \quad (7.131)$$

where ξ is the heated perimeter, $\bar{h}_{c1}$ and $\bar{h}_{c2}$ are heat transfer coefficients, and $\bar{h}_G$ is the mass transfer coefficient.

Solving Eq. (7.131) for T_w , we obtain

$$T_w = \frac{\bar{h}_{c1} T_1 + \bar{h}_{c2} T_2}{\bar{h}_{c1} + \bar{h}_{c2}} + \frac{h_{fg} \bar{h}_G (P_v - P_{sat})}{\bar{h}_{c1} + \bar{h}_{c2}}. \quad (7.132)$$

Substituting Eq. (7.132) into Eq. (7.131), we get

$$q_w' = \xi \left[\frac{T_2 - T_1}{\frac{1}{\bar{h}_{c1}} + \frac{1}{\bar{h}_{c2}}} + \frac{\bar{h}_{c1}}{\bar{h}_{c1} + \bar{h}_{c2}} h_{fg} \bar{h}_G (P_v - P_{sat}) \right]. \quad (7.133)$$

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On the tube side, the caloric equation of state yields:

$$h_1 = C_{pt}(T_1 - T_3), \quad (7.134)$$

where T_3 is the triple point temperature ($T_3 = 273.15^\circ\text{C}$). Thus, we have

$$\frac{dh_1}{dz} = C_{pt} \frac{dT_1}{dz}. \quad (7.135)$$

On the shell side, the gas mixture enthalpy is given by

$$h_2 = X_N C_{pN}(T_2 - T_3) + X_v [h_{fg,3} + C_{pv}(T_2 - T_3)] + X_L C_{pt}(T_2 - T_3), \quad (7.136)$$

where X_N is the mass fraction of nitrogen, X_v is the mass fraction of vapor, and X_L is the mass fraction of liquid in the gas mixture. In general, X_L is nearly zero. Therefore, $X_N + X_v \approx 1$, or $X_N \approx 1 - X_v$.

Define the average specific heat capacity for the mixture,

$$\bar{C}_{p2} = X_N C_{pN} + X_v C_{pv} + X_L C_{pt} \approx C_{pN} + (C_{pv} - C_{pN})X_v, \quad (7.137)$$

then we get from Eq. (7.136), while neglecting insignificant small terms,

$$\frac{dh_2}{dz} = h_{fg,3} \frac{dX_v}{dz} + \bar{C}_{p2} \frac{dT_2}{dz}. \quad (7.138)$$

Substituting Eqs. (7.135) and (7.138) into Eq. (7.130), we obtain the following two equations:

$$\frac{dT_1}{dz} = \frac{q_w'}{W_1 C_{pt}}, \quad (7.139)$$

and

$$\frac{dT_2}{dz} = \frac{q_w'}{W_2 \bar{C}_{p2}} - \frac{h_{fg,3}}{\bar{C}_{p2}} \frac{dX_v}{dz}. \quad (7.140)$$

Combine the above two equations to yield

$$\frac{d(T_2 - T_1)}{dz} = q_w' \left(\frac{1}{W_2 \bar{C}_{p2}} - \frac{1}{W_1 C_{p1}} \right) - \frac{h_{fg,3}}{\bar{C}_{p2}} \frac{dX_v}{dz}. \quad (7.141)$$

For convenience, define the following variables:

$$\theta = T_2 - T_1, \quad (7.142)$$

$$U = \frac{1}{\frac{1}{\bar{h}_{c1}} + \frac{1}{\bar{h}_{c2}}}, \quad (7.143)$$

$$C_j = W_j C_{pj}; \quad j = 1 \text{ or } 2. \quad (7.144)$$

Using the above definitions and substituting Eq. (7.133) into Eq. (7.141), we obtain

$$\frac{d\theta}{dz} = \xi U \left(\frac{1}{C_2} - \frac{1}{C_1} \right) \left[\theta + \frac{\bar{h}_G}{\bar{h}_{c2}} h_{fg} (P_v - P_{sat}) \right] - \frac{h_{fg,3}}{\bar{C}_{p2}} \frac{dX_v}{dz}. \quad (7.145)$$

To obtain the expression for the derivative of the vapor mass fraction, consider the vapor mass balance:

$$W_2 \frac{dX_v}{dz} = \xi \bar{h}_G (P_v - P_{sat}) \quad \text{or} \quad (7.146)$$

$$\frac{dX_v}{dz} = \frac{\xi \bar{h}_G}{W_2} (P_v - P_{sat}).$$

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Upon using the above equation, Eq. (7.145) becomes

$$\begin{aligned} \frac{d\theta}{dz} = & \left(\frac{1}{C_2} - \frac{1}{C_1} \right) \xi U \theta \\ & + (P_v - P_{sat}) \frac{\bar{h}_G}{\bar{h}_{c2}} \xi U \left(\frac{1}{C_2} - \frac{1}{C_1} \right) h_{fg,3} \left(\frac{h_{fg}}{h_{fg,3}} - \frac{\bar{h}_{c2}}{U} \frac{1}{1 - \frac{C_2}{C_1}} \right). \end{aligned} \quad (7.147)$$

Using a similar correlation as Eq. (7.47) for the mass transfer coefficient above, we get

$$\frac{\bar{h}_G}{\bar{h}_{c2}} = \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/4} \frac{1}{\bar{C}_{p2}} \frac{P_2}{\rho_2 T_2} \frac{M_v}{\bar{R}} \frac{1}{P_{Bm}}, \quad (7.148)$$

where P_{Bm} is given by Eq. (7.48); namely,

$$P_{Bm} = \frac{P_v - P_{sat}}{\ell n \left(\frac{P_2 - P_{sat}}{P_2 - P_v} \right)}. \quad (7.149)$$

Assuming that the gas mixture acts as an ideal gas, then we have

$$\frac{P_2}{\rho_2 T_2} = \frac{\bar{R}}{M_2}, \quad (7.150)$$

where M_2 is the molecular weight of the gas mixture.

Thus, we have

$$\frac{\bar{h}_G}{\bar{h}_{c2}} (P_v - P_{sat}) = \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/4} \frac{1}{\bar{C}_{p2}} \frac{M_v}{M_2} \ell n \left(\frac{P_2 - P_{sat}}{P_2 - P_v} \right). \quad (7.151)$$

Since the mass fraction of nitrogen is much greater than that of the water vapor in the drywell, we may approximate $M_2 \approx M_N$ and

$$\ln \left(\frac{P_2 - P_{sat}}{P_2 - P_v} \right) = \ln \left(1 + \frac{P_v - P_{sat}}{P_2 - P_v} \right) \approx \frac{P_v - P_{sat}}{P_2 - P_v} - \frac{1}{2} \left(\frac{P_v - P_{sat}}{P_2 - P_v} \right)^2 + \dots \quad (7.152)$$

Moreover, since $P_v, P_{sat} \ll P_2$, the above equation reduces to

$$\ln \left(\frac{P_2 - P_{sat}}{P_2 - P_v} \right) \approx \frac{P_v - P_{sat}}{P_2}. \quad (7.153)$$

From the ideal gas law, we have

$$\frac{P_v}{P_2} = \frac{\frac{M_N}{M_v} X_v}{1 + X_v \left(\frac{M_N}{M_v} - 1 \right)} \approx \frac{M_N}{M_v} X_v. \quad (7.154)$$

Using the above relation, we get from Eq. (7.146)

$$\frac{dX_v}{dz} \approx - \frac{\xi \bar{h}_G}{W_2} P_2 (X_v - X_{sat}) \frac{M_N}{M_v}. \quad (7.155)$$

Since X_{sat} is independent of z , we may write

$$\frac{d(X_v - X_{sat})}{dz} \approx - \frac{\xi \bar{h}_G P_2}{W_2} \frac{M_N}{M_v} (X_v - X_{sat}). \quad (7.156)$$

Note that the coefficient on the right-hand side is constant. Therefore, the solution of Eq. (7.156) is simply

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$$(X_v - X_{sat}) = (X_v - X_{sat})_i e^{-nz}, \quad (7.157)$$

where the subscript i denotes the inlet of the drywell cooler and

$$n = \frac{\xi \bar{h}_G P_2}{W_2} \frac{M_N}{M_v}. \quad (7.158)$$

For simplicity, we define

$$m = \left(\frac{1}{C_2} - \frac{1}{C_1} \right) U \xi, \quad (7.159)$$

and

$$\phi = m (X_{vi} - X_{sat}) \frac{h_{fg,3}}{\bar{C}_{p2}} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/2} \left[\frac{h_{fg}}{h_{fg,3}} - \frac{\bar{h}_{e2}}{U \left(1 - \frac{C_2}{C_1} \right)} \right]. \quad (7.160)$$

Then Eq. (7.147) reduces to

$$\frac{d\theta}{dz} - m\theta - \phi e^{-nz} = 0. \quad (7.161)$$

The solution of the above equation is

$$\theta = \left(\theta_0 + \frac{\phi}{n+m} \right) e^{mz} - \frac{\phi}{n+m} e^{-nz}, \quad (7.162)$$

where $\theta_0 = \theta$ at $z=0$ (cooler inlet). At the cooler exit $z=L$, we have

$$\theta_e = \left(\theta_0 + \frac{\phi}{n+m} \right) e^{mL} - \frac{\phi}{n+m} e^{-nL}. \quad (7.163)$$

Consider the global energy balance of the cooler, we have

$$\dot{Q} = W_1 C_{p\ell} (T_{1e} - T_{1i}) = W_2 \left[\bar{C}_{p2} (T_{2i} - T_{2e}) + h_{fg} (X_{vi} - X_{ve}) \right]. \quad (7.164)$$

From Eq. (7.157), we get

$$X_{vi} - X_{xe} = (X_{vi} - X_{sat})(1 - e^{-nL}). \quad (7.165)$$

Substituting Eq. (7.165) into Eq. (7.164) and solving for $(T_{1e} - T_{1i})$, we obtain

$$T_{1e} - T_{1i} = \frac{W_2}{W_1 C_{p\ell}} \left[\bar{C}_{p2} (T_{2i} - T_{2e}) + h_{fg} (X_{vi} - X_{sat})(1 - e^{-nL}) \right]. \quad (7.166)$$

From Eq. (7.163), we get

$$\theta_e - \theta_0 = (T_{2e} - T_{1i})(e^{mL} - 1) + \frac{\phi}{n+m} (e^{mL} - e^{-nL}). \quad (7.167)$$

Since

$$\theta_e = T_{2i} - T_{1e}, \quad (7.168)$$

and

$$\theta_0 = T_{2e} - T_{1i}, \quad (7.169)$$

we obtain from Eq. (7.167)

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$$T_{2i} - T_{2e} = (T_{1e} - T_{1i})e^{-mL} + (T_{2i} - T_{1i})(1 - e^{-mL}) + \frac{\phi}{m+n} [1 - e^{-(m+n)L}]. \quad (7.170)$$

Upon using the above equation in Eq. (7.166), we obtain from Eq. (7.164)

$$\begin{aligned} \dot{Q} = & \frac{W_2 \bar{C}_{p2} \left\{ (T_{2i} - T_{1i})(1 - e^{-mL}) + \frac{\phi}{m+n} [1 - e^{-(m+n)L}] \right\}}{1 - \frac{W_2 \bar{C}_{p2}}{W_1 \bar{C}_{p1}} e^{-mL}} \\ & + W_2 h_{fg} (X_{vi} - X_{sat}) \frac{1 - e^{-nL}}{1 - \frac{W_2 \bar{C}_{p2}}{W_1 \bar{C}_{p1}} e^{-mL}}. \end{aligned} \quad (7.171)$$

Some simplification is possible by noting that, from Eqs. (7.158) and (7.159),

$$\begin{aligned} \frac{m}{m+n} &= \frac{1}{1 + \frac{C_1}{C_1 - C_2} \frac{\bar{h}_G \bar{C}_{p2} P_2 M_N}{U M_v}}, \\ &\approx \frac{1}{1 + \frac{C_1}{C_1 - C_2} \frac{\bar{h}_{c2}}{U} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67}}. \end{aligned} \quad (7.172)$$

Further, note that

$$\begin{aligned} \frac{h_{fg}}{h_{fg,3}} &\approx 1, \quad \text{and} \\ \frac{\bar{h}_{c2}}{U} &\approx 1 \quad \text{since } \bar{h}_{c1} \gg \bar{h}_{c2}. \end{aligned} \quad (7.173)$$

Thus,

$$\frac{m}{m+n} \approx \frac{1}{1 + \frac{C_1}{C_1 - C_2} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67}}. \quad (7.174)$$

From Eqs. (7.160) and (7.173), we obtain

$$\frac{\phi}{m+n} \approx (X_{vi} - X_{sat}) \frac{h_{fg,3}}{\bar{C}_{p2}} \frac{C_2 \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/3}}{C_2 - C_1 \left[1 + \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} \right]}. \quad (7.175)$$

Upon using Eqs. (7.173) and (7.175), Eq. (7.171) becomes

$$\begin{aligned} \dot{Q} = & C_2 (T_{2i} - T_{1i}) \frac{1 - e^{-mL}}{1 - \frac{C_2}{C_1} e^{-mL}} \\ & + W_2 h_{fg,3} (X_{vi} - X_{sat}) \left\{ - \frac{C_2 \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/3}}{\left[1 + \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} \right] C_1 - C_2} \frac{1 - e^{-(m+n)L}}{1 - \frac{C_2}{C_1} e^{-mL}} + \frac{1 - e^{-nL}}{1 - \frac{C_2}{C_1} e^{-mL}} \right\} \end{aligned} \quad (7.176)$$

In terms of the nomenclature for drywell, we may write

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$$\begin{aligned} \dot{Q}_{cl} = & C_2 (T_{GI} - T_{SRW,i}) \frac{1 - e^{-mL}}{1 - \frac{C_2}{C_1} e^{-mL}} \\ & + W_{CLRI} h_{fg,3} (X_{v1} - X_{sat}) \left\{ - \frac{C_2 \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/2}}{\left[1 + \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} \right] C_1 - C_2} \frac{1 - e^{-(m+n)L}}{1 - \frac{C_2}{C_1} e^{-mL}} + \frac{1 - e^{-nL}}{1 - \frac{C_2}{C_1} e^{-mL}} \right\}. \end{aligned} \quad (7.177)$$

where W_{CLRI} is the service water flow rate, $T_{SRW,i}$ is the service water temperature at inlet, and

$$X_{sat} = X_{sat}(T_{SRW,i}) = \frac{P_{sat} \left(\frac{M_{v1}}{M_{N1}} \right)}{P_{GI} - \left(1 - \frac{M_{v1}}{M_{N1}} \right) P_{sat}}, \quad (7.178)$$

with P_{sat} being the saturation pressure corresponding to $T_{SRW,i}$.

There are 10 coolers in the drywell. Let N_{CLRI} be the number of active drywell coolers, then the fraction of active drywell coolers is simply

$$\bar{N}_{CLRI} = \frac{N_{CLRI}}{10}. \quad (7.179)$$

The constants C_1 and C_2 are given by Eq. (7.144):

$$C_1 = \bar{N}_{CLRI} W_{SWR,i} C_{pt}, \quad (7.180)$$

and

$$C_2 = \bar{N}_{CLRI} W_{CLRI,i} [C_{pN} + (C_{pv} - C_{pN}) X_{v1}]. \quad (7.181)$$

Equation (7.177) can be written upon using Eqs. (7.180) and (7.181) as

$$\dot{Q}_{cl} = \bar{N}_{CLRI} W_{CLRI,i} \left\{ (T_{G1} - T_{SRW,i}) f_{CLRI1} + [X_{v1} - X_{sat}(T_{SRW,i})] f_{CLRI2} \right\}, \quad (7.182)$$

where

$$f_{CLRI1} = \bar{C}_{p2} \left(\frac{1 - e^{-mL}}{1 - \frac{C_2}{C_1} e^{-mL}} \right), \quad (7.183)$$

$$f_{CLRI2} = h_{fg,3} \frac{\left\{ (1 - e^{-nL}) - \frac{\left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/3} [1 - e^{-(m+n)L}]}{\left[1 + \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} \right] \frac{C_1}{C_2} - 1} \right\}}{1 - \frac{C_2}{C_1} e^{-mL}}. \quad (7.184)$$

In the above equations, mL is calculated from Eq. (7.159) as follows:

$$mL = L \xi U \left(\frac{1}{C_2} - \frac{1}{C_1} \right) = (AU)_{cl} \left(\frac{1}{C_2} - \frac{1}{C_1} \right), \quad (7.185)$$

where $(AU)_{cl} = L\xi U$ is the overall heat transfer coefficient of the drywell cooler (typical value is 45.35957 KJ/s/°C).

Similarly, nL can be calculated from Eq. (7.158). However, nL is nearly constant during a transient as can be demonstrated as follows:

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$$\begin{aligned}
 nL &= \frac{L \xi \bar{h}_G P_{G1}}{W_{CLRI}} \left(\frac{M_{NI}}{M_{v1}} \right) \\
 &\approx \frac{L \xi \bar{h}_{c2}}{W_{CLRI} \bar{C}_{p2}} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/2} \left(\frac{M_{NI}}{M_{G1}} \right) \left(\frac{P_{G1}}{P_{v1} - P_{sat}} \right) \ell n \left(\frac{P_{G1} - P_{sat}}{P_{G1} - P_{v1}} \right).
 \end{aligned}
 \tag{7.186}$$

Using the approximation Eq. (7.153) in the above equation, we obtain

$$nL \approx \frac{L \xi \bar{h}_{c2}}{W_{CLRI} \bar{C}_{p2}} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/2} \left(\frac{M_{NI}}{M_{G1}} \right).
 \tag{7.187}$$

We see that nL is nearly constant because the right-hand side of the above equation is independent of time.

The condensate flow rate from the coolers is given by

$$W_{cndc1} = \bar{N}_{CLRI} W_{CLRI,i} (X_{v1} - X_{sat}) (1 - e^{-nL}).
 \tag{7.188}$$

Thus, if the initial value of W_{cndc1} is known, nL can be estimated from the above equation:

$$nL = -\ell n \left[1 - \frac{W_{cndc1}}{\bar{N}_{CLRI} W_{CLRI}} \frac{1}{X_{v1} - X_{sat}} \right]_0,
 \tag{7.189}$$

where the subscript 0 denotes the initial condition. At the rated condition at steady state, the following values are typical for Mark I containment:

W_{cndc1}	=	0.301 kg/s,	W_{CLRI}	=	86.102 kg/s,
X_{v1}	=	0.055515,	X_{sat}	=	0.029342,
P_{G1}	=	1.08248 bar,	P_{v1}	=	0.09065 bar,
P_{NI}	=	0.99183 bar,	P_{sat}	=	0.04817 bar.

Thus, nL = 0.143372.

The drywell cooler exit temperature $T_{cl,e}$ is determined from Eq. (7.170) along with Eq. (7.164):

$$T_{cl,e} = T_{SRW,i} (1 - e^{-mL}) + \left(T_{GI} - \frac{\dot{Q}_{cl}}{C_1} \right) e^{-mL} - (X_{vl} - X_{sat}) \left(\frac{h_{fg,3}}{\bar{C}_{p2}} \right) \frac{\left(\frac{N_{Pr}}{N_{Sc}} \right)^{1/3} [1 - e^{-(m+n)L}]}{1 - \frac{C_1}{C_2} \left[1 + \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67} \right]}, \quad (7.190)$$

where the temperatures are in degree Kelvin.

7.2.2.4 Drywell Mass and Heat Transfer

The drywell mass and heat transfer deal with the bulk condensation in the gas space and the wall condensation.

7.2.2.4.1 Bulk Condensation in Gas Space

The bulk condensation from the vapor in drywell gas space is modeled by the same approach as that in the wetwell gas space described in Section 7.2.1.3.2. Following Eq. (7.61), the bulk condensation rate per unit volume in the drywell gas space is given by

$$\Gamma_{vl} = \frac{1}{V_{vl}} \frac{dm_{vl}}{dt} = \frac{M_{vl}}{\bar{R} T_{GI} \tau_0} (P_{sl} - P_{vl}) \geq 0, \quad (7.191)$$

where τ_0 is the relaxation time (~ 0.25 s) treated as an input and P_{sl} is the saturation pressure at the gas temperature T_{GI} ; namely,

$$P_{sl} = P_s(T_{GI}). \quad (7.192)$$

Note that Γ_{vl} is zero if $P_{sl} < P_{vl}$ because steam can only condense if subcooled. The total bulk condensation rate in the drywell gas space is simply $V_{GI} \Gamma_{vl}$.

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7.2.2.4.2 Drywell Wall Condensation

Following the same model development as in Section 7.2.1.3.3, we obtain the wall condensation in the drywell from Eqs. (7.84) through (7.87):

$$W_{cndwl} = A_{wl} C_{cndwl} f_{ndl1} f_{ndl2}, \quad (7.193)$$

where

$$C_{cndwl} = 0.129 k_g \left(\frac{g N_{Sc}}{v^2} \right)^{1/5} \left(\frac{\gamma_{Gl} - 1}{\gamma_{Gl}} \right) \frac{M_{vl}}{\bar{R}} \left(\frac{N_{Pr}}{N_{Sc}} \right)^{0.67}, \quad (7.194)$$

$$f_{ndl1} = \begin{cases} \left(\frac{P_{vl}}{P_{si,1}} - 1 \right)^{1/5} & \text{if } P_{vl} > P_{si,1}, \\ 0, & \text{otherwise.} \end{cases} \quad (7.195)$$

$$f_{ndl2} = \ln \left(\frac{P_{Gl} - P_{si,1}}{P_{Gl} - P_{vl}} \right). \quad (7.196)$$

Here $P_{si,1}$ is the saturation pressure corresponding to the temperature T_{i1} .

Note that A_{wl} is the wall surface area in the gas space of the drywell treated as an input.

The drywell wall heat flow rate is given by a similar expression as Eq. (7.90) for the wetwell wall heat flow rate:

$$\dot{Q}_{wl} = h_{fg}(T_{i1}) W_{cndwl} + A_{wl} \bar{h}_{cl} (T_{Gl} - T_{i1}). \quad (7.197)$$

The heat flow from the reactor pressure vessel (RPV) to the drywell is given by

$$\dot{Q}_{RPV} = U_{RPV} (T_{RPV} - T_{Gl}), \quad (7.198)$$

where U_{RPV} is the overall heat transfer coefficient of the RPV times its heat transfer area and T_{RPV} is the average temperature of the RPV wall (assumed to be the fluid temperature in the middle of the downcomer).

7.2.2.4.3 Interface Temperature T_{i1}

Following the same derivation as in Section 7.2.1.3.4 for the wetwell, we obtain

$$T_{i1} = \bar{T}_{w1} + \left(1 + D_1 E_{1m}^{1/3}\right) \left[D_2 E_{2m} + D_3 E_{1m} \frac{h_{fg}(T_{i1})}{h_{fg}(T_3)} \right], \quad (7.199)$$

where

$$E_{1m} = \left[\frac{P_{vl}}{P_s(T_{i1})} - 1 \right]^{1/3} \ln \left(\frac{P_{Gl} - P_s(T_{i1})}{P_{Gl} - P_{vl}} \right) \quad \text{if } P_{vl} > P_s(T_{i1}),$$

$$= 0, \quad \text{otherwise,} \quad (7.200)$$

$$E_{2m} = \left(\frac{T_{Gl}}{T_{i1}} - 1 \right)^{1/3} (T_{Gl} - T_{i1}) \quad \text{if } T_{Gl} \geq T_{i1},$$

$$= \left(\frac{T_{i1}}{T_{Gl}} - 1 \right)^{1/3} (T_{Gl} - T_{i1}) \quad \text{otherwise,} \quad (7.201)$$

$$D_1 = \frac{3 k_{w1} D_\delta A_{w1}^{1/3} C_{cndwl}^{1/3}}{k_{l1} s_{w1}}, \quad (7.202)$$

$$D_2 = \frac{0.129 k_g s_{w1}}{3 k_{w1}} \left(\frac{g N_{Pr}}{v^2} \right)^{1/3}, \quad (7.203)$$

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$$D_3 = \left(\frac{C_{cndwl} s_{w1}}{3 k_{w1}} \right) h_{fg}(T_3), \quad (7.204)$$

and

$$D_6 = \frac{4}{5} \left(\frac{3 \mu_l}{\rho_l \Delta p g P_{cndwl}} \right)^{1/5} \quad (7.205)$$

with P_{cndwl} being the drywell wall perimeter.

Equation (7.199) is an implicit equation for T_{i1} and must be solved by iteration. Newton-Raphson method of iteration is used to obtain the interface temperature T_{i1} from Eq. (7.199).

7.2.2.5 Drywell Sump Level Tracking

The drywell sump level is determined from the drywell liquid inventory m_{L1} as follows:

$$L_{SUMP} = \frac{m_{L1}}{\rho_{l1} A_{L1}}, \quad (7.206)$$

where A_{L1} is the drywell cross sectional area treated as an input and ρ_{l1} is the drywell liquid density.

7.2.3 Drywell and Wetwell Mass Flow Rates

7.2.3.1 Drywell-to-Wetwell Mass Flow Rate

There will be mass transfer from the drywell to the wetwell if the pressure differential between the two is sufficiently high so as to open the vent system valve, which is normally closed. The drywell-to-wetwell mass flow rate is modeled as follows:

$$\begin{aligned} W_{12}^* &= K_{12} \sqrt{P_{G1} - P_{G2}} && \text{if } P_{G1} - P_{G2} > 0.12 \text{ bar,} \\ &= 0 && \text{otherwise,} \end{aligned} \quad (7.207)$$

subject to the choking condition,

$$W_{12}^* \leq (W_{12})_{\max} \quad \text{if } P_{G1} - P_{G2} \geq 0.89 \text{ bar.} \quad (7.208)$$

The typical value of $(W_{12})_{\max}$ is 276.2 kg/s [Harrington and Fuller, 1985]. Also, the value of the loss coefficient K_{12} can be estimated from the reference conditions given by Harrington and Fuller to be $0.92587 \text{ (m kg)}^{1/2}$.

To account for the inertia effect of the vent system valve, the above mass flow rate is fed into a first-order LAG:

$$\frac{dW_{12}}{dt} = \frac{1}{\tau_{W12}} (W_{12}^* - W_{12}), \quad (7.209)$$

where τ_{W12} is the time constant for the vent system valve treated as an input.

7.2.3.2 Wetwell-to-Drywell Mass Flow Rate

There is a vacuum breaker in the wetwell which will open to discharge extra steam to the drywell if the wetwell gas pressure becomes too high. In this case, the wetwell-to-drywell mass flow rate is modeled as follows:

$$\begin{aligned} W_{21}^* &= K_{21} \sqrt{P_{G2} - P_{G1}} + Z_{CMPR} V_{CMPR} \rho_{G2} \quad \text{if } P_{G2} - P_{G1} > 0.0345 \text{ bar}, \\ &= 0 \quad \text{otherwise,} \end{aligned} \quad (7.210)$$

where K_{21} is the loss coefficient (typical value at reference conditions is 0.7407), Z_{CMPR} is the operation logic for the vacuum breaker, and V_{CMPR} is the volumetric flow rate through the compressor treated as an input (typical value at reference conditions is $0.07079 \text{ m}^3/\text{s}$).

The vacuum breaker logic is given by

$$\begin{aligned} Z_{CMPR} &= (M_{CMPR} + 1)(T_{CMPR,2} T_{CMPR,3} - T_{CMPR,1}) \\ &\quad - M_{CMPR} S_{CMPR}(T_{CMPR,1} + T_{CMPR,2}), \end{aligned} \quad (7.211)$$

where M_{CMPR} is the vacuum breaker switch (0= automatic mode, -1=manual mode), S_{CMPR} is the on/off switch for the manual mode (0=off, -1=on), and the T's are defined as follows:

$$\begin{aligned} T_{CMPR,1} &= -1 \quad \text{if } P_{G1} - P_{G2} \leq 0.0759 \text{ bar}, \\ &= 0 \quad \text{otherwise.} \end{aligned} \quad (7.212)$$

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$$\begin{aligned} T_{CMR,2} &= -1 && \text{if } P_{G1} - P_{G2} \leq 0.0931 \text{ bar,} \\ &= 0 && \text{otherwise.} \end{aligned} \quad (7.213)$$

$$\begin{aligned} T_{CMR,3} &= 0 && \text{if } Z_{CMR} \leq 0, \\ &= -1 && \text{otherwise.} \end{aligned} \quad (7.214)$$

To account for the inertia effect of the vacuum breaker, the above mass flow rate is fed into a first-order LAG:

$$\frac{dW_{21}}{dt} = \frac{1}{\tau_{W21}} (W_{21}^* - W_{21}), \quad (7.215)$$

where τ_{W21} is the time constant for the vacuum breaker treated as an input.

7.2.3.3 HPCI/RCIC Mass Flow Rates

The mass flow rates for the high-pressure coolant injection (HPCI) system and the reactor core isolation cooling (RCIC) system are available in RAMONA-4B, turned on automatically by the L2 low-level trip setpoint with their flow capacities treated as input data.

The HPCI and RCIC mass flow rates are given by

$$W_{HPCI} = (W_{HPCI})_0 \bar{n}_{HPCI} (-S_{HPCI}), \quad (7.216)$$

$$W_{RCIC} = (W_{RCIC})_0 \bar{n}_{RCIC} (-S_{RCIC}), \quad (7.217)$$

where $(W_{HPCI})_0$ is the rated HPCI flow capacity treated as an input (typical value is 315.5 kg/s), $(W_{RCIC})_0$ is the rated RCIC flow capacity also treated as an input (typical value is 37.86 kg/s), S_{HPCI} is the HPCI on/off switch (0=off, -1=on), and S_{RCIC} is the RCIC on/off switch (0=off, -1=on).

The normalized HPCI and RCIC signals, $\bar{n}_{HPCI}$ and $\bar{n}_{RCIC}$, are determined as follows:

$$\frac{d\bar{n}_j}{dt} = -\frac{S_j + \bar{n}_j}{\tau_j}; \quad j = \text{HPCI or RCIC}, \quad (7.218)$$

where τ_j is the first-order lag time constant. Both HPCI and RCIC flow have similar characteristics for the time constant:

For HPCI: in 30 sec to 99% of full flow, $(\tau_{\text{HPCI}})_{\text{on}} = 6.5140$ s and
 In 60 sec to 1% of full flow, $(\tau_{\text{HPCI}})_{\text{off}} = 13.028$ s.

For RCIC: in 25 sec to 99% of full flow, $(\tau_{\text{HPCI}})_{\text{on}} = 3.8380$ s and
 In 60 sec to 1% of full flow, $(\tau_{\text{HPCI}})_{\text{off}} = 13.028$ s.

The HPCI and RCIC systems are steam-driven pumping systems that can be actuated either automatically or manually whenever necessary to supply cooling water to the reactor vessel. The HPCI/RCIC pumps are induction pumps driven by a steam turbine. The required steam flow rate for the induction pump turbine is represented by the correlation:

$$W_{IPT} = W_{HCRC} (0.1384 + 1.11084 \times 10^{-7} P_s), \quad (7.219)$$

where P_s is the average RPV pressure in N/m^2 and

$$W_{HCRC} = -S_{\text{HPCI}} (W_{\text{HPCI}})_0 - S_{\text{RCIC}} (W_{\text{RCIC}})_0. \quad (7.220)$$

7.2.3.4 Core Spray System Mass Flow Rate

For those BWRs equipped with a core spray system (CSS), the CSS takes water from the suppression pool in the wetwell and pumps it to the reactor pressure vessel (RPV) by a pump to spray the water in the upper plenum region of the RPV.

The CSS pump must overcome the pressure difference between the RPV and PSP, the gravitational force, and the frictional loss on the pipe. That is,

$$\Delta P_p = (P_s - P_{G2}) + \rho_{i,\text{CSS}} g (H_{\text{RPV}} - H_{\text{PSP}}) + B_{f,\text{CSS}} \left(\frac{Q_{\text{CSS}}}{Q_{\text{CSS,max}}} \right)^2, \quad (7.221)$$

where ΔP_p is the pump head, P_s is the RPV pressure, H_{RPV} is the height of RPV relative to a reference zero, H_{PSP} is that of the pressure suppression pool (PSP), and the last term represents the frictional loss with Q_{CSS} being the volumetric flow rate of the CSS. The density $\rho_{i,\text{CSS}}$ is typically 995.2 kg/m^3 .

The CSS pump head is well represented by the following empirical correlation:

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$$\Delta P_p = \Delta P_{0,CSS} \left[1 - \left(\frac{Q_{CSS}}{Q_{CSS,max}} \right)^2 \right], \quad (7.222)$$

where $\Delta P_{0,CSS}$ is the rated pump head (typically, 25.73 bar).

Equating Eqs. (7.221) and (7.222), then solving the resultant equation for Q_{CSS} , we obtain

$$Q_{CSS} = Q_{CSS,max} \sqrt{\frac{\Delta P_{0,CSS} - \Delta P}{\Delta P_{0,CSS} + B_{f,CSS}}}, \quad (7.223)$$

where

$$\Delta P = (P_s - P_{G2}) + \rho_{l,CSS} g (H_{RPV} - H_{PSP}). \quad (7.224)$$

Since the gravitational force is constant, it is more convenient to express

$$\Delta P_{0,CSS} - \Delta P = P_{CSS} \left(1 - \frac{P_s - P_{G2}}{P_{CSS}} \right), \quad (7.225)$$

where

$$P_{CSS} = \Delta P_{0,CSS} - \rho_{l,CSS} g (H_{RPV} - H_{PSP}). \quad (7.226)$$

Thus, Eq. (7.223) reduces to

$$Q_{CSS} = Q_{CSS,0} \sqrt{1 - \frac{P_s - P_{G2}}{P_{CSS}}} \quad \text{if } P_s - P_{G2} \leq P_{CSS}, \quad (7.227)$$

$$= 0 \quad \text{otherwise,}$$

where

$$Q_{CSS,0} = Q_{CSS,max} \sqrt{\frac{P_{CSS}}{\Delta P_{0,CSS} + B_{f,CSS}}} \quad (7.228)$$

For a BWR4 at reference conditions, we have the following data [Harrington and Fuller, 1985]:

$$\begin{array}{ll} \Delta P_{0,CSS} = & 25.73 \text{ bar,} \\ H_{RPV} = & 29.465 \text{ m,} \end{array} \quad \begin{array}{ll} Q_{CSS,max} = & 0.322 \text{ m}^3/\text{s,} \\ H_{PSP} = & 4.4196 \text{ m.} \end{array}$$

Furthermore, we can estimate B_f from the data given in Table 6.3-1 of the Browns Ferry 1 FSAR:

$$Q_{CSS} = 6250 \text{ gpm (0.3943 m}^3/\text{s) at } P_s - P_{GZ} = 122 \text{ psid (8.4116 bar)}$$

Thus,

$$\begin{array}{ll} B_{f,CSS} = & 13.871 \text{ bar,} \\ P_{CSS} = & 23.280 \text{ bar,} \\ Q_{CSS,0} = & 0.246888 \text{ m}^3/\text{s.} \end{array}$$

The CSS mass flow rate is calculated from the volumetric flow rate:

$$W_{CSS} = \bar{n}_{CSS} \rho_{l,CSS} Q_{CSS} \quad (7.229)$$

Here $\bar{n}_{CSS}$ is the normalized CSS signal defined by the equation,

$$\frac{d\bar{n}_{CSS}}{dt} = \frac{-S_{CSS} \left(\frac{N_{CSP}}{4} \right) - \bar{n}_{CSS}}{\tau_{CSP}}, \quad (7.230)$$

where S_{CSS} is the CSS pump on/off switch (0=off, -1=on), N_{CSP} is the number of active CSS pump (a total of four pumps are available), and τ_{CSP} is the lag time constant for the CSS pump (typically, $\tau_{CSP} = 30$ s).

7.2.3.5 Low-Pressure Coolant Injection (LPCI) System Mass Flow Rate

The LPCI system also takes water from the suppression pool and pumps it to the RPV via a total of four pumps to inject the water into the lower plenum region of the RPV.

We use the same approach for the core spray system to model the LPCI mass flow rate. Thus, following the derivations in the previous section, we obtain

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$$W_{LPCI} = \bar{n}_{LPCI} \rho_{e,LPCI} Q_{LPCI} \quad (7.231)$$

Here $\bar{n}_{LPCI}$ is the normalized LPCI signal defined by the equation,

$$\frac{d\bar{n}_{LPCI}}{dt} = \frac{-S_{LPCI} \left(\frac{N_{LPCI}}{4} \right) - \bar{n}_{LPCI}}{\tau_{LPCI}}, \quad (7.232)$$

where S_{LPCI} is the LPCI pump on/off switch (0=off, -1=on), N_{LPCI} is the number of active LPCI pump (a total of four pumps are available), and τ_{LPCI} is the lag time constant for the LPCI pump (typically, $\tau_{CSP} = 30$ s).

The volumetric LPCI flow rate Q_{LPCI} is given by

$$Q_{LPCI} = Q_{LPCI,0} \sqrt{1 - \frac{P_s - P_{G2}}{P_{LPCI}}} \quad \text{if } P_s - P_{G2} \leq P_{LPCI}, \quad (7.233)$$

$$= 0 \quad \text{otherwise,}$$

where

$$Q_{LPCI,0} = Q_{LPCI,max} \sqrt{\frac{P_{LPCI}}{\Delta P_{LPCI,0} + B_{f,LPCI}}}, \quad (7.234)$$

and

$$P_{LPCI} = \Delta P_0 - \rho_{e,LPCI} g (H_{RPV} - H_{PSP}). \quad (7.235)$$

For a BWR4 at reference conditions, we have the following data [Harrington and Fuller, 1985]:

$$\begin{array}{llll} \Delta P_{0,LPCI} = & 24.699 \text{ bar,} & Q_{LPCI,max} = & 1.1488 \text{ m}^3/\text{s,} \\ H_{RPV} = & 29.413 \text{ m,} & H_{PSP} = & 4.4196 \text{ m.} \end{array}$$

Furthermore, we can estimate B_f from the data given in Table 6.3-1 of the Browns Ferry 1 FSAR:

$$Q_{LPCI} = 10,000 \text{ gpm (0.6309 m}^3/\text{s) at } P_s - P_{G2} = 20 \text{ psid (1.379 bar)}$$

Thus,

$$\begin{aligned} B_{f,LPCI} &= 44.2357 \text{ bar,} \\ P_{LPCI} &= 22.5536 \text{ bar,} \\ Q_{LPCI,0} &= 0.65570 \text{ m}^3/\text{s.} \end{aligned}$$

7.2.3.6 Break Mass Flow Rates (Henry-Fauske Critical Flow Model)

There may be three different break flows in the drywell from a steamline break, feedwater line break, and recirculation line break. These break flows are calculated by means of the Henry-Fauske choke flow model [Henry and Fauske, 1971].

The break mass flow rates are calculated as follows:

$$W_j = A_j G_c(P_t, \alpha_0) S_j; \quad j = SLB, FWB, RLB, \quad (7.236)$$

where A_j is the break area, G_c is the Henry-Fauske critical mass flux function, P_t is the pressure at the throat of break, α_0 is the void fraction at the break, s_j is the break flow switch (0=no break, 1=break), S.B. stands for steam line break, F.B. for feedwater line break, and RLB for recirculation line break.

In what follows, we describe the critical mass flux function for single-phase and two-phase conditions.

Case 1: $\alpha_0 = 0$ (single-phase condition), the critical mass flux function is given by

$$G_c(P_t, \alpha_0) = \sqrt{\frac{2 P_0 (1 - \eta)}{v_{t0}}}, \quad (7.237)$$

where P_0 is the pressure upstream of the break, v_{t0} is $1/\rho_{t0}$, and η is defined as the pressure ratio,

$$\eta = \frac{P_t}{P_0}, \quad (7.238)$$

which, for $\alpha_0 = 0$, turns out to be

$$\eta = 1 - \frac{v_{t0}}{2(v_{vt} - v_{t0}) \frac{N}{s_{fg}(P_t)} [(1 - x_e) s_f' + x_e s_g'] P_0}. \quad (7.239)$$

In the above equation, the variables on the right-hand side are

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$$v_{vt} = v_{v0} \eta^{-1/n_0}, \quad (7.240)$$

$$n_0 = \frac{(1 - x_0) C_{pf} + C_{pg}}{(1 - x_0) C_{pf} + \frac{C_{pg}}{\gamma_0}}, \quad (7.241)$$

$$x_0 = \frac{\alpha_0 \rho_g(P_0)}{\alpha_0 \rho_g(P_0) + (1 - \alpha_0) \rho_f(P_0)}, \quad (7.242)$$

$$x_e = \frac{s_0 - s_f(P_t)}{s_{fg}(P_t)}, \quad (7.243)$$

$$s_0 = s_f(P_0) + x_0 s_{fg}(P_0), \quad (7.244)$$

$$N = \text{Min} \left(1, \frac{x_e}{0.14} \right), \quad (7.245)$$

$$s_f' = \left(\frac{ds_f}{dP} \right)_{P=P_t}, \quad (7.246)$$

$$s_g' = \left(\frac{ds_g}{dP} \right)_{P=P_t}. \quad (7.247)$$

In Eq. (241), γ_0 is the isentropic expansion coefficient ($\gamma_0 = 1.275$ is used), and in Eq. (245), N is an empirical correlation, called n -relation, relating the pressure derivative of flow quality:

$$\left(\frac{dx}{dP} \right)_t = N \left(\frac{dx_e}{dP} \right)_t. \quad (7.248)$$

Case 2: $0 < \alpha_0 \leq 1$ (two-phase condition), the critical mass flux function is

$$G_c(P_t, \alpha_0) = \sqrt{\frac{P_t}{\beta_t v_{vt}}}, \quad (7.249)$$

where

$$\beta_t = \frac{x_0}{n_0} + \left(1 - \frac{v_{l0}}{v_{vt}} \right) N \frac{P_t}{s_{fg}(P_t)} [(1 - x_e)s_f' + x_e s_g']_t. \quad (7.250)$$

In this case, the pressure ratio η is given by

$$\eta = \left[\frac{\frac{(1 - \alpha_0)}{\alpha_0} (1 - \eta) + \frac{n_0}{n_0 - 1}}{\frac{x_0}{2 \beta_t \alpha_t^2} + \frac{n_0}{n_0 - 1}} \right]^{\frac{n_0}{n_0 - 1}}, \quad (7.251)$$

where

$$\alpha_t = \frac{1}{1 + \frac{1 - x_0}{x_0} \frac{v_{l0}}{v_{vt}}}. \quad (7.252)$$

Note that Eqs. (7.239) and (7.251) are both implicit equations in η . Therefore, they must be solved by iteration to obtain η . The following iteration technique is used:

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Let $f(\eta)$ represent the two implicit equations. That is,

$$\eta = f(\eta). \quad (7.253)$$

Then calculate η successively by the following iterative procedure:

Step 1. Set initial guess of η to be 0.8.

Step 2. Make successive guesses on η as

$$\eta_k = \eta_{k-1} + 0.01, \quad (7.254)$$

where the subscript k is the iteration number.

Step 3. Calculate the differences,

$$y_k = f(\eta_k) - \eta_k, \quad (7.255)$$

and

$$y_{k-1} = f(\eta_{k-1}) - \eta_{k-1}. \quad (7.256)$$

Step 4. Update the value of η by

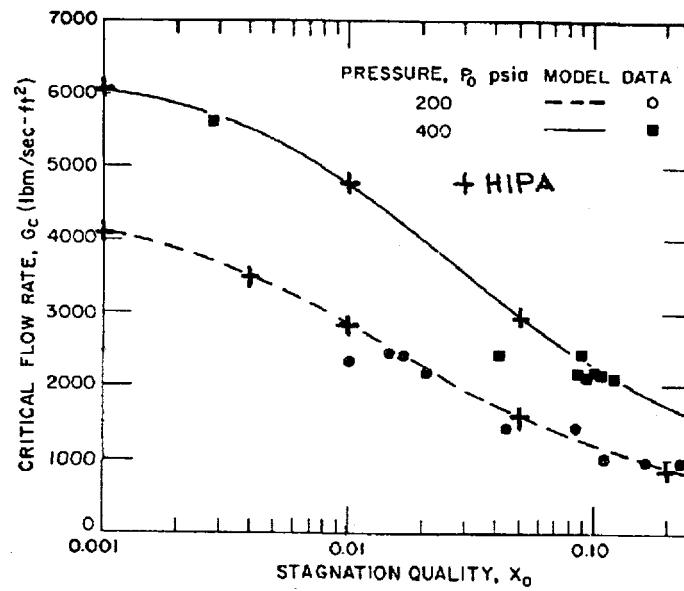
$$\eta_{k+1} = \frac{\eta_{k-1}y_k - \eta_k y_{k-1}}{y_k - y_{k-1}}; \quad k = 1, 2, \dots, m. \quad (7.257)$$

Step 5. Check the convergence,

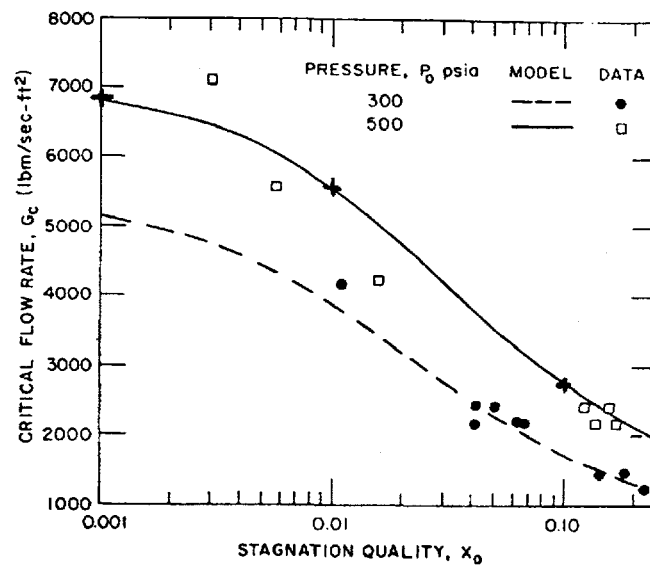
$$|\eta_m - \eta_{m-1}| \leq 10^{-6}. \quad (7.258)$$

If the above criterion is true, stop the iteration. Otherwise, go to Step 2 and repeat the iterative procedure.

The present critical flow model has been compared against experimental data in good agreement [Henry and Fauske 1971] as shown in Figure 7.4.



(a) For Pressure 200 psia and 400 psia.



(b) For Pressure 300 psia and 500 psia.

Figure 7.4 Comparison Between Present Model and Experimental Data [4].

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7.2.3.7 Leakage Mass Flow Rates

There are two leakage flows associated with the drywell. One is the leakage flow from the drywell to the reactor building. The other is the leakage flow from the RPV to the drywell.

The leakage flow rate from the drywell to reactor building is proportional to the pressure difference between the two:

$$\begin{aligned} W_{LKI} &= C_{LKI} (P_{GI} - P_{CNT}) \quad \text{if } P_{GI} \geq 1.1 \text{ bar,} \\ &= 0 \quad \text{otherwise,} \end{aligned} \quad (7.259)$$

where P_{CNT} is the pressure in the reactor building (assumed to be 1 atm) and C_{LKI} is an empirical constant treated as an input (typical value is $1.5038 \times 10^{-8} \text{ m s}$).

The leakage flow rate from the RPV to drywell is calculated from the equation,

$$\begin{aligned} W_{LKV} &= W_{LKV,0} \sqrt{\frac{T_s(P_s)}{T_{RPV,0}}} \quad \text{if } P_s \geq 3 P_{GI}, \\ &= 0 \quad \text{otherwise,} \end{aligned} \quad (7.260)$$

where $W_{LKV,0}$ is the reference leakage flow rate at the reference RPV pressure (P_s at $t=0$), T_s is the saturation temperature corresponding to the current RPV pressure P_s , and $T_{RPV,0}$ is the saturation temperature at the reference RPV pressure. $W_{LKV,0} = 0.301 \text{ kg/s}$ is used.

7.2.4 Residual Heat Removal System Cooler

The RHR cooler (heat exchanger) is used to cool the RHR flow W_{RHR} of which W_{CLR2} and W_{SPR2} are returned to the pressure suppression pool for pool cooling. Figure 7.5 shows the schematic diagram of the RHR system.

The modeling assumptions for the RHR cooler are the following:

1. The RHR cooler is a counter-current heat exchanger.
2. The service water for the cooler on the shell side has a fixed flow rate W_{SRW} and a fixed inlet temperature $T_{SRW,i}$.
3. Heat transfer in the cooler is quasi-static.

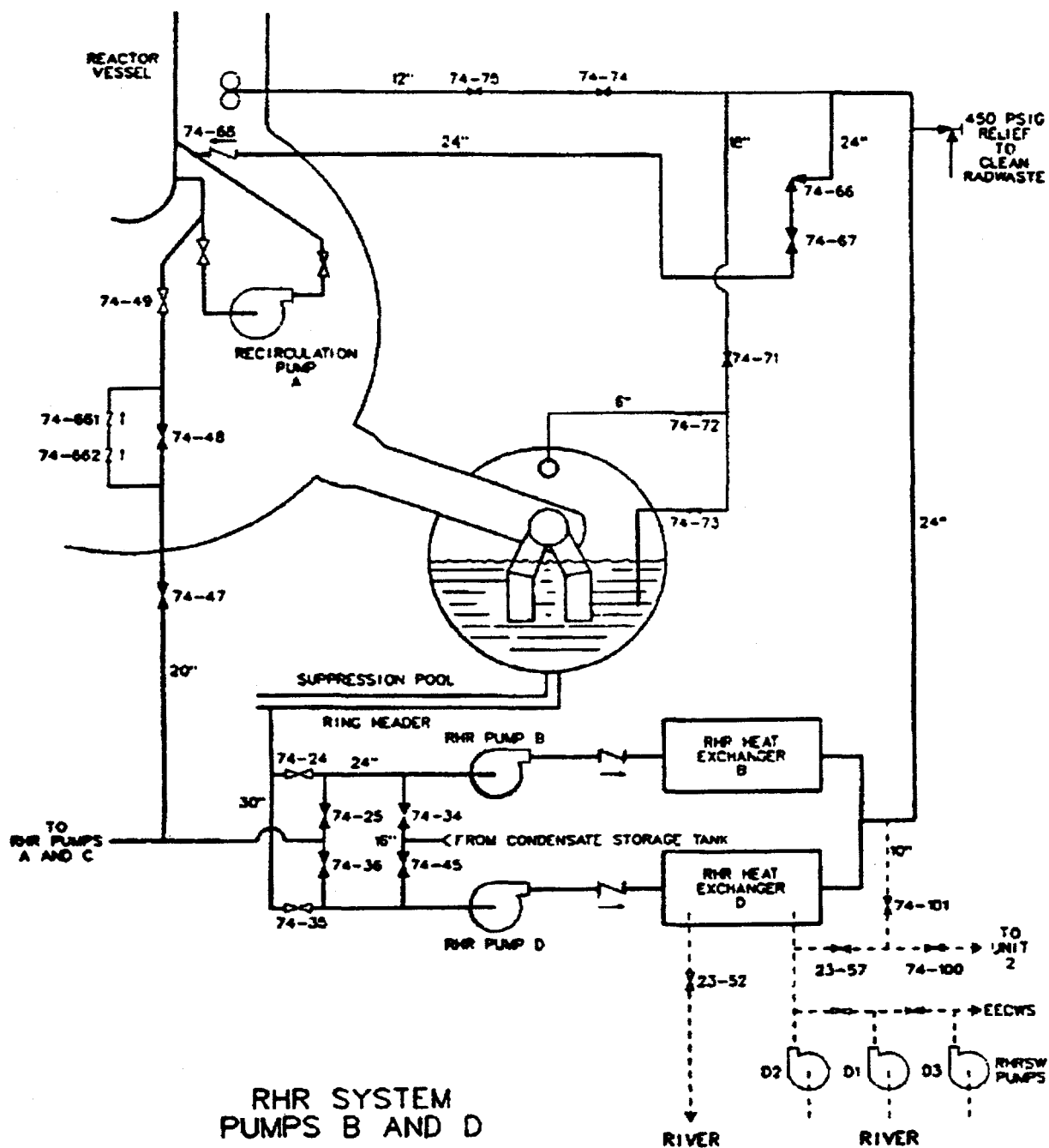


Figure 7.5 Schematic of Residual Heat Removal (RHR) System.

7. Modeling of BWR Containment

Let C_{RHR} be the perimeter of heat transfer area and U_{RHR} be the overall heat transfer coefficient of the RHR cooler. The energy balance on the shell side and tube side requires that

$$W_{SRW} C_{pl} \frac{dT_{SRW}}{dz} = W_{RHR} C_{pl} \frac{dT_{RHR}}{dz} = C_{RHR} U_{RHR} (T_{RHR} - T_{SRW}), \quad (7.261)$$

where W_{SRW} is the mass flow rate of service water, W_{RHR} is the mass flow rate through the RHR cooler, T_{SRW} is the service water temperature (treated as an input), and T_{RHR} is the RHR fluid temperature.

The above equation gives rise to the following two equations:

$$\frac{dT_{SRW}}{dz} = \frac{C_{RHR} U_{RHR}}{W_{SRW} C_{pl}} (T_{RHR} - T_{SRW}), \quad (7.262)$$

and

$$\frac{dT_{RHR}}{dz} = \frac{C_{RHR} U_{RHR}}{W_{RHR} C_{pl}} (T_{RHR} - T_{SRW}). \quad (7.263)$$

Subtracting Eq. (7.262) from Eq. (7.263), we obtain

$$\frac{d(T_{RHR} - T_{SRW})}{dz} = \frac{C_{RHR} U_{RHR}}{C_{pl}} \left(\frac{1}{W_{RHR}} - \frac{1}{W_{SRW}} \right) (T_{RHR} - T_{SRW}). \quad (7.264)$$

For convenience, define

$$\theta_{RHR} = T_{RHR} - T_{SRW}, \quad (7.265)$$

$$m_{RHR} = \frac{A_{RHR} U_{RHR}}{C_{pl}} \left(\frac{1}{W_{SRW}} - \frac{1}{W_{RHR}} \right) > 0, \quad (7.266)$$

where $A_{RHR} = C_{RHR} L_{RHR}$ is the RHR heat transfer area with L_{RHR} being the length of RHR cooler.

Equation (7.264) then becomes

$$\frac{d\theta_{RHR}}{dz} = -\frac{m_{RHR}}{L_{RHR}} \theta_{RHR}. \quad (7.267)$$

Since the coefficient is constant, the solution of the above equation is simply

$$\theta_{RHR} = \theta_{RHR,0} e^{-\frac{m_{RHR}}{L_{RHR}} z}, \quad (7.268)$$

where the subscript 0 denotes the inlet of RHR cooler.

At the exit of RHR cooler, $z=L_{RHR}$, thus

$$\theta_{RHR,e} = \theta_{RHR,0} e^{-m_{RHR}}. \quad (7.269)$$

Using the definition (7.265), we may write for Eq. (7.269)

$$T_{RHR,i} - T_{SRW,e} = (T_{RHR,e} - T_{SRW,i}) e^{-m_{RHR}}. \quad (7.270)$$

Subtracting $T_{RHR,e}$ from both sides of the above equation and rearranging, we obtain

$$\begin{aligned} T_{RHR,i} - T_{RHR,e} &= (T_{SRW,i} - T_{RHR,i}) (1 - e^{-m_{RHR}}) \\ &+ (T_{SRW,e} - T_{SRW,i}) + (T_{RHR,i} - T_{RHR,e}) (1 - e^{-m_{RHR}}). \end{aligned} \quad (7.271)$$

Note that

$$W_{SRW} (T_{SRW,e} - T_{SRW,i}) = W_{RHR} (T_{RHR,i} - T_{RHR,e}). \quad (7.272)$$

Using Eq. (7.272) in Eq. (7.271) and solving the resultant equation for $T_{RHR,i} - T_{RHR,e}$, we obtain

7. Modeling of BWR Containment

$$T_{RHR,i} - T_{RHR,e} = (T_{RHR,i} - T_{SRW,i}) \frac{1 - e^{-m_{RHR}}}{\frac{W_{RHR}}{W_{SRW}} e^{-m_{RHR}}} \quad (7.273)$$

In terms of the internal energies,

$$u_{RHR,e} = C_{vl}(T_{RHR,e} - T_3), \quad (7.274)$$

$$u_{L2} = u_{RHR,i} = C_{vl}(T_{RHR,i} - T_3), \quad (7.275)$$

Equation (7.273) becomes

$$u_{RHR,e} = u_{L2} - (u_{L2} - u_{SRW,i}) \frac{1 - e^{-m_{RHR}}}{\frac{W_{RHR}}{W_{SRW}} e^{-m_{RHR}}} \quad (7.276)$$

The effectiveness of the RHR heat exchanger is defined as

$$\begin{aligned} E_{RHR} &= \frac{W_{RHR} C_{pl}(T_{RHR,i} - T_{RHR,e})}{W_{SRW} C_{pl}(T_{RHR,i} - T_{SRW,i})}, \\ &= \frac{1 - e^{-m_{RHR}}}{1 - \frac{W_{SRW}}{W_{RHR}} e^{-m_{RHR}}}. \end{aligned} \quad (7.277)$$

At normal reference conditions, the following are typical for the RHR cooler [Harrington and Fuller, 1985]:

$$\begin{array}{llll} W_{RHR} & = & 631.4 \text{ kg/s per cooler,} & W_{SRW} = 284.1 \text{ kg/s per cooler,} \\ T_{SRW,i} & = & 308.16 \text{ }^\circ\text{K (95 }^\circ\text{F),} & E_{RHR} = 0.433. \end{array}$$

Using these reference values, we can estimate the overall heat transfer coefficient of RHR cooler as follows:

$$m_{RHR} = \ell n \left(\frac{1 - \frac{W_{SRW}}{W_{RHR}} E_{RHR}}{1 - E_{RHR}} \right) = 0.350695, \quad (7.278)$$

hence

$$\frac{A_{RHR} U_{RHR}}{C_{pl}} = \frac{m_{RHR}}{\frac{1}{W_{SRW}} - \frac{1}{W_{RHR}}} = 181.134 \text{ kg/s}. \quad (7.279)$$

The RHR mass flow rate is calculated by the equation,

$$W_{RHR} = \bar{n}_{RHR} W_{RHR,0}, \quad (7.280)$$

where $W_{RHR,0}$ is the rated mass flow rate of the RHR cooler treated as an input (typical value is 2593.1 kg/s) and $\bar{n}_{RHR}$ is the normalized RHR signal defined by the equation,

$$\frac{d\bar{n}_{RHR}}{dt} = \frac{S_{RHR} \left(\frac{N_{RHR}}{4} \right) - \bar{n}_{RHR}}{\tau_{RHR}}. \quad (7.281)$$

In the above equation, S_{RHR} is the on/off switch for the RHR cooler (0=off, 1=on), N_{RHR} is the number of *active* RHR pumps (a total of 4 pumps available), and τ_{RHR} is the lag time constant for the RHR pump treated as an input. Note that τ_{RHR} may be different for on/off conditions, hence two different input parameters, $\tau_{RHR,on}$ and $\tau_{RHR,off}$ are used.

7.2.5 Condensate Storage Tank

The condensate storage tank (CST) supplies water for the wetwell, the RHR pump, and the HPCI/RCIC pump as shown in Figure 2.

The mass inventory of the condensate storage tank (CST) is tracked by the balance equation

7. Modeling of BWR Containment

$$\frac{dm_{CST}}{dt} = -W_{AHM} - \sum W_{CST}, \quad (7.282)$$

where m_{CST} is the mass inventory of CST, W_{AHM} is the automatic hotwell makeup, and

$$\sum W_{CST} = S_{SCST}(W_{HPCI} + W_{RCIC}) + W_{CRD} + W_{CSS} + W_{LPCI}. \quad (7.283)$$

Here S_{SCST} is the option switch for taking HPCI/RCIC flow from CST (1 = HPCI/RCIC flow from CST (automatic mode), and 0 = HPCI/RCIC flow from suppression pool), and W_{CRD} is the control rod drive (CRD) mass flow rate treated as an input (typical value is 0.96 kg/s).

The automatic hotwell makeup flow W_{AHM} is driven by gravity and represented by the correlation [Harrington and Fuller, 1985]:

$$W_{AHM} = 0.03311225 + 4.927132 \times 10^{-9} \frac{m_{CST}}{\rho_{l,CST}}, \quad (7.284)$$

where W_{AHM} is in kg/s and $\rho_{l,CST}$ is the density of CST water.

The water level in the CST is calculated from the CST mass inventory:

$$L_{CST} = \frac{m_{CST}}{\rho_{l,CST} A_{CST}}, \quad (7.285)$$

where A_{CST} is the cross sectional area of CST treated as an input.

7.2.6 Initial Conditions

Initial conditions are the initial steady-state values of all state variables and any time-lagging secondary variables, which must be determined in order to start a transient.

7.2.6.1 Wetwell Initial Conditions

We assume that the initial values of wetwell gas pressure ($P_{G2,ic}$), gas temperature ($T_{G2,ic}$), and suppression pool level ($L_{pool,ic}$) are known and specified by input.

The initial wetwell vapor pressure is then given by

$$P_{v2,ic} = \xi_{2,ic} P_{sat}(T_{G2,ic}), \quad (7.286)$$

where $\xi_{2,ic}$ is the initial relative humidity of wetwell (assumed to be 1.0).

The initial wetwell nitrogen pressure is simply

$$P_{N2,ic} = P_{G2,ic} - P_{v2,ic}. \quad (7.287)$$

The initial wetwell vapor mass fraction is calculated from

$$X_{v2,ic} = \frac{P_{v2,ic} \left(\frac{M_{v2}}{M_{N2}} \right)}{P_{G2,ic} - \left(1 - \frac{M_{v2}}{M_{N2}} \right) P_{v2,ic}}. \quad (7.288)$$

The initial values of the wetwell vapor and nitrogen densities are obtained from the ideal gas law:

$$\rho_{v2,ic} = \frac{P_{v2,ic}}{T_{G2,ic} \left(\frac{\bar{R}}{M_{v2,ic}} \right)}, \quad (7.289)$$

and

$$\rho_{N2,ic} = \frac{P_{N2,ic}}{T_{G2,ic} \left(\frac{\bar{R}}{M_{N2,ic}} \right)}, \quad (7.290)$$

respectively.

The initial wetwell gas mixture density is then simply

$$\rho_{G2,ic} = \rho_{v2,ic} + \rho_{N2,ic}. \quad (7.291)$$

7. Modeling of BWR Containment

The initial liquid volume in the wetwell is determined from Eq. (7.57):

$$V_{L2,ic} = \pi R_{major} R_{minor}^2 (\theta_0 - \sin \theta_0), \quad (7.292)$$

where θ_0 is the initial θ determined from the initial suppression pool level (input):

$$\theta_0 = 2 \cos^{-1} \left(-\frac{L_{pool,ic}}{R_{minor}} \right). \quad (7.293)$$

Note that the initial suppression level $L_{pool,ic}$ is normally about 4 inches below the midplane of the wetwell. Thus, $L_{pool,ic}$ is negative since it is measured relative to the midplane (negative below and positive above).

The initial wetwell gas space volume is then given by

$$V_{G2,ic} = V_2 - V_{L2,ic}, \quad (7.294)$$

where V_2 is the total volume of wetwell specified by input.

The initial gas mixture mass and vapor mass of wetwell are given by

$$m_{G2,ic} = \rho_{G2,ic} V_{G2,ic}, \quad (7.295)$$

and

$$m_{v2,ic} = \rho_{v2,ic} V_{G2,ic}, \quad (7.296)$$

respectively.

The initial wetwell liquid mass is calculated from the equation,

$$m_{L2,ic} = \rho_{L2,ic} V_{L2,ic}, \quad (7.297)$$

where $\rho_{L2,ic}$ is the wetwell liquid density determined from the wetwell gas pressure and the suppression pool temperature, both of which are specified by input.

The initial gas and liquid internal energies of wetwell are determined from

$$(mu)_{G2,ic} = m_{G2,ic} u_{G2,ic}, \quad (7.298)$$

and

$$(mu)_{L2,ic} = m_{L2,ic} u_{L2,ic}, \quad (7.299)$$

respectively.

Here the initial specific internal energies are calculated by the equations,

$$u_{G2,ic} = (1 - X_{v2,ic}) C_{v,N2} T_{G2,ic} + X_{v2,ic} [u_{fg}(T_{c3}) + C_{v,v} T_{G2,ic}], \quad (7.300)$$

$$u_{L2,ic} = C_{p,l2} T_{pool,ic}, \quad (7.301)$$

respectively.

The initial liquid mass of the condensate storage tank is obtained from the initial liquid level in the tank ($L_{CST,ic}$) specified as an input:

$$m_{CST,ic} = \rho_{l,CST} A_{CST} L_{CST,ic}. \quad (7.302)$$

7.2.6.2 Drywell Initial Conditions

We assume that the initial values of drywell gas pressure ($P_{G1,ic}$), gas temperature ($T_{G1,ic}$), and sump level ($L_{sump,ic}$) are known and specified by input.

The initial drywell vapor pressure is then given by

$$P_{v1,ic} = \xi_{1,ic} P_{sat}(T_{G1,ic}), \quad (7.303)$$

where $\xi_{1,ic}$ is the initial relative humidity of drywell (assumed to be 0.4).

7. Modeling of BWR Containment

The initial drywell nitrogen pressure is simply

$$P_{NI,ic} = P_{GI,ic} - P_{vI,ic} \quad (7.304)$$

The initial drywell vapor mass fraction is calculated from

$$X_{vI,ic} = \frac{P_{vI,ic} \left(\frac{M_{vI}}{M_{NI}} \right)}{P_{GI,ic} - \left(1 - \frac{M_{vI}}{M_{NI}} \right) P_{vI,ic}} \quad (7.305)$$

The initial values of the drywell vapor and nitrogen densities are obtained from the ideal gas law:

$$\rho_{vI,ic} = \frac{P_{vI,ic}}{T_{GI,ic} \left(\frac{\bar{R}}{M_{vI,ic}} \right)} \quad (7.306)$$

and

$$\rho_{NI,ic} = \frac{P_{NI,ic}}{T_{GI,ic} \left(\frac{\bar{R}}{M_{NI,ic}} \right)} \quad (7.307)$$

respectively.

The initial drywell gas mixture density is then simply

$$\rho_{GI,ic} = \rho_{vI,ic} + \rho_{NI,ic} \quad (7.308)$$

The initial drywell gas space volume is then given by

$$V_{GI,ic} = V_1 - V_{sump,ic} \quad (7.309)$$

where V_1 is the total volume of drywell specified by input and $V_{\text{sump,ic}}$ is normally negligible.

The initial gas mixture mass and vapor mass of drywell are given by

$$m_{GI,ic} = \rho_{GI,ic} V_{GI,ic}, \quad (7.310)$$

and

$$m_{vI,ic} = \rho_{vI,ic} V_{GI,ic}, \quad (7.311)$$

respectively.

The initial drywell liquid mass is calculated from the equation,

$$m_{LI,ic} = \rho_{lI,ic} A_1 L_{\text{sump,ic}}, \quad (7.312)$$

where $\rho_{lI,ic}$ is the drywell liquid density determined from the drywell gas pressure and the gas temperature, both of which are specified by input.

The initial gas energy of drywell is determined from

$$(mu)_{GI,ic} = m_{GI,ic} u_{GI,ic}, \quad (7.313)$$

where the initial specific internal energy of gas mixture is given by the equation,

$$u_{GI,ic} = (1 - X_{vI,ic}) C_{v,NI} T_{GI,ic} + X_{vI,ic} [u_{fg}(T_{c3}) + C_{v,v} T_{GI,ic}]. \quad (7.314)$$

7.2.6.3 Consistent Steady-State Solutions

To insure the compatibility between the initial steady state and the subsequent transient, the consistent steady-state solutions for both the wetwell and drywell are obtained from a sufficiently long *null* transient starting from the initial estimates of the initial conditions as described above. The steady-state solutions are considered converged when the maximum residual of all state variables becomes less than a prescribed convergence criterion (0.0001).

The maximum residual is defined as follows:

7. Modeling of BWR Containment

$$\delta_{\max} = \text{Max} [\bar{y}_j], \quad j = 1, 2, 3, \dots \quad (7.315)$$

where $\bar{y}_j$ is the normalized state variable of type j given by

$$\bar{y}_j = \frac{y_j^n - y_j^{n-1}}{y_j^{n-1}}, \quad j = 1, 2, 3, \dots \quad (7.316)$$

with the superscript n being the iteration number. The maximum iteration number is limited to 20 which is sufficient because the initial conditions described above are formally exact.

7.3 Implementation

The new containment model described herein has been implemented in RAMONA-4B as the subroutine MARK1. The initial steady state is obtained as a converged null transient in the subroutine MARK1IC. Both subroutines MARK1 and MARK1IC are called by the subroutine NUMINT after the hydraulic time step has been accepted by both the accuracy and stability limits. The steam flow input to the containment is taken from the steam flow at the cell where the SRVs are located on the steam line calculated by steam line dynamics.

The input data of the new containment model are read by the subroutine PRINP on Cards 625000 through 625900. The output variables are saved on the plot file (fort.49) by the subroutine PRINT. All the state variables and secondary variables are printed at specified time points on the debug file (fort.64). Typical input data used for the present analysis are given in Table 7.1.

Subroutine FPTOT was also modified to take care of the wrapup/restart capability for the new containment model in RAMONA-4B.

7.4 Test Results

The calculational model used for testing is illustrated Figures 7.6 and 7.7. It includes the reactor pressure vessel (RPV) with all important internal components, steam lines, balance of plant (BOP), control systems, and the SLCS for boron injection.

The reactor core was modeled with 101 neutronic channels and 25 hydraulic channels in a 1/8-core symmetry. Twenty four axial cells were used in each of the neutronic and hydraulic channels. Seven evenly distributed spacers were located along the axial height of each hydraulic channel (except for the bypass channel) to account for the local form losses due to the spacer grids.

The nuclear parameters for the 3D neutron kinetics were based on the Browns Ferry Unit 1 at the end of cycle 5. The two-group cross sections and their feedback coefficients were generated from the lattice physics data provided by TVA to BNL in early 1980.

The testing was performed for a MSIV closure ATWS event per EPG level control with boron injection. In this testing, the BOP, motor/generator set dynamics and control systems options in RAMONA-4B were all activated in order to produce realistic simulation results.

Table 7.1 Typical Input Data for the Present Mechanistic Containment Model

```

*****
*****          -----
*****          Containment Model Input Data
*****          -----
*
*****          IPSP   = 0, no containment calculation,
*****                  = 1, simple model for suppression pool dynamics,
*****                  = 2, Mark1 containment model of EPA.
*
*****          IDFINP = 0, input your own data,
*****                  = 1, use built-in default input data.
*
*****          IPSP   IDFINP
*****12341234512345123451234512345
625000      2      0
*
*****          -----
*****          Input Data for Old Simplistic Containment Model
*****          -----
*
*****          PRPSP      TPSP      RMLPSP      WRHR      TRHR      WSRW      AUCP      NRHR
*****123412345678901234567890123456789012345678901234567890123456789012345
625100      1.0000E+5      32.22      3.6400E+6      631.4      25.0      284.1      362.00      4
*
*****          -----
*****          Input Data for New Mechanistic Mark1 Containment Model
*****          -----
*
*****          RMAJOR      RMINOR      ACST      ABFW      ABRL      ABSL      TAU0
*****123412345678901234567890123456789012345678901234567890123456789012345
625200      18.288      4.572      9.00      0.00263      0.00263      0.00263      0.25
*
*****          ALL      AW1      TRHRON      TRHROF      SW1      SW2      TGISPT
*****123412345678901234567890123456789012345678901234567890123456789012345
625300      502.41      1737.3      1.0E+9      1.0E+9      0.01905      0.01905      63.00
*
*****          CLPCI      CCSS      PLPCI      PCSS      TAUWC21      TAUWC12      URPV
*****123412345678901234567890123456789012345678901234567890123456789012345
625400      0.6508385      0.2447269      2.2828E+6      2.3586E+6      10.0      10.0      3456.7
*
*****          LSUMP      HUMC1      HUMC2      TSRW1      TSRWI      VCMRPR      VCSTMX
*****123412345678901234567890123456789012345678901234567890123456789012345
625500      0.004026      0.400      1.000      32.00      32.00      0.07079      1371.0
*
*****          VOLC1      VOLC2      W12MAX      W21MAX      WCLR1      WCRDHS      WCSP0
*****123412345678901234567890123456789012345678901234567890123456789012345
625600      4502.4      7561.0      276.20      220.97      86.10      0.9600      982.1
*
*****          TG2SPT      TL2SPT      WLKVO      WRHRO      WSPR20      WSRW1      WSRW2
*****123412345678901234567890123456789012345678901234567890123456789012345
625700      55.000      35.000      0.301      2591.10      3.60      65.1800      284.10
*
*****          NRHR      NLPCI      NCSP      NCLR1      TLPCI      TCSPS      TSLB      TRLB      TFWB
*****123412345123451234512345123456789012345678901234567890123456789012345
625800      4      4      4      10      1.0E+9      1.0E+9      1.0E+9      1.0E+9      1.0E+9
*
*****          PGC1      TGC1      PGC2      TGC2      TCPOOL      LPOOL      LCST
*****123412345678901234567890123456789012345678901234567890123456789012345
625900      1.08248E+5      62.80      0.99560E+5      32.22      32.22      -0.1017      152.30

```

7. Modeling of BWR Containment

The transient for this testing was an ATWS event initiated by inadvertent closure of all MSIVs at 2 sec (This was done to make the initial power spike visible from the vertical axis). The normal scram system and alternate rod insertion (ARI) system were assumed to have failed, thus constituting an ATWS. Boron injection was initiated manually at 30 sec with 60-sec delay. The boron was injected by SLCS into the middle of lower plenum. (The code allows other injection points such as the downcomer, core and riser). The water level in the RPV was controlled by a EPG level controller using a time-dependent level setpoint specified by an input tabular function.

The initial condition was the hot full power and full flow condition. The key parameters calculated by RAMONA-4B for this case are given below:

Core Thermal Power	3293 Mwt
System Pressure	6.962 Mpa
Feedwater Flow Rate	1685 kg/s
Core Flow Rate	13,040 kg/s
Core Inlet Subcooling	10.65 °C

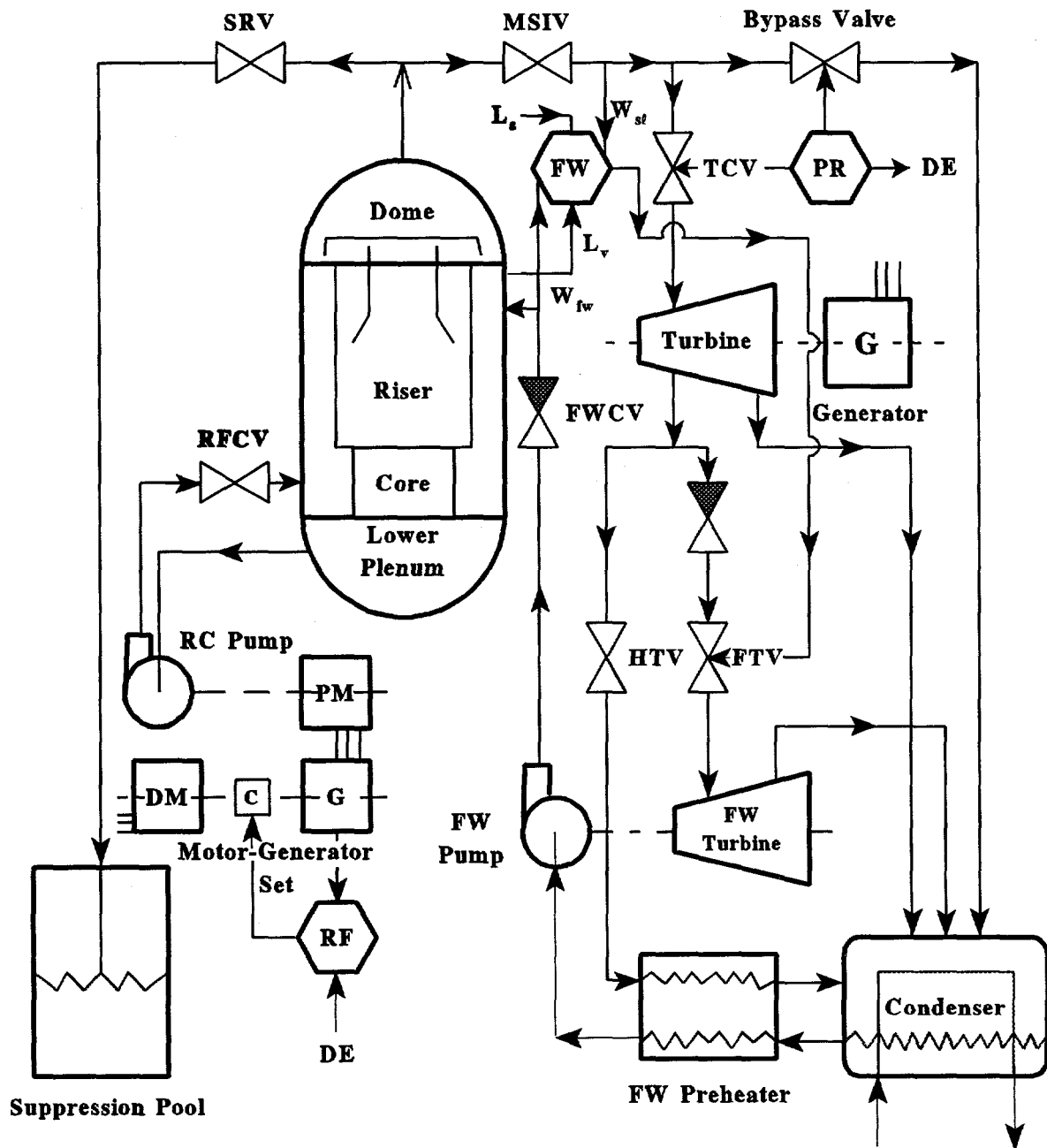
For the purpose of testing, the transient was run for 1200 seconds. The ATWS event was initiated from the full power condition at $t=2$ sec by a manual closure of all MSIVs in 4.5 seconds, which resulted in a sharp rise of the system pressure from 6.96 MPa to a peak of 8.93 MPa in 12 seconds as shown in Figure 7.8. The pressure oscillations are due to cycling of SRV. A break in the oscillation at around 200 sec is due to the change in the bank of SRV cycling (from 2nd bank to 1st bank). The sharp rise in pressure caused a core-wide collapse of voids, resulting in a power spike of 240% of rated power as shown in Figure 7.9. The reactor power was at decay-heat level after 1100 seconds. The effect of EPG level control is evident in the feedwater flow and water level response as shown in Figures 7.10 and 7.11, respectively. The flow coastdown to natural circulation due to the recirculation pump trip is clear from the response of core inlet mass flow rate in Figure 7.12 and from that of some individual coolant channels in Figure 7.13.

Figure 7.14 shows the transient response of the suppression pool temperature and Figure 7.15 shows that of the suppression level. We see that the pool temperature approached the Tech Spec limit of 79.45 °C (175 °F) at end of transient (1200 s), which is a few degree higher than that predicted by the old simplistic model (See Figure 7.16). The wetwell and drywell pressure responses are shown in Figures 7.17. The wetwell and drywell temperature responses are presented in Figures 7.18. The relative humidity in drywell is shown in Figure 7.19 and the wetwell relative humidity is presented in Figure 7.20. We see that the drywell relative humidity (initially at 40%) decreased slightly during the transient and recovered at the end, while the wetwell relative humidity stays within its physical limit (≤ 1).

7.5 Summary and Conclusions

The new containment model has been implemented in RAMONA-4B and successfully tested with an input deck based on the Browns Ferry 1 cross sections for a MSIV closure ATWS event. The following conclusions can be drawn from the this work:

1. The new detailed model predicts a higher suppression pool temperature than the old simple model, implying that the old simplistic model is non-conservative for prediction of the containment integrity.
2. The new containment model, although fairly detailed, consumes little CPU time relative to the total CPU time which is consumed mostly by the 3D kinetics calculation.



RF = Recirculation Flow Controller FW = Feedwater Flow Controller PR = Pressure Regulator

Figure 7.6 Schematic of the BWR4 System.

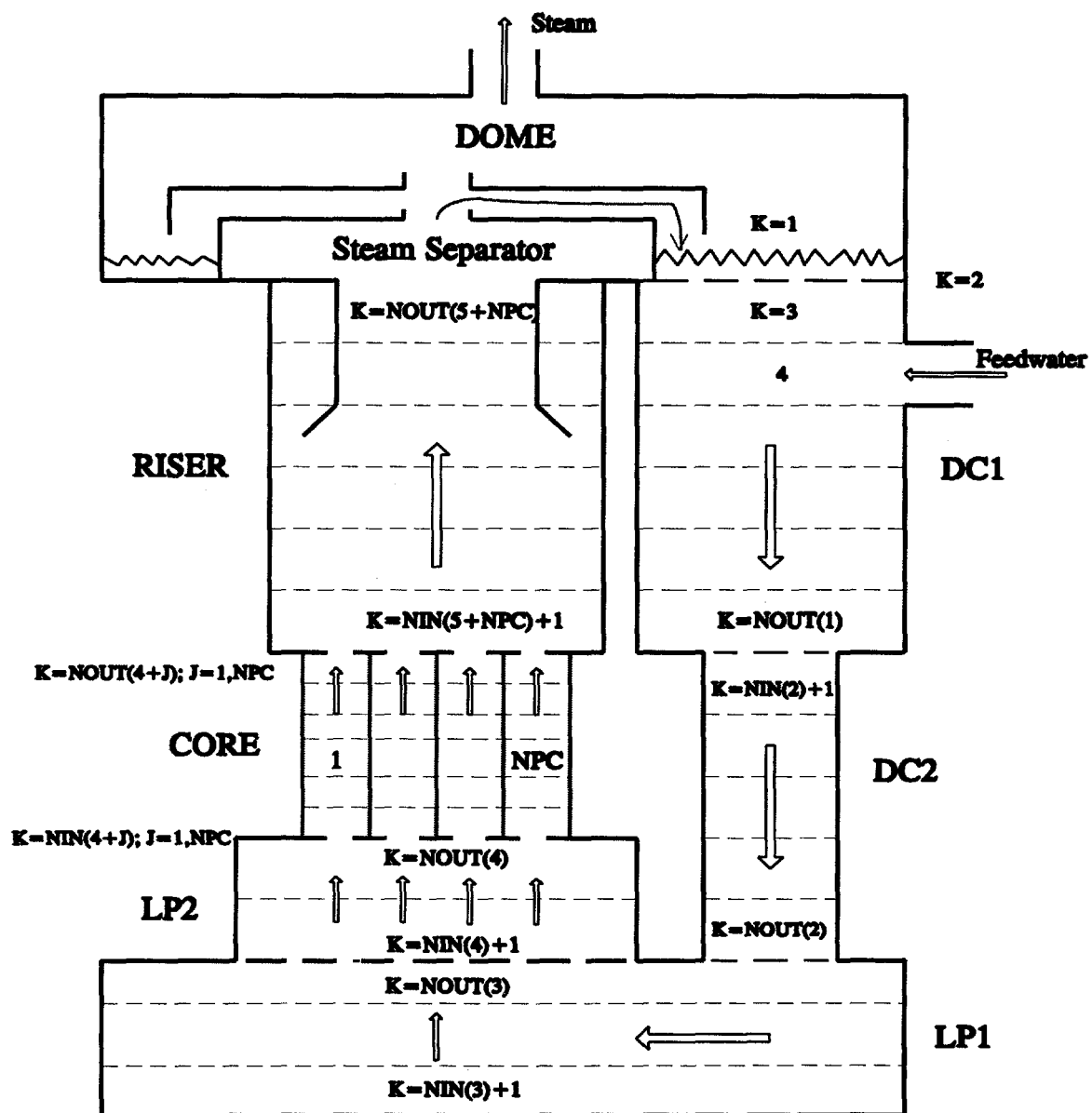


Figure 7.7 RAMONA-4B Nodalization for Reactor Pressure Vessel.

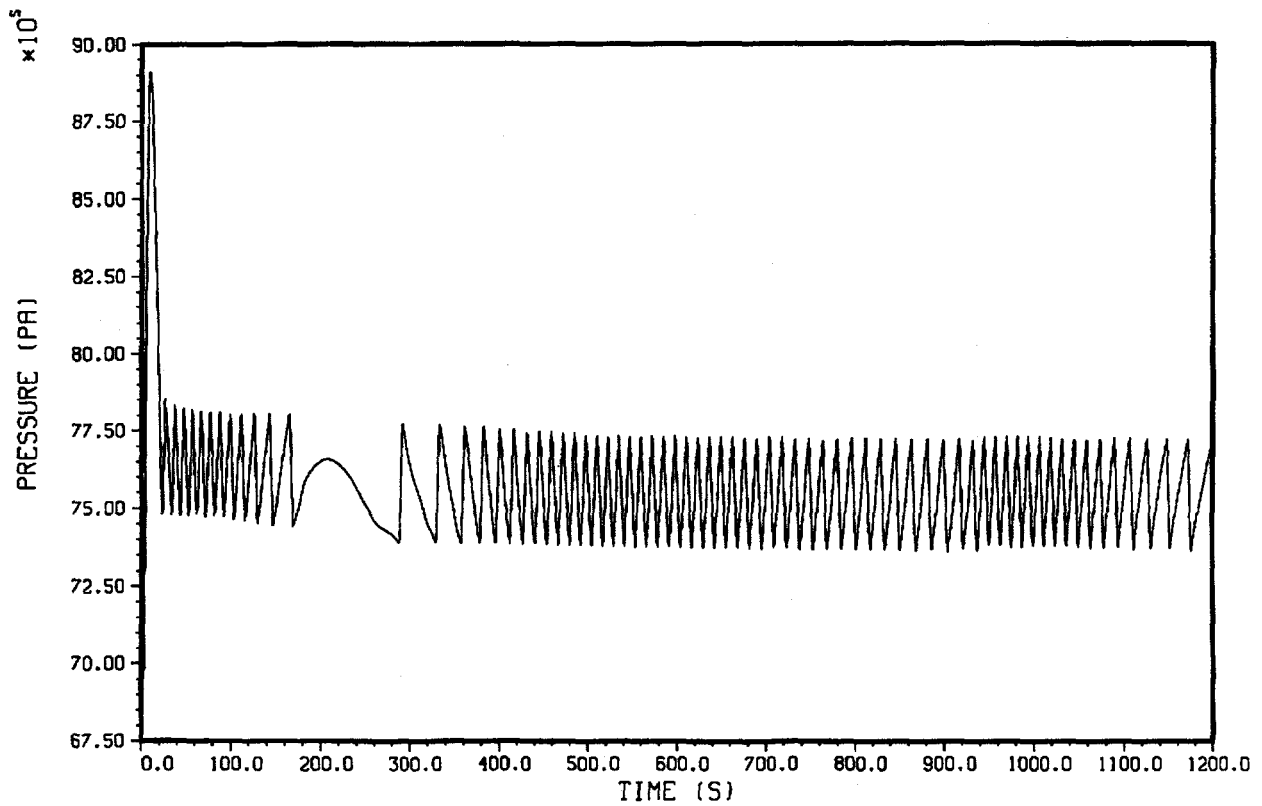


Figure 7.8 System Pressure Response During the MSIV Closure ATWS Event.

7. Modeling of BWR Containment

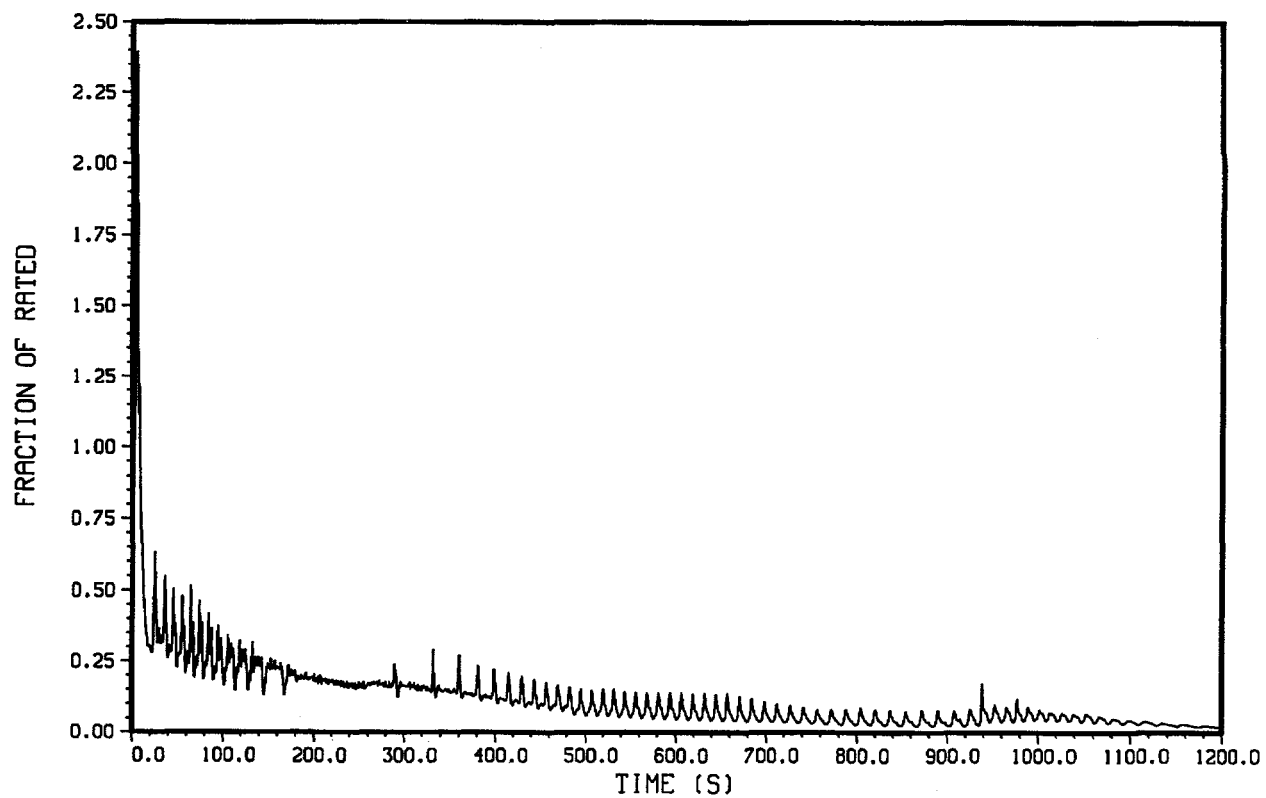


Figure 7.9 Reactor Power Response During the MSIV Closure ATWS Event.

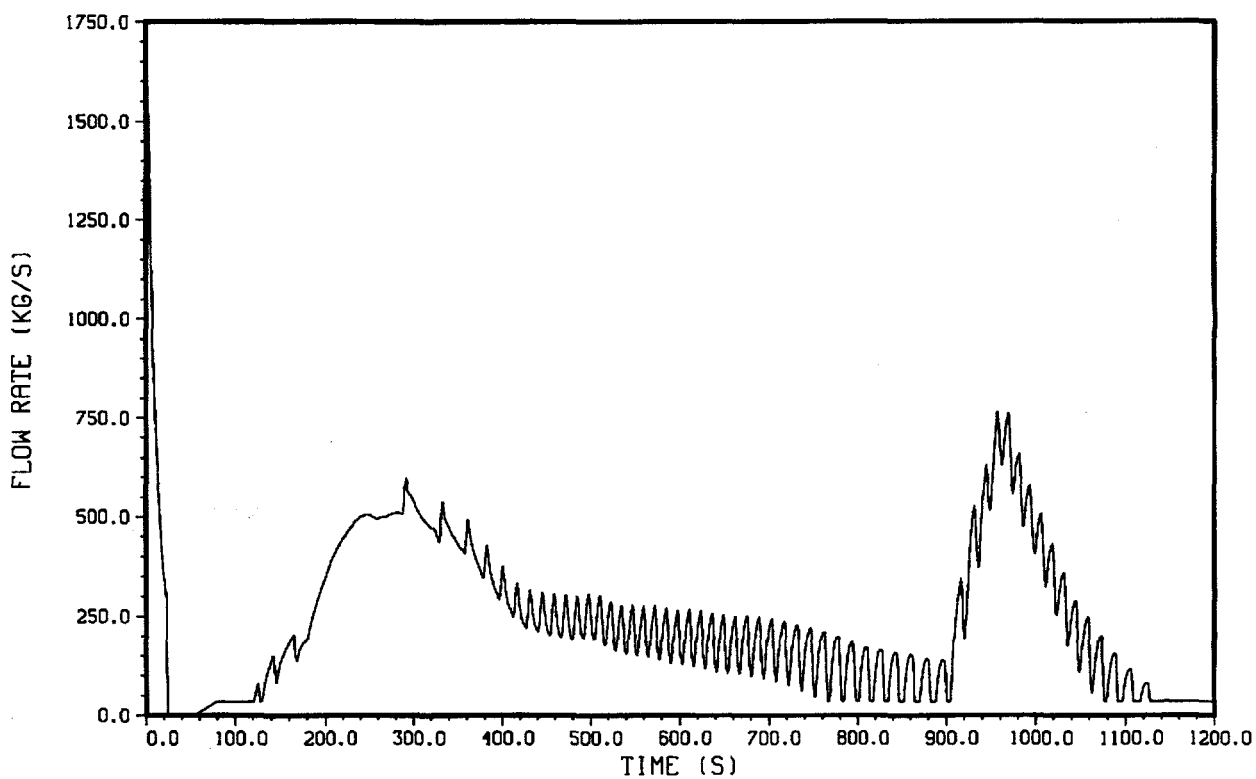


Figure 7.10 Feedwater Flow Response During the MSIV Closure ATWS Event.

7. Modeling of BWR Containment

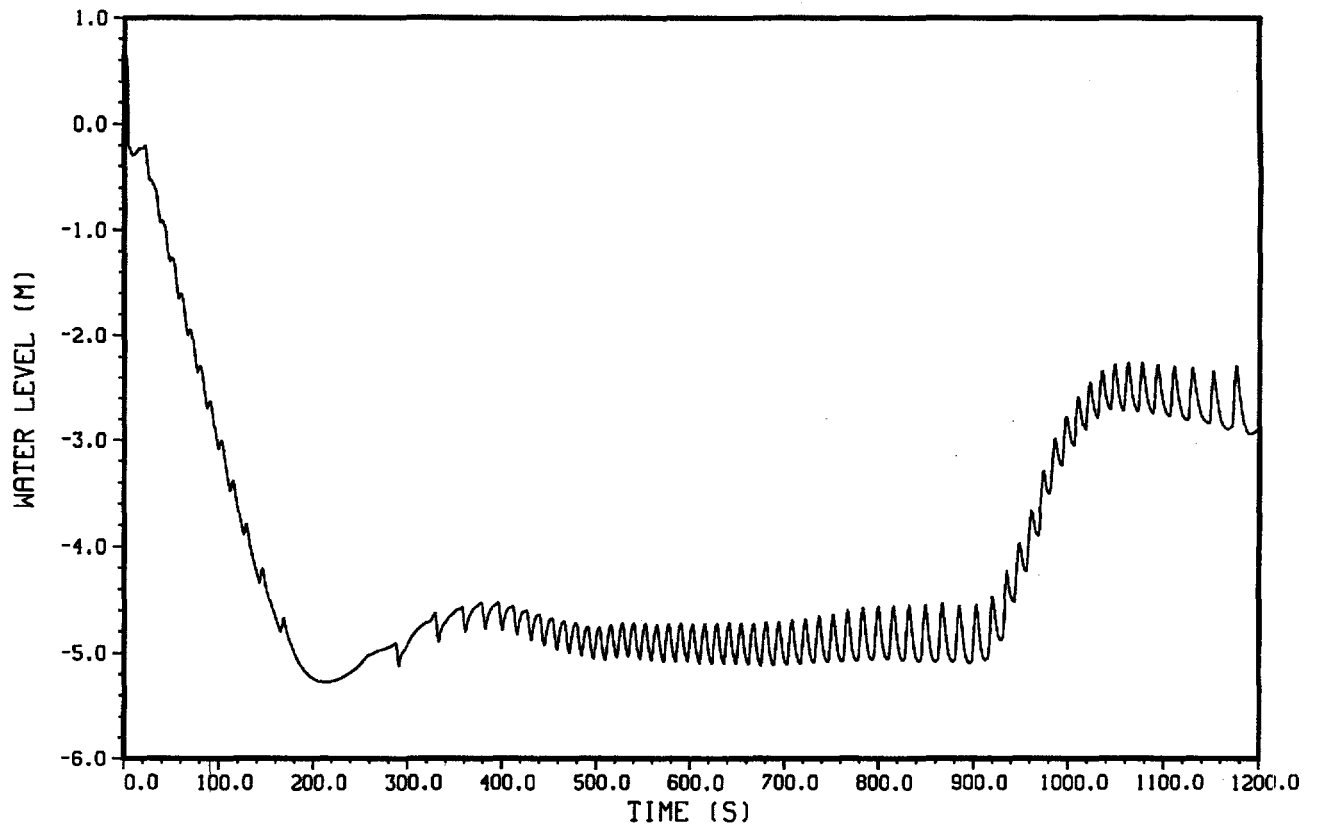


Figure 7.11 Downcomer Collapsed Liquid Level Response During the MSIV Closure ATWS Event.

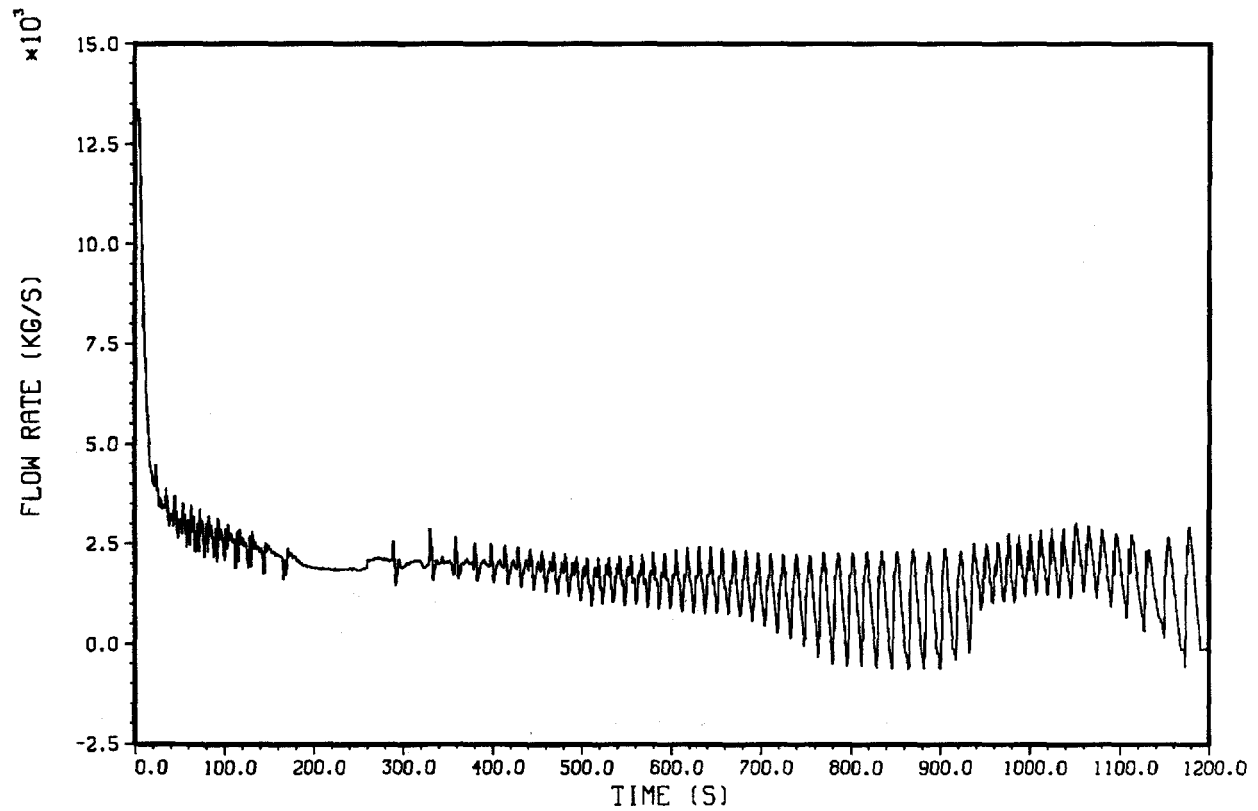


Figure 7.12 Total Core Inlet Flow Response During the MSIV Closure ATWS Event.

7. Modeling of BWR Containment

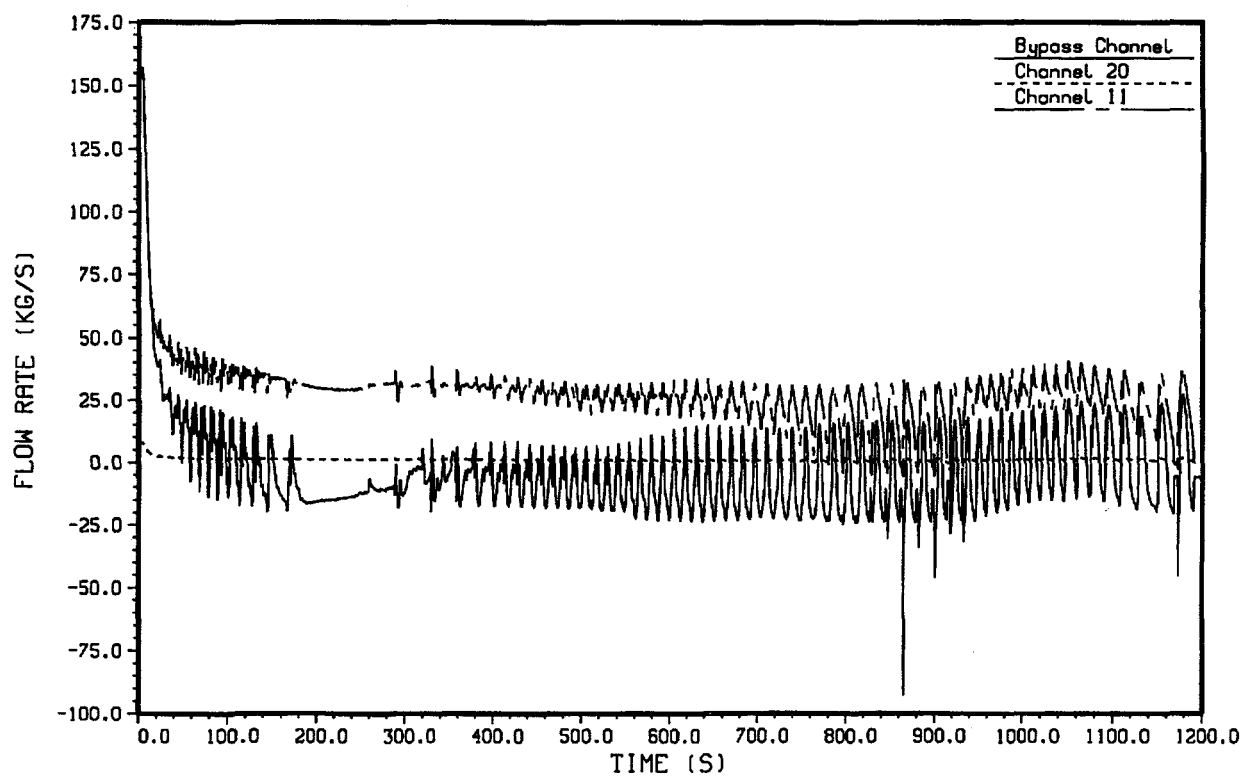


Figure 7.13 Core Inlet Flow Response of Individual Channels During the MSIV Closure ATWS Event.

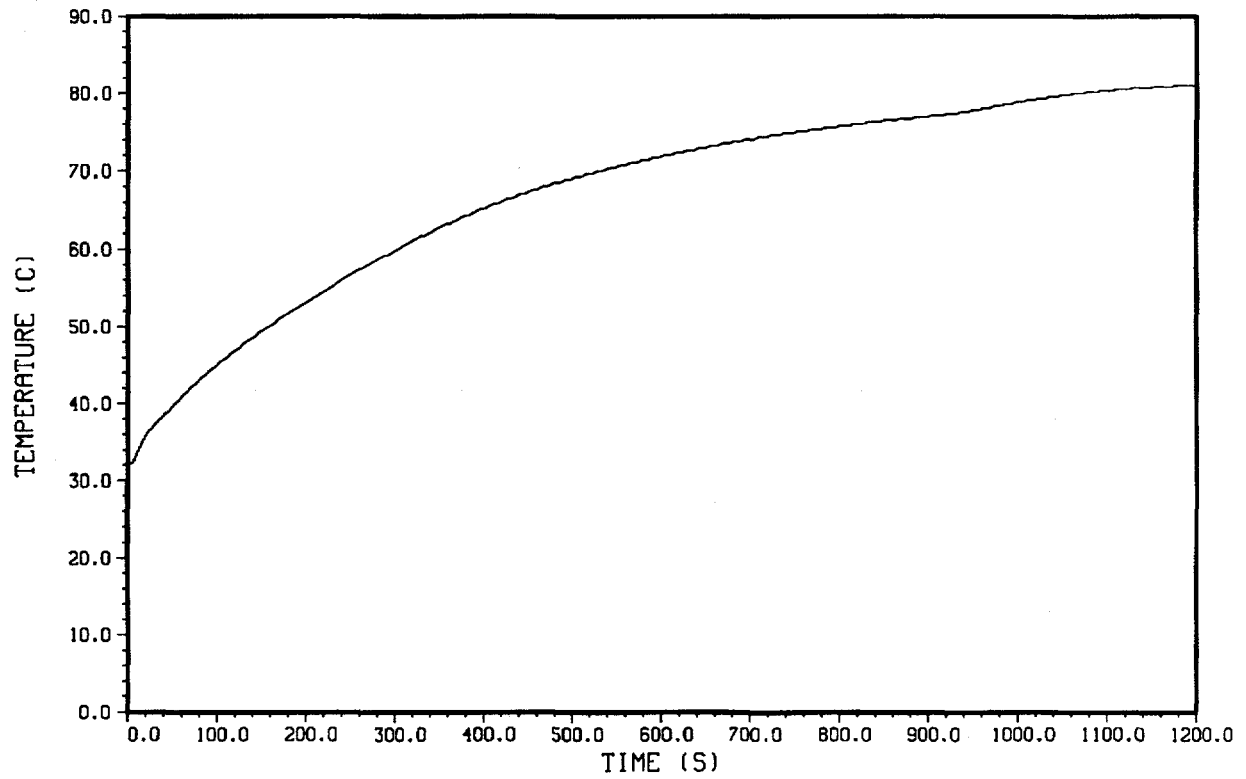


**Figure 7.14 Suppression Pool Temperature Response During the MSIV Closure ATWS Event
Predicted by the New Mechanistic Containment Model.**

7. Modeling of BWR Containment

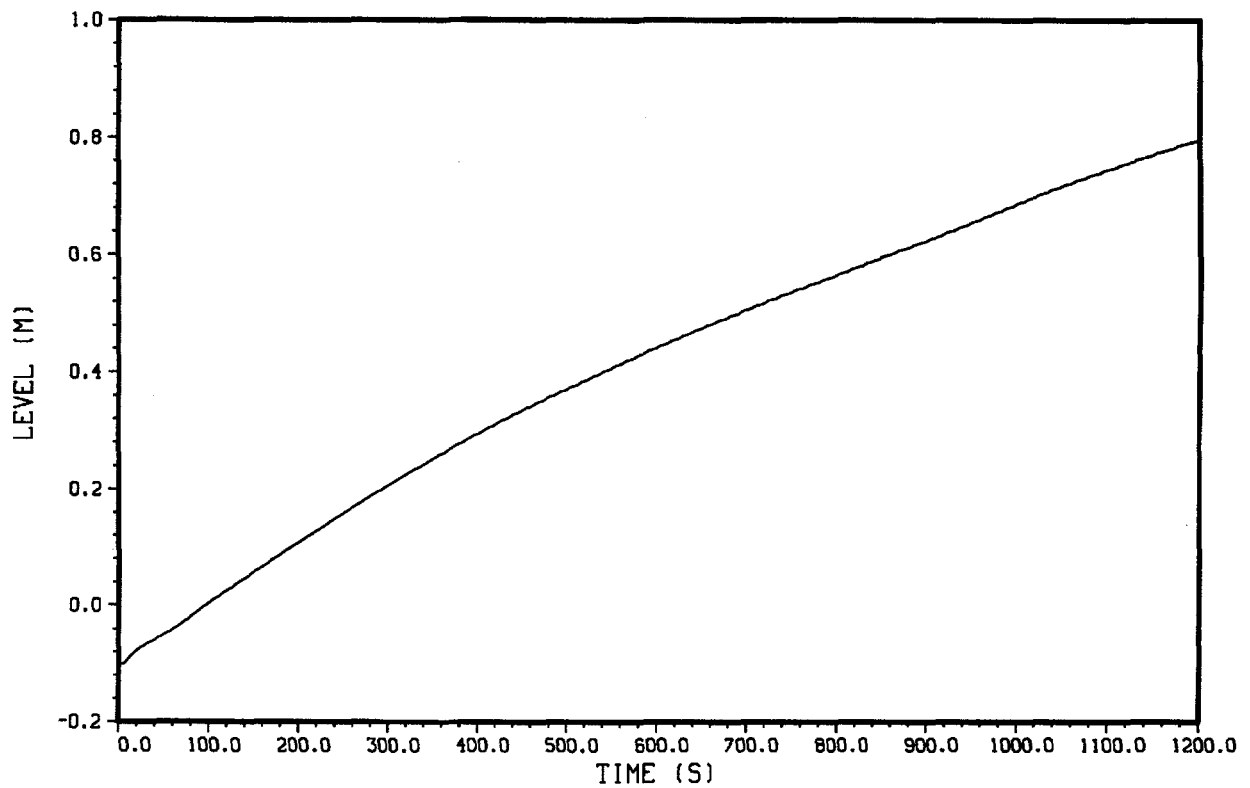


**Figure 7.15 Suppression Pool Level Response During the MSIV Closure ATWS Event
(Reference zero at midplane of the wetwell torus).**

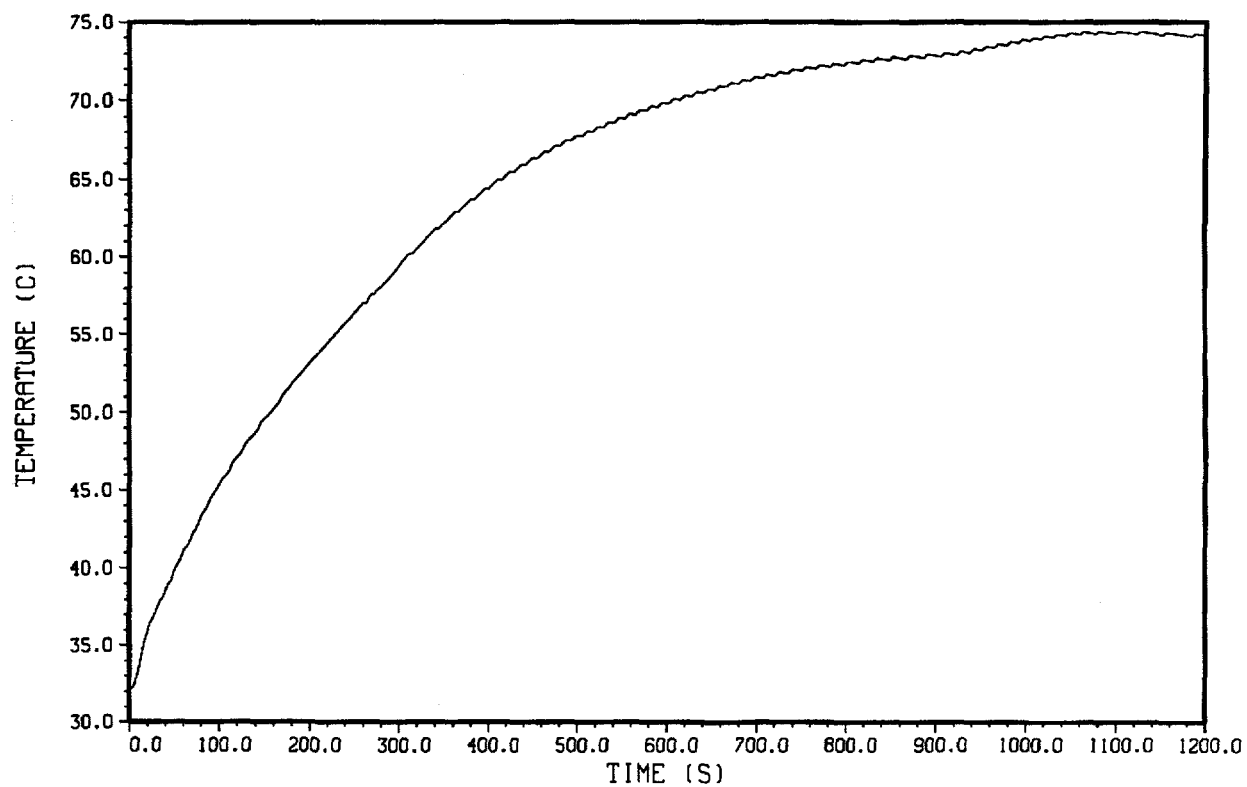


**Figure 7.16 Suppression Pool Temperature Response During the MSIV Closure ATWS Event
Predicted by the Simplistic Containment Model.**

7. Modeling of BWR Containment

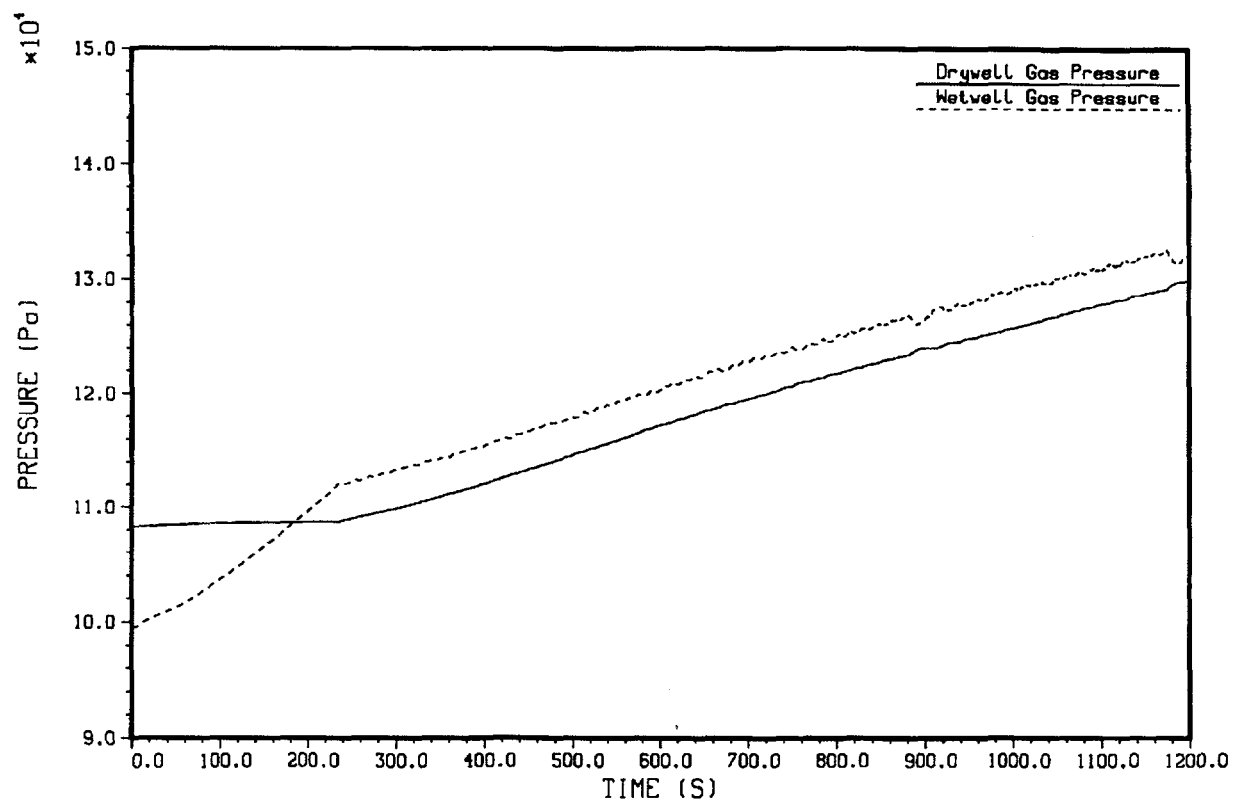


Figure 7.17 Wetwell and Drywell Pressure responses During the MSIV Closure ATWS Event.

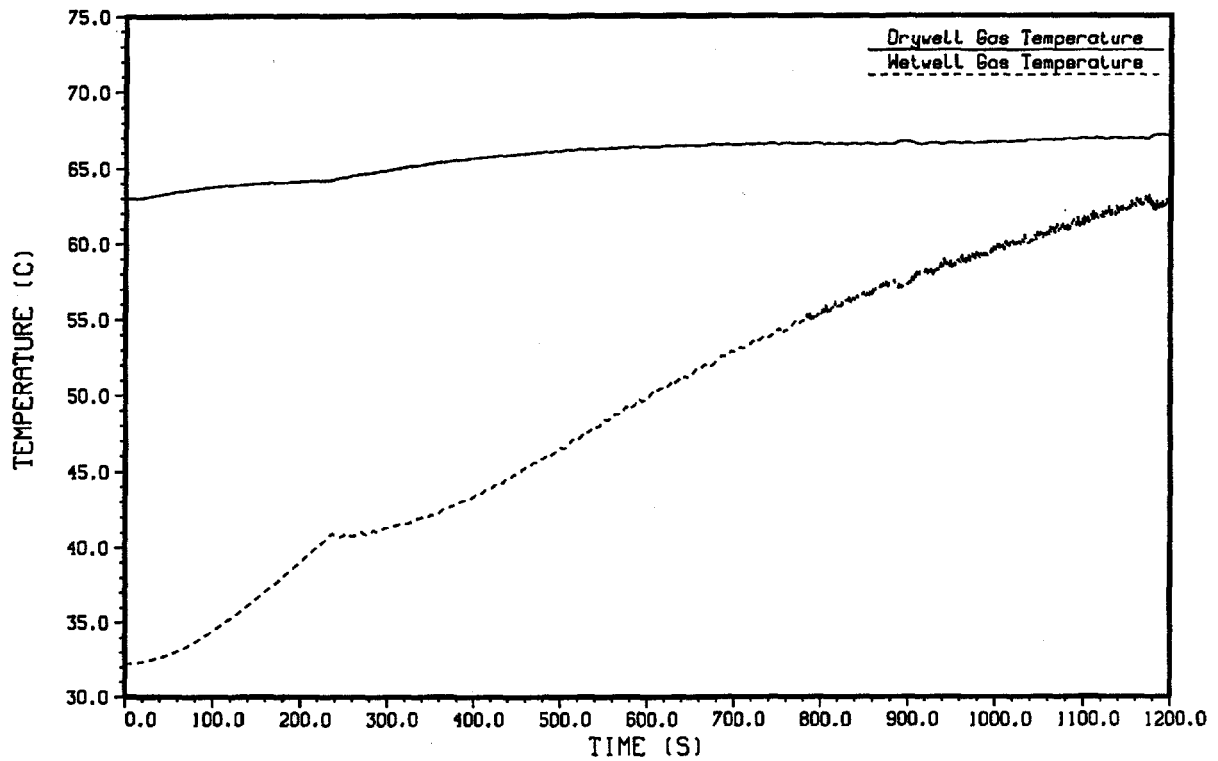


Figure 7.18 Wetwell and Drywell Gas Temperature Responses During the MSIV Closure ATWS Event.

7. Modeling of BWR Containment

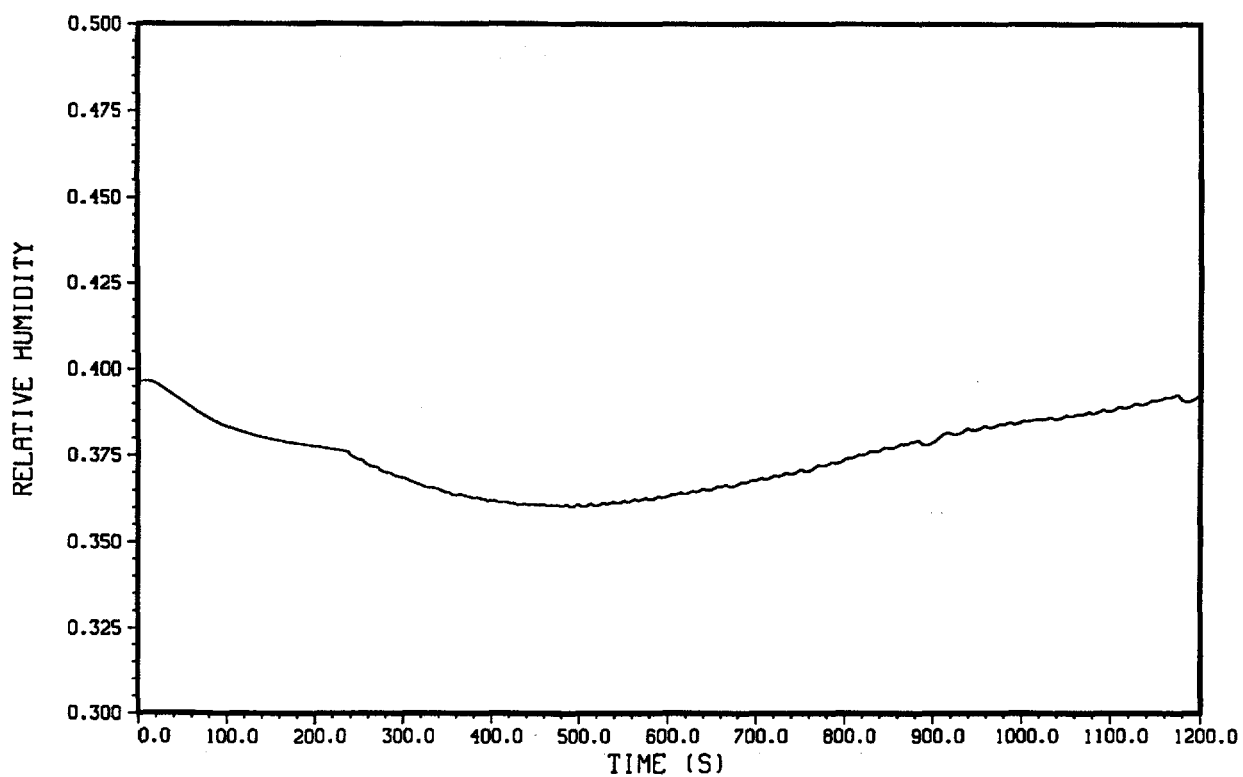


Figure 7.19 Drywell Relative Humidity Response During the MSIV Closure ATWS Event.

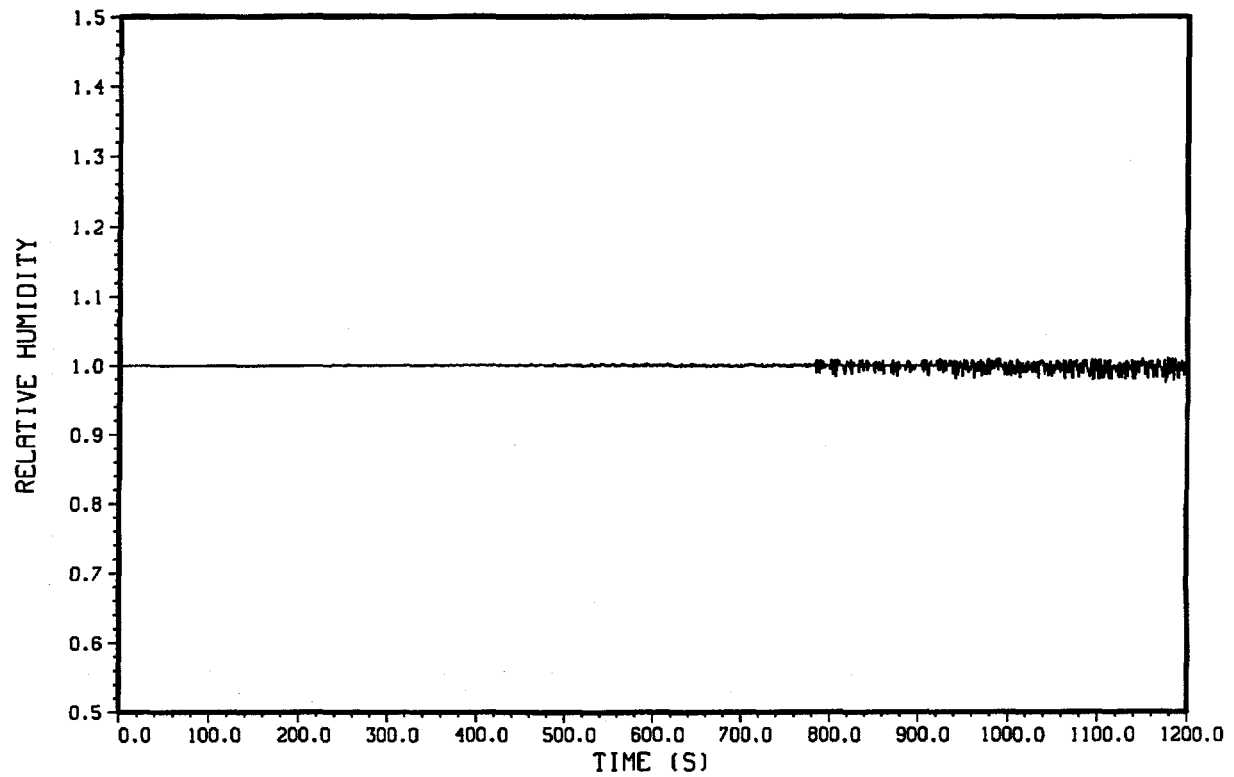


Figure 7.20 Wetwell Relative Humidity Response During the MSIV Closure ATWS Event.

8. MODELING OF SBWR SPECIFIC COMPONENTS

8.1 Isolation Condenser Model

The isolation condensers are designed to passively remove heat after the reactor is isolated and normal heat rejection options are not available. ICs are heat exchangers used to release heat to large compartmentalized tanks called the PCCS/IC pools. Steam enters the ICs directly from stub tubes connected to the steam dome. Condensation of steam from the reactor continue within the tubes while the temperature of pool water increases resulting in nucleate pool boiling on the tube outer surfaces. Continuous condensation of steam within the ICs help maintain a reduced pressure in the RPV.

ATWS events which result in reactor pressure rise or downcomer liquid level drop will automatically initiate the IC. Therefore, implementation of IC in RAMONA-4B was essential for enhancing the code capabilities for SBWR ATWS studies.

Implementation of Isolation Condenser (IC) model into RAMONA-4B was completed through the following steps:

- (1) Development of a model for the IC design in SBWR
- (2) Selection of governing equations for IC operation
- (3) Selection of constitutive relations for heat transfer calculations.
- (4) Stand alone code development
- (5) Interfacing the stand alone code to RAMONA-4B
- (6) Development of input, output, and restart capabilities

8.1.1 Model Development for Isolation Condenser

Model Assumptions

The Isolation Condenser model incorporated into RAMONA-4B is based on the design in GE SBWR [6]. A schematic representation of the reactor pressure vessel (RPV) coupled to an isolation condenser is presented in Figure 8.1. The IC has been modeled as a single control volume enclosing the condenser tubes connected to upper and lower plenum which are immersed in a pool of water. Steam entering the IC from the steam dome is condensed within the tubes, and the condensate liquid from the ICs is returned to the downcomer region of the RPV. The activation of IC operation is automatic. If the steam-dome pressure exceeds a design set point, or the MSIV is closed by a given fraction, then the IC condensate return valve is opened. Transient mass and energy balance equations are used to solve for the pressure and enthalpy within the IC, while the momentum change in the IC is assumed to be negligible. Steam inflow and condensate outflow from the IC are determined from quasi-steady-state momentum balances applied to the inlet steam line and the condensate return line. Heat removal from the system depends on the IC pool conditions and the heat transfer characteristics of the IC tubes in response to the variable thermal conditions inside the tubes. A schematic representation of the isolation condenser model is shown in Figure 8.2.

The assumptions for the geometric and thermodynamic modeling of the IC are as follows:

IC Modeling Assumptions:

1. The IC is modeled as a single control volume consisting of multiple parallel tubes in a pool of water.
2. Transient mass and energy balance are used to solve for pressure and enthalpy within the control volume, where momentum change is negligible.

3. The steam flow rate is obtained from the momentum balance applied to the steam supply line, with transient pressure boundary conditions from the IC and the RPV.
4. The condensate flow rate is a solution of the quasi-steady-state momentum balance applied to the condensate return line connected between the IC and the RPV.
5. Steam entering the IC is saturated at the inlet pressure.
6. Liquid leaving the IC is at a subcooled or saturated conditions depending on the heat removal capacity.
7. The initial level of liquid in the IC is a variable depending on the initial two phase mixture state.

1. The pool is at atmospheric pressure and the initial pool temperature is an input.
2. The pool energy balance is used to calculate the increase in pool temperature.



Heat-Transfer Assumptions:

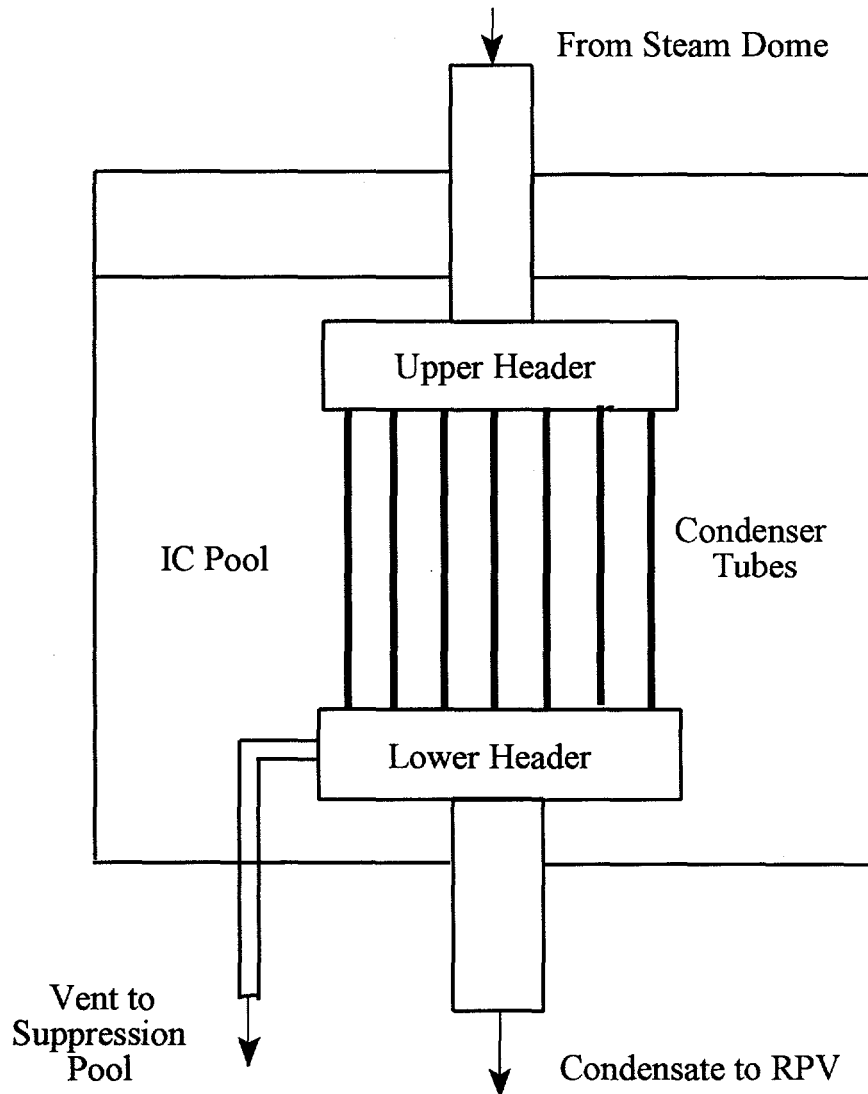


Figure 8.2 Schematic Diagram of Isolation Condenser Model in RAMONA-4B

Three important heat-transfer mechanisms are considered in this analysis, as illustrated in Figure 8.3:

1. Turbulent film condensation heat transfer inside the IC tubes occurs in parallel tubes, where condensed steam is assumed to be at a saturated liquid state in thermal equilibrium with the residual liquid,
2. Conduction heat transfer through the tube wall thickness, and
3. Natural convection and pool boiling heat transfer between the tube external surfaces and the pool liquid.

8. Modeling of SBWR Specific Components

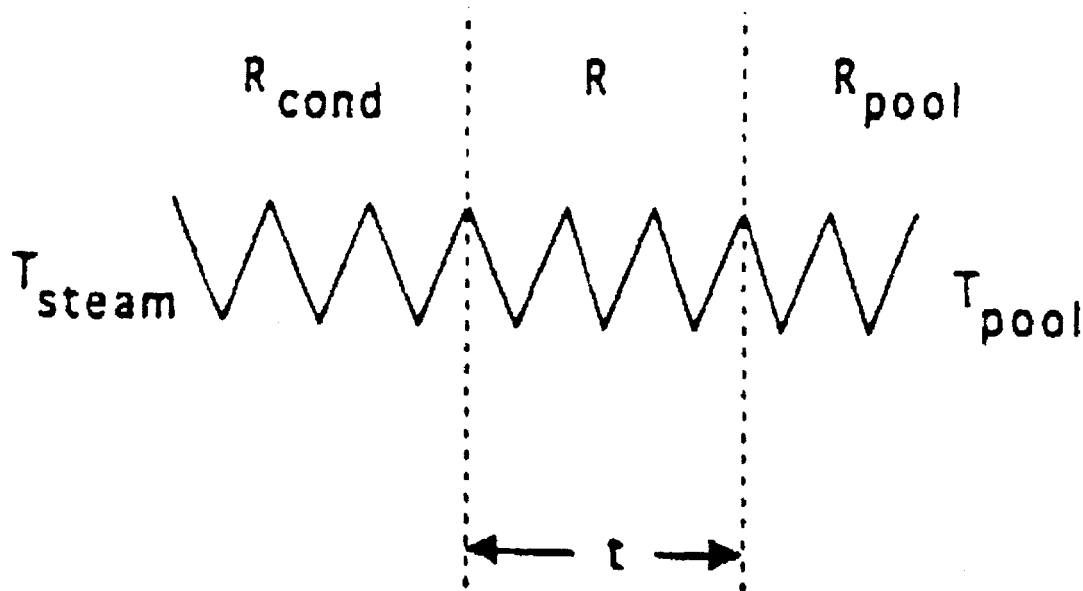


Figure 8.3 Resistance to Heat Transfer in the Isolation Condenser Tubes.

8.1.2 Model Equations

Transient mass and energy balance equations are solved for the isolation condenser, which has been modeled as a single control volume. The governing equations for transient solution of enthalpy and pressure are obtained from the mass and energy balance in combination with the equation of state as follows:

Mass Balance Equation:

The transient mass balance equation for the fixed control volume of the IC is:

$$\frac{d}{dt}(\rho V_c) = \sum W_i \quad (8.1.1)$$

where ρ , V_c and W_i represent the mixture density, volume occupied by the mixture, and inlet or exit mass flow rate of steam and liquid, respectively. Expressed in terms of the partial derivatives of the density, we get,

$$\frac{dh}{dt} = \frac{\sum W_i}{\rho_h V_c} - \frac{\rho_p}{\rho_h} \frac{dP}{dt}, \quad (8.1.2)$$

$$\text{where, } \rho_h = \frac{\partial \rho}{\partial h}, \quad \rho_p = \frac{\partial \rho}{\partial P}. \quad (8.1.3)$$

Energy Balance Equation:

Applying energy balance to a control volume enclosing the isolation condenser, we have

$$\frac{d}{dt}(\rho V_c h) = -Q_c + \sum W_i h_i + V_c \frac{dP}{dt} \quad (8.1.4)$$

Here, W_i and h_i are mass flow rate and enthalpy into the control volume, respectively, and Q_c is the net energy transfer to the pool. Expanding the left-hand side of the above equation and substituting equation 8.1.2, and then rearranging the terms [Wulff, 1984b] we have

$$\frac{dP}{dt} = \left(\frac{1}{V_c} \right) \left(\frac{1}{1 + \rho_m \frac{\rho_p}{\rho_h}} \right) \left[Q_c - \sum W_i h_i + \left(h + \frac{\rho_m}{\rho_h} \right) \sum W_i \right], \quad (8.1.5)$$

$$\text{where } \rho_m = \frac{1}{v_m} = \frac{1}{v_f + x v_{fg}}, \quad (8.1.6)$$

$$x = \frac{h_m - h_f}{h_g - h_f}. \quad (8.1.7)$$

Using known values of flow rates W_i , Eq. (8.1.5) is used to calculate dP/dt . Eq. (8.1.2) is then used to obtain dh/dt within the IC. Correspondingly pressure and the enthalpy are obtained after integration and static quality is determined from Eq. (8.1.7) and the state is defined.

Steam Inlet Flow:

The steam inlet flow rate is calculated from the quasi-steady-state momentum balance applied to the steam inlet line connecting the RPV to the IC.

$$\text{Steam Flow Rate: } \dot{W}_{\text{steam}} = A_s \rho_g(P) V_v \quad (8.1.8)$$

8. Modeling of SBWR Specific Components

$$\text{Steam Velocity ; } V_v = \sqrt{2 \frac{\Delta P_{RPV} - z_v \rho_v g}{\rho_v (1 + k_v)}} \quad (8.1.9)$$

$$\text{where, } \Delta P_{RPV} = P_{RPV} - P(t) . \quad (8.1.10)$$

Here P_{RPV} and $P(t)$ are the pressures in the vessel steam dome and the IC, respectively, while k_v is the loss coefficient for the steam line. Equation (4.3.7) is modified such that $w_{se} = w_{steamline} + w_{IC,steam}$ steam flow rate into the IC.

Condensate Return Flow:

The condensate return flow rate is obtained from the quasi-steady-state momentum balance applied to the condensate return line connected between the RPV and the IC.

$$\text{Liquid Flow Rate; } \dot{W}_{liq} = A_l \rho_l(h) V_l \quad (8.1.11)$$

$$\text{where, Liquid Velocity ; } V_l = \sqrt{2 \frac{\Delta P_{RET} + z_l \rho_l g}{\rho_l (1 + k_l)}} \quad (8.1.12)$$

Pressure within the IC, $P(t)$, and the condensate return boundary pressures are related by,

$$\Delta P_{RET} = P(t) - P_{RET} , \quad (8.1.13)$$

where, P_{RET} represents vessel pressures at the locations of condensate return line connections of the IC. No vapor or gas entrainment in the liquid return line has been considered in this flow equation. The flow loss coefficient due to bends and turns in the flow path has been lumped together through k_l . The effect of variation of the loss factor k_l in liquid momentum balance equation has been demonstrated in this study. Equation (4.3.7) is modified for condensate return from the IC, $w_{fw} = w_{feedwater} + w_{IC,liq}$.

8.1.3 Boundary Conditions and Constitutive Relations

8.1.3.1 Boundary Conditions:

1. The steam-dome pressure, P_{RPV} , and condensate return pressure P_{RET} , determine the flow boundary conditions for the steam inlet and condensate exit from the IC. For a stand-alone calculation of the IC, these pressures are specified as constant input. However, in case of the IC coupled to RAMONA-4B, these are time-varying pressures from the reactor-vessel interface to the isolation condenser.

2. The pool temperature is an input imposed as a boundary condition to the heat-transfer calculation. The default value is $T_{pool} = 105^\circ\text{C}$ at time, $t = 0$.

8.1.3.2 Constitutive Relations:

The different models used for the heat-transfer coefficient and the corresponding resistances are as follows:

1. The turbulent film condensation inside the tubes is given by the heat-transfer correlation [Holman, 1990]:

$$h_{cond} = 0.943 \left[\frac{\rho_l(\rho_l - \rho_v) g h_{fg} K_f^3}{L \mu_f (T_g - T_w)} \right]^{1/4} \quad (8.1.14)$$

$$R_{cond} = \frac{1}{h_{cond} A_{cond}}, \quad (8.1.15)$$

where R_{cond} represents the resistance due to condensation.

2. Natural convection [Holman, 1990] from heated tubes to the pool-side liquid is obtained from the following correlation:

$$h_{pool} = N_f \left(\frac{K_f}{D} \right), \quad (8.1.16)$$

where, the Nusselt number is obtained from

$$N_f = C (Gr_f Pr_f)^m, \quad (8.1.17)$$

where $C = 0.01$ and $m = 0.33$ are used. This correlation assumes the boundary layer thickness on the tube outer surface is much smaller than the tube diameter.

The pool-side heat-transfer coefficient, Grashoff number Gr_f and Prandlt number Pr_f are given by

$$Gr_f = \frac{g\beta(T_w - T_{pool})L^3}{\nu^2}, \quad (8.1.18)$$

8. Modeling of SBWR Specific Components

$$\beta = \frac{1}{V} \left(\frac{\partial V}{\partial T} \right)_p = \frac{\rho_{pool} - \rho_f}{\rho_f (T_f - T_{pool})}, \quad (8.1.19)$$

$$Pr_f = \frac{C_p \mu}{\rho_f K_f}. \quad (8.1.20)$$

- 3a. **Nucleate Pool Boiling:** Two correlations for nucleate pool boiling are available. (a) If CORRL = 1.0, then the Roshenow correlation [Holman, 1990] for pool boiling is used,

$$\frac{C_l \Delta T_x}{h_{fg} P_{rl}^s} = C_{sf} \left[\frac{q/A}{\mu_l h_{fg}} \left(\frac{g_c \sigma}{g (\rho_l - \rho_v)} \right)^{1/27} \right]^{0.33}, \quad (8.1.21)$$

An alternative correlation for pool boiling, for CORRL = 2., is the (b) Stephan and Abdelsalam [Carey, 1992] correlation for water applicable in the range $6 < \Delta T_{sat} < 22^\circ\text{C}$.

$$q = \{ C_l [T_w - T_{sat}(P)] \}^{1/0.327} \quad (8.1.22)$$

$$h_{pool} = \frac{q}{A \Delta T_x}, \quad (8.1.23)$$

$$R_{pool} = \frac{1}{h_{pool} A_{pool}}. \quad (8.1.24)$$

Equation (8.1.21) is an implicit relation for the pool heat-transfer coefficient. Heat flux from the previous iteration is used to estimate the heat-transfer coefficient at the current step.

- 3b. **Transition Boiling:** In order to account for the possible increase in heat flux exceeding CHF, a transition boiling curve has been implemented. A linear relation between the CHF heat flux, and the minimum film boiling heat flux (Bui, 1985a) has been assumed on a log-log scale, for a temperature range of $30 < \Delta T_{sat} < 70^\circ\text{C}$. Accordingly, the correlation used in RAMONA-4B is

$$q'' = 1.64 \times 10^{11} \Delta T^{-3.7} \quad (8.1.25)$$

- 3c. **Stable Film Boiling:** Stable film boiling heat-transfer correlation is well established. For the range of wall superheat temperature between the minimum heat flux (at approximately 70°C), up to the stable film boiling (approximately 150°C, at atmospheric condition), is the region of transition film boiling. The same correlation as for transition boiling is used for this region. The stable-film-boiling correlation derived by (Bui 1985b) for vertical surfaces, for the range of $150 < \Delta T_{\text{sat}} < 200^\circ\text{C}$, is given as

$$h = 832.0 \Delta T^{-0.25} \quad (8.1.26)$$

4. **Conduction** heat transfer through the tubes is calculated for multiple parallel tubes placed between the upper and lower plenum of the IC. The total surface area and the tube volume have been preserved in the calculations, such that the resistance to heat transfer is calculated as

$$R_{\text{tube}} = \frac{\ln(r_o/r_i)}{2\pi k_{\text{tube}} L_{\text{tube}} n_{\text{tubes}}} \quad (8.1.27)$$

5. The **fouling factor** on the outside surface of the tubes has been accounted for by an input constant **RFOUL**. This value is additive to the conduction resistance of the tube material.

Using the models, an equivalent heat-transfer correlation is obtained

$$Q_{\text{out}} = \frac{T_{\text{steam}} - T_{\text{pool}}}{\sum R} \quad (8.1.28)$$

Here Q_{out} represents total rate of heat transfer from steam to the pool. The energy balance for the pool liquid is used to obtain the pool temperature.

8.1.3.3 Wall-Temperature Iteration

Since the heat transfer to the pool fluid is to be calculated using the outer and inner wall temperatures of the IC tubes, an iteration procedure is required for the wall-temperature calculation from the implicit equations. From Figure 8.3, we have

$$\frac{T_s - T_i}{R_{\text{cond}}} = \frac{T_i - T_o}{R_{\text{tube}}} = \frac{T_o - T_{\text{pool}}}{R_{\text{pool}}}$$

or

8. Modeling of SBWR Specific Components

$$\frac{T_s - T_i}{R(T_i)} = \frac{T_i - T_o}{R_{tube}} = \frac{T_o - T_{pool}}{R(T_o)} \quad (8.1.29)$$

These equations have been solved simultaneously for the inner-wall temperature T_i and outer-wall temperature T_o . The convergence criterion is set for 1% of the absolute temperatures. The temperatures are upgraded according to

$$T_{new} = (1 - \alpha) T_{new} + \alpha T_{old} \quad (8.1.30)$$

where α is the relaxation parameter set by the rate of convergence.

8.1.4 Spatial Distribution of Heat-Transfer Coefficient in the Pool Side

The radial temperature distribution in the liquid outside the tubes has not been accounted for in the above model. The interior tubes of the IC are in the shadow region of the external tubes. The fluid in the interior of the tube bundle does not interact with the pool fluid as well as the external tubes. This causes a region of entrapped liquid. Therefore, a local temperature rise associated with excessive vapor concentration may result. A change of the rate of heat transfer is therefore anticipated in this region. Nonuniform heat transfer from the tubes to the pool can therefore become important.

In order to account for the effect of a local temperature rise in the pool-side heat transfer, in RAMONA-4B, a nonuniform heat-transfer coefficient can be modeled through the input. As shown in Figure 8.4, the total number of tubes can be divided into several groups of equal values of heat-transfer coefficient. Corresponding to each group of tubes a modifying factor for the heat-transfer coefficient can be input.

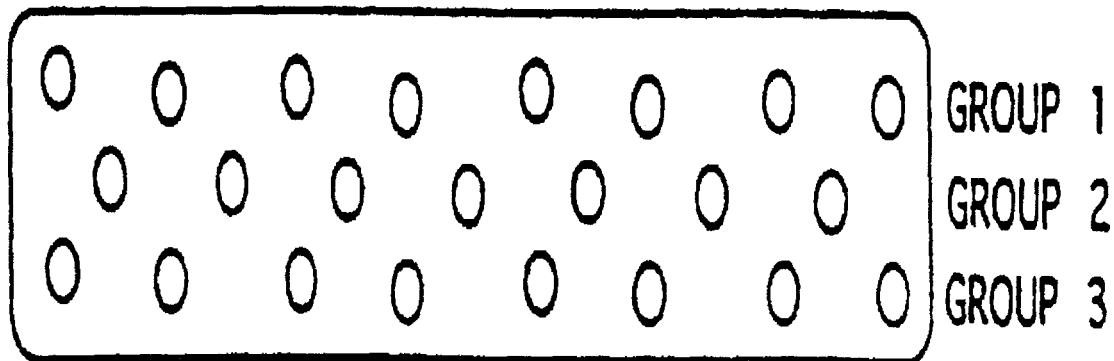


Figure 8.4 IC Tube Distribution for Effect of Shadowing on Heat Transfer

The following is the procedure for correction:

Number of groups = **ng**
 Number of tubes in each group = **nt(1), nt(2), nt(3), ..., nt(ng)**
 Surface area in each group = **AR(I)** given by

$$AR(I) = \pi d_{tube} c_{tube}, \quad (8.1.31)$$

where

d_{tube} = the tube outer diameter, and
 c_{tube} = length of condensing tube (above liquid level).

Heat-transfer coefficient in each group = **h_R(I)** given by

$$h_R(I) = hfac(I) + h_{pool}, \quad (8.1.32)$$

Resistance to heat transfer in each group = **R_{pool}(I)** given by

$$R_{pool} = \frac{1}{h_R(I) \cdot AR(I)}, \quad (8.1.33)$$

where **hfac(I)** is the input profile of heat-transfer coefficient modifier and **h_{pool}** refers to the pool heat-transfer coefficient.

8.1.5 Effect of Water Level on Condensate Return

Condensate from the IC is returned to the RPV at a location adjacent to the feed-water entrance. This return flow rate is, therefore, dependent on the liquid level inside the downcomer. In RAMONA-4B, the component defined as Downcomer 1 is present in this region, and the pressure to the IC condensate return line is modified by the height of the liquid level above the point of liquid return as shown in Figure 8.5.

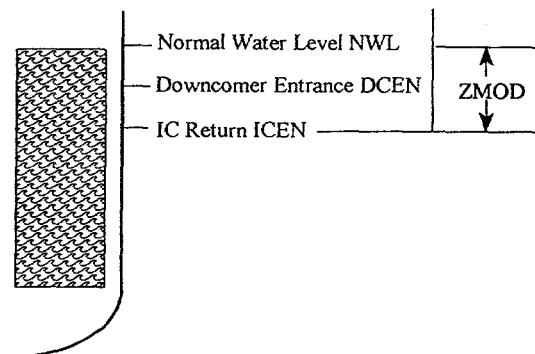


Figure 8.5 Effect of Liquid level in the Downcomer on IC Performance

8. Modeling of SBWR Specific Components

ICEN (Input) = Location of IC return port on the RPV.
 DCEN (Input) = Location of the Downcomer 1 entrance
 CWFIC = Water level relative to DC 1 entrance.

Define $DCIC = DCEN - ICEN$.

If $CWFIC > 0$, then $ZMOD = DCIC + CWFIC$.

If $CWFIC < 0$, then $ZMOD = DCIC - |CWFIC|$.

$z_1 = z_i + ZMOD$ applied to Equation (8.1.12).

8.1.6 Valve Model in the Isolation Condenser

The valve model implemented in RAMONA-4B for condensate flow from the IC is related to the design of the SBWR. IC activation signal and options are as follows:

Option **LIC = 1** ; IC Activation based on System Pressure $> P_{ic}$ (Input),
LIC = 2 ; IC Activation based on MSIV $> 10\%$ closure.

The flow rate through the IC valve is calculated as follows:

$$WLIQ_{modified} = WLIQ_{original} \cdot AR \cdot f, \quad (8.134)$$

where the valve control timing diagram is illustrated in Figure 8.6,

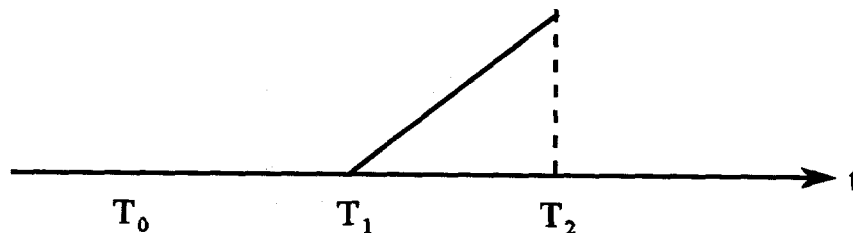


Figure 8.6 Valve Control Timing for IC System

T_0 = IC activation signal start time

$T_1 = T_0 + \delta T_d$; Beginning Time for IC Valve Opening

$T_2 = T_1 + \delta T_v$; Time for Fully Open Condensate Return Valve

$t < T_1$ --- $f = 0.0$,

$T_1 < t < T_2$ --- $f = (t - T_1) / \delta T_v$,

$$T_1 < t \text{ --- } f = 1.0,$$

and the area ratio AR is defined as $AR = A_v/A_p$ with A_v = valve area and A_p = return pipe area.

8.2 Standby Liquid Control System Model

The Standby Liquid Control System (SLCS) is a safety-related system designed for manual or automatic initiation of boron injection into the RPV. This system comprises the following:

- (1) High-pressure accumulator containing nitrogen cover gas.
- (2) Injection line with control valves.
- (3) Control System coupled to the reactor operating set points.
- (4) Cover-gas pressure-regulation system.

8.2.1 Model Development for SLCS

In the RAMONA-4B code, the SLCS system has been modeled based on the geometric and operating specifications given in the SSAR of Simplified Boiling Water Reactor (1992). The control logic for activation of the system has also been implemented within the guidelines. The SLCS system of SBWR is the source of a high concentration (12.5%) of sodium pentaborate solution available for hot or cold shutdown of the reactor in case of failure of the normal scram system.

A schematic diagram of the processes in the SLCS system is presented in Figure 8.7. The SLSC accumulator tank has been modeled as a single control volume with a pipe line for liquid draining. High-pressure injection of boron solution into the bypass area of the core has been modeled and documented in section 4.11 of this document. The assumptions for modeling the SLCS system are as follows:

SLCS Modeling Assumptions:

1. The accumulator tank of the SLCS is modeled as a single control volume.
2. The cover gas expands polytropically during draining.
3. The liquid is incompressible and the flow within the pipe is fully developed.
4. The fluid properties are averaged within each phase across the interface.
5. There is no refilling during the period of draining.
6. The system is at a thermal equilibrium.
7. The control logic is fixed during a transient.

8.2.2 Governing Equations

The accumulator tank is connected to the RPV through valves and control logic based on system-operation set points. Actuation of the SLCS system is therefore integrated to the RPV operating conditions, and the pressure at the liquid return port is used as the boundary condition to the transient solution of flow.

Based upon the assumptions stated in section 8.2.1, and the schematic diagram of Figure 8.8, momentum balance between tank and RPV return port is given by (Fox, 1985):

8. Modeling of SBWR Specific Components

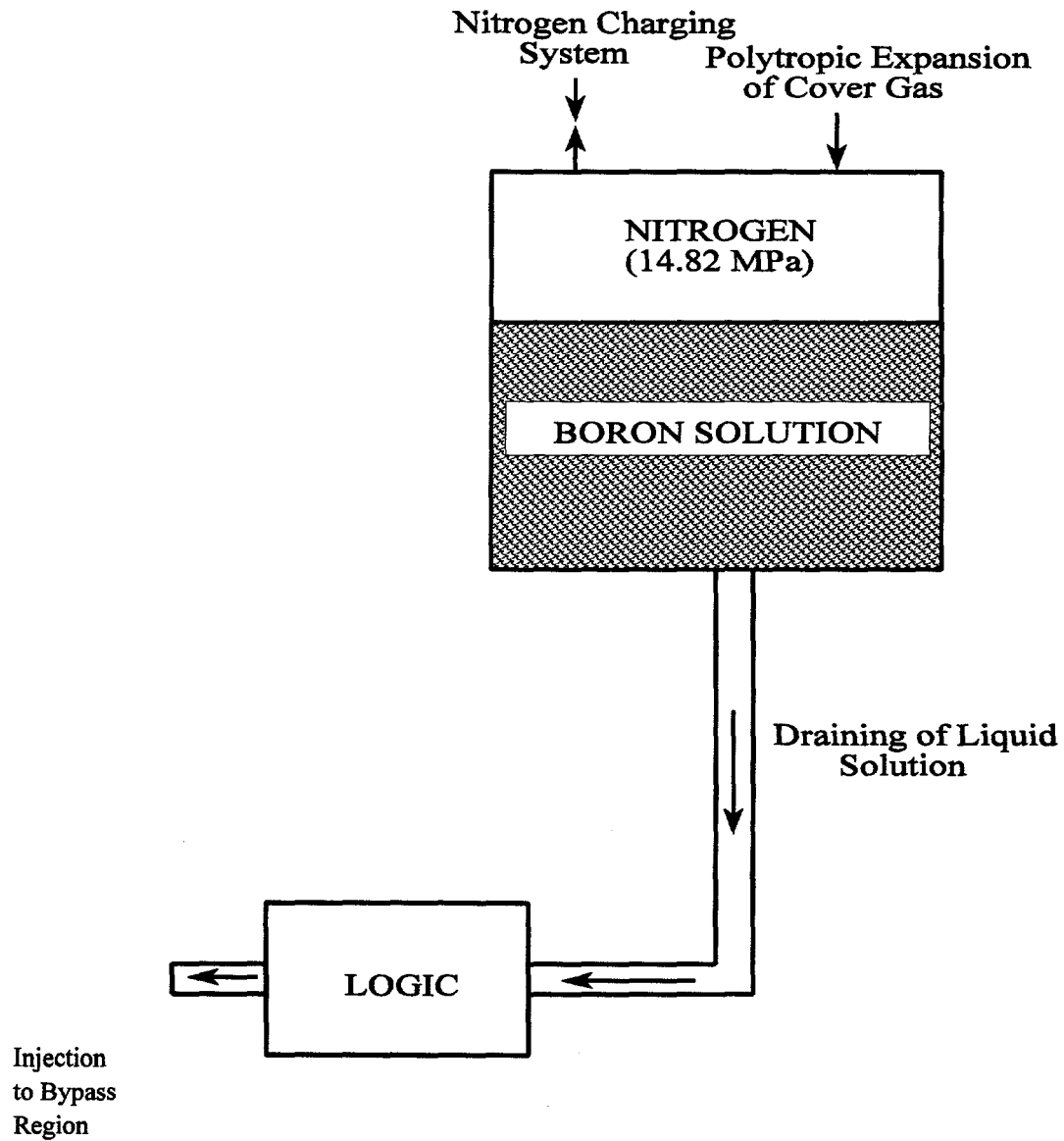


Figure 8.7 Schematic of Standby Liquid Control System (SLCS).

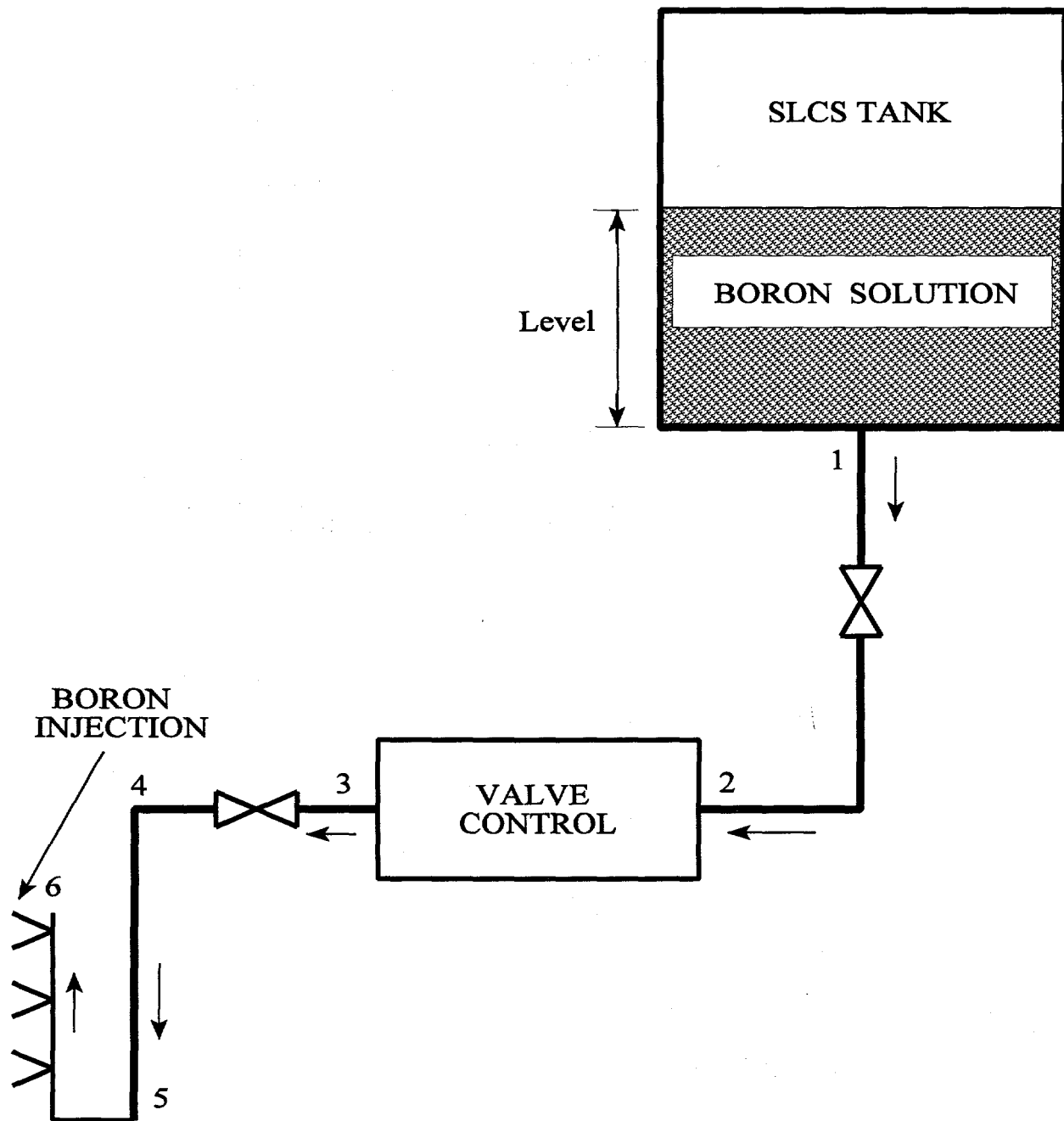


Figure 8.8 Schematic of SLCS Model Used in RAMONA-4B.

8. Modeling of SBWR Specific Components

$$\frac{dV_L}{dt} = \frac{1}{L'} \left(\frac{(P_2 - P_3)}{\rho_L} + gL' - \frac{\tau_w P_w L'}{\rho_L A} \right) \quad (8.2.1)$$

assuming wall shear stress to be

$$\tau_w = 0.5 C_f \rho_L V_L^2 \quad (8.2.2)$$

and

$$(P_2 - P_1) = \rho_L [gL_1 - V_L (1 + k_c)], \quad (8.2.3)$$

one gets

$$\frac{L}{t} = \frac{1}{L'} \left[\frac{P_1 - P_3}{\rho_L} + g(L_1 + L_2) - 0.5 \left\{ \frac{C_f P_w L'}{A} V_L^2 + (1 + k_c) V_L^2 \right\} \right] \quad (8.2.4)$$

where the following nomenclature apply,

P_1	=	Cover-gas pressure
P_3	=	RPV return port pressure
ρ_L	=	Solution density
L'	=	Total pipe length
L_1	=	Liquid level in tank
L_2	=	Height of return pipe
A	=	Return-pipe area
V_L	=	Velocity of Liquid at RPV inlet
P_w	=	Perimeter of the pipe wall
k_c	=	Line-loss coefficient
C_f	=	Wall-friction coefficient

8.2.3 Constitutive Relations

8.2.3.1 Wall-Friction Coefficient

Coefficient of friction C_f in Equation 4.6.35 is obtained as follows,

For **laminar** flow:

$$C_f = \frac{16}{Re} \quad ; \quad Re < 2500 . \quad (8.2.5)$$

For **turbulent** flow:

$$C_f = \frac{0.079}{Re^{.25}} \quad ; \quad Re > 2500 , \quad (8.2.6)$$

where Reynolds number is defined as

$$Re = \frac{\rho_L D V_L}{\mu_L} . \quad (8.2.7)$$

8.2.3.2 Polytropic Expansion of Cover Gas

Nitrogen cover gas is maintained at a high pressure P_1 , which is a user-specified quantity. Liquid draining from the control volume is accompanied by an expansion of the cover gas during the period of the transient. Polytropic expansion of this cover gas is as follows:

$$\frac{dP_1}{dt} = \frac{-n P_1^{\frac{n+1}{n}}}{C_1^{\frac{1}{n}}} V_L A , \quad (8.2.8)$$

where

- n = Coefficient of expansion
 C_1 = Constant for a polytropic process,

$$P_1 V_G^n = C_1 . \quad (8.2.9)$$

The rate of change of liquid volume in the tank is

$$\frac{dV_L}{dt} = - V_L A , \quad (8.2.10)$$

and total volume is conserved, such that $V_T = V_G + V_L$. Therefore, using Equations 8.2.9 and 8.2.10, and the definition of void fraction within the tank, $\alpha = v_G/(v_G + v_L)$, Equation 8.2.8, can be rewritten as

$$\frac{dP_1}{dt} = - \frac{n P_1}{\alpha V_T} V_L A , \quad (8.2.11)$$

where V_T = Total volume of tank.

8. Modeling of SBWR Specific Components

8.2.3.3 Void-Fraction Calculation

Rate of change of the void fraction within the tank is calculated as

$$\frac{d\alpha}{dt} = \frac{V_G A}{V_T}, \quad (8.2.12)$$

which is used to track the level within the accumulator.

8.2.3.4 Liquid Flow Rate to RPV

Liquid flow velocity and gas pressure are obtained from the simultaneous solution of the coupled differential equations 8.2.4 and 8.2.11. Liquid flow rate to the RPV is calculated from

$$\dot{W}_L = A V_L \rho_L, \quad (8.2.13)$$

and the boron injection rate is

$$\dot{W}_B = C_B \dot{W}_L, \quad (8.2.14)$$

where

C_B	=	Boron concentration
ρ_L	=	Liquid density = $\rho_w(P_I) * SG$
SG	=	Specific Gravity
ρ_w	=	Water density

8.2.4 Control Logic of SLCS

The control logic of SLCS actuation in the SBWR has been implemented into RAMONA-4B. The time delay associated with the signal transmission, valve actuation, and others are user-specified inputs.

For **SBWR**, **ATWS**-related actuation is as follow:

Automatic power range monitor not downscale (>6%) and either of the following persists for 180 seconds;

- (1) $P_{dome} > 7.76 \text{ MPa}$
- (2) **Level < Level 2**

NSLCS is a control variable for SLCS initiation check: i.e., conditions (1) and (2) are satisfied.

Time Input Data Associated to SLCS:

DATWS	= ATWS delay time before SLCS signal (seconds)
TSLCS	= Manual initiation time for SLCS signal (seconds)
DELSC	= SLCS signal transmission delay time (seconds)

DTSVAL = Valve opening time (seconds)

Figure 8.9 shows the time delays associated to SLCS operation.

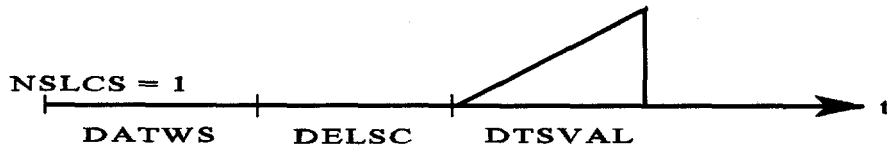


Figure 8.9 Valve Control Timing and Logic for SLCS System

Example:

Let **R** = Rated power
P = Reactor power
 if ($P < 0.06 * R$) then skip SACS logic or
 if ($P > 0.06 * R$ and $NSLCS = 1$) then initiate SLCS

8.2.5 Effect of Wall Friction and Loss Coefficient

$$\frac{dV_L}{dt} = \frac{1}{L' \rho_L} \left\{ (P_1 - P_3) + \rho_L g (L + L_1) - \frac{\tau P_w L'}{A} - (1 + K_c) \frac{V_L^2}{2} \rho_L \right\} \quad (8.2.15)$$

Table 8.1 shows the relative effect of different types of friction model on the flow rate assuming steady state.

8.2.6 Estimation of Loss Coefficient for SLCS

Given Data	Pipe Diameter (in)	Velocity (m/s)
1 → 4	3	10.1
4 → 5	2	24.4
5 → 6	1	28.4 (at the hole of the sparger)
6		7.00 mm holes on sparger

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Table 8.1 Effect of Friction Models on SLCS Flow Rate

Type of Friction Model	Loss Coefficient Friction Model	Liquid Velocity V_L (m/s)	Flow Rate W (kg/s)
No Wall Friction	$K_c = 1.4$	80	364.8
No Wall Friction	$K_c = 14$	32	145.9
Constant Wall Friction	$C_f = 0.5$ $K_c = 1.4$ $\tau_w = 0.5 C_f \rho_L$	7.65	34.88
Laminar Wall Friction	$C_f = -$ $K_c = 1.4$ $\tau_w = 0.5 C_f \rho_L$	44.23	201.6
Turbulent Wall Friction	$C_f = -$ $K_c = 1.4$ $\tau_w = 0.5 C_f \rho_L$	66.34	302.5

For Steady-State Condition,

$$(1 + k_c) \frac{V_L^2 \rho_L}{2} + \frac{\tau P_w L}{A} = (P_1 - P_3) + \rho_L g (L + L_1) \quad (8.2.16)$$

According to this equation, $V_L = 10.10$ m/s with boundary conditions of

$$\begin{aligned} P_1 &= 14.82 \text{ MPa} \\ P_3 &= 7.2 \text{ MPa} \\ L &= 10 \text{ M,} \end{aligned}$$

corresponds to $k_c = 150.0$

Using SBWR data, corresponding to $k_c = 120$, we get $V_L = 10.1$ m/s.

8.2.7 Sample Calculation for Boron Concentration Estimation

SBWR

12.5% Solution of Sodium Pentaborate with 94% B₁₀ Isotope Enrichment

Thermal Cross Sections B¹⁰ = 3860 barns
 B¹¹ = 5 barns

Boron ppm = 0.125 x 0.94 x 10⁶

B_p = 1.175 x 10⁵ ppm

Flow rate of enriched boron solution is therefore obtained from

$$\dot{W}_{boron} = \dot{W}_{solution} \cdot B_p \frac{kg\ ppm}{s} \quad (8.2.17)$$

BWR

12% Solution of Sodium Pentaborate

19.78% Natural Abundance of B¹⁰

Boron ppm = 0.12 X 0.1978 X 10⁶

B_p = 23,800 ppm

8.3 Chimney Model in RAMONA-4B

RAMONA-4B has been modified to incorporate a model of the chimney structure appearing in the SBWR. This is a large vertical section used to serve as additional two-phase-flow driving potential to the natural-circulation flow. This region is located immediately above the core channel exit. The flow region is transversely partitioned into smaller sections. Flow through each vertical section comprises the flow leaving multiple fuel-channel boxes. The design objective of partitioning this component was to maintain a one-dimensional flow. Therefore, RAMONA-4B model of this component is adequate.

In the RAMONA-4B code, the region above the core is defined as the riser component. The upper plenum, riser, and separator are combined within this component. The assumptions related to the riser model are:

- (1) Flow is one dimensional.
- (2) No steam generation due to heating.
- (3) Heat transfer through wall with downcomer region.
- (4) Heat capacity of the internal components are treated as a lumped parameter.

The separator model is able to include the following effects:

8. Modeling of SBWR Specific Components

- (1) Account for flow inertia of the spiral path.
- (2) Account for form losses in the separator.
- (3) Predict volumetric flow rate of saturated vapor.

Equation 4.4.156 of [Wulff et al., 1984a] gives a description of the separator model.

8.3.1 SBWR Chimney Implementation

Since the chimney region is an extension of the upper plenum of the regular BWR, this is modeled in RAMONA-4B as a modification to the riser component. The chimney section is an open area to the flow path as compared to the separator region. Therefore, the friction and form pressure loss related to the separator are not identical in this region. The total height of the riser has been divided into two sections, such that

$$L_{Riser} = L_{Separator} + L_{Chimney}, \quad (8.3.1)$$

where

- L_{riser} = The actual riser height used in RAMONA-4B calculation,
- $L_{chimney}$ = The height of the chimney section,
- $L_{separator}$ = The height of the separator.

According to the modified model, the form and two-phase friction losses related to the standpipe and other parts of the riser uses $L_{separator}$ instead of the total riser height L_{riser} . On the other hand, the chimney region only affects the two-phase friction pressure drop in the momentum balance equation. The effect of riser pressure drop is therefore segmented in two parts. This procedure has been adopted to prevent the addition of a separate component for chimney in RAMONA-4B.

8.3.2 Sample Calculation for the Chimney Model of SBWR

The effect of steam separator on the flow calculation is due to the spiral flow path inside the separator. Since the separators are located in the riser, the flow path in the riser is enhanced by the separators. Thus, the actual flow path in the riser is the physical height of the riser times a multiplier called CRISER given by the correlation,

$$CRISER = \frac{A_{RSR}}{L_{RSR} N_{SEP}} (1-x) \left[\left(\frac{L}{A} \right)_o + x (A_1 + B_1 x) m^{-1} \right], \quad (8.3.2)$$

where A_1 and B_1 are input constants through Card #200200, $(L/A)_o$ is effective L/A ratio for single phase, also an input via Card #200200, x is flow quality in the riser, N_{SEP} is number of separators in the riser, A_{RSR} is the riser flow area, and L_{RSR} is the separator length given by

$$L_{RSR} = L_{Riser} - L_{Chimney}. \quad (8.3.3)$$

For $x = 15\%$, $A = 16.83 \text{ m}^2$, $L_{RISER} = 10.582 \text{ m}$, $L_{CHIMNEY} = 6.5 \text{ m}$, $L_{RSR} = 4.082 \text{ m}$, $N_{SEP} = 211$, we obtain $CRISER = 7.81607$.

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APPENDIX A

Algebraic Equations for Section 4.4.3.2

$$\begin{aligned}
 W_{m,0} = & \left(RMULT \sum_{j=1}^{N P C} \frac{\left(M_j + \sum_{k=j,in}^{j,out} B_{2,k}^j \Delta z_k - \sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_j} W_{dl\ t,k}^j \Delta z_k \right)}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right) \\
 & \left(1 + RMULT \left(\sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right) \sum_{j=1}^{N P C} \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right) \\
 & + (RMULT \left(\sum_{k=DC1,in}^{LP2,out} B_{2,k} \Delta z_k + \sum_{k=R,in}^{R,out} B_{2,k} \Delta z_k - \sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dl\ t,k} \Delta z_k \right. \\
 & \left. - (W_{dl\ t}^{DC} + W_{dl\ t}^{core}) \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} \Delta z_k - \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dl\ t,k} \Delta z_k \right) \sum_{j=1}^{N P C} \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} - W_{dl\ t}^{DC} \\
 & / \left(1 + RMULT \left[\sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right] \sum_{j=1}^{N P C} \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \right)
 \end{aligned} \tag{4.A.1}$$

Following the notations used in the code, the last expression can be written as follows:

$$W_{m,0} = \frac{RMULT [ANOM1 + ANOM2 DEN2] - DELWD}{DEN1} \tag{4.A.2}$$

Appendix A Algebraic Equations for Section 4.4.3.2

where

$$ANOM1 = \sum_{j=1}^{NPC} \frac{\left(M_j + \sum_{k=j,in}^{j,out} B_{2,k}^j \Delta z_k - \sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_j} W_{dl\ t,k}^j \Delta z_k \right)}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k}, \quad (4.A.3)$$

$$\begin{aligned} ANOM2 = & \sum_{k=DC1,in}^{LP2,out} B_{2,k} \Delta z_k + \sum_{k=R,in}^{R,out} B_{2,k} \Delta z_k \\ & - \sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dl\ t,k} \Delta z_k \\ & - \frac{W_{dl\ t}^{DC} + W_{dl\ t}^{core}}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k - \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dl\ t,k} \Delta z_k \end{aligned} \quad (4.A.4)$$

$$DEN1 = 1 + RMULT \times \left(\sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right) \sum_{j=1}^{NPC} \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k}, \quad (4.A.5)$$

$$DEN2 = \sum_{j=1}^{NPC} \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k}, \quad (4.A.6)$$

and the total mixture dilatation rate in the 1-D components upstream of the core is:

$$DELWD = W_{dl\ t}^{DC} \quad (4.A.7)$$

As a final step, Equation (4.4.28) for $W_{m,in}^j$ is rewritten as follows:

$$\begin{aligned}
 W_{m,in}^j = & \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \left[M_j + \sum_{k=DCI,in}^{LP2,out} B_{2,k} \Delta z_k + \sum_{k=R,in}^{R,out} B_{2,k} \Delta z_k + \sum_{k=j,in}^{j,out} B_{2,k}^j \Delta z_k \right] \\
 & - \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \left[W_{m,0} \left(\sum_{k=DCI,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k + \frac{1}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right) \right] \\
 & - \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \left[\sum_{k=DCI,in}^{LP2,out} \frac{B_{1,k}}{A_k} W_{dt,k} \Delta z_k + \frac{W_{dt}^{DC} + W_{dt}^{core}}{A_R} \sum_{k=R,in}^{R,out} B_{1,k} \Delta z_k \right] \\
 & - \frac{1}{\sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k} \left[\sum_{k=j,in}^{j,out} \frac{B_{1,k}}{A_k^j} W_{dt,k}^j \Delta z_k + \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dt,k} \Delta z_k \right]
 \end{aligned} \tag{4.A.8}$$

In terms of FORTRAN implementation, the above equation becomes:

$$\begin{aligned}
 W_{m,in}^j = & \frac{1}{BIDZC(j)} [M_j + B2DZD + B2DZR + B2DZC(j)] \\
 & - \frac{1}{BIDZC(j)} [W_{m,0} (B1DZD + B1DZR) - B1DZWD] \\
 & - \frac{1}{BIDZC(j)} [B1DZR (DELWD + DELWC)] \\
 & - \frac{1}{BIDZC(j)} [B1DZWR + B1DZWC(j)] \\
 = & \frac{1}{BIDZC(j)} (AUXNOM + M_j),
 \end{aligned} \tag{4.A.9}$$

where,

$$DELWD = W_{dt}^{DC} \tag{4.A.10}$$

Appendix A Algebraic Equations for Section 4.4.3.2

$$DELWC = W_{dl\ t}^{core}, \quad (4.A.11)$$

$$B1DZR = \sum_{k=R,in}^{R,out} B_{-1,k} \Delta z_k, \quad (4.A.12)$$

$$B1DZWR = \sum_{k=R,in}^{R,out} \frac{B_{1,k}}{A_R} W_{dl\ t,k} \Delta z_k \quad (4.A.13)$$

$$B1DZD = \sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{A_k} \Delta z_k \quad (4.A.14)$$

$$B2DZD = \sum_{k=DC1,in}^{LP2,out} B_{2,k} \Delta z_k, \quad (4.A.15)$$

$$B1DZWD = \sum_{k=DC1,in}^{LP2,out} \frac{B_{1,k}}{W_{dl\ t}} \Delta z_k \quad (4.A.16)$$

$$B2DZR = \sum_{k=R,in}^{R,out} B_{2,k} \Delta z_k \quad (4.A.17)$$

$$B2DZC(j) = \sum_{k=j,in}^{j,out} B_{2,k} \Delta z_k \quad (4.A.18)$$

$$BIDZWC(j) = \sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} W_{dl\ t,k} \Delta z_k \quad (4.A.19)$$

$$BIDZC(j) = \sum_{k=j,in}^{j,out} \frac{B_{1,k}^j}{A_k^j} \Delta z_k \quad (4.A.20)$$

The volumetric flow rates, $W_{m,o}$ and $W_{m,j}^{in}$ $j = 1 \dots NPC$ have been expressed in terms of the integrated loop momenta M_j .

The momenta of NPC loops are initialized at the beginning of the transient calculation by using the following formula derived from (4.A.9).

$$M_j = W_{m,in}^j BIDZC(j) - AUXNOM. \quad (4.A.21)$$

During the transient calculations, solution of NPC momentum conservation equations provide values of M_j for the volumetric flow rate calculations described above.