DESIGN AND SIMULATION OF A DIGITAL CONTROL SYSTEM
FOR
A MULTI-MODULAR POWER PLANT

by

KEUNG KOO KIM

B.S., Nuclear Engineering, Seoul National University (1981)
M.S., Nuclear Engineering, Seoul National University (1983)

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Signature of Author ____________________________

Department of Nuclear Engineering
August 10, 1992

Certified by ____________________________

John E. Meyer, Ph.D.
Professor of Nuclear Engineering
Thesis Supervisor

Certified by ____________________________

John A. Bernard, Jr., Ph.D.
Director of Reactor Operations
Thesis Supervisor

Certified by ____________________________

David D. Lanning, Ph.D.
Professor of Nuclear Engineering
Thesis Supervisor

Accepted by ____________________________

Allan F. Henry, Ph.D.
Chairman, Departmental Committee on Graduate Students
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Keung Koo Kim

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Abstract

Next-generation nuclear power plants should demonstrate that power generation technology is both safe and cost-effective. One advanced nuclear power plant concept is a multi-modular arrangement in which several power modules (each with its own reactor core and steam generator) supply steam to a common turbine generator. Proposed multi-modular power plants can improve reactor safety and possibly economics. Specifically, the small size of the reactor core may enable a modular reactor to ride out a loss of cooling accident without active decay heat removal systems or active safety systems of any kind. The characteristics of inherently safe reactors may make an automatic digital control strategy more acceptable to both licensing authorities and the public and the use of automatic system can reduce the demand on the operation and improve the operating costs. Thus, the multi-modular concept has the potential to make the overall plant more reliable and less demanding on human factor of the control system than a large, single-reactor, plant.

One result of this research was to develop and evaluate an integrated plant simulation program for a PWR-type multi-modular power plant. This program has the capability to simulate normal operational transients in a multi-modular power plant including operation with unbalanced loads. Models of each module's reactor core and its primary and secondary coolant loop as well as the main steam line header that is common to all modules were included in the simulation program which is named PMSIM for PWR-type Multi-modular power plant SIMulation program. PMSIM has been used both to obtain physical information about a PWR-type multi-modular power plant and to verify the proposed controller's performance.

The second result of this research was to develop and evaluate a robust, digital, closed-loop steam generator level controller that was designed to ensure satisfactory automatic control of level in both PWR-type multi-modular power plants and existing PWR plants. A digital non-linear controller is especially needed to maintain steam generator level within allowed tolerances on multi-modular plants because the steam flowrate from each generator will be different when the plant is operated under conditions of unbalanced loads. Model-based inverse response compensators were designed for each perturbation parameter including feedwater flowrate, steam flowrate, and primary coolant temperature. Simulation studies of various transients showed that the use of this new controller greatly reduces the effect of inverse response (i.e., shrink and swell) and significantly improves the controllability of steam generator level. In addition to the design of this controller, a
predictive display for use as an operator aid during the manual control of steam generator level was developed.

The third result of this research was to develop and evaluate a closed-loop, digital power control system for a PWR-type multi-modular power plant. One objective of this controller was to ensure that no automated action would result in a challenge to any operational constraint or safety limit. Another objective was for the resulting controller to allow different modules to operate at different power levels thereby accommodating differing fuel depletion rates. A multi-tiered, closed-loop digital power control system was designed. Among the tiers for this hierarchical controller are one to allocate demanded power for control of the primary coolant temperature and neutronic power, one to ensure the absence of challenges to the reactor safety system, and one to provide signals to the system actuators. The performance of this power control system was demonstrated by simulating various operational transients on a PWR-type multi-modular power plant. All evaluation results showed satisfactory control performance. In particular, the primary coolant temperature followed the desired trajectory within allowed error bands and hence, the steam flowrates shared their desired fractions without unsatisfactory oscillations among modules.

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Thesis Supervisor:  John E. Meyer, Ph.D.
Title: Professor of Nuclear Engineering

Thesis Supervisor:  John A. Bernard, Jr. Ph.D.
Title: Director of Reactor Operations, Nuclear Reactor Laboratory

Thesis Supervisor:  David D. Lanning, Ph.D.
Title: Professor of Nuclear Engineering
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Table of Contents

Title ......................................................................................................................... 1
Abstract .................................................................................................................. 2
Acknowledgment ...................................................................................................... 4
Table of Contents ..................................................................................................... 5
List of Tables ........................................................................................................... 14
List of Figures ......................................................................................................... 16

Chapter One Introduction ......................................................................................... 29
1.1 Introduction ......................................................................................................... 29
   1.1.1 Use of Digital Technology in Nuclear Industry .............................................. 31
   1.1.2 Digital Control ............................................................................................. 33
   1.1.3 Multi-Modular Nuclear Power Plants ............................................................. 35

1.2 Research Objectives ............................................................................................ 37
   1.2.1 Simulation Program Development ............................................................... 38
   1.2.2 Steam Generator Level Controller .............................................................. 38
1.2.3 Power Control System ................................................................. 39

1.3 Organization of This Report ................................................................ 40

Chapter Two Simulation Program Development .................................. 42

2.1 Introduction ....................................................................................... 42
  2.1.1 General ....................................................................................... 42
  2.1.2 Review of Previous Work ............................................................. 44
  2.1.3 Boundaries for the Simulation Program Model ....................... 46

2.2 Multi-Modular Power Plant .............................................................. 48
  2.2.1 LMR-Type Multi-Modular Power Plants ................................... 48
    2.2.1.1 PRISM ............................................................................. 48
    2.2.1.2 SAFR ............................................................................. 50
  2.2.2 Gas-Cooled Multi-Modular Power Plants .............................. 52
  2.2.3 PWR-Type Multi-Modular Power Plants ............................... 56

2.3 Simulation Model .............................................................................. 61
  2.3.1 Neutron Kinetics Model ............................................................. 61
    2.3.1.1 Point Kinetics Equations ..................................................... 61
    2.3.1.2 Reactivity ......................................................................... 62
  2.3.2 Fuel Average Temperature and Thermal Power Calculation Model ..... 66
  2.3.3 Primary Coolant System Simulation Model ............................ 70
  2.3.4 Steam Generator Secondary Side Model ............................... 75
  2.3.5 Main Steam Line Common Header Model ............................... 78
  2.3.6 Controller Simulation Model ..................................................... 86
2.4 Numerical Solution Method ................................................................. 87
  2.4.1 Finite Difference Approximation to System Equations ..................... 87
    2.4.1.1 Neutron Kinetics Model .......................................................... 87
    2.4.1.2 Fuel Average Temperature and Thermal Power Calculation
                Model ................................................................................. 88
    2.4.1.3 Primary Coolant System Simulation Model .................................. 89
    2.4.1.4 Steam Generator Secondary Side Model ...................................... 92
    2.4.1.5 Main Steam Line Common Header Model ...................................... 93
  2.4.2 Numerical Solution Procedure .......................................................... 93
    2.4.2.1 Steady-State Simulation ............................................................. 95
    2.4.2.2 Transient Simulation ................................................................. 98

2.5 Evaluation of Simulation Program ......................................................... 101
  2.5.1 Steady-State Simulation ................................................................. 101
  2.5.2 Transient Simulation ...................................................................... 103
    2.5.2.1 Null Transient Simulation ......................................................... 104
    2.5.2.2 Symmetry Test ....................................................................... 109

2.6 Chapter Summary .................................................................................. 115

Chapter Three Steam Generator Level Controller ................................. 116

3.1 Introduction ......................................................................................... 116
  3.1.1 General ......................................................................................... 116
  3.1.2 Review of Analyses of Previous Operational Data ............................ 118
3.2 Description of Steam Generator ................................................................. 121
  3.2.1 Steam Generator Internals ................................................................. 121
  3.2.2 Associated Secondary Plant ............................................................. 124
    3.2.2.1 Main Steam System .................................................................. 125
    3.2.2.2 Main Feed System ................................................................... 127
  3.2.3 Feedwater Flow Control System ......................................................... 128
    3.2.3.1 Steam Generator Water Level Control Program .......................... 129
    3.2.3.2 Steam Generator Water Level Control System ........................... 131

3.3 Steam Generator Water Level Dynamics ..................................................... 133
  3.3.1 Swell and Shrink Effects ..................................................................... 133
    3.3.1.1 Steam Generator Operating Characteristics ............................. 135
    3.3.1.2 Steam Generator Level Shrink and Swell Effects ..................... 137
    3.3.1.3 Inverse Response ..................................................................... 145
  3.3.2 Simple Steam Generator Model ............................................................ 148
    3.3.2.1 Simplified Transfer Function .................................................. 148
    3.3.2.2 Identification of Simplified Model Parameters ........................ 152
    3.3.2.3 Characteristics of the Simplified Model Parameters ................ 155
    3.3.2.4 Validation of the Simplified Model .......................................... 156

3.4 Conventional Controller ........................................................................... 164
  3.4.1 Modeling of a Conventional Controller ............................................... 164
    3.4.1.1 Level Controller ...................................................................... 164
    3.4.1.2 Feedwater Regulating Valve and Sensors ................................. 169
  3.4.2 Controller Analysis ............................................................................. 169
    3.4.2.1 Dynamic Characteristics of the Simplified Steam Generator Level Transfer Function .................................................. 170
    3.4.2.2 Closed-Loop Transfer Function ............................................... 173
3.4.2.3 Root Locus Analysis of Closed-Loop Transfer Function ........ 175
3.4.3 Time Domain Analysis ......................................................... 183
3.4.4 Existing Control Problems .................................................. 187

3.5 Proposed Controller ............................................................ 189
  3.5.1 Previous Proposed Solutions to Inverse Response ................. 189
  3.5.2 Feedwater Flowrate Compensator ........................................ 192
    3.5.2.1 Design Approach to Feedwater Flowrate Compensator .......... 192
    3.5.2.2 Laplace Domain Stability Analysis .................................... 197
  3.5.3 Load Parameter Compensators ........................................... 202
    3.5.3.1 Control Performance Index ............................................ 202
    3.5.3.2 Design Approach of Load Parameter Compensators ............... 203
  3.5.4 Feedforward Control ....................................................... 209
  3.5.5 Controller Tuning .......................................................... 211

3.6 Evaluation of Proposed Controller ........................................ 215
  3.6.1 Feedwater Flowrate Perturbation Transients ............................ 215
  3.6.2 Power Transients .......................................................... 218
  3.6.3 Application to the Multi-Modular Power Plant ......................... 223
  3.6.4 Sensitivity Study of the Compensation Parameters .................... 228

3.7 Chapter Summary ............................................................... 231

Chapter Four  Power Control in PWR-Type Multi-Modular Power Plants .......... 233

4.1 Introduction ......................................................................... 233
4.1.1 General .................................................................................. 233
4.1.2 Power Control System Functions ............................................. 235

4.2 Power Control System of Nuclear Power Plant ......................... 239
  4.2.1 PWR .................................................................................. 239
    4.2.1.1 PWR Reactivity Control Devices ..................................... 240
    4.2.1.2 PWR Power Control Strategy ........................................ 241
    4.2.1.3 Analog PWR Controllers .............................................. 244
    4.2.1.4 Performance of Analog PWR Controllers ...................... 248
  4.2.2 CANDU ............................................................................. 249
    4.2.2.1 CANDU Reactivity Devices .......................................... 249
    4.2.2.2 CANDU Power Control Strategy .................................... 250
    4.2.2.3 CANDU Digital Controller ........................................... 252
  4.2.3 PRISM .............................................................................. 255
    4.2.3.1 PRISM Power Control System ...................................... 255
    4.2.3.2 PRISM Model-Based Reactivity Controller .................... 258
  4.2.4 Reactivity Constraint Approach ........................................... 260

4.3 Operational Characteristics of a PWR-Type Multi-Modular Power Plant ....... 265
  4.3.1 Operational Characteristics ............................................... 265
  4.3.2 Power Control Program ...................................................... 268
  4.3.3 Operating Strategies and Operational Modes .......................... 272
    4.3.3.1 Operating Strategies .................................................... 272
    4.3.3.2 Operational Modes ...................................................... 273

4.4 Proposed Power Control System .............................................. 276
  4.4.1 Power Control System Design Approach ............................... 276
    4.4.1.1 Power Control System Design Goals ............................... 276
4.4.1.2 Control Action ............................................................ 277
4.4.2 Multi-Tiered Control System ............................................. 280
4.4.3 Plant Power Controller .................................................... 282
  4.4.3.1 Specified Power Demand Mode ................................. 290
  4.4.3.2 Arbitrary Power Demand Mode ................................. 292
4.4.4 Module Power Controller ................................................ 294
  4.4.4.1 Reactivity Constraint Approach for Module Power ......... 294
  4.4.4.2 Reactivity and Multi-Group Decay Parameter Estimator .... 297
  4.4.4.3 Smoothing Technique for Measured Data ................. 299
4.4.5 Rod Controller .......................................................... 302
  4.4.5.1 Model-Based Rod Controller ................................. 302
  4.4.5.2 Feedback Rod Controller ................................. 306
  4.4.5.3 Feedback Reactivity Estimator .............................. 307
  4.4.5.4 Composite Design of Rod Controller .......... 309

4.5 Evaluation of Proposed Controller ..................................... 311
  4.5.1 Operational Transients ............................................ 311
  4.5.2 Evaluation of Rod and Module Supervisory Controller .... 314
    4.5.2.1 Evaluation of Rod Controller ............................ 315
    4.5.2.2 Evaluation of Module Power Controller ............ 318
  4.5.3 Evaluation of Composite Power Control System ............ 319
    4.5.3.1 Case 1: Power Increase in the Specified Power Demand Mode .... 320
    4.5.3.2 Case 2: Power Increase in the Arbitrary Power Demand Mode .... 325
    4.5.3.3 Case 3: Power Decrease in the Specified Power Demand Mode .... 326
4.5.3.4 Case 4: Power Decrease in the Arbitrary Power Demand Mode ................................................................. 327
4.5.3.5 Case 5: Power Transient When Demand Allocation Method is Changed .......................................................... 335
4.5.3.6 Case 6: Low Power Transient with Slow Ramp Rate............. 341
4.5.4 Discrete Control Rod Movement ................................................................. 344

4.6 Chapter Summary .................................................................................. 352

Chapter Five Summary, Conclusions, and Recommendations .................. 354

5.1 Summary ......................................................................................... 354
  5.1.1 Summary of Simulation Program Development .......................... 355
  5.1.2 Summary of Steam Generator Level Controller ......................... 357
  5.1.3 Summary of Power Control System ............................................. 360

5.2 Conclusions .................................................................................... 363
  5.2.1 Conclusions on Simulation Program .......................................... 363
  5.2.2 Conclusions on Steam Generator Level Controller ................... 364
  5.2.3 Conclusions on Power Control System ....................................... 365

5.3 Recommendations for Future Work .................................................. 367
  5.3.1 Recommendations for Plant Simulation Program ....................... 367
  5.3.2 Recommendations for Controller Development ......................... 368
Appendix A  Steam Generator Water Level Predictive Display Program .......................... 370
A.1 Introduction ..................................................................................................................... 370
A.2 Predictive Display Program ............................................................................................ 373

Appendix B  Stability Analysis of Steam Generator Inverse Simulation Model ..................... 377
B.1 Introduction ...................................................................................................................... 377
B.2 Steam Generator Inverse Simulation Model .................................................................... 381
B.3 Stability Analysis of Steam Generator Inverse Simulation Model ................................. 385

Appendix C  Smith's Dead-time Compensator ................................................................. 391

References ............................................................................................................................. 394
Chapter One Introduction

Chapter Two Simulation Program Development

Table 2-1 Typical PWR-Type Multi-Modular Power Plant Parameters............. 59

Chapter Three Steam Generator Level Controller

Table 3-1 Unplanned Reactor Trip Frequency (Average per Year) [S-1]............. 119
Table 3-2 Initiating Events of Feedwater Related Trips [S-1]......................... 120
Table 3-3 Types of Feedwater Related Trips Contribution to Total [S-1].............. 120
Table 3-4 Control Performance During Power Ramp Transients from 10 %FP to
15 %FP at a 5.0 %/minute Ramp Rate........................................ 205
Table 3-5 Control Performance During Feedwater Flowrate Perturbation
Transients at 10 %FP ............................................................... 217
Table 3-6 Control Performance During Power Ramp Transients from 10 %FP to
15 %FP at a 5.0 %/minute Ramp Rate........................................ 219
Table 3-7 Control Performance During Power Ramp Transients from 15 %FP to
10 %FP at a 5.0 %/minute Ramp Rate........................................ 221
Table 3-8  Control Performance During Power Ramp Transients from 95 %FP to 100 %FP at a 5.0 %/minute Ramp Rate .................................................. 222

Table 3-9  Control Performance During Power Ramp Transients When Compensators are Designed Based on Incorrect Parameters .................. 229

Chapter Four Power Control in PWR-Type Multi-Modular Power Plants

Table 4-1  Duration of Time Delays for Module Power and Turbine Power Demand Allocation ................................................................. 290

Table 4-2  Example of Demand Power Allocation ................................................................. 292

Table 4-3  Power Increase Sequence for Cases 1 and 2 .................................................. 321

Table 4-4  Power Decrease Sequence for Cases 3 and 4 ............................................. 327

Table 4-5  Power Increase Sequence for Case 5 ................................................................. 335

Table 4-6  Power Increase Sequence for Case 6 ................................................................. 341

Chapter Five Summary, Conclusions, and Recommendations

Appendix A  Steam Generator Water Level Predictive Display Program

Appendix B  Stability Analysis of Steam Generator Inverse Simulation Model

Table B-1  Input Variables and State Variables of Forward Simulation Model........... 378

Table B-2  Input Variables and State Variables of Inverse Simulation Model .......... 379

Table B-3  Eigenvalues of the Fourth Order Inverse Simulation Model................. 389

Appendix C  Smith's Dead-time Compensator
Chapter One  Introduction

Chapter Two  Simulation Program Development

Figure 2-1  PRISM Power Module [C-3] ......................................................... 49
Figure 2-2  PRISM Power Block Overview [D-1]............................................ 51
Figure 2-3  Schematic of SAFR Power Pak and Reactor Core [C-3]............. 53
Figure 2-4  A German Company (HRB) and GA Technologies Designed MHTGR [L-1] .......................................................... 54
Figure 2-5  Simplified Flow Diagram of DOE Designed MHTGR [L-1]........... 55
Figure 2-6  Triso-Coated HTGR Fissile and Fertile Fuel Particles [L-1]......... 56
Figure 2-7  Schematic Diagram of PWR-Type Multi-Modular Power Plant .......... 58
Figure 2-8  Control Rod Worth Versus Control Rod Position [C-5] ............... 64
Figure 2-9  Fuel Temperature Reactivity Coefficient [C-5] ............................ 64
Figure 2-10 Moderator Temperature Reactivity Coefficient [C-5] ................. 65
Figure 2-11  Dissolved Boron Reactivity Coefficient [N-1] ........................................ 65
Figure 2-12  Typical Fuel Rod Cross Section ......................................................... 67
Figure 2-13  Control Volume of Primary Coolant Loop ......................................... 71
Figure 2-14  Typical Control Volume of Primary Coolant Loop ............................... 73
Figure 2-15  Steam Generator Secondary Side Simulation Model [S-2] ....................... 76
Figure 2-16  Feedwater Temperature as Function of Power ..................................... 78
Figure 2-17  MSLCH Simulation Model ................................................................. 80
Figure 2-18  Relation among the System Submodels ............................................... 94
Figure 2-19  Steady-State Simulation Procedure ..................................................... 96
Figure 2-20  Flow Chart for Steady-State Calculation of Reactor Primary and Secondary Coolant System ................................................................. 97
Figure 2-21  Transient Simulation Procedure ......................................................... 100
Figure 2-22  Primary Coolant Average Temperature at Steady-State ....................... 102
Figure 2-23  Steam Flowrate from Each Steam Generator at Steady-State ............... 102
Figure 2-24  Steam Generator Internal Pressure at Steady-State ............................ 103
Figure 2-25  Calculated Steam Flowrates of Module #1 for Two Different Time Step Sizes ................................................................. 105
Figure 2-26  Calculated Coolant Average Temperature and Steam Generator Pressure of Module #1 for Two Different Time Step Sizes ...................... 106
Figure 2-27  Neutron Power During Null Power Transient ..................................... 106
Figure 2-28  Primary Coolant Average Temperature During Null Power Transient ...... 107
Figure 2-29  Steam Generator Pressures During Null Power Transient .................... 107
Figure 2-30 Steam Flowrate from Each Power Module During Null Power Transient…………………………………………………………………………………………… 108
Figure 2-31 Pressure and Quality at the Main Steam Line Header During Null Power Transient…………………………………………………………………………………………… 108
Figure 2-32 Neutron Power During Symmetry Test (Only Module #2’s Power is Changed)…………………………………………………………………………………………… 110
Figure 2-33 Neutron Power During Symmetry Test (Only Module #4 ’s Power is Changed)…………………………………………………………………………………………… 110
Figure 2-34 Primary Coolant Average Temperature During Symmetry Test (Only Module #2’s Power is Changed)…………………………………………………………………………………………… 111
Figure 2-35 Primary Coolant Average Temperature During Symmetry Test (Only Module #4’s Power is Changed)…………………………………………………………………………………………… 111
Figure 2-36 Steam Flowrate from Each Power Module During Symmetry Test (Only Module #2’s Power is Changed)…………………………………………………………………………………………… 112
Figure 2-37 Steam Flowrate from Each Power Module During Symmetry Test (Only Module #4’s Power is Changed)…………………………………………………………………………………………… 112
Figure 2-38 Steam Generator Levels During Symmetry Test (Only Module #2’s Power is Changed)…………………………………………………………………………………………… 113
Figure 2-39 Steam Generator Levels During Symmetry Test (Only Module #4’s Power is Changed)…………………………………………………………………………………………… 113
Figure 2-40 Pressure and Quality at the Main Steam Line Common Header during Symmetry Test (Only Module #2’s Power is Changed)…………………………………………………………………………………………… 114
Figure 2-41 Pressure and Quality at the Main Steam Line Common Header during Symmetry Test (Only Module #4’s Power is Changed)…………………………………………………………………………………………… 114

Chapter Three Steam Generator Level Controller

Figure 3-1 Schematic of U-Tube Steam Generator Internals [S-5]…………………………… 122
Figure 3-2  Schematic of Steam Generator, Main Steam, and Main Feedwater System [S-6] ................................................................. 124

Figure 3-3  Steam Generator Water Level Control [S-6] ....................................................... 131

Figure 3-4  Geometry Input of Westinghouse Model F Steam Generator [C-2] .......... 134

Figure 3-5  Perturbation Parameters that Affect Steam Generator Level Dynamics ...... 135

Figure 3-6  Steady-State Recirculating Flowrate ................................................................. 136

Figure 3-7  Schematic View of Steam Generator Level Shrink Phenomena .......... 138

Figure 3-8  Steam Generator Level Shrink Effect due to Sudden Feedwater Flowrate Increase ........................................................................ 139

Figure 3-9  Steam Generator Level Shrink Effect due to Sudden Steam Flowrate Decrease ........................................................................ 140

Figure 3-10 Steam Generator Level Shrink Effect due to Sudden Primary Coolant Temperature Decrease .................................................................. 140

Figure 3-11 Schematic View of Steam Generator Level Swell Phenomena ........... 142

Figure 3-12 Steam Generator Level Swell Effect due to Sudden Feedwater Flowrate Decrease ........................................................................ 143

Figure 3-13 Steam Generator Level Swell Effect due to Sudden Steam Flowrate Increase ........................................................................ 144

Figure 3-14 Steam Generator Level Swell Effect due to Sudden Primary Coolant Temperature Increase ................................................................ 144

Figure 3-15 Block Diagram of the of Drum Boiler Level Response ..................... 146

Figure 3-16 Typical Inverse Response of System Consisted with Integral Term and First Order Lag Term ....................................................... 147

Figure 3-17 Noninverse Response of System Consisted with Integral Term and First Order Lag Term ....................................................... 147

19
Figure 3-18  Steam Generator Simplified Transfer Function Coefficient, $G_{2f}$ ............. 157
Figure 3-19  Steam Generator Simplified Transfer Function Coefficient, $T_{2f}$ ............. 157
Figure 3-20  Steam Generator Simplified Transfer Function Coefficient, $G_{3f}$ ............. 158
Figure 3-21  Steam Generator Simplified Transfer Function Coefficient, $T_{3f}$ ............. 158
Figure 3-22  Steam Generator Simplified Transfer Function Coefficient, $\omega_{3f}$ ............. 159
Figure 3-23  Steam Generator Simplified Transfer Function Coefficient, $G_{2s}$ ............. 159
Figure 3-24  Steam Generator Simplified Transfer Function Coefficient, $T_{2s}$ ............. 160
Figure 3-25  Steam Generator Simplified Transfer Function Coefficient, $G_{1T}$ ............. 160
Figure 3-26  Steam Generator Simplified Transfer Function Coefficient $G_{2T}$ ............. 161
Figure 3-27  Steam Generator Simplified Transfer Function Coefficient, $T_{1T}$ ............. 161
Figure 3-28  Steam Generator Simplified Transfer Function Coefficient, $T_{2T}$ ............. 162
Figure 3-29  Steam Generator Level after Feedwater Flowrate Increase with Step Fashion at 20 %FP ................................................................. 162
Figure 2-30  Steam Generator Level after Steam Flowrate Increase with Step Fashion at 20 %FP ................................................................. 163
Figure 2-31  Steam Generator Level after Primary Coolant Temperature Increase with Step Fashion at 20 %FP ................................................................. 163
Figure 3-32  Block Diagram of Current Steam Generator Water Level Controller and Closed Loop System at High Power Operation ...................... 165
Figure 3-33  Block Diagram of Current Steam Generator Water Level Controller and Closed Loop System at Low Power Operation ...................... 168
Figure 3-34  Pole and Zero Map of the Simplified Transfer Function at 10 %FP ...... 171
Figure 3-35  Pole and Zero Map of the Simplified Transfer Function at 100 %FP ...... 172
Figure 3-36 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller \( T_1 = \infty \) at 10 %FP .................................................. 177

Figure 3-37 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller \( T_1 = \infty \) at 50 %FP .................................................. 178

Figure 3-38 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller \( T_1 = \infty \) at 100 %FP .................................................. 179

Figure 3-39 Maximum Feedback Control Gain of Proportional Feedback Controller .................................................. 181

Figure 3-40 Undamped Natural Frequencies vs. Power .................................................. 181

Figure 3-41 Root Locus Plot of the Closed Loop Transfer Function with Proportional plus Integral Controller with \( T_1 = 228 \) s (Tuned by Ziegler-Nichols Method) at 10 %FP .................................................. 182

Figure 3-42 Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation with Step Fashion. Current PI Controller and 10 %FP .... 185

Figure 3-43 Conventional Controller Tuning Effect During the Feedwater Flowrate Perturbation Transients at 10 %FP .................................................. 185

Figure 3-44 Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation with Step Fashion. Current PI Controller and 100 %FP ..... 186

Figure 3-45 Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation with Step Fashion. (Current 3 Element PI Controller and 10 %FP) .................................................. 186

Figure 3-46 Block Diagram of Feedwater Flowrate Compensator .................................................. 193

Figure 3-47 Compensated Level after Feedwater Perturbation without Level Control Action .................................................. 196

Figure 3-48 Equivalent Block Diagram of Feedback Type Feedwater Flowrate Compensator .................................................. 198
Figure 3-49  Root Locus Plot of the Closed Loop Transfer Function Associated with Compensator ($\alpha_f = 10$ s) at 10 %FP ......................................................... 200

Figure 3-50  Root Locus Plot of the Closed Loop Transfer Function Associated with Compensator ($\alpha_f = 100$ s) at 10 %FP......................................................... 201

Figure 3-51  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate......................................................... 205

Figure 3-52  Block Diagram of Load Parameter Compensators............................................. 206

Figure 3-53  Steady State Steam Generator Mass Inventory vs. Operating Power …… 210

Figure 3-54  Schematic of the Overall Proposed Steam Generator Water Level Control System ................................................................. 212

Figure 3-55  The Effect of Amount of Inverse Response Compensation During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate................................. 214

Figure 3-56  Steam Generator Level During the Feedwater Flowrate Perturbation Transients at 10 %FP................................................................. 217

Figure 3-57  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate......................................................... 219

Figure 3-58  Steam and Feedwater Flowrate During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate......................................................... 220

Figure 3-59  Steam Generator Level During Power Ramp Transients from 15 %FP to 10 %FP at a 5.0 %/minute Ramp Rate......................................................... 221

Figure 3-60  Steam Generator Level During Power Ramp Transients from 95 %FP to 100 %FP at a 5.0 %/minute Ramp Rate ......................................................... 222

Figure 3-61  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate on Only Module #1............................. 224

Figure 3-62  Steam Flowrate During Unbalanced Power Transient from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate on Only Module #1......................... 224
Chapter Four Power Control in PWR-Type Multi-Modular Power Plants

Figure 4-1 PWR NSSS Integrated Plant Control System [M-2].......................... 238

Figure 4-2 The Constant Primary Coolant Average Temperature Control Program ................................................................. 242

Figure 4-3 The Constant Steam Pressure Control Program ........................................ 243

Figure 4-4 Block Diagram of an Existing PWR Average Coolant Temperature Controller [N-1] .......................................................... 245

Figure 4-5 Typical PWR Control Rod Speed Program [M-2] .............................. 247

Figure 4-6 Layout of CANDU Reactivity Devices [A-4] ........................................ 251

Figure 4-7 Block Diagram of CANDU Power Control System [A-4] .................. 253

Figure 4-8 Inlet Flow Control Program for Zone Control System Unit [A-4] .......... 255

Figure 4-9 Supervisory Control System for Multi-Modular ALMRs [O-3] .......... 257
Figure 4-10 Logic Diagram of Digital Controller for PWR Average Coolant Temperature Controller Proposed by Cabral [C-1]................................. 263

Figure 4-11 Logic Diagram of Digital Controller for PWR Controller Proposed by Aviles [A-2] ................................................................. 264

Figure 4-12 Steady State Pressure Difference between Steam Generator and Main Steam Line Header ....................................................................... 267

Figure 4-13 Sliding Primary Coolant Average Temperature Control Program ........ 268

Figure 4-14 Different Load Operation under Sliding Average Temperature Control Program ................................................................................. 270

Figure 4-15 PWR-Type Multi-Modular Power Plant Operation Map (Steady-State Average Primary Coolant Temperature) ........................................... 271

Figure 4-16 PWR-Type Multi-Modular Power Plant Operation Map (Steady State Steam Generator Pressure) ......................................................... 271

Figure 4-17 Control Action and Controlled Variables of PWR-Type Multi-Modular Power Plant ............................................................................. 278

Figure 4-18 The Schematic of the Proposed Multi-Tiered Power Control System...... 281

Figure 4-19 Relationship between Plant Power Controller and Other System........... 283

Figure 4-20 Illustration of the Requirement of Time Delay Control ..................... 285

Figure 4-21 Demand Allocation Methods ............................................................. 287

Figure 4-22 Static Time Constant When Power Increasing Transient .................. 289

Figure 4-23 Demand Allocation in the Specified Demand Power Operation Mode-I .... 291

Figure 4-24 Demand Allocation in the Specified Demand Power Operation Mode-II .............................................................................................. 291

Figure 4-25 Demand Allocation in the Arbitrary Power demand Operation Mode-I..... 293
Figure 4-26 Demand Allocation in the Arbitrary Power Demand Operation Mode-II ........................................................................................................... 293

Figure 4-27 Estimated Reactivity when 0.1 %FP White Noise is Imposed on the Measurements ................................................................................. 301

Figure 4-28 Estimated Reactivity when Moving Average Signal Smoothing Method is Applied (0.1 %FP White Noise) ................................... 301

Figure 4-29 Period-Generated Control as Applied to Trajectory Tracking of Reactor Power [B-8] ................................................................. 304

Figure 4-30 Rod Speed and Direction Control Program ............................................................................................................................... 308

Figure 4-31 Schematic of Proposed Rod Controller .................................................. 310

Figure 4-32 Module Power without Measurement Noise ........................................ 316

Figure 4-33 Module Power with Measurement Noise ............................................. 317

Figure 4-34 Module Power with Signal Smoothing Technique .......................... 317

Figure 4-35 Module Power When the Module Power Controller Supervises the Power Increase ................................................................. 319

Figure 4-36 Module and Turbine Power (Case 1 Transient) .................................. 322

Figure 4-37 Primary Coolant Average Temperature (Case 1 Transient) .............. 322

Figure 4-38 Equivalent Control Rod Position (Case 1 Transient) ....................... 323

Figure 4-39 Steam Flowrates from Individual Steam Generators (Case 1 Transient) ......................................................................................... 323

Figure 4-40 Steam Generator Level (Case 1 Transient) ...................................... 324

Figure 4-41 Steam Generator Pressure (Case 1 Transient) .................................. 324

Figure 4-42 Module and Turbine Power (Case 2 Transient) .............................. 328

Figure 4-43 Primary Coolant Temperature (Case 2 Transient) ......................... 328
Figure 4-61 Primary Coolant Temperature in Arbitrary Power Demand Operational Mode (Case 5 Transient) ................................................................. 339

Figure 4-62 Steam Flowrates from Individual Steam Generators in Arbitrary Power Demand Operational Mode (Case 5 Transient) ....................... 340

Figure 4-63 Steam Generator Level in Arbitrary Power Demand Operational Mode (Case 5 Transient) ................................................................. 340

Figure 4-64 Module and Turbine Power in Specified Power Demand Operational Mode (Case 6 Transient ) ................................................................. 342

Figure 4-65 Primary Coolant Temperature in Specified Power Demand Operational Mode (Case 6 Transient ) ................................................................. 342

Figure 4-66 Steam Flowrates from Individual Steam Generators in Specified Power Demand Operational Mode (Case 6 Transient ) ....................... 343

Figure 4-67 Steam Generator Level in Specified Power Demand Operational Mode (Case 6 Transient ) ................................................................. 343

Figure 4-68 Control Rod Drive Assembly [S-10]. .................................................. 344

Figure 4-69 Effect of Discrete Rod Movement on Control Rod Speed .................. 347

Figure 4-70 Effect of Discrete Rod Movement on Reactor Reactivity Where is Presence of Feedback Effects ................................................................. 347

Figure 4-71 Module Power When Control Rod Moves in Discrete Steps ............... 348

Figure 4-72 Control Rod Chattering When Control Rod Moves in Discrete Steps .... 348

Figure 4-73 Module Power When Control Rod is Chattering ............................... 349

Figure 4-74 Module Power (Case 7 Transient) ..................................................... 349

Figure 4-75 Primary Coolant Temperature (Case 7 Transient) ........................... 350

Figure 4-76 Steam Flowrates from Steam Generators (Case 7 Transient) ............ 350

Figure 4-77 Steam Generator Level (Case 7 Transient) ........................................ 351
Chapter Five  Summary, Conclusions, and Recommendations

Appendix A  Steam Generator Water Level Predictive Display Program

Figure A-1  Block Diagram of SGLDP ................................................................. 374

Figure A-2  Steam Generator Level Display After Start of Power Ramp with Ramp Rate of 2.5% of Full Power Per Minute with Reactor Initially at 10% of Rated Power ................................................................. 376

Appendix B  Stability Analysis of Steam Generator Inverse Simulation Model

Figure B-1  Conceptual Idea for Model-Based Controller ......................... 380

Figure B-2  Forward and Inverse Simulation Results (Internal Energy at the Bottom of Downcomer, 10 %FP Steady State) ........................................ 389

Figure B-3  Forward and Inverse Simulation Results (Void Fraction at the Tube Bundle Outlet, 10 %FP Steady State) ........................................ 390

Figure B-4  Forward and Inverse Simulation Results (Void Fraction at the Riser Outlet, 10 %FP Steady State) ........................................ 390

Appendix C  Smith's Dead-time Compensator

Figure C-1  Schematic of the Control System with Smith's Dead-time Compensator ................................................................. 392

Figure C-2  Equivalent Block Diagram of the Control System with Smith's Dead-time Compensator ................................................................. 393
Chapter One

Introduction

1.1 Introduction

Current thinking within the U.S. nuclear industry suggests that future stations for the nuclear generation of electricity will consist of clusters of modular reactors. There are several reasons for this trend. First, there have been more difficulties associated with the operation of large, light-water reactors than with smaller units. Second, if a large unit is removed from service for refueling, then a significant fraction of the electricity supply on a given grid must be replaced using more expensive fuels. Third, incorporation of 'inherently safe' design features is more readily accomplished for smaller units. Countering these advantages is the major disadvantage that 'economies of scale' would be lost if several modularized units were built and operated instead of one large facility. One way of minimizing this problem, at least in regard to operating costs, is to develop and implement techniques for the automated control of the modularized plants. Specific advantages include:
i) **Containment of Operating Costs:** Automation, if properly done, may allow the same number of operating personnel to control two or three reactors as are now necessary to operate a single unit.

ii) **Reduction in Training Costs:** The cost of a licensed operator is not merely that individual's salary. A far greater cost is usually the support staff and equipment needed to first train and then provide continual upgrading of each operator. If the number of operators can be limited to that now required for a large plant, then training costs can at least be kept constant.

iii) **Fuel Management:** One of the advantages of the modular design concept is that only one of the clustered units would be out of service at any time. Hence, the capacity factor for the station as a whole would remain high and the need for expensive replacement electricity would be minimized. A prerequisite to the achievement of this advantage is that it be possible to deplete the fuel in each modular unit at a different rate. Otherwise, all units would require refueling at the same time. This in turn creates a complicated control problem because each reactor is supplying steam to a common header. Thus, for each unit, the pressure of the steam being generated must be equal even though it is desired that the thermal power outputs be different. This might be accomplished by operating each reactor at a different temperature. (That is, the average primary coolant temperatures would be different.) So doing will require a sophisticated control strategy.

The Massachusetts Institute of Technology (MIT) is engaged in an ongoing program to develop and evaluate new techniques for the closed-loop digital control of nuclear reactors. One objective of that effort is the development of a comprehensive system for the automation of multi-modular reactors so that one crew will be capable of
supervising the operation of several reactors. The research reported here constitutes one part of the program to automate multi-modular reactors. Specifically, it address the design and development of methods for the control of steam generator level and reactor power in a multi-modular plant under conditions of unbalanced loads. That is, each individual module is supplying a different fraction of the total load.

1.1.1 Use of Digital Technology in Nuclear Industry

The uses made of digital computers in nuclear power plants include data acquisition and storage, plant monitoring, and fully automatic control. Not all plants employ digital computers for this entire range of applications. In particular, the direct digital control of nuclear power plants is not presently practiced in the United States. Regulations require that any system which is needed for the shutdown of the plant or which is associated with safety be subject to exhaustive quality control tests. The result is that only proven technology is employed. In the past, this has meant that only hard-wired analog devices could be used. This situation is slowly changing as is illustrated by the approval and installation of digital feedwater systems in several power plants [A-1,S-1] and by digital control experiments conducted on Massachusetts Institute of Technology's Research Reactor, MITR-II. Also, one method for the control of neutronic power via a digital computer has been licensed by the U.S. Nuclear Regulatory Commission for use on the MITR-II [B-1, B-2]. At present, other countries, including Canada, France, Germany, Korea, and Japan are either investigating or employing nuclear power plant automation including digital control. Comparisons of the use made of advanced instrumentation and control technologies in U.S. nuclear plants versus those of Europe and Japan respectively are given in two recent reports [W-1,H-1].
One of the difficulties associated with the incorporation of digital control technologies in U.S. nuclear plants is that no new plants are being ordered. Hence, any digital installations are done as retrofits. In fact, the only universally adopted use of a digital technology was the result of a regulatory action. Specifically, in response to the TMI accident, the U.S. Nuclear Regulatory Commission (NRC) required that all nuclear utilities commit to the implementation of a Safety Parameter Display System (SPDS) in every nuclear plant. An SPDS provides an on-line display of key safety parameters with the objective of providing the plant operating crew with a concise summary of the status of the plant's critical plant safety functions such as reactivity control, heat removal, inventory control, and containment isolation [T-1]. Even though most SPDSs use digital computers, these systems are purely for display and serve no control function.

Interest is growing in the application of digital technologies to the control and management of nuclear power plants because of the many advantageous features which include proven experience in other industries, a small number of switching elements, small size, a simple and sturdy technology, equipment of durable design and consistent fabrication, and virtually negligible maintenance. Also, the characteristics of inherently safe reactors may make an automatic digital control strategy more acceptable to licensing authorities and the public. The complete automation of commercial nuclear power plants in the United States may therefore be possible in the next generation of nuclear power plants.

MIT has a strong belief that the nuclear industry can benefit from the application of digital technology for the advanced instrumentation and control of nuclear reactors. To that end, the MIT Department of Nuclear Engineering and the MIT Nuclear Reactor Laboratory initiated a joint program on the advanced instrumentation and control of nuclear reactors in the late 1970s [B-3]. At that time, most of the effort was focused on the development of
accurate, fast-running models of various plant components that were fundamental to control studies. Also, in the 1980s, significant theoretical and experimental research was performed that resulted in the development of closed-loop digital control laws for reactor power control. During this period, the 'MIT-CSDL Non-Linear Digital Controller' [B-1], the 'Reactivity Constraint Approach' [B-1,B-4], a 'Rule-Based Controller' [B-5], and the 'MIT-SNL Period-Generated Minimum Time Control Laws' [B-1] were developed. In the late 1980s, the MIT effort was expanded to include the application of advanced closed-loop digital control methodologies to both large PWR core power control [C-1,A-2] and steam generator level control [C-2]. Recently, the application of digital closed-loop control to a multi-modular power plant has been studied [W-2]. This report concerns this later topic. It involves the use of advanced digital control techniques for the automated operation of multi-modular power plants.

1.1.2 Digital Control

After Honeywell announced and introduced the 'microprocessor based digital controller' in 1976, the application of digital control technology expanded rapidly [T-2]. The introduction of the direct digital control concept has allowed an entirely new approach to control system design. Before direct digital control, most computers were used as special add-on equipment to existing analog control systems. The role of the computer might be only for data acquisition and storage, or for plant monitoring. The advent of digital computers has made possible the use of advanced control and monitoring techniques that are now being employed in major industries such as aerospace, chemical processing, and modern fossil-fired power generation.
Some of the benefits that might be obtained from the advanced automatic digital control of nuclear power plants are summarized here [A-2, B-6, W-1]:

i) **More sophisticated control capability:** The availability of digital computers means that non-linear system models and control algorithms can be utilized. Hence, a system can be automated over its full range of operation. In contrast, analog devices are often limited to some narrow operating region over which the plant model has been linearized.

ii) **Efficient and cost-effective plant operation:** Plant automation makes it possible to reduce operating crew size and hence reduce operating costs and training.

iii) **Low capital outlay:** Automated digital control techniques are economical because they require less space and have less cabling.

iv) **Enhanced safety:** Fault-tolerant digital technology can ensure reliable operation. Also, digital systems can do certain tasks such as instrument fault detection and signal validation more effectively than can a human. Hence, safety is enhanced through the early detection of instrument failures and/or undesired plant trends.

v) **Increased the plant reliability and availability:** Digital equipment improves communication among distributed sub-control systems and hence facilitates the on-line evaluation and modification of control parameter setpoints.

vi) **Ease of operation:** Digital technology makes operating and maintenance actions easier to perform, and therefore reduces the risk of error.

In this research, much effort has been directed to designing digital controllers to replace existing local analog controllers, particularly power and steam generator level
controllers. The proposed controllers are highly reliable, robust, microprocessor-based digital controllers.

1.1.3 Multi-Modular Nuclear Power Plants

Next-generation nuclear power plants should demonstrate that power generation technology is both safe and cost-effective. One advanced nuclear power plant concept is a multi-modular arrangement in which several power modules (each with its own reactor core and steam generator) supply steam to a common turbine generator. Proposed multi-modular power plants can improve reactor safety and possibly economics.

The small size of the reactor core may enable it to ride out a loss of coolant accident without active decay heat removal systems or active safety systems of any kind. Thus, the multi-modular concept has the potential to make the overall plant more reliable and less demanding of the control system than a large, single-reactor plant [U-1]. The individual modules of a multi-modular power plant are to be sized so that components related to nuclear safety can be factory-fabricated. This is an advantage because quality can be more readily controlled in a factory than in the field. After the major components are made, they are to be transported to the site for rapid installation. This manufacture and construction method is expected to reduce the licensing effort because the module will be pre-licensed and only site-specific issues will have to be considered in the final licensing procedures.

In addition to enhancing reactor safety, a multi-modular power plant also has the potential to provide a higher capacity factor. A capacity factor improvement over that of existing, large, single reactors is expected for several reasons:
i) The reduction in the number of redundant active safety systems should minimize shutdowns because of these systems' malfunctions.

ii) The small size of components and the simplicity of the power loop should reduce maintenance.

iii) The modular make-up of the plant ensures partial power output from unaffected modules whenever a module is down for refueling and/or maintenance. In fact, this mode of operation will probably be used to permit refueling without the need for taking the entire plant off-line. This in turn means that each module in a multi-modular plant will be routinely operated at a different load factor so as to stagger the times at which individual cores must be refueled.

If conventional control methods (PWR-type analog controllers and licensed operator supervisory control) are employed for a multi-modular power plant, then there will be a tremendous number of control signals and operating parameters to be monitored and processed. Each power module will require its own complete control room with the usual complement of operators. The resulting construction, operating, and training costs could make multi-modular power plants prohibitively expensive. New control techniques that rely on digital technology can avoid these costs by permitting the use of a single control room. This approach, coupled with passive safety features, will make multi-modular reactors both safe and economical.
1.2 Research Objectives

The primary objectives of the research described in this report are to:

i) Develop and evaluate an integrated plant simulation program for a PWR-type multi-modular power plant. This program should have the capability to simulate normal operational transients, especially operation with unbalanced loads. Hence, the simulation program should model the reactor core and the primary and secondary coolant loops of each module (four modules in all) as well as the common main steam line header. Also, for the development of a steam generator level controller, the steam generator model should accurately describe the complicated level dynamics especially 'shrink and swell' effects.

ii) Develop and evaluate a robust, digital, closed-loop steam generator level controller that always ensures satisfactory automatic control for steam generator water level in PWR-type multi-modular power plants as well as in existing PWR plants. As part of this objective, a predictive display intended as an operator aid for the manual control of steam generator level control was developed. This display is described in Appendix A of this report.

iii) Develop and evaluate a closed-loop, digital power control system for a PWR-type multi-modular power plant that ensures that no automated action results in a challenge to either an operational constraint or a safety limit. The resulting controller should allow different modules to operate at different power levels thereby accommodating differing core burnups.
1.2.1 Simulation Program Development

Simulations of operational transients are needed as part of this research for the following purposes:

i) To obtain physical information on the response characteristics of PWR-type multi-modular power plants,

ii) To verify the performance of the proposed controllers, and

iii) To be used for direct incorporation in a model-based controller.

Other possible uses for this simulation program are signal validation, operator assistance, and implementation as a plant simulator.

In the simulation of the actual power plant, it is important to model the reactor core, the fuel rods, the primary coolant loop, the steam generator, and the common steam line header that leads to the turbine. Also, these models should be coupled to create a whole plant simulation. In this research, four PWR power-generating modules and the attendant secondary-side plants were considered.

1.2.2 Steam Generator Level Controller

Steam generator level dynamics have been extensively studied [C-2,S-2, I-1]. In particular, there are inverse effects known as 'shrink and swell' that are important, especially during low power operation. Because the steam flowrate from each steam generator will differ in a multi-modular power plant, a digital non-linear controller is
needed to maintain the level in all generators within allowed tolerances regardless of the module's assigned power level. Thus, the requirements imposed on the controller developed here included:

i) A stable response,

ii) A fast, effective, and desirable response in counteracting external disturbances, and

iii) A robust response which implies a measure of controller tolerance to sensor uncertainties, actuator disturbances, and system parameters changes.

The satisfaction of these requirements ensures proper control performance under any circumstances.

1.2.3 Power Control System

Many combinations of modular powers are possible for a given turbine load because each module can be operated at a different fraction of the load. In order to allow each module to supply a desired fraction of the total plant load, the pressure balance between the steam generator and the main steam line header must be considered. The power control system's function is to divide the given turbine load into a fraction for each module and to maintain the balance among the power modules.

The power control system also ensures satisfactory control performance under any circumstance, including responses that are stable, fast and desirable, and robust as described in the previous section. It must do this under both steady-state and transient conditions.
1.3 Organization of This Report

This report is divided into five chapters. Chapter One provides introductory material and explains the relation between the research reported here and the overall objectives of the now decade-old MIT program on the advanced instrumentation and control of nuclear reactors. The main body of the work is contained in Chapters Two through Four. These chapters describe the simulation program (Chapter Two), the steam generator level controller (Chapter Three), and the power control system (Chapter Four). Chapter Five provides a summary as well as suggestions for further work.

Chapter Two describes the development of the simulation program for a PWR-type multi-modular power plant. First, a review is given of multi-modular plants designs including one for a PWR-type multi-modular arrangement. Next, the plant simulation model which includes models of the reactor kinetics, fuel heat transfer, the coolant loop system, the steam generator, the main steam line header, and various control systems is described. The general numerical solution method for this simulator is then presented. Finally, several evaluations of the developed simulation program are given.

Chapter Three describes the steam generator level controller. First, the design of U-tube steam generators, steam generator level dynamics, and existing analog control problems are reviewed. Next, the design of a digital, level controller that uses model-based compensators is presented. Finally, the performance of the proposed controller is shown over a wide range of operation including its use in a multi-modular power plant.

Chapter Four describes the power control system developed here for a PWR-type multi-modular power plant. Specifically, a multi-tiered supervisory control system is
developed based on knowledge of existing control systems for other types of power plants and on an understanding of the operating characteristics of multi-modular power plants. Evaluations are given of the proposed controller under a variety of normal operational transients.

Chapter Five summarizes the important results and conclusions of this research and also presents a list of specific recommendations for future research.
Chapter Two

Simulation Program Development

2.1 Introduction

2.1.1 General

One goal of this research was to develop a computer simulation model of a PWR-type multi-modular power plant for use in the study of normal operational transients. There were two objectives to this effort. One was to have a model for control studies where a real-time simulation capability is important. The other was to have a capability for accident analysis where accurate modeling is important. While the former was the main objective, the latter was also considered. Hence, the concern was that the plant simulation program be both an accurate and efficient means of calculating the time-dependent characteristics of plant parameters.
A nuclear power plant consists of many systems and a simulation program should describe all of the major ones including the reactor core, primary coolant system, steam generator, steam line header, turbine, and condenser. Generally, the dynamic behavior of these systems is nonlinear and very complicated. Also, there may be interactions between systems. In order to study the dynamic behavior of the plant, two different tasks must be addressed. First, the dynamic behavior of the plant should be modeled in terms of a set of nonlinear differential and/or algebraic equations that, upon solution, describe the dynamic or static behavior of the plant. Second, the mathematical equations should be solved properly. Because nonlinear differential and/or algebraic equations cannot, in general, be solved analytically, digital computer-based numerical solutions are required. The first of these tasks is the system simulation model development and the second is the numerical solution.

An integrated, whole-plant simulation program was particularly important here because analytic tools that simulate the entire plant are needed in order to design a control system. Simulation programs can be used both to provide physical information about plant operational transients and to evaluate the performance of a controller design. In addition, simulation programs can be used in a model-based digital controller, as well as for on-line diagnosis, signal validation, and operator support.

In this research, a simulation program for a multi-modular power plant is developed that can simulate up to four power modules. This program is designated as PMSIM, for PWR-type Multi-modular power plant SIMulation program. This chapter presents a summary of the governing equations for plant subsystem models and the corresponding numerical solution methods used in the simulation program. References to more detailed descriptions of the mathematical models are provided in the text.
Section 2.2 surveys multi-modular power plants including a PWR-type multi-modular one. Section 2.3 describes the plant simulation model which includes reactor kinetics, fuel heat transfer, the coolant loop system, the steam generator, the main steam line header, and various control systems. Section 2.4 outlines the general numerical solution method for the simulation code PMSIM. Several evaluation results are given in Section 2.5. A chapter summary is given in Section 2.6.

2.1.2 Review of Previous Work

There have been several efforts at MIT and elsewhere to develop PWR simulation programs. In the early 1980s, Strohmayer developed a dynamic simulation program for a vertical, U-tube steam generator [S-2]. This program uses one-dimensional conservation equations of mass, momentum, and energy and predicts the steam generator level dynamics, especially 'shrink and swell' effects. Choi modified this model to improve the simulation of steam generator level dynamics during low power operation and used it to design a model-based digital steam generator level controller [C-2].

In Korea, Auh has developed a PWR transient and accident simulation program that runs on a micro-computer [A-3]. This program includes a two-fluid pressurizer model, a boiling pot steam generator model, and a point-kinetic core model. At MIT, S.P. Kao developed a multiple-loop primary system model for a PWR power plant. Early whole plant simulation model [K-1] has since been extended to a version designated Pressurized Reactor Interactive Simulation Model (PRISM) [K-3]. PRISM includes neutron kinetics, heat transfer from fuel to coolant, a multiple-loop primary coolant system, a pressurizer, and a steam generator. In Japan, a real-time, accident-tracking PWR simulation program
has been developed to investigate small break loss-of-coolant accidents [G-1]. Cabral developed a three-dimensional, thermal-hydraulic simulation model for a reactor core [C-1]. This simulation program consisted of a three-dimensional heat transfer and fluid flow model of the reactor core with neutron kinetics, a primary coolant loop model, and steam generator models similar to those of S.P. Kao. P.W. Kao developed a three-dimensional neutronics model using analytical nodal methods [K-2]. Aviles combined the Cabral and P.W. Kao programs to create a space-dependent simulation program [A-2]. Because he focused on the plant's primary side, especially the reactor core, his simulation program does not simulate the secondary side except for a boiling pot steam generator model.

The previous MIT work was focused on PWR simulation for the purpose of conducting control studies. Because of the PWR emphasis, it was not possible to apply those models directly to multi-modular power plants. For example, Strohmayer and Choi did not address primary-side simulations and the others used a single pot steam generator simulation model which cannot describe variations in steam generator level. However, the subsystem models and solution methods developed previously at MIT are the basis of the PMSIM. As regards previous MIT work on the multi-modular power plants, Waltrip had developed a simple simulation program [W-2] for application to a multi-modular liquid metal cooled reactor. However, it could handle only one power module and therefore could not be used for the study of unbalanced load operation. Also, a simulation program for LMR-type multi-modular power plants is under development at ORNL [R-1]. It uses a parallel computer.
2.1.3 Boundaries for the Simulation Program Model

The primary objective in developing a multi-modular plant simulation program as part of this research was to use it for control studies. Because the plant control system should exhibit proper control performance under any normal operational circumstances, it is important that the control system designer understand plant behavior during these various operational transients. This type of information can be obtained through plant simulation. Another use of the simulation program in control studies is to evaluate the as-designed control system.

As mentioned in Chapter One, another objective of this research was to develop both a steam generator level and power control system for a PWR-type multi-modular power plant. Therefore, the simulation program developed here is focused on those control systems and hence its simulation capacity was limited to standard operational transients, especially normal power increases and decreases. This limitation meant that some simplifying assumptions were possible. These are explained in the following sections.

For the steam generator level controller, a detailed steam generator simulation model that can analyze a steam generator's complicated level dynamics including 'shrink and swell' effects is required. For the power control system, an integrated plant simulation model is needed to produce important plant variables such as reactor power, primary coolant temperature, and steam flowrate during the power increases and decreases. For both objectives, a main steam line common header simulation model is important because, in multi-modular plants, the thermal-hydraulic behavior of both the steam generator and the main steam line header determines the steam flowrate from individual steam generators. Multiple reactor core and primary coolant system simulation models are also needed in
order to replicate the effect of module inter-dependencies. Thus, each module in PMSIM consists of a neutron kinetics model, a model of the heat transfer from fuel to coolant, a one-dimensional primary coolant loop model, a steam generator model, and controller simulation model. Each of these module models is combined with the main steam line header model. A total of four modules are simulated.

Some items are not modeled. For example, models of instruments and measuring devices are not included. Instead it is assumed that plant variables are measured without any time delay. (Note: Measurement noise on the principal plant variables is considered.) Also excluded is the dynamics of the various actuators. Thus, it is assumed that control rod drives and feedwater regulating valves act perfectly and without delay. Finally, pressurizers, turbine-generator and condenser models are not included in this simulation program. Instead, steam flowrate from the main steamline header through the turbine control valves is specified on the basis of energy conservation. Temperature of feedwater flow is specified as a function of turbine power.
2.2 Multi-Modular Power Plant

2.2.1 LMR-Type Multi-Modular Power Plants

Interest in small inherently safe reactors has led to renewed study of liquid metal cooled reactors or LMRs. LMR type multi-modular power plants such as PRISM (Power Reactor Inherently Safe Module), proposed by General Electric Corporation, and SAFR (Sodium Advanced Fast Reactor), proposed by Rockwell International, have been conceptually designed [C-3]. The PRISM plant concept was subsequently selected by the U.S. Department of Energy (DOE) as the basis for an Advanced Liquid Metal Reactor [D-1]. A brief summary of both power plants is given here.

2.2.1.1 PRISM

PRISM is a concept currently under development by General Electric as a part of the innovative liquid metal reactor program. PRISM represents a typical multi-modular nuclear power plant design. It consists of three power blocks, each made of three identical reactors and steam generator modules.

A PRISM reactor is a single loop, pool-type fast breeder reactor with annular flow that produces heat for 135 MWe. Three such reactors and steam generators form a single power block with one turbine. Figure 2-1 is a diagram of PRISM. Primary coolant (low-pressure liquid sodium) circulates through the core to remove heat generated by the fission
Figure 2-1  PRISM Power Module [C-3]
process. The advantage of sodium is that it has excellent heat transfer characteristics. Its disadvantage is that it becomes activated by the neutron flux in the core and therefore may represent a hazard to personnel. In order to isolate the radioactive coolant, the sodium is pumped through an intermediate heat exchanger. Heat is transferred from the primary loop to the secondary loop by intermediate heat exchangers. A secondary sodium coolant loop then transports the thermal energy from the intermediate heat exchanger to a steam generator. Steam that is generated in each power module is supplied to the turbine through a common steam header. Feedwater to the three steam generators in the power block is supplied through a common feedwater header.

The PRISM modules are grouped so that they share major balance of plant components and thus capture the economies of scale of a larger plant without incurring the penalties associated with a larger core, such as the need for active emergency core cooling systems. A PRISM power block consists of three modules feeding a single turbine-generator. A 1205 MWe plant consists of three power blocks (nine modules). Figure 2-2 is a schematic of one of the power blocks. Each power block will be a complete and independent power plant.

2.2.1.2 SAFR

The SAFR plant concept employs multiple 330 MWe pool-type, sodium-cooled, fast reactors [D-1]. It was designed to achieve low cost, inherent safety, and favorable operating and maintenance characteristics. A typical 1320 MWe SAFR plant would consist of four separate 330 MWe SAFR 'power paks' with a 'pak' defined as the portion of the nuclear island that consists of one reactor assembly unit and its associated heat transfer and
auxiliary equipment as well as the buildings and structures needed to produce superheated steam. Thus, each SAFR module will have its own turbine-generator and will share only auxiliary systems. Figure 2-3 shows the SAFR power pak.

2.2.2 Gas-Cooled Multi-Modular Power Plants

Another multi-modular design is the Modular High Temperature Gas-Cooled Reactor (MHTGR). Two designs have been proposed, one jointly by Germany and GA Technologies and the other by the US. Department of Energy (DOE) [M-1,L-1]. Each power module is a pebble-bed reactor with four in-line steam generators located above or to the side of the reactor core. German pebble-bed core designs are graphite-reflected cylinders and are operated at 200 to 250 MWt per module. Figure 2-4 shows the MHTGR module designed by Germany and GA Technologies. Another possibility is the MHTGR that was designed under the U.S. DOE MHTGR Program. The DOE design is a 350-MWt annular core with a steam generator located below and to the side of the core. Figure 2-5 shows a typical schematic of the DOE design. In addition to these, MIT designed a direct-cycle gas turbine coupled MHTGR that gives high thermal efficiencies, up to ~45% [S-3]. Because it does not use a steam generator or secondary coolant loop, the MIT design has less potential for water ingress, less complex balance of plant, simpler operation and maintenance, and greater cost-effectiveness.

The MHTGR is a passively safe reactor with the design goal of essentially zero risk to the health and safety of the public. Hence, no evacuation planning zone is needed. The
Figure 2-3  Schematic of SAFR Power Pak and Reactor Core [C-3]
Figure 2-4  A German Company (HRB) and GA Technologies Designed MHTGR [L-1]
Figure 2-5  Simplified Flow Diagram of DOE Designed MHTGR [L-1]
key element in the MHTGR's passive safety concept is the fuel. The fuel is encapsulated in multiple layers of pyrolytic carbon (PyC) and silicon carbide (SiC) as shown in Figure 2-6. The fuel kernels are ~0.5 mm in diameter, and the coated particle < 1.0 mm in diameter. These particles will not release fission products at burnups of well over 100,000 MWd/t, and the fuel can withstand temperatures greater than 1600 °C without significant release of fission products.

![Diagram of Triso-Coated HTGR Fissile and Fertile Fuel Particles]

Figure 2-6   Triso-Coated HTGR Fissile and Fertile Fuel Particles [L-1]

### 2.2.3 PWR-Type Multi-Modular Power Plants

In this research, a multi-modular plant that consists of several PWR-type cores, coolant loops, and steam generators was studied. Figure 2-7 is a schematic diagram of
such a PWR-type multi-modular power plant. The control techniques under development at MIT for multi-modular reactors are intended to be generic. Therefore, the results of this study should be applicable to almost all multi-modular power plant designs.

Data from a typical 4-loop PWR plant were used here in the construction of a multi-modular power plant simulator. One change was necessary in the modeling of the core. Specifically, because a module’s power is only one fourth that of a typical 1100 MWe PWR, the core volume was treated by dividing it by four and placing it in four identical modules. Other general features of the model that should be noted are as follows. First, the momentum equation is not solved in the primary coolant system model because primary pressure and coolant flowrate remain nearly constant over the range of transients studied. Second, detailed geometry data for the reactor core and primary coolant loop were not required. For the fuel rod structure, PWR fuel rod geometry data were used. For the steam generators, Westinghouse type 'F' units were assumed. Typical plant and steam generator data were taken from Kao [K-3] and Choi [C-4] respectively. Table 2-1 lists many of the characteristic values used in the multi-modular power plant simulation program.

As described in the previous section, each module may be operated at a different load. Thus, the thermal hydraulic condition of each power module should be coordinated so as to maintain the desired steam fraction among the modules. The reactor coolant temperature of the highest-power module should follow a sliding temperature program which will be addressed in Section 4.3. This establishes the main steam header pressure to which the other modules must conform. As a result, the other modules will operate at different reactor coolant temperatures so as to establish the thermal-hydraulic conditions that result in the appropriate power fractions being assigned to each of them.
Figure 2-7  Schematic Diagram of PWR-Type Multi-Modular Power Plant.
## Table 2-1  Typical PWR-Type Multi-Modular Power Plant Parameters

1. **Plant**

   - Number of power modules: 4
   - Total power of multi-modular power plant (MWt): 3411
   - Power of individual module (MWt): 853
   - Heat generated in the fuel (%): 97.0
   - Heat generated in the moderator (%): 2.6
   - Primary heat per pump (MWt): 4

2. **Core**

   - Volume of coolant (m³): 17.33
   - Mass of fuel (Mg): 25.25
   - Mass of cladding (Mg): 5.775

3. **Primary Coolant System**

   - System pressure (MPa): 15.5
   - Coolant flowrate (Mg/s): 4.473
   - Upper plenum volume (m³): 41.69
   - Inlet plenum volume (m³): 10.14
   - Downcomer volume (m³): 19.61
   - Lower plenum volume (m³): 23.7
   - Hot leg volume (m³): 3.07
   - Suction leg volume (m³): 5.13
   - Cold leg volume (m³): 2.57
4. **Fuel Rods**

   Total number: 12738
   Fuel material: \text{UO}_2
   Density (% of Theoretical \text{UO}_2 density): 95
   Pellet diameter (mm): 8.2
   Cladding inside diameter (mm): 8.36
   Rod outside diameter (mm): 9.5
   Rod height (m): 3.65
   Total heat transfer area (m$^2$): 1386.5
   Gap heat transfer coefficient (W/m$^2$ K): 5678

5. **Reactivity Parameters**

   Doppler temperature coefficient (pcm/K): -5.2 \sim -1.8
   Moderator temperature coefficient (pcm/K): 0 \sim -63
   Boron reactivity coefficient (pcm/ppm)): -12.5 \sim -7.5
   Delayed neutron fraction: 0.0075
   Prompt neutron life time (\mu s): 19.4

6. **Steam Generator**

   Type: U-tube
   Full load pressure (MPa): 6.89
   Heat transfer area (m$^2$): 5110
   Primary side flow area (m$^2$): 1.05
   Tube outside diameter (mm): 17.48
   Tube inside diameter (mm): 15.44
   Tube metal mass (Mg): 39.69
2.3 Simulation Model

2.3.1 Neutron Kinetics Model

2.3.1.1 Point Kinetics Equations

A complete description of the neutron flux is generally complicated and often not necessary. In particular, a simplification can be made by separating the time-dependent behavior of the neutron population from its behavior in space and energy. Specifically, a point-kinetics model is often sufficient to simulate reactor power for control studies. The point-kinetics equations can be derived from the fundamental Boltzmann neutron transport equation and have the following form [H-2]:

\[
\frac{d}{dt} T(t) = \frac{\rho(t) - \beta}{\Lambda} T(t) + \sum_{i=1}^{I} \lambda_i C_i(t) \tag{2-1}
\]

\[
\frac{d}{dt} C_i(t) = \frac{\beta_i}{\Lambda} T(t) - \lambda_i C_i(t), \quad i = 1, 2, ..., I \tag{2-2}
\]

where \( T \) is the amplitude function and is a weighted integral of all neutrons in the core, \( \rho \) is the net reactivity, \( \beta_i \) is the fractional yield of the \( i \)th precursor group, \( \beta \) is total delayed neutron fraction, \( C_i \) is the concentration of the \( i \)th precursor group, \( \lambda_i \) is the decay constant of the \( i \)th precursor group, \( \Lambda \) is the prompt neutron generation time, and \( I \) is the number of delayed neutron precursor groups.
In principle, all coefficients in Equations (2-1) and (2-2) could be time-dependent. However, as a practical matter, it can be assumed that only the reactivity, \( \rho(t) \), varies with time. Therefore, Equations (2-1) and (2-2) represent I+1 unknowns and I+1 equations with a time-varying reactivity for an input. Because the neutron population is directly related to reactor power by a constant that represents the fission rate (i.e., energy production by the fission reaction), the amplitude function in Equations (2-1) and (2-2) can be replaced by the reactor power.

2.3.1.2 Reactivity

The total reactivity present in a reactor is produced from several different mechanisms and includes the following:

i) Control Reactivity: In a PWR, there are two types of control mechanisms, control rods and soluble boron. These are the actuators that are adjusted as part of control actions chosen to alter the reactor power. Movement of the control rods can be done more rapidly than can adjustment of the soluble boron concentration. Hence, rod movements are used here to initiate transients.

ii) Feedback Reactivity: Neutron levels affect heat and xenon production. Consequently fuel and moderator temperature changes and variations in xenon concentration affect reactivity.

Each of these reactivities can be added linearly as shown by the following expression:
\[ \rho = \Delta \rho_{\text{rod}} + \Delta \rho_f + \Delta \rho_m + \Delta \rho_{\text{Xe}} + \Delta \rho_B \] (2-3)

where \( \Delta \rho_{\text{rod}} \) is the control rod reactivity, \( \Delta \rho_f \) and \( \Delta \rho_m \) are the fuel and moderator feedback reactivities respectively, \( \Delta \rho_{\text{Xe}} \) is the xenon reactivity, and \( \Delta \rho_B \) is the soluble boron reactivity. \( \Delta \) means difference between current value of each variable and its initial value.

Because control rod reactivity depends on the rod's position, a predetermined reactivity curve is incorporated in the model. A detailed, space-dependent reactor physics code was used to generate this relation from a typical PWR. The resulting data was fitted to a parabolic equation. Figure 2-8 shows the reactivity curve as a function of equivalent rod position.

In power reactors, fuel and coolant feedback reactivity changes are determined by using temperature coefficients. These are defined as the ratio of the total reactivity change to the fuel (or coolant) temperature change. Thus,

\[ \Delta \rho_f = \int_{T_{fo}}^{T} \alpha_{Tf}(T) \, dT \] (2-4)

\[ \Delta \rho_m = \int_{T_{mo}}^{T} \alpha_{Tm}(T) \, dT \] (2-5)

where \( \alpha_{Tf} \) and \( \alpha_{Tm} \) are the fuel and moderator temperature coefficients and \( T_{fo} \) and \( T_{mo} \) are the fuel and moderator temperature, respectively. As shown in the above equations, the
Figure 2-8  Control Rod Worth Versus Control Rod Position [C-5]

Figure 2-9  Fuel Temperature Reactivity Coefficient [C-5]
Figure 2-10  Moderator Temperature Reactivity Coefficient [C-5]

Figure 2-11  Dissolved Boron Reactivity Coefficient [N-1]
fuel temperature coefficient is a function of the fuel temperature. Similarly, the moderator temperature coefficient depends on both the moderator temperature and the boron concentration in the moderator. Typical PWR fuel and moderator temperature coefficients, shown in Figures 2-9 and 2-10, were fitted to a polynomial form and used in the simulation program.

Boron reactivity can be calculated from the measured boron concentration and a 'dissolved boron reactivity' coefficient. A typical dissolved boron reactivity coefficient is shown in Figure 2-11. Feedback reactivity resulting from changes in xenon concentration was included in the simulation.

2.3.2 Fuel Average Temperature and Thermal Power Calculation Model

The fuel average temperature was calculated in order to estimate both the fuel temperature feedback reactivity and the thermal power transferred from the fuel rods to the bulk coolant. In a typical PWR primary coolant system, the thermal transport path proceeds from a point of fission energy deposition within solid fuel, through layers of fuel, through gas at the interface of the fuel and cladding, and then through the cladding to the interface with the light-water coolant. In order to simulate these processes, a doubly-lumped-parameter model was adopted as shown in Figure 2-12 [L-3]. The thermal energy balance equations of the fuel and cladding regions were written as:
Figure 2-12  Typical Fuel Rod Cross Section
i) **Fuel Region:**

\[ M_f C_f \frac{d\overline{T}_f}{dt} = \dot{Q} - \dot{Q}_g \]  

(2-6)

where  \( M_f \), is the fuel mass,

\( C_f \), is the specific heat capacity of the fuel region,

\( \overline{T}_f \), is the average fuel temperature,

\( \dot{Q} \), is the heat generation rate within the fuel region, and

\( \dot{Q}_g \), is the heat transfer rate from fuel to cladding.

ii) **Cladding Region:**

\[ M_{cl} C_{cl} \frac{d\overline{T}_{cl}}{dt} = \dot{Q}_g - \dot{Q}_{th} \]  

(2-7)

where  \( M_{cl} \), is the cladding mass,

\( C_{cl} \), is the specific heat capacity of the cladding region,

\( \overline{T}_{cl} \), is the average cladding temperature, and

\( \dot{Q}_{th} \), is the heat transfer rate from the cladding to coolant.

To solve Equations (2-6) and (2-7), it is assumed that heat transfer coefficients derived from steady-state relationships between the radial average temperatures of fuel, cladding,
and coolant are maintained during transients. These heat transfer coefficients, which are designated as $R_g$ and $R_c$, are defined as:

$$\dot{Q}_g = \frac{NL}{R_g}(\overline{T_f} - \overline{T_{cl}})$$

$$\dot{Q}_{th} = \frac{NL}{R_c}(\overline{T_{cl}} - T_c)$$

where $N$ is the total number of fuel rods and $L$ is the length of a fuel rod. $R_g$ and $R_c$ can then be expressed as:

$$R_g = \frac{1}{8\pi k_f} + \frac{1}{2\pi a h_g} + \frac{1}{2\pi k_{cl}} \left\{ \frac{c^2}{c^2 - b^2} \ln\left(\frac{c}{b}\right) - \frac{1}{2} \right\}$$

$$R_c = \frac{1}{2\pi k_{cl}} \left\{ \frac{1}{2} - \frac{c^2}{c^2 - b^2} \ln\left(\frac{c}{b}\right) \right\} + \frac{1}{2\pi h_c}$$

where $k_f$ and $k_{cl}$ are the thermal conductivities of fuel and cladding, respectively; $a$ is the outer radius of the fuel pellet; and $b$ and $c$ are the inner and outer radii of the cladding, respectively. $h_g$ and $h_c$ are heat transfer coefficients in the gap and fuel rod outer surface, respectively.
2.3.3 Primary Coolant System Simulation Model

The primary coolant system transports heat deposited in the fuel to the heat exchanger in which steam is generated. The characteristics of the reactor coolant system strongly depend on the reactor power and conditions in the steam generator secondary. The heat transport path from within the fuel to the coolant was described in section 2.3.2.

The model of the secondary side of the steam generator will be explained in section 2.3.4. In the control study, a major concern with the primary coolant system model is that time delays associated with heat transfer and fluid transport be accurately represented. S.P. Kao developed a one-dimensional primary loop model for a typical multi-loop PWR plant simulation [K-1]. Cabral also developed a simple one-dimensional primary loop model that solves only the mass and energy equations [C-1]. These primary loop models were modified for the multi-modular coolant system model that is described here.

The number of control volumes in each primary coolant system was minimized so as to represent only the major components. These were the reactor core, the reactor vessel upper plenum, the hot leg and steam generator inlet plenum, the steam generator tube bundle, the steam generator outlet plenum and the cold leg, and the reactor vessel downcomer region and the lower plenum. Figure 2-13 shows the control volumes assigned in the primary coolant loop model. Because the pressure of the primary coolant does not change significantly during operational transients, pressurizer operation does not affect the primary coolant temperature. Therefore, instead of including a pressurizer model, the primary system pressure is given as a user-specified boundary condition. Similarly, a momentum balance equation was not needed and changes in the primary coolant flowrate are input as a boundary condition. The primary coolant pump in the cold
Figure 2-13  Control Volume of Primary Coolant Loop
leg is modeled as an energy source in the primary coolant. Pump dynamics are not considered and the pump power input is assumed to be constant.

A typical control volume of the primary coolant loop is shown in Figure 2-14. A donor cell method is used and the 'mixing-cup' enthalpy, \( h_i \), is defined in terms of the total mass and energy content inside the \( i \)th control volume cell. The governing equations for a given control volume are those of mass and energy conservation, respectively. These are:

i) **Mass Conservation**:

\[
\frac{dM_i}{dt} = \dot{m}_i - \dot{m}_{i-1}
\]  

(2-12)

ii) **Energy Conservation**:

\[
\frac{dU_i}{dt} = (\dot{m}h)_i - (\dot{m}h)_{i-1} + \dot{Q}_i
\]  

(2-13)

where \( M_i \) is the total mass of the primary coolant in the \( i \)th control volume,

\( \dot{m}_i \) is the mass flowrate leaving the \( i \)th control volume,

\( U_i \) is the total internal energy of the primary coolant in the \( i \)th control volume,

\( h_i \) is the mixing cup enthalpy leaving the \( i \)th control volume, and

\( \dot{Q}_i \) is the heat input rate for the \( i \)th control volume.
Strohmayer suggested that the mass and energy equations could be combined because the mass flowrate through the primary coolant system is approximately uniform during normal operational transients. Therefore, a single mass flowrate in the primary coolant loop was assumed and treatment of the mass conservation equation was not needed. This assumption is valid for transients in which the temporal variation in the control volume’s mass is too small to affect the spatial mass flowrate distribution. A further assumption, and a very valid one, is that the coolant in the primary system is always single phase under operational conditions. Any vapor that might be produced in the hottest channel of the core would be condensed in the reactor vessel upper plenum before traveling into the hot leg. Under the above assumptions, Equations (2-12) and (2-13) can be expressed as follows:

\[ V_i \rho_i \frac{du_i}{dt} = m_i (h_i - h_{i-1}) + \dot{Q}_i \]

(2-14)
where $V_i$ is the $i$th control volume, $\rho_i$ is the coolant density of the $i$th control volume, $u_i$ is the specific internal energy of the $i$th control volume, and $\dot{m}$ is the primary coolant system flowrate.

Internal energy ($u$) is related to the enthalpy ($h$), pressure ($p$), and density ($\rho$) by the relation:

$$u = h - \frac{p}{\rho}.$$  \hspace{1cm} (2-15)

Thus, for constant density, the temporal variation of internal energy can be written as:

$$\rho \frac{du}{dt} = \rho \frac{dh}{dt} - \frac{dp}{dt}.$$  \hspace{1cm} (2-16)

For slow pressure changes, the temporal variation of the pressure can be neglected. Also, it is assumed that the temporal variation of the static enthalpy of the $i$th control volume equals that of the mixing cup enthalpy. Under these assumptions, the combined mass and energy equation can be expressed as follows:

$$M_i \frac{dh_i}{dt} = \dot{m}(h_i - h_{i-1}) + \dot{Q}_i$$  \hspace{1cm} (2-17)

where $h_i$ does not represent the average enthalpy of the $i$th control volume, but rather the mixing cup enthalpy of the coolant leaving that volume.
2.3.4 Steam Generator Secondary Side Model

U-tube steam generator water level dynamics are complex. In particular, there are counterintuitive effects known as 'shrink' and 'swell.' For example, upon increasing steam flowrate from a generator, the void fraction in the tube bundle region will increase. This will cause a temporary rise in level in the downcomer region and hence give the false impression that mass inventory is actually increasing. Accordingly, a detailed steam generator model is required. For this purpose, an existing steam generator model, that developed by Strohmayer [S-2] and improved by Choi [C-2], was adopted for the steam generator secondary side simulation.

This model is low-order, non-linear, and fast-running. Two salient features of the model are the incorporation of an integrated secondary recirculation loop momentum equation and retention of all non-linear effects. This model has been validated over a wide range of steady-state and transient conditions by comparing results calculated with the model to experimental data or to other calculated results. Choi modified this model to improve simulation of the shrink and swell effects. The modified model uses a different specific volume profile in the tube bundle region. The original steam generator model and the modified model are both described in more detail in reference [C-2].

In this model, the steam generator secondary side is divided into four regions. These are the tube bundle region, the riser region, and the steam dome-downcomer region (which in turn is divided into a saturated and a subcooled volume). Figure 2-15 shows the steam generator secondary side regions. The steam generator secondary side equations consist of mass and energy conservation relations for each steam generator region and
Figure 2-15  Steam Generator Secondary Side Simulation Model [S-2]
momentum conservation relations for the recirculation loop. The system equations were expressed as six, first-order, coupled, differential equations of the form:

\[
\dot{x} = f(x, \dot{Q}_{sg}, \dot{m}_s, \dot{m}_{fw}, T_{fw})
\]  

(2-18)

where \( x \) is a state vector whose elements are \( U_0, V_v, <\alpha_r>, <\alpha_n>, P_{sg}, \) and \( \dot{m}_r, \)

\( U_0 \) is the internal energy of the steam dome downcomer,

\( V_v \) is the void volume in the steam dome downcomer,

\( <\alpha_r> \) is the void fraction at the riser exit,

\( <\alpha_n> \) is the void fraction at the tube bundle exit,

\( P_{sg} \) is the saturation pressure inside the steam generator,

\( \dot{m}_r \) is the recirculation flowrate at the steam generator,

\( \dot{m}_s \) is the steam flowrate,

\( \dot{m}_{fw} \) is the feedwater flowrate,

\( T_{fw} \) is the feedwater temperature, and

\( \dot{Q}_{sg} \) is the heat transfer rate from the primary to secondary side.

In order to increase the thermal efficiencies and to reduce the level inverse response, which will be explained in Chapter Three, the feedwater is often preheated by the steam extracted from the turbine. Thus, the feedwater temperature changes as a function of plant power. In this research, it is assumed that the feedwater temperature is a known function
of the total plant power. Figure 2-16 shows the feedwater temperature as a function of a plant power.

![Graph showing feedwater temperature as a function of module power.](image)

**Figure 2-16** Feedwater Temperature as Function of Power.

### 2.3.5 Main Steam Line Common Header Model

The main steam line common header (MSLCH) receives steam from each power module and discharges to the turbine. Because the steam flowrate from each steam generator to the MSLCH depends on the hydraulic conditions that exist between that power module and the MSLCH, the MSLCH model must include momentum conservation
equations. Figure 2-17 shows the simplified MSLCH simulation model. The mass, energy, and momentum conservation equations that are solved consist of:

i) **Mass Conservation Equation:**

\[
\frac{dM_{ms}}{dt} = \sum_{i=1}^{NM} \dot{m}_{s,i} - \dot{m}_{ms}
\]  \hspace{1cm} (2-19)

where \(M_{ms}\) is the steam mass in the MSLCH,

\(\dot{m}_{s,i}\) is the steam flowrate from the steam generator of the \(i\)th power module,

\(NM\) is the number of power modules, and

\(\dot{m}_{ms}\) is the steam flowrate from the MSLCH to the turbine.

In Equation (2-19), the total mass in the MSLCH can be replaced by a product of volume and density. Thus,

\[
V_{ms} \frac{d\rho_{ms}}{dt} = \sum_{i=1}^{NM} \dot{m}_{s,i} - \dot{m}_{ms}
\]  \hspace{1cm} (2-20)

where \(\rho_{ms}\) is the density of the steam and moisture mixture in the MSLCH. It is calculated as follows:

\[
\frac{1}{\rho_{ms}} = V_f + X_{ms} V_{fg}
\]  \hspace{1cm} (2-21)
Figure 2-17  MSLCH Simulation Model
where $X_{ms}$ is the steam quality, $V_f$ is the specific volume of liquid, and $V_{fg}$ is the increase in specific volume upon evaporation. In the simulation model, steam quality is greater than 1.0 if steam is superheated.

For the state variable method,

$$
\frac{dp_{ms}}{dt} = \frac{\partial p_{ms}}{\partial p_{ms}} \frac{dp_{ms}}{dt} + \frac{\partial p_{ms}}{\partial X_{ms}} \frac{dX_{ms}}{dt}
$$

(2-22)

where

$$
\frac{\partial p_{ms}}{\partial p_{ms}} = -\frac{1}{\rho_{ms}} \left( \frac{\partial V_f}{\partial p_{ms}} + X_{ms} \frac{\partial V_{fg}}{\partial p_{ms}} \right) \quad \text{and}
$$

(2-23)

$$
\frac{\partial p_{ms}}{\partial X_{ms}} = -\frac{1}{\rho_{ms}^2} V_{fg}.
$$

(2-24)

Substitution of Equation (2-22) into Equation (2-20) yields the final mass conservation equation:

$$
V_{ms} \left( \frac{\partial p_{ms}}{\partial p_{ms}} \frac{dp_{ms}}{dt} + \frac{\partial p_{ms}}{\partial X_{ms}} \frac{dX_{ms}}{dt} \right) = \sum_{i=1}^{NM} \dot{m}_{s,i} - \dot{m}_{ms}.
$$

(2-25)
ii) **Energy Conservation Equation:**

\[
\frac{dU_{ms}}{dt} = \sum_{i=1}^{NM} \dot{m}_{s,i} h_{sg,i} - \dot{m}_{ms} h_{ms} \tag{2-26}
\]

where \( U_{ms} \) is the internal energy of steam in the MSLCH,

\( h_{sg,i} \) is the enthalpy of the steam flowrate from the steam generator of the \( i \)th power module, and

\( h_{ms} \) is the enthalpy of steam flowrate to the turbine.

Similarly, the internal energy of the steam in the MSLCH can be represented by:

\[
U_{ms} = M_{ms} h_{ms} - V_{ms} P_{ms} \tag{2-27}
\]

where \( h_{ms} = h_f + X_{ms} h_{fg} \).

Insertion of Equation (2-27) into Equation (2-26) yields:

\[
M_{ms} \frac{dh_{ms}}{dt} - V_{ms} \frac{dP_{ms}}{dt} = \sum_{i=1}^{NM} \dot{m}_{s,i} \left(h_{sg,i} - h_{ms}\right) \tag{2-28}
\]

where

\[
\frac{dh_{ms}}{dt} = \frac{\partial h_{ms}}{\partial P_{ms}} \frac{dP_{ms}}{dt} + \frac{\partial h_{ms}}{\partial X_{ms}} \frac{dX_{ms}}{dt}, \tag{2-29}
\]
\[ \frac{\partial h_{ms}}{\partial P_{ms}} = \frac{\partial h_{ms}}{\partial P_{ms}} + X_{ms} \frac{\partial h_{ms}}{\partial P_{ms}} , \text{ and} \quad (2-30) \]

\[ \frac{\partial h_{ms}}{\partial X_{ms}} = h_{fg} . \quad (2-31) \]

Substitution of Equation (2-29) into Equation (2-28) yields an energy conservation equation of the form:

\[ M_{ms} \left( \frac{\partial h_{ms}}{\partial P_{ms}} \frac{dP_{ms}}{dt} + \frac{\partial h_{ms}}{\partial X_{ms}} \frac{dX_{ms}}{dt} \right) - V_{ms} \frac{dP_{ms}}{dt} = \sum_{i=1}^{NM} \dot{m}_{s,i} (h_{sg,i} - h_{ms}) . \quad (2-32) \]

The momentum conservation equation through the steam line of the \textit{i}th power module is:

\[ \left( \frac{L}{A_i} \right) \frac{d\dot{m}_{s,i}}{dt} = P_{sg,i} - P_{ms} - F_i \quad i = 1, 2, ..., NM \quad (2-33) \]

where \[ \left( \frac{L}{A_i} \right) \] is a geometric parameter that relates the steam flowrate to the inertia of the steam in the \textit{i}th power module,

\[ P_{sg,i} \] is the pressure inside the steam generator of the \textit{i}th power module,

\[ P_{ms} \] is the pressure at the MSLCH, and
\( F_i \) is the resistance through the steam line of the \( i \)th power module.

The line from each steam generator to the main steam header is made of 30" pipe. Each steam line contains components that cause pressure losses including several valves, a restrictor, and several curvatures. It was assumed that friction pressure losses were negligible when compared to those through these components. Also, while each line may be different in its length and structure, these differences were not considered. Thus, the resistance to flow through the steam line of the \( i \)th power module can be expressed as:

\[
F_i = \frac{f_r}{2\rho_{ms}} \frac{\dot{m}_{s,i}^2}{A_s^2} + \frac{1}{\rho_{ms}} \frac{1}{\rho_{sg,i}} \frac{\dot{m}_{s,i}^2}{A_s^2}
\]  

(2-34)

where \( f_r \) is the flow resistance (K-loss), \( A_s \) is the cross-sectional area of the steam line, and \( \rho_{sg,i} \) is the steam density in the steam generator of the \( i \)th power module.

The state equations of the MSLCH consist of mass, energy, and momentum relations. The state variables are the pressure and quality at the MSLCH and the steam flowrate from each power module. Equations (2-25), (2-32), and (2-33) can be expressed in matrix form as:

\[
A\dot{x} = f(x, P_{sg,i}, \dot{m}_{ms})
\]

(2-35)

where

\[
x = [P_{ms}, X_{ms}, \dot{m}_{s,i}]^T \quad i = 1,2,\ldots, \text{NM}
\]

(2-36)
\[
A = \begin{bmatrix}
A_1 & O_1 \\
O_2 & A_2
\end{bmatrix}
\] (2-37)

\[
A_1 = \begin{bmatrix}
V_{\ms} \frac{\partial p_{\ms}}{\partial p_{\ms}} & V_{\ms} \frac{\partial p_{\ms}}{\partial x_{\ms}} \\
-V_{\ms} + M_{\ms} \frac{\partial h_{\ms}}{\partial p_{\ms}} & M_{\ms} \frac{\partial h_{\ms}}{\partial x_{\ms}}
\end{bmatrix}
\] (2-38)

\[
A_2 = \text{diagonal} \begin{bmatrix}
\left( \frac{L}{A_1} \right) \\
\end{bmatrix}
\quad i = 1, 2, \ldots, \text{NM}
\] (2-39)

\[O_1\] is a 2 \times \text{NM} zero matrix,

\[O_2\] is an \text{NM} \times 2 zero matrix, and

\[f\] is a super matrix whose element matrices are \(f_1\) and \(f_2\). The former is defined as:

\[
f_1 = \begin{bmatrix}
\sum_{i=1}^{\text{NM}} \hat{m}_{s,i} - \hat{m}_{\ms} \\
\sum_{i=1}^{\text{NM}} \hat{m}_{s,i} (h_{sg,i} - h_{\ms})
\end{bmatrix}
\] (2-40)

and

the latter (\(f_2\)) is an \text{NM} \times 1 matrix whose elements are:

\[
f_{2,i} = P_{\ms} - P_{\ms} - F_i, \quad i = 1, 2, \ldots, \text{NM}
\] (2-41)
2.3.6 Controller Simulation Model

There are many control systems involved in the plant dynamics. For example, these include the following:

- Reactor power control system,
- Primary coolant pressure control system,
- Steam bypass control system,
- Turbine control system,
- Feedwater control system including steam generator level control system, and
- Chemical and volume control system.

In addition to these, there are many subsystem control systems. In order to simulate plant operational transients exactly, each and every control system should be properly modeled. However, in reality, it is difficult to include all of these control system models in the simulation program and it is not necessary for power system and steam generator level control studies. In this research, the reactor power control system and steam generator level control system are considered. It was assumed that the other control systems functioned perfectly. Both more detailed descriptions and simulation models of these two control systems are given in Chapters Three and Four respectively.
2.4 Numerical Solution Method

2.4.1 Finite Difference Approximation to System Equations

2.4.1.1 Neutron Kinetics Model

In order to solve the point kinetics equations without undue numerical stability restrictions, an implicit procedure was employed. Several such methods exist including the stiffness confinement method [C-6] and the omega method [P-1]. However, in this simulation program, a method used by Cabral [C-1] was adopted because it could be executed quickly and because it gave a solution that was sufficiently accurate for simulation and control studies.

Equations (2-1) and (2-2) are transformed into finite difference equations through the use of first-order backwards differences. Thus,

\[
\frac{T^n - T^{n-1}}{\Delta t} = \frac{\rho^n - \beta}{\Lambda} T^n + \sum_{i=1}^{I} \lambda_i C_i^n
\]

(2-44)

\[
\frac{C^n - C^{n-1}}{\Delta t} = \frac{\beta}{\Lambda} T^n - \lambda_i C_i^n \quad i = 1, 2, \ldots, I
\]

(2-45)

where the superscript \( n \) denotes the time step and \( \Delta t \) is the time step size. In Equations (2-44) and (2-45), all state variables except the one which forms the time difference are
unknown at the current time step. Therefore, these equations define a set of linear equations in \( I + 1 \) unknowns. These equations are of the form:

\[
\frac{x^n - x^{n-1}}{\Delta t} = f(x^{n-1}, \rho^n)
\]  

(2-46)

where \( x^n \) is a column vector of the neutron power and the precursor concentrations at each time step. The reactivity at the current time step is determined from the power demand and the reactor state variables at that same time step. Thus,

\[
\rho^n = f(T^n_f, T^n_c, C^n_B, X^n_e, I^n)
\]  

(2-47)

where \( T^n_f \) and \( T^n_c \) represent the fuel and coolant average temperatures, \( C^n_B \) and \( X^n_e \) are the boron and xenon concentrations, and \( I^n \) is the control reactivity at the current time step.

### 2.4.1.2 Fuel Average Temperature and Thermal Power Calculation Model

The heat conduction equations, Equations (2-6) and (2-7), are solved explicitly by applying first-order forward differences to the time derivatives. Thus,

\[
M_f C_f \frac{(T^n_f - T^{n-1}_f)}{\Delta t} = Q^{n-1} - \frac{N}{R_f} (T^n_f - T^{n-1}_{cl})
\]  

(2-48)
\[ M_{cl}C_{cl}\left(\frac{T^n_{cl} - T^{n-1}_{cl}}{\Delta t}\right) = \frac{NL}{R_g}(T^n_{f} - T^{n-1}_{cl}) - \frac{NL}{R_c}(T^n_{cl} - T^{n-1}_{c}) \]  

(2-49)

Equations (2-48) and (2-49) form a linear equation set in two unknown variables, the average fuel and cladding temperature. Thus, they can be written as:

\[ \frac{x^n - x^{n-1}}{\Delta t} = f(x^{n-1}, T^{n-1}_{c}, Q^{n-1}) \]  

(2-50)

where \( x^n \) is a column vector of the average fuel and cladding temperatures at the current time step, \( T^{n-1}_{c} \) is the coolant average temperature at the previous time step, and \( Q^{n-1} \) is the neutron power at the previous time step. The property correlations for the fuel and cladding materials as a function of fuel and cladding temperatures are taken from the THERMIT program which was developed at MIT [K-4].

2.4.1.3 Primary Coolant System Simulation Model

The difference equation set for the primary coolant system is obtained by specifying a mass-energy relation of the form of Equation (2-17) for each control volume. Explicit finite difference equations are then obtained by approximating the time derivatives in those equations as first-order backwards differences. Thus, we obtain the following for each of the nodes in the model:
Core:

\[ M_c \left( \frac{h_c^n - h_c^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{lp}^n - h_c^{n-1} \right) + \dot{Q}_{th}^{n-1} \]  
(2-51(a))

Reactor Vessel Upper Plenum:

\[ M_{up} \left( \frac{h_{up}^n - h_{up}^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{c}^{n-1} - h_{up}^{n-1} \right) \]  
(2-51(b))

Hot Leg and Steam Generator Inlet Plenum:

\[ M_{hl} \left( \frac{h_{hl}^n - h_{hl}^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{up}^{n-1} - h_{hl}^{n-1} \right) \]  
(2-51(c))

Steam Generator Tube (Primary Side):

\[ M_{sgp} \left( \frac{h_{sgp}^n - h_{sgp}^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{hl}^{n-1} - h_{sgp}^{n-1} \right) - \dot{Q}_{sg}^{n-1} \]  
(2-51(d))

Cold Leg and Steam Generator Outlet Plenum:

\[ M_{cl} \left( \frac{h_{cl}^n - h_{cl}^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{sgp}^{n-1} - h_{cl}^{n-1} \right) + \dot{Q}_{pump} \]  
(2-51(e))
Reactor Vessel Lower Plenum:

\[
M_{lp} \left( \frac{h_{lp}^n - h_{lp}^{n-1}}{\Delta t} \right) = \dot{m} \left( h_{cl}^{n-1} - h_{lp}^{n-1} \right) \tag{2-51(f)}
\]

where following subscript nomenclature is used:

- \( c \) denotes reactor core,
- \( up \) denotes reactor vessel upper plenum,
- \( hl \) denotes hot leg,
- \( sgp \) denotes steam generator primary side coolant,
- \( cl \) denotes cold leg, and
- \( lp \) denotes reactor vessel lower plenum.

\( \dot{Q}_{pump} \) is the energy transferred to the coolant by the coolant pump and it is assumed to be a known constant at all times. \( \dot{Q}_{sg}^{n-1} \) denotes the heat transfer rate from primary to the secondary side of the steam generator at the previous time step. Therefore, Equations (51(a)–51(f)) form a linear equation set for six unknowns, the enthalpies of each control volume. Thus,

\[
\frac{x^n - x^{n-1}}{\Delta t} = f\left(x^{n-1}, \dot{Q}_{th}^{n-1}, \dot{Q}_{sg}^{n-1}\right) \tag{2-52}
\]

In order to solve Equation (2-52), the heat transfer rate from the primary to the secondary side of the steam generator must be known. In order to do this, Strohmayer's heat transfer model was adopted [S-2]. In this model, the heat transfer rate to secondary side is a
function of the log mean temperature difference between the primary coolant and the secondary saturated temperature. Thus,

\[ \dot{Q}_{sg} = f(T_{sg\text{pin}}, T_{sg\text{pout}}, T_{sgs}) \]  

(2-53)

where \( T_{sg\text{pin}} \) and \( T_{sg\text{pout}} \) are the primary coolant temperatures at the steam generator tube inlet and outlet respectively, and \( T_{sgs} \) is the saturated temperature at the steam generator secondary side.

### 2.4.1.4 Steam Generator Secondary Side Model

Finite difference equations are obtained from Equation (2-18) by applying first order forward finite differences to the time derivatives. Thus,

\[ \frac{x^n - x^{n-1}}{\Delta t} = f(x^{n-1}, \dot{Q}_{sg}^{n-1}, m_s^{n-1}, m_{fw}^{n-1}, T_{fw}^{n-1}) \]  

(2-54)

where \( x^n \) is a column vector whose elements are the internal energy at the bottom of the steam dome downcomer, the void volume in the steam dome downcomer, the void fraction at the tube bundle exit, the void fraction at the riser exit, the saturation pressure inside the steam generator, and the recirculation flowrate in the steam generator. Each of these variables is at the current time step.
2.4.1.5 Main Steam Line Common Header Model

The finite difference version of Equation (2-35) is:

\[ A^{n-1} \left( x^n - x^{n-1} \right) / \Delta t = f(x^{n-1}, P_{sg,i}^{n-1}, \dot{m}_{ms}^{n}) \]  \hspace{1cm} (2-55)

Equation (2-55) is a linear equation set in NM+2 unknown variables. These are the pressure and quality at the MSLCH and the steam flowrates from the power modules. The steam flowrate from the MSLCH at the current time step is calculated from the current load demand.

2.4.2 Numerical Solution Procedure

In section 2.4.1, four subsets of finite difference equations were obtained for each power module and one subset was obtained for the MSLCH. Figure 2-18 shows both the subsets of the finite difference equations and the information flow among them. The whole plant can be now simulated by specifying the demanded load and a controller logic. In this section, both steady-state and transient solution procedures are explained.
Figure 2-18  Relation among the System Submodels.
2.4.2.1 Steady-State Simulation

Initialization of the model requires that all module powers and coolant average temperatures for the module that is operating at the highest power level be specified. All other state variables can then be determined as described below.

Figure 2-19 shows the flowchart for the steady-state solution procedure. First, the input data is read and all geometries, variables, and arrays are initialized. Second, steady-state system equations are solved. For the primary and secondary reactor coolant systems, a special procedure is required to allow each power module to operate at a different load. Figure 2-20 shows the procedure used to solve for the steady-state, thermal-hydraulic state variables:

i) Solve the primary loop mass-energy equation for the highest-power module using the reactor power and coolant average temperature.

ii) Estimate the steam generator pressure in the highest-power module using the heat transfer constraint from the primary to secondary coolant.

iii) Initialize the steam generator state variables using the power and steam generator pressure.

iv) For the MSLCH, calculate both the pressure of the MSLCH and those of the steam generators for each power module using the pressure drop through each steam line.

v) Calculate the steam flowrate for each of the other power modules using each module's power and steam generator pressure.
Input and Initialization

- Read Input Data
- Initialize Geometry
- Initialize Variables and Arrays

Steady-State Calculation

- Calculate RCS Primary and Secondary Variables
- Fuel Temperature and Thermal Power Calculation
- Initialize Xenon
- Initialize Feedback Reactivity
- Initialize Point Kinetics Parameters

Figure 2-19 Steady-State Simulation Procedure
Figure 2-20  Flow Chart for Steady-State Calculation of Reactor Primary and Secondary Coolant System
vi) Initialize the steam generator state variables in the other power modules using the power and steam generator pressures.

vii) Determine the primary coolant temperatures at the steam generator inlet and outlet using the heat transfer constraint from the primary to secondary coolant.

viii) Determine the core inlet temperature and all other coolant temperatures for the other power modules.

Fuel and cladding average temperatures are calculated from the average coolant temperature and power. The xenon concentration equations are solved to obtain the initial xenon concentrations. The point kinetics equations are also initialized to calculate the initial concentrations of the delayed neutron precursors. Initial reactivity is assumed to be zero (steady-state).

2.4.2.2 Transient Simulation

Once the steady-state solution has been determined, the transient simulation is begun. The transient simulation routine consists of the controller simulation routine and the various system models. The latter entail four subsets of system difference equations for each power module and one subset for the MSLCH. A tandem approach is used to advance the transient solution. Figure 2-21 illustrates the transient simulation procedure.

i) The boundary conditions for each time step consist of the demanded load and the non-modeled parameters in the simulation program including primary coolant
flowrate and pressure, charging flowrate, and soluble boron concentration in the charging flowrate.

ii) The reactor coolant system and steam generator secondary side equations for each power module are solved simultaneously to give coolant average temperatures and steam generator pressures.

iii) The MSLCH equations are solved to determine the steam flowrate of each power module.

iv) The fuel and coolant average temperatures are calculated for each power module.

v) The control action to change control rod motion and feedwater flowrate is simulated.

vi) The point kinetics equations are solved to calculate the reactor power.

The above six steps are then repeated for the duration of the simulation.
Figure 2-21  Transient Simulation Procedure
2.5 Evaluation of Simulation Program

In this section, the results of the validation and testing of the developed simulation program are described. Given that neither reference plant design data nor a reference simulation program for a PWR-type, multi-modular power plant exist, the validation and testing were accomplished by simulating static and transient cases using typical Westinghouse PWR plant data. Simulation results were compared with either actual plant operational data or simulation results from other reference programs. It is important to note that primary and secondary coolant conditions for PWR-type multi-modular power plants are essentially the same as those for a PWR power plant provided that all the modules are operated at the same demanded load. Some of the subsystem simulation models, including those for steam generator secondary side, steam generator heat transfer, point kinetics, and primary loop had been previously validated [K-1,C-1]. Therefore, the focus of attention here was the validation of the overall integrated performance of the simulation program.

2.5.1 Steady-State Simulation

The validation of the steady-state simulation was accomplished by analyses of PWR steady-state characteristics. Simulation results were compared with those of the Pressurized Reactor Interactive Simulation Model [K-3]. For these simulations it was assumed that all modules were operated at same load. Figures 2-22 through 2-24 show comparisons of the average primary coolant temperature, steam flowrate, and steam generator pressure as a function of module power.

101
Figure 2-22  Primary Coolant Average Temperature at Steady-State.

Figure 2-23  Steam Flowrate from Each Steam Generator at Steady-State
2.5.2 Transient Simulation

In order to validate the transient simulation capability of the developed program, the following cases were analyzed:

i) **Null Transient Simulation**: Simulate transients with no perturbations and no control action and compare the results with steady-state cases.

ii) **Symmetry Test**: Perturb only one of the power modules and compare the transient behavior of each of the other modules. The initial configuration for these tests was that all modules were at a uniform power level.
2.5.2.1 Null Transient Simulation

This test was required to establish the numerical stability of the simulation program. First, some of the system differential equations were solved using explicit numerical approximation methods for which it was expected that there would be a limit on the time step size. Because the simulation program consisted of many complex differential equations, it was not possible to calculate the time step size limit directly by using analytical or numerical methods. Therefore, the allowable time step size was found by performing a series of transients, each with a different time step size. For these simulations, the initial power levels of modules #1, #2, #3, and #4 were assumed to be 100% MFP (Module Full Power), 95% MFP, 90% MFP, and 85% MFP, respectively. The null transient was simulated up to 700 s and calculation time step sizes were increased by 0.01 s until numerical instability appeared.

Figure 2-25 shows the steam flowrate of module #1 when time step sizes of 0.18 s and 0.19 s were used. If time step sizes of less than 0.18 s were used, neither oscillation nor divergence of the solutions occurred. However, if a 0.19 s time step size was used, the solutions oscillated and diverged. Based on these results, it was concluded that the simulation program had a time step size limit between 0.18 s and 0.19 s and that time step sizes less than 0.18 s should be used to avoid numerical instability. Figure 2-26 shows the time behavior of the primary coolant average temperature and steam generator pressure for module #1 when 0.18 s and 0.19 s time step sizes were used. Even though the steam flowrate oscillated dramatically, variations in the primary coolant average temperature and steam generator pressure were negligible. This means that the overall time step limit of this simulation program is determined by the MSLCH simulation model. According to Strohmayer's calculation, the limiting time step size of the steam generator secondary side
model is ~ 0.7 s [S-2] which is larger than the MSLCH limit time step size. Therefore, the method of numerical solution for the MSLCH model should be changed to an implicit method that can accommodate a larger time step size limit. A 0.1 s time step size was used in all subsequent null transient simulations. Figures 2-27 through Figure 2-31 show the null transient results when a 0.1 s time step was used. All plant parameters remained at their steady-state values.

Figure 2-25  Calculated Steam Flowrates of Module #1 for Two Different Time Step Sizes
Figure 2-26  Calculated Coolant Average Temperature and Steam Generator Pressure of Module #1 for Two Different Time Step Sizes

Figure 2-27  Neutron Power During Null Power Transient
Figure 2-28  Primary Coolant Average Temperature During Null Power Transient

Figure 2-29  Steam Generator Pressures During Null Power Transient
Figure 2-30  Steam Flowrate from Each Power Module During Null Power Transient

Figure 2-31  Pressure and Quality at the Main Steam Line Header During Null Power Transient
2.5.2.2 Symmetry Test

As mentioned previously, each module can be operated at a different power level. Symmetry tests were carried out to verify this capability. The initial power of all modules was assumed to be 100% MFP and the following two cases were simulated:

i) **Case 1**: Only module #2’s power was changed to meet the variation in plant demand. No perturbations except thermal-hydraulic feedback effects were allowed.

ii) **Case 2**: Same as Case 1, but power was perturbed in module #4 instead of module #2.

The simulation results are shown in Figures 2-32 through 2-41. As shown in these figures, all plant parameters for the three uncontrolled modules behaved in the same way in both cases. Also, the controlled modules, module #2 in Case 1 and module #4 in Case 2, showed the same transient behavior. Based on these results, it was concluded that the simulation program could simulate multi-modular power plant transients.
Figure 2-32  Neutron Power During Symmetry Test  (Only Module #2’s Power is Changed)

Figure 2-33  Neutron Power During Symmetry Test  (Only Module #4’s Power is Changed)
Figure 2-34  Primary Coolant Average Temperature During Symmetry Test (Only Module #2’s Power is Changed)

Figure 2-35  Primary Coolant Average Temperature During Symmetry Test (Only Module #4’s Power is Changed)
Figure 2-36  Steam Flowrate from Each Power Module During Symmetry Test (Only Module #2’s Power is Changed)

Figure 2-37  Steam Flowrate from Each Power Module During Symmetry Test (Only Module #4’s Power is Changed)
Figure 2-38  Steam Generator Levels During Symmetry Test (Only Module #2’s Power is Changed)

Figure 2-39  Steam Generator Levels During Symmetry Test (Only Module #4’s Power is Changed)
Figure 2-40  Pressure and Quality at the Main Steam Line Common Header during Symmetry Test (Only Module #2’s Power is Changed)

Figure 2-41  Pressure and Quality at the Main Steam Line Common Header during Symmetry Test (Only Module #4’s Power is Changed)
2.6 Chapter Summary

A simulation program for a PWR-type multi-modular power plant has been developed. This program can simulate up to four PWR power modules and the main steam line common header. A point kinetics model and a single coolant-loop model are used for the primary system of each module. Fuel and coolant thermal-hydraulic feedback, fission product poisons, control rod motion, and chemical shim are included in the reactivity calculations. A U-tube steam generator simulation model was adapted to simulate steam generator level variations. The MSLCH simulation model incorporates the moisture content at the MSLCH. Two types of module power controller simulation models were implemented in the simulation program to analyze the module power controller’s performance. These were a PWR analog average coolant temperature controller and an advanced digital average coolant temperature controller.

The simulation program can reproduce typical PWR steady-state operational data. However, because explicit numerical approximations are used in some of the subsystem models, the time step size is limited to 0.18 s during transient simulations. Verification tests, in which a symmetric response of the associated modules was observed, were used to determine that this program can simulate a PWR-type multi-modular power plant.

PMSIM developed here can simulate the operational transients about three times faster than real-time on a VAX station-II. According to reference [A-2], this minicomputer’s relative speed is much slower than that of other VAX stations or Apollo minicomputers (~1/30). This means that this simulation program can be used as part of the simulator for a PWR-type multi-modular power plant.
Chapter Three

Steam Generator Level Controller

3.1 Introduction

3.1.1 General

In nuclear power plants, the fission energy released in the fuel is used to produce steam, either directly in the reactor itself or in steam generators which transfer heat from the primary coolant to the secondary feedwater. Steam generators function by transferring energy from the primary coolant to the turbines where it is converted to electricity. As such, steam generators serve as a heat sink for the primary coolant system. For proper performance, the steam generator water level must be held within a predetermined operating bound. Too low a water level may cause loss of a heat sink during steam-line break accidents. Too high a water level may degrade steam quality in the separators, dryers, and steam outlets. This will in turn cause corrosion problems in the turbine blades. So, if
steam generator water level is not maintained within its allowed operating bound, a reactor trip must be initiated.

Steam generator level control is complicated by the counterintuitive dynamics of the steam generator itself. Specifically, steam generator water level dynamics are characterized by an inverse response known as the ‘Shrink and Swell’ effect. Because there is a lot of vapor present in the tube bundle region and because measured water level is sensed in the downcomer region, the indicated (i.e. the observed) steam generator water level behaves initially in manner a opposite to long-term asymptotic behavior. These phenomena are accentuated during start up and low power operation. As a result, it is not uncommon for a human operator to initiate an incorrect control action.

Flow measurements, which are used in existing analog automatic controllers for feedforward correction, are too uncertain for reliable use during low power operation. Therefore, at low power, both automatic and manual control action depend on only steam generator level measurements. It is well known that current automatic steam generator level controllers do not provide satisfactory performance at low power because of the aforementioned inverse response and long time constants. Unsatisfactory performance of automatic level controllers may either cause a reactor trip or require that the operator take manual control. Even for skilled operators, it is difficult to react properly to the inverse level response. Operators sometimes overreact when restoring the level and cause a reactor trip.

The main objective of this chapter is to describe the development and evaluation of a new automatic steam generator water level controller which ensures satisfactory performance over all power regions. This controller is applicable to both conventional PWR power plants and PWR-type multi-modular power plants.
3.1.2 Review of Analyses of Previous Operational Data

The need for a new method of steam generator level control when operating at low power has been verified in many previous studies [C-2,S-1,I-2]. According to these reports, unplanned or inadvertent reactor trips that result from level control problems contribute significantly to reactor unavailability. Table 3-1 gives an operating summary for unplanned reactor trips of PWRs manufactured by Combustion Engineering and Westinghouse, both of which use U-tube steam generators. As is seen in the table, a reactor was scrambled more than five times per reactor operating year from 1978 to 1983 with the most frequent cause being the main feedwater system and/or an instrumentation failure. Table 3-2 gives the breakdown of the sources and causes of reactor trips that are attributable to the main feedwater system. Table 3-3 gives the contributions of each type of feedwater trip to the total. Over 80% of all the reactor trips that were related to the feedwater system were initiated by high or low steam generator level. The severe frequency situation today is improved but the sensitivity to the steam generator level control still exists.

Based on these three tables, it is evident that reactor trips caused by steam generator level instability are a significant contributor to unplanned reactor outages. It is also apparent that trips related to the feedwater system occur most frequently at startup or during low power operation. This suggests that a new steam generator water level control system is needed which gives satisfactory performance during both low and high power operation.
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<tr>
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<tr>
<td>Other System</td>
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<tr>
<td><strong>Total</strong></td>
<td></td>
<td>5.26</td>
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(1978-1983)
### Table 3-2  Initiating Events of Feedwater Related Trips [S-1]

(1978~1983)  [\%]

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<th>Initiator</th>
<th>CE</th>
<th>WH</th>
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<td>Feed Pump</td>
<td>9.6</td>
<td>16.4</td>
</tr>
<tr>
<td>Valves</td>
<td>17.3</td>
<td>16.0</td>
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<td>Low Power Control</td>
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<td>26.2</td>
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<td>Loss of Power</td>
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<td>6.6</td>
</tr>
<tr>
<td>Input Signal</td>
<td>--</td>
<td>14.8</td>
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<tr>
<td>Control Signal Component Failure</td>
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<td>8.2</td>
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### Table 3-3  Types of Feedwater Related Trips Contribution to Total [S-1]

(1978~1983)  [\%]

<table>
<thead>
<tr>
<th>Type</th>
<th>CE</th>
<th>WH</th>
</tr>
</thead>
<tbody>
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</tr>
<tr>
<td>SG High Level</td>
<td>32.1</td>
<td>22.2</td>
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<td>Steam Flow/Feed Flow Mismatch</td>
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<td>21.7</td>
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<tr>
<td>Overpower Trip</td>
<td>--</td>
<td>1.1</td>
</tr>
<tr>
<td>CPC Initiated</td>
<td>3.7</td>
<td>--</td>
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<tr>
<td>High Pressurizer Pressure</td>
<td>5.7</td>
<td>--</td>
</tr>
<tr>
<td>Other</td>
<td>3.8</td>
<td>--</td>
</tr>
</tbody>
</table>
3.2 Description of Steam Generator

3.2.1 Steam Generator Internals

Heat generated by the nuclear fission reaction in the reactor core of a PWR is removed by the primary coolant and is transferred to the secondary coolant via the steam generators. This heat transfer results in the production of steam which is then used to drive a turbine and electric generator. The steam generators described here are used only in PWRs. Each of the three PWR manufacturers (Westinghouse Electric Corp., Combustion Engineering Inc., and Babcock and Wilcox Co.) designs and manufactures its own steam generators. Westinghouse Electric Corp. and Combustion Engineering Inc. use a U-tube steam generator while Babcock and Wilcox Co. uses a ‘once-through’ steam generator. The features described here are based on a current typical Westinghouse Model-F steam generator which is used in a 4-loop, 1150 MWe Westinghouse PWR.

A representative Westinghouse Model-F U-tube steam generator is shown in Figure 3-1. The unit consists of two interacting fluid systems: the hot primary coolant system and the relatively colder secondary coolant system. The primary and secondary sides are linked by heat transfer through the tube walls. The primary fluid system consists of both the hot reactor coolant that is within the tube bundle and the primary coolant that is contained in the inlet and outlet plena located at the bottom of the steam generator. Hot primary reactor coolant enters the steam generator through the primary inlet nozzle. It then flows inside the U-tubes, first upward and then downward, where it transfers heat to the secondary coolant. The primary coolant then leaves the outlet plenum through the outlet nozzle.

The steam generator secondary side consists of two integral sections: an evaporator and a steam drum. The evaporator section contains a U-tube heat exchanger while the
Figure 3-1  Schematic of U-Tube Steam Generator Internals [S-5]
steam drum section contains a riser, a moisture separator, and the dryer that is located in the upper part of the steam generator. The secondary coolant has two distinct regions, upflow and downflow, that are separated by a wrapper. The inner (upflow) region consists of the tube bundle and riser. The outer (downflow) region consists of the downcomer and feedwater mixing region. Subcooled feedwater is introduced into the steam generator via the feedwater nozzle and is distributed throughout the feedwater mixing region by the feedwater ring. There, it mixes with saturated liquid that is being returned from the steam separation devices. This is termed the 'recirculation flow'. The resulting subcooled liquid flows downward through the annular downcomer region formed by the wrapper and the steam generator's outer shell. At the bottom of the downcomer, the water is turned and flows upward through the shell side of the tube bundle region, where it is heated to saturation and boils. The secondary fluid exits the tube bundle region as a saturated two-phase mixture and then flows upward through the riser into the steam-separating equipment. Steam separation is achieved by using a combination of centrifugal steam separators for bulk liquid-vapor separation and chevron-type steam dryers for the removal of any residual moisture. The relatively dry steam, with moisture content of less than 0.25 %, leaves through the steam outlet nozzle at the top of the steam generator, while the saturated water is directed downward to mix with the entering feedwater.

The secondary fluid path described above constitutes a natural circulation loop. The driving head for this recirculating flow is the density difference between the subcooled column of liquid in the downcomer region and the two-phase mixture in the tube bundle and riser regions. This driving head is counterbalanced by the various pressure losses in the loop, such as frictional losses in the tube bundle and the losses within the steam separators.
3.2.2 Associated Secondary Plant

A number of secondary plant systems influence the steam generator water level control system either directly or indirectly. A representative group of such components is shown schematically in Figure 3-2 [S-5,S-6]. The components illustrated are part of the either main feedwater system or the main steam system. Accordingly, a brief description of these systems is given here. Additional information is addressed elsewhere [S-5,S-6].

Figure 3-2  Schematic of Steam Generator, Main Steam, and Main Feedwater System [S-6]
3.2.2.1 Main Steam System

The major function of the main steam system is to transport the steam generated in all team generators to the turbine generator for conversion to electrical power. In addition to supplying the turbine generator with steam, the main steam system performs several functions important to safe operation. For example, it regulates steam generator pressure during startup and shutdown and during emergency conditions when the main condenser is not available. Also it provides overpressure protection for the steam generator secondary side, and it will automatically isolate the appropriate header in the event of a steam line rupture.

The main steam pipe leaves the top of the steam generator and travels through the containment building to the main steam and feedwater pipe chase. The steam exiting the steam generator passes through a flow restrictor prior to entering the main steam line. The flow restrictor limits steam flow during an unisolable steam line rupture. The differential pressure across the flow restrictor determines the steam flow signal that is in turn used in the steam generator water level control system.

The flow of steam from the steam generator is controlled by three types of valves. These are the Atmospheric Steam Dump Valve (ASDV), the steam generator safety valves, and the Main Steam Isolation Valves (MSIVs). The ASDV is situated between the steam generator and the safety valves. It is utilized during hot standby operation to regulate steam generator pressure and reactor coolant temperature when the main condenser is not available. In addition, the ASDV provides an alternate path for removal of decay heat during a cooldown of the reactor until the residual heat removal system can be placed in operation.
Downstream of the ASDV, five steam generator safety valves provide overpressure protection for the steam generator secondary side. Steam generator secondary side pressure varies directly with reactor primary coolant temperature. In the event of an abnormal temperature increase in the reactor coolant system, the steam generator pressure will increase and safety valves will lift to remove energy. That action will lower the reactor primary coolant system temperature. Thus, the steam generator safety valves ensure that the reactor core thermal safety limits are not exceeded.

The MSIVs also serve to protect against abnormal conditions. Each main steam line can be isolated by a MSIV. These close automatically on high steam flow coincident with either 'low-low' coolant average temperature or low steam line pressure, or on a 'high-high' containment pressure condition.

Steam from the each steam generator flows into the main steam line header which serves to cross connect all steam generators. Therefore, the pressure at the exit of this header is common to all steam generators. Hence, the pressures at the individual inlets to the header should all be same. Otherwise, steam will not flow from the low-pressure units and the desired power ratio will not be maintained among power modules. The main steam line header discharges to the turbine building via six steam lines. Four of these steam lines supply the high pressure turbine. The remaining two supply the moisture separator/reheater, the turbine-driven main and auxiliary feed pumps, and the steam dumps.
3.2.2.2 Main Feed System

The heat energy of the steam is converted to directed kinetic energy during passage through the turbine. Exhaust steam from the main turbines and the feedwater pump turbines enters the condensate system which condenses the steam to water under a vacuum condition. The condensate is collected in the condenser and pumped by condensate and condensate-booster pumps through feedwater heaters to the suction of the main feed pumps.

Feedwater heating increases plant efficiency and decreases the temperature difference between the incoming feedwater and the saturation temperature in the steam generator. A small temperature difference is beneficial because it minimizes the shrink and swell effects, described in Section 3.3, and hence, stabilizes control of the steam generator water level. The feedwater heating system is of the closed type with deaeration accomplished in the condenser hotwell. Steam to heat the feedwater that flows through the tube side of the feedwater heaters is extracted from the turbine casing after various stages in the high and low pressure turbines. The energy of this extracted steam will vary depending on the point of extraction in the turbine cycle. As expected, high-energy extracted steam comes from the high pressure turbine and low-energy extracted steam from the low pressure turbines. Low-energy extracted steam is supplied to the first of the feedwater heaters with successively higher-energy extracted steam supplied to subsequent heaters.

Three feedwater pumps with a common suction and discharge header are provided. These pumps are sized so that each is capable of providing 50% of the flow required to operate the plant at full capacity. Two pumps are turbine-driven variable speed pumps. These are used at power operation. The third is electric-driven and is used for startup or as
a reserve/backup pump. The feedwater pumps discharge through the high pressure feedwater heaters into a common header. This header is monitored for pressure and that signal is transmitted to the feed pump speed control input. The feedwater header has several branch lines which lead to each of the steam generators.

There is one control valve and flowrate measurement venturi in each feedwater line. These are used in the steam generator water level control system. The valve for a particular steam generator is operated by an automatic control system which regulates feedwater flow in order to maintain the desired water level in the associated steam generator. There is an independent feedwater control system for each steam generator. At low loads that requires low feedwater flows, a small bypass valve is used to control feedwater flow in lieu of the large main control valve. These valves are installed in a small bypass line that goes around the feedwater regulating valve. These bypass valves also have both an automatic and a manual flow control capability.

3.2.3 Feedwater Flow Control System

The feedwater control system is used to adjust the feedwater flowrate to the steam generator so as to maintain the steam generator water level within the operational band. It has both a manual and an automatic mode. The feedwater flow control system consists of two individual systems. These are the steam generator water level control system and the feed pump speed control system. The two interact with each other.

The steam generator water level control system computes a desired steam generator water level (i.e., a setpoint) based on the turbine load. To maintain this level, the steam
generator water level control system produces a control signal which positions the feedwater regulating valve for each of the steam generators. To develop a control law for steam generator water level control is a major consideration of the research described in this report.

The feed pump speed control system is designed to complement the operation of the steam generator water level control system. It computes a desired pump speed that is based on the total steam flow. That desired speed is then achieved by regulating steam flow to the feed pump turbine. Variation of pump speed in this fashion result reduced erosion of the feedwater regulating valve surfaces and improves the feedwater regulating valve flow control characteristics. Additional details for the feed pump speed control system are given elsewhere [C-2,S-6]. Feed pump speed control was not considered in this research.

3.2.3.1 Steam Generator Water Level Control Program

There are several conditions which must be evaluated prior to choosing the optimum operational steam generator water level. These factors are:

i) The effects of shrink which may cause loss of level indication;

ii) The effects of swell which may cause poor moisture separation performance and subsequent turbine blade damage; and

iii) The influence on the magnitude of the peak containment building pressure attained as a result of the complete blowdown of a steam generator's inventory from a steam-line break accident.
The first factor establishes a lower bound for the programmed level. With programmed level above this bound, the chance that a sudden load rejection will result in so much shrink so as to cause a reactor trip due to a 'low-low' steam generator water level is minimized. The second factor establishes an upper bound for the programmed level. If the level is kept below this bound, then the steam generator water level swell that results from a sudden load increase should not cause the downcomer level to back up into the moisture separators, thereby degrading their performance. The third factor also sets an upper bound on the programmed level because a steam line break at hot zero power (mass inventory inside the steam generator is maximum) sets a limit on the maximum allowable steam generator fluid inventory. If a steam line break were to occur inside the containment, the subsequent vapor release to the enclosed environment would cause building pressure to rise. The magnitude of this pressure rise is related to the amount of steam released, which would be proportional to fluid inventory of steam generator [T-3].

For the Westinghouse model F-type steam generator, the programmed steam generator water level is set at 50% for all power levels. However, for the smaller steam generators of some other PWR plants, the steam generator water level is programmed to vary linearly from 33% at zero power to 44% at 20 %FP and then be maintained at 44% for all higher power levels [Q-1]. In this research, the Westinghouse model-F type steam generator was selected as the basis of study and a constant steam generator level control program was adopted.
3.2.3.2 Steam Generator Water Level Control System

The steam generator water level control system, shown in Figure 3-3, produces an electrical control signal that positions a pneumatically-operated feedwater regulating valve. The throttling action of this valve manipulates the feedwater flow and thereby enables the steam generator control system to maintain steam generator water level within the programmed band.

Figure 3-3  Steam Generator Water Level Control [S-6]
The steam generator water level control system compares the measured water level to the programmed level. The difference between these two levels is the input to a PI (Proportional plus Integral) level controller. A positive error signal, which means that programmed level is greater than the measured level, causes the feedwater regulating valve to open. In contrast, a negative error signal, which means that programmed level is less than the measured level, causes the valve to move toward its closed position.

In addition, because steam generator water level dynamics involve long time constants, feedforward control action is applied to improve system stability. To do this, the level control system compares steam flow to feed flow. A positive error signal, which means that steam flow is greater than feed flow, causes the feedwater regulating valve to open, while a negative signal causes it to close. The two basic error signals (level and flow) are summed to yield a total error signal in the summer unit. From the summer, a total error signal is directed through a manual or automatic station to operate the feedwater regulating valve.

To adjust the feedwater regulating valve position, a PI analog control strategy is implemented. It uses three elements (steam generator water level, steam flow, and feed flow) in a PI configuration during high power operation. However, because the flow measurements are quite uncertain at low power, steam generator water level control depends on only a single element, steam generator water level, at low power. Details of current generation steam generator water level control systems are explained in Section 3.4. In this research, a major effort was focused on the development of a proper control law which would be consistent over the entire power range and which would give good control performance.
3.3 Steam Generator Water Level Dynamics

The successful design of a steam generator water level control system requires that generator level dynamics are well understood. Steam generator level dynamics are strongly nonlinear because of inverse response and long time constants. Also, because the volume of the downcomer region is not proportional to water level, as shown in Figure 3-4, the steam generator internal geometry influences the level dynamics and creates non-linear effects.

In this section, a general review of steam generator level dynamics is given. A simplified transfer function model is then introduced and improved.

3.3.1 Swell and Shrink Effects

Shrink and swell effects result from changes in the volume of the steam in the tube bundle region. After a sudden decrease in steam volume in the tube bundle region, that volume is filled with liquid from the downcomer. This results in an increase in the downcomer flowrate which causes the indicated level shrink. Similarly, an increase in the volume of the steam in the tube bundle region will cause a decrease in the downcomer flowrate and a backup of the liquid in the downcomer region. This causes level swell. Details of the shrink and swell effects are discussed in Section 3.3.1.2. Figure 3-5 is a simplified block diagram for a steam generator. There are three different perturbation parameters, steam flowrate, feedwater flowrate, and primary coolant temperature. Steam generator water level responds differently to variations in these parameters.
Figure 3-4  Geometry Input of Westinghouse Model F Steam Generator [C-2]
3.3.1.1 Steam Generator Operating Characteristics

Normally a steam generator is operated in the recirculating mode. Feedwater enters the steam generator through the feedwater sparger and flows into the downcomer where it mixes with the recirculating saturated liquid. The combined flow moves through the downcomer and enters the tube bundle region at the bottom of the steam generator. As fluid rises through the tube bundle region, it absorbs heat from the primary coolant. This causes steam formation. The resulting two-phase mixture rises until it reaches the separators which remove liquid from the steam, return the liquid to the downcomer for further recirculation, and allow the steam to rise to the dryers. The recirculating process is sustained by an imbalance in the hydraulic head of the fluid between the downcomer and the tube bundle regions. During power operation, this driving force is significant and recirculating flow is dominant. It is important to note that any perturbation in the amount of vapor in the tube bundle region will cause the driving head to change and that in turn will alter the recirculating flow and ultimately the downcomer level change.
However, at very low power, the amount of boiling in the tube bundle region is too low to create a sufficient driving head for recirculating flow. As the power level decreases, the recirculating flowrate also decreases and finally stops because the hydraulic head difference can no longer provide the needed driving force. Under this condition, the steam generator behaves like a boiling pot. Feedwater simply enters the downcomer, passes through the tube bundle region, and exits as steam.

Figure 3-6 shows the steady-state recirculation flow rate as a function of power. As shown in this figure, the recirculating is very large except at extremely low power levels. Thus, recirculating operational mode is dominant at high power levels.
3.3.1.2 Steam Generator Level Shrink and Swell Effects

Steam generator level shrink refers to the temporary reduction of the water level in the downcomer region that results from steam bubble collapse in the tube bundle region. With the collapse of steam bubbles, the volume taken by the two phase mixture suddenly decreases and is filled by liquid from the downcomer region. Hence, the indicated level, which is obtained from the downcomer, drops even though the mass of fluid in the steam generator has risen. Figure 3-7 explains the steam generator shrink phenomena.

Shrink and swell may be caused by either a change in the feedwater or steam flowrates. The mechanisms involved are different. First, consider the effect of changing the feedwater flowrate. Introduction of feedwater at a temperature below saturation in the tube bundle region will cause internal condensation. Thus, a sudden increase in feedwater flowrate will momentarily reduce the boiling rate. Next consider the effect of a changing the steam flowrate. A decrease in steam flowrate will cause steam generator pressure to rise which will collapse the existing steam bubbles in the tube bundle region. Higher pressure also will cause an increase in the saturation temperature which in turn reduces heat transfer from primary coolant. Therefore, the boiling rate will decrease temporarily following a steam flowrate decrease. Another initiator of shrink and swell effects is the primary coolant temperature. Specifically, a primary coolant temperature decrease that results from a reactor power drop will also cause a decrease in both the heat transfer rate from the primary coolant and the boiling rate in the tube bundle region.

Figures 3-8, 3-9, and 3-10 show steam generator level shrink as a function of these three different perturbation parameters at different power levels. For these simulations, the detailed nonlinear model described in Chapter Two was used. In Figure 3-8, the effect of
Figure 3-7  Schematic View of Steam Generator Level Shrink Phenomena
a step increase in feedwater flow is shown. The level first rises, then drops (shrink effect), and then again rises with the rise rate related to the increase in feedwater flow. The initial small rise in level is the result of a mass increase in the downcomer region. However, the increase in downcomer hydraulic head soon causes an increase of flow into the tube bundle region. This causes the level to shrink.

Figure 3-8  Steam Generator Level Shrink Effect due to Sudden Feedwater Flowrate Increase
Figure 3-9  Steam Generator Level Shrink Effect due to Sudden Steam Flowrate Decrease

Figure 3-10  Steam Generator Level Shrink Effect due to Sudden Primary Coolant Temperature Decrease
Changes in steam flowrate change cause a greater shrink effect than do ones in the feedwater flowrate because the saturation pressure increase occurs immediately after the steam flowrate decrease. The increased pressure results in significant vapor collapse in the tube bundle region and hence, a major shrink in level. This is shown in Figure 3-9.

As shown in Figure 3-10, the level response to a primary coolant temperature perturbation differs from those of the feedwater or steam flowrate perturbations. Because the mass inventory remains constant (i.e., constant feedwater flowrate and steam flowrate) the level ultimately stabilizes at new equilibrium value. There are two factors that determine this final level. First, the lower primary coolant temperature causes a decrease in vapor production in the tube bundle region and hence a level decrease. Second, a lower primary coolant temperature causes a decrease in steam generator pressure that results in an increase of the vapor fraction in the tube bundle region and a level increase. Which of these two conflicting effects is dominant depends on the power level. At low power, the level ultimately attained will be less than the initial one because the vapor fraction in the tube bundle region decreases substantially with the primary coolant temperature decrease. However, at high power, a lot of vapor exists in tube bundle region with the result that the second effect is dominant and the ultimate level is increased.

Steam generator water level swell is the reverse of the shrink effect. The expansion of the steam volume in the tube bundle region displaces liquid which backs up into the downcomer region thereby causing the indicated steam generator water level to rise. Figure 3-11 shows the steam generator swell phenomena. A decrease in the feedwater flowrate causes the fluid into the tube bundle region to become hotter. Thus, for the same amount of heat transfer, there is an increase in the boiling rate and consequently in the steam bubble void fraction. This causes water to back up in the downcomer region and hence gives a
Figure 3-11  Schematic View of Steam Generator Level Swell Phenomena
transitory indication of a rising level. Similarly, an increase in steam flow causes an increase in the steam bubble volume and a decrease in steam pressure. This causes the saturated water to flash to steam and hence creates an increase in tube bundle steam volume with a concomitant temporary rise of fluid in the downcomer. Finally, an increase in primary coolant temperature creates an increased heat transfer rate from primary coolant. This in turn increases the boiling rate and creates the level swell effect.

Figures 3-12 to 3-14 show steam generator level swell as a function of the three different perturbation parameters at different power levels. The explanations for the observed behaviors are the reverse of those given for Figures 3-8 to 3-10 which described shrink effects.

Figure 3-12  Steam Generator Level Swell Effect due to Sudden Feedwater Flowrate Decrease
Figure 3-13  Steam Generator Level Swell Effect due to Sudden Steam Flowrate Increase

Figure 3-14  Steam Generator Level Swell Effect due to Sudden Primary Coolant Temperature Increase
3.3.1.3 Inverse Response

Steam generator water level ultimately follows changes in the mass inventory. However, as was described in Section 3.3.1.2, the initial behavior of the steam generator’s level following a feedwater flow or a steam flow change will be in direction opposite to its asymptotic behavior because of shrink and swell effects. In the case of a change in primary coolant temperature, the initial steam generator water level response will also be in the opposite direction to its final behavior if at high power. This behavior is termed ‘inverse’ response or ‘non-minimum phase response’ [S-7,S-8,O-2]. Such behavior is exhibited by certain other processing units including drum boilers (level) [S-7] and distillation tower columns (temperature) [Y-1].

Ilinoya and Altpeter list several transfer functions that exhibit inverse responses [I-2]. These represent or approximate a number of physical processes. In all cases, it is evident that if a system is characterized by an inverse response, its transfer function contains a zero in the right-half plane in the Laplace domain. An example is given here that is illustrative of inverse response. Consider a process described by an integral term and a first order lag.

Figure 3-15 is a block diagram of the boiler level response in a drum boiler system [S-8]. Its transfer function consists of an integral response and a negative first order response. The combined transfer function is expressed as follows:

\[
\frac{\bar{y}(s)}{\bar{f}(s)} = \frac{K_2}{s} - \frac{K_1}{\tau s + 1}
\]  

(3-1)

Poles: 0, - 1/\tau
Zero: \( \frac{K_2}{K_1 - K_2 \cdot \tau} \)

This transfer function has two poles and one zero. If \( K_2 \cdot \tau < K_1 \), then it has a positive zero and its response to a step change in input is inverse in nature. Figure 3-16 shows the output after a step input (this illustration is based on \( K_1 = 10, \ K_2 = 0.2 \) and \( \tau = 10s \)). Because the first order term dominates initially, the overall response is temporarily opposite to its asymptotic direction. However, if the condition, \( K_2 \cdot \tau < K_1 \) is not satisfied, an inverse response does not occur. This is shown in Figure 3-17 (this illustration is based on \( K_1 = 1, \ K_2 = 0.2 \) and \( \tau = 10s \)).

![Block Diagram of the of Drum Boiler Level Response](image)
Figure 3-16  Typical Inverse Response of System Consisted with Integral Term and First Order Lag Term

Figure 3-17  Noninverse Response of System Consisted with Integral Term and First Order Lag Term
3.3.2 Simple Steam Generator Model

As described in Chapter Two, the steam generator simulation model consists of six coupled differential equations and many additional equations. These accurately describe steam generator level dynamics. Because this set of equations is high order and non-linear, it is not easy to obtain an analytic solution. This in turn makes it difficult to evaluate the performance of a steam generator water level controller. Therefore, reasonably accurate but low-order steam generator model is preferred.

The complicated dynamics of a steam generator is approximated here by a simplified mathematical model that is deduced through system identification from transient simulation results that used a detailed, high-order, non-linear model. Fortunately, it is not necessary to model entire steam generator for control performance analyses. Only level dynamics including shrink and swell effects are of interest.

3.3.2.1 Simplified Transfer Function

The response of a dynamic system to an input may be obtained by solving the differential equations that characterize it. The equation set can in turn be obtained from the physical laws that govern the actual system. These include laws of mass, momentum, and energy conservation. The mathematical description of the dynamic characteristics of a system is called a mathematical model. For control purposes, such models may be written in either the time domain (differential equations) or the Laplace domain (Laplace transform). The latter approach is used here. A transfer function is defined as the ratio of the Laplace transform of the system output to that of the input both expressed in deviation
form. If the mathematical model is nonlinear, then it must first be linearized about an operating point and expressed in terms of deviation variables. In cases where the system dynamics are very complicated, the mathematical model will also consist of complicated differential equations. It will not be trivial to obtain the Laplace transform. Therefore, it is often useful to approximate a complicated physical system by a simplified mathematical model that is based on either simulation analyses of a detailed mathematical model or experimental study of the physical system.

Irving and Bihoreaux suggest a simple transfer function which successfully describes the shrink and swell effects that are created by feedwater or steam flowrate change [I-1]. The inputs to the equation are feedwater flowrate and steam flowrate. The output is the downcomer water level. The equation is as follows:

\[
L_w(s) = \left[ \frac{G_1}{s} - \frac{G_{2f}}{(\tau_{2f} s + 1)} + \frac{G_{3f} s}{(s + 1/\tau_{3f})^2 + \omega^2} \right] \dot{m}_{fw}(s) + \\
\left[ -\frac{G_1}{s} + \frac{G_{2s}}{\tau_{2s} s + 1} \right] \dot{m}_s(s) \tag{3-2}
\]

where \(s\) is the Laplace variable, \(L_w(s)\) is the Laplace transform of the steam generator level change due to feedwater or steam flowrate change and \(\dot{m}_{fw}(s)\) and \(\dot{m}_s(s)\) are the Laplace transforms of the change in feedwater and steam flowrate respectively. Each quantity represents a deviation. Thus, they are \([L_w(t) - L_w(0)]\), \([\dot{m}_{fw}(t) - \dot{m}_{fw}(0)]\), and \([\dot{m}_s(t) - \dot{m}_s(0)]\) respectively.

Each term of Equation (3-2) has a physical significance. \(G_1/s\) is the mass capacity term of the steam generator where \(G_1\) is a constant developed from the steam generator
dimensions. $G_1$ is a measure of the steam generator's height to volume ratio. The mass capacity term represents changes in the steam generator level caused by mass influx or efflux from the volume of a particular steam generator. If the $G_1/s$ term were the only term in the level equation, then the level indicator would be an accurate representation of the steam generator mass level.

The $G_2f/(\tau_{2f} s + 1)$ and $G_{2s}/(\tau_{2s} s + 1)$ terms are first order and represent the shrink and swell effects. $G_{2f}$ and $G_{2s}$ are variables which describe the magnitude of the shrink and swell effects that result from changes in feedwater and steam flowrate respectively. These two values depend on the operating power of the reactor. The quantities $\tau_{2f}$ and $\tau_{2s}$ are the characteristic decay times for the shrink and swell effects caused by the feedwater and steam flowrate changes, respectively.

The rest term in Equation (3-2) is the mechanical oscillation term which is caused by the direct addition of feedwater to the steam generator. This term only appears in response to a feedwater change and decays rapidly.

Equation (3-2) does not include the effect of a change in primary coolant temperature. As described in Section 3.3.1, primary temperature changes can cause shrink or swell effects. These effects can be represented by the addition of two opposing first order terms:

$$L_T(s) = \left[ \frac{G_{1T}}{1 + \tau_{1T} s} - \frac{G_{2T}}{1 + \tau_{2T} s} \right] T_p(s)$$ (3-3)
where \( L_T(s) \) is the Laplace transform of the steam generator level change due to primary coolant temperature change and \( T_p(s) \) is the Laplace transform of the change in primary coolant temperature.

The steam generator level response is sum of the individual responses to the three different inputs and is as follows:

\[
L(s) = G_{fw}(s) \dot{m}_{fw}(s) + G_s(s) \dot{m}_s(s) + G_T(s) T_p(s)
\]  
(3-4)

where:

\[
G_{fw}(s) = \frac{L_{fw}(s)}{\dot{m}_{fw}(s)} = \frac{G_1}{s} - \frac{G_{2f}}{(\tau_{2f} s + 1)} + \frac{G_{3f} s}{(s + 1/\tau_{3f})^2 + \omega^2}
\]  
(3-5)

\[
G_s(s) = \frac{L_s(s)}{\dot{m}_s(s)} = -\frac{G_1}{s} + \frac{G_{2s}}{(\tau_{2s} s + 1)}
\]  
(3-6)

\[
G_T(s) = \frac{L_T(s)}{T_p(s)} = \frac{G_{1T}}{(1 + \tau_{1T} s)} - \frac{G_{2T}}{(1 + \tau_{2T} s)}
\]  
(3-7)

where \( L_{fw}(s), L_s(s), \) and \( L_T(s) \) are the Laplace transforms of the steam generator level change that results from feedwater flowrate change, steam flowrate change, and primary coolant temperature change respectively. In Equations (3-5) to (3-7), the quantities \( G_{fw}(s), G_s(s), \) and \( G_T(s) \) are called the process transfer functions of the feedwater flowrate change, steam flowrate change, and primary coolant temperature change respectively. In reality, all of the so-called constants in Equations (3-2) to (3-7) are functions of the operating power.
and must be determined for the specific reactor and steam generator. The system identification method is explained in the following section.

3.3.2.2 Identification of Simplified Model Parameters

Because the values of all constants in Equation (3-4) must be determined for the specific reactor and steam generator system, system identification is needed to obtain a simplified linear model for the steam generator level dynamics. However, even if a mathematical model of the steam generator is obtained, it is often very difficult to derive the transfer function by taking the Laplace transform of the differential equations. Thus, it is often preferable to obtain the transfer function by means of experimental analysis, usually frequency response methods [O-2]. If the amplitude ratio and phase shift between the input and output have been measured at a sufficient number of frequencies within the frequency range of interest, then they may be plotted on a Bode diagram and the transfer function determined from asymptotic approximations.

However, because it is impossible to use an actual steam generator for experimental analyses, simulation results from a detailed non-linear model are used in lieu of experimental data. For the problem under study here, the forms of the transfer functions are already given (Equation (3-5) to (3-7)). Hence, only the unknown constants need be determined. As mentioned, a transfer function describes the dynamic behavior of the system output in relation to a change of input. Thus, for a particular variation of the input, the transfer function can be found and the response of the system can be calculated. Therefore, instead of using a general frequency response method, the unknown constants
are calculated to minimize the error between the level response of the simple transfer function and that of the detailed simulation.

To obtain an equation for the steam generator level, it was necessary to solve Equation (3-4) by taking the inverse Laplace transform of each term. All changes of perturbation parameters were in the form of a step. Therefore, the Laplace-transformed input of the perturbation parameters were expressed as follows:

\[ \dot{m}_{fw}(s) = \delta \dot{m}_{fw} \cdot \frac{1}{s} \quad (3-8) \]

\[ \dot{m}_s(s) = \delta \dot{m}_s \cdot \frac{1}{s} \quad (3-9) \]

\[ T_p(s) = \delta T_p \cdot \frac{1}{s} \quad (3-10) \]

where \( \delta \dot{m}_{fw}, \delta \dot{m}_s, \) and \( \delta T_p \) are multipliers that are directly proportional to the magnitude of the feedwater flowrate, steam flowrate, and primary coolant temperature respectively.

Substitution of Equations (3-8) to (3-10) into Equation (3-4) allows Equation (3-4) to be solved. In order to identify constants, one of the perturbation parameters is selected as the input and the others kept constant. If only feedwater flowrate is changed in a step fashion, then \( \dot{m}_{fw}(s) = \delta \dot{m}_{fw} / s, \dot{m}_s(s) = 0, \) and \( T_p(s) = 0 \) and the transient response is:

\[ L_{fw}(s) = G_{fw}(s) \cdot \dot{m}_{fw}(s) \]

\[ = \left[ \frac{G_1}{s} - \frac{G_{2f}}{(\tau_{2f} s + 1)} + \frac{G_{3f} s}{(s + 1/\tau_{3f})^2 + \omega^2} \right] \cdot \frac{\delta \dot{m}_{fw}}{s} \cdot \quad (3-11) \]
Upon taking the inverse Laplace transform of Equation (3-11), the steam generator water level response in the time domain is obtained:

\[
L_{fw}(t) = \left[ G_{1f} - G_{2f} \left( 1 - \exp \left( -\frac{t}{\tau_{2f}} \right) \right) + \frac{G_{3f}}{\omega} \exp \left( -\frac{1}{\tau_{3f}} \right) \sin(\omega t) \right] \delta \dot{m}_{fw} .
\]  

(3-12)

Similarly the level transient responses can be calculated for the steam flowrate and the primary coolant temperature perturbation cases. For the steam flowrate change case:

\[
L_s(s) = G_s(s) \cdot \dot{m}_s(s)
\]

\[
= \left[ \frac{G_1}{s} + \frac{G_{2s}}{(\tau_{2s} s + 1)} \right] \frac{\delta \dot{m}_s}{s} .
\]  

(3-13)

\[
L_s(t) = \left[ -G_{1f} + G_{2s} \left( 1 - \exp \left( -\frac{t}{\tau_{2s}} \right) \right) \right] \delta \dot{m}_s .
\]  

(3-14)

For the primary coolant temperature change case:

\[
L_T(s) = G_T(s) \cdot T_p(s)
\]

\[
= \left[ \frac{G_{1T}}{(1 + \tau_{1T} s)} - \frac{G_{2T}}{(1 + \tau_{1T} s)} \right] \frac{\delta T_p}{s} .
\]  

(3-15)
\[ L_T(t) = \left[ G_{1T} \left( 1 - \exp \left( - \frac{t}{\tau_{1T}} \right) \right) - G_{2T} \left( 1 - \exp \left( - \frac{t}{\tau_{2T}} \right) \right) \right] \delta T_p. \] (3-16)

The constants that are to be determined are evidently functions of the power and feedwater temperature. However, because the feedwater temperature is considered to be a function of power, only power dependence needs be considered. In order to obtain an exact level response to a specific perturbation parameter, a small step perturbation of only that parameter is applied to the detailed nonlinear steam generator model. Many detailed simulations at different power level (three different simulations, one each for changes in feedwater flowrate, steam flowrate, and primary coolant temperature at each power level) were performed for this purpose.

The MATLAB [G-2] subfunctions were used to calculate the unknown constants by minimizing the quadratic error between the level responses to a step change of a specific perturbation parameter obtained from the simplified model and those obtained from a detailed nonlinear model. The constants were fitted as polynomial functions of power in order to use them throughout the power range. The results are shown in Figures 3-18 to 3-28.

### 3.3.2.3 Characteristics of the Simplified Model Parameters

Figure 3-18 and 3-23 show the resulting constants which, as mentioned, are actually functions of the power level and hence 'constants' in name only. A better term would be power-dependent coefficients. One interesting feature of these coefficients is that
their magnitudes increase dramatically as power decreases. There are several reasons for this. First, at low power, the driving head for recirculating flow is reduced and hence, the recirculating flowrate decreases. Second, the amount of saturated water in the tube bundle region is large and the steam bubble volume is, as a result, smaller. Third, small changes in steam generator pressure due to steam flowrate changes can cause the flashing of a lot of nearly saturated water. Therefore, a small perturbation can create a large change in the steam volume. This is especially true for changes in steam flowrate. Also, small changes in feedwater flow can result in large deviations from equilibrium in the steam generator at low power because the feedwater is colder at low power.

The fact that shrink and swell effects are more severe at low power than at high power makes the steam generator level control problem worse. This is discussed in Section 3.4.

3.3.2.4 Validation of the Simplified Model

Various steam generator water level transients were simulated using the simplified model and the results are plotted in Figures 3-29 to 3-31. As shown in these Figures, the simplified model predicts the steam generator water level response almost exactly for the case of a feedwater perturbation. When the steam flowrate or primary coolant temperature is changed, the simplified model shows less than 0.5 % difference when compared to the detailed nonlinear model.
Figure 3-18  Steam Generator Simplified Transfer Function Coefficient, $G_{2t}$

Figure 3-19  Steam Generator Simplified Transfer Function Coefficient, $T_{2t}$
Figure 3-20  Steam Generator Simplified Transfer Function Coefficient, $G_{3t}$

Figure 3-21  Steam Generator Simplified Transfer Function Coefficient, $T_{3t}$
Figure 3-22  Steam Generator Simplified Transfer Function Coefficient, $\omega_{3f}$

Figure 3-23  Steam Generator Simplified Transfer Function Coefficient, $G_{2s}$
Figure 3-24  Steam Generator Simplified Transfer Function Coefficient, $T_{2s}$

Figure 3-25  Steam Generator Simplified Transfer Function Coefficient, $G_{1T}$
Figure 3-26  Steam Generator Simplified Transfer Function Coefficient $G_{2T}$

Figure 3-27  Steam Generator Simplified Transfer Function Coefficient, $T_{1T}$
Figure 3-28  Steam Generator Simplified Transfer Function Coefficient, $T_2T$

Figure 3-29  Steam Generator Level after Feedwater Flowrate Increase with Step Fashion at 20 %FP
Figure 2-30  Steam Generator Level after Steam Flowrate Increase with Step Fashion at 20 %FP

Figure 2-31  Steam Generator Level after Primary Coolant Temperature Increase with Step Fashion at 20 %FP
3.4 Conventional Controller

In this section a conventional steam generator level controller for a PWR of current design is discussed together with analyses of existing control problems. Included is a description of the mathematical modeling of the conventional controller and stability analyses of the system.

3.4.1 Modeling of a Conventional Controller

3.4.1.1 Level Controller

A steam generator level controller is quite complex in itself and, to further complicate matters, it is normally connected to another controller, such as one for feed pump speed. Feedwater flowrate is controlled directly by means of a control valve which is in turn regulated directly by the level controller and indirectly by the header pressure that is common to all feedlines. The latter is in turn regulated by the feedwater pump speed controller. Both controllers are connected to each other to form a complex cascaded control system. It is assumed here, for simplicity, that the feedwater pump speed controller works perfectly. Under the this assumption, the level controller can be modeled as shown in Figure 3-32. The controller is a three-element PI design and is intended for use at high power. This controller can be regarded as containing two subcomponents: a feedback controller that operates on level and a feedforward controller that operates on flowrate mismatch. For the standard level controller, the error is generated by comparing the level setpoint and the measured level. For the feedforward controller, the flowrate error is
Figure 3-32 Block Diagram of Current Steam Generator Water Level Controller and Closed Loop System at High Power Operation
computed as the difference between steam and feedwater flowrates. Each of these subcontrollers is of the PI design.

The overall control law of three-element steam generator level controller can be expressed as follows:

\[\dot{m}_{fw}(s) = K_1 \left( 1 + \frac{1}{T_1 s} \right) \varepsilon_1 + K_w \left( 1 + \frac{1}{T_w s} \right) \varepsilon_w\]  \hspace{1cm} (3-17)

where \(s\) is the Laplace variable,

- \(K_1\) is the proportional level gain,
- \(T_1\) is the level error reset time,
- \(\varepsilon_1\) is the level error \((L_{ref} - L)\),
- \(K_w\) is the proportional flowrate gain,
- \(T_w\) is the flowrate error integral time,
- \(\varepsilon_w\) is the flowrate error \((\dot{m}_s - \dot{m}_{fw})\), and
- \(L_{ref}\) is the reference steam generator water level (50%).

In order to simulate the control action in the time domain, the level response can be calculated by expressing Equation (3-17) as a time differential equation:

\[\frac{d(\dot{m}_{fw}(t))}{dt} = K_1 \left( \frac{d\varepsilon_1}{dt} + \frac{\varepsilon_1}{T_1} \right) + K_w \left( \frac{d\varepsilon_w}{dt} + \frac{\varepsilon_w}{T_w} \right).\]  \hspace{1cm} (3-18)
As described in Section 3.2.3.2, steam and feedwater flowrate measurements are not used in the steam generator level controller when operating at low power. This means that several changes must be made to Equation (3-18) in order for it describe the functioning of a steam generator level controller at low power. First, feedforward control action is excluded because the flowrate error is not used. Second, the proportional level gain decreases because the inverse response gain (the term $G_{2f}$ in the Figure 3-18) is greatly increased.

A block diagram of the low-power steam generator water level controller is given in Figure 3-33. The numerical model for the single element controller is obtained by simply setting the flowrate gain term to zero. Thus, the control laws for the single element controller in the s domain and in the time domain are given by Equations (3-19) and (3-20) respectively:

\[
\dot{m}_{fw}(s) = K_i \left( 1 + \frac{1}{T_i s} \right) \varepsilon_l
\]

\[
\frac{dm_{fw}(t)}{dt} = K_i \left( \frac{de_l}{dt} + \frac{\varepsilon_l}{T_i} \right)
\]

Implementation of the control signal from the low-power level controller is achieved through use of the bypass valves. These provide much finer regulations than do the main feedwater valves that are adjusted for level control by the high power, three-element controller.
Figure 3-33  Block Diagram of Current Steam Generator Water Level Controller and Closed Loop System at Low Power Operation
3.4.1.2 Feedwater Regulating Valve and Sensors

As shown in Figures 3-32 and 3-33, the overall system dynamics depends on the characteristics of the feedwater regulating valves and measuring devices as well as on the controller and the system process. The response time of the sensors and that of the final control element (main feedwater valves or bypass valves) must be carefully evaluated to determine their impact on overall plant performance. Sometimes, the time lag associated with the sensor is longer than the process time lag and, hence, it may have a major impact on the performance of the control loop.

In this research, however, it was assumed that the dynamic characteristics of the measuring devices and the final control element act perfectly and without any time lag. This is a reasonable approximation because the steam generator level control system only uses flowrate and level signals that are obtained from differential pressure transmitters. The corresponding plant devices do have relatively short time constants.

3.4.2 Controller Analysis

The steam generator level controller is analyzed using the transfer function approach. This entails first using Laplace domain techniques to find the transfer function, then specifying the desired performance of the closed-loop system and controller, and finally determining stability criteria. The analysis of system's dynamics in the Laplace domain offers significant computational advantages. Specifically, time-dependent differential equations are converted to frequency-dependent algebraic ones. The single
element controller, which is shown in Figure 3-33, is considered here because steam generator level control problems occur mostly at low power.

3.4.2.1 Dynamic Characteristics of the Simplified Steam Generator Level Transfer Function

Transfer function analysis in the Laplace domain makes it possible to determine whether or not a system is intrinsically stable. Mathematically, if a system is open-loop unstable, it must have an open-loop transfer function that has at least one pole in the right-half of the s plane. If one were to consider the same problem in the time domain, the presence of a pole in the right-half plane corresponds to a growing exponential term. Hence, system response to a perturbation is unbounded and therefore unstable. Figures 3-34 and 3-35 show the poles and zeroes of the steam generator level transfer function at two power levels (10 %FP and 100 %FP).

Because zeroes lie in the right-half s plane at each power level, the system is of a non-minimum phase and, as was discussed in Section 3.3.1.3, its response will exhibit inverse behavior. It is important to note that the positive zeroes do not make the open-loop system unstable. Stability depends only on the poles of the transfer function. However, when the system is combined with the feedback controller, the system’s positive zeroes do affect closed-loop stability and do cause instability at high feedback gains.
Figure 3-34  Pole and Zero Map of the Simplified Transfer Function at 10 %FP
Figure 3-35  Pole and Zero Map of the Simplified Transfer Function at 100 %FP

x: poles
0.0
-0.4488 + 0.3876i
-0.4488 - 0.3876i
-0.1110

o: zeroes
-0.2611
0.1009 + 0.1853i
0.1009 - 0.1853i
3.4.2.2 Closed-Loop Transfer Function

The closed loop transfer function can be obtained from the block diagram shown in Figures 3-32 and 3-33. The transfer functions corresponding to each of its four components (process, measuring device, controller mechanism, and final control element) are given here.

Steam generator level response:

\[ L(s) = G_{fw}(s) \dot{m}_{fw}(s) + G_{d}(s) \dot{m}_{d}(s) + G_{T}(s) T_{P}(s) \]  \hspace{1cm} (3-21)

Level measuring device:

\[ L_{m}(s) = G_{ml}(s) L(s) \]  \hspace{1cm} (3-22)

Controller mechanism:

\[ \epsilon_{l}(s) = L_{ref}(s) - L_{m}(s) \]  \hspace{1cm} \text{level comparator} \hspace{1cm} (3-23)

\[ c_{l}(s) = G_{cl}(s) \epsilon_{l}(s) \]  \hspace{1cm} \text{control action} \hspace{1cm} (3-24)

Final control element:

\[ \dot{m}_{fw}(s) = G_{f}(s) c_{l}(s) \]  \hspace{1cm} (3-25)
where \( c_l(s) \) and \( L_m(s) \) are Laplace transforms of controller output and level measurement variations, respectively. The series of blocks between the comparator and the controlled output constitutes the forward path. The block for the measuring device is in the feedback or return path which goes from the controlled level output to the comparator. Equations (3-21) to (3-25) can be combined to give a single expression for the level output. It is:

\[
L(s) = \frac{G_{f_w}(s) G_f(s) G_{c_l}(s)}{1 + G_{f_w}(s) G_f(s) G_{c_l}(s) G_{m_l}(s)} L_{ref}(s) + \frac{G_{s}(s)}{1 + G_{f_w}(s) G_f(s) G_{c_l}(s) G_{m_l}(s)} \dot{r}_s(s) + \frac{G_{f}(s)}{1 + G_{f_w}(s) G_f(s) G_{c_l}(s) G_{m_l}(s)} T_p(s) .
\] (3-26)

Equation (3-26) gives the closed-loop response of the process. The first term shows the effect on the output that results from a change in the level set point. The second and third terms give the effect on the output of a change in steam flowrate and primary coolant temperature, respectively. Changes in these two quantities are referred to as load disturbances.

If the measuring devices and final control element do act perfectly and without any time delay, then the closed-loop transfer functions are:

\[
G_{sp}(s) = \frac{G_{f_w}(s) G_{c_l}(s)}{1 + G_{f_w}(s) G_{c_l}(s)}
\] (3-27)
\[ G_{\text{load,s}}(s) = \frac{G_s(s)}{1 + G_{fw}(s) G_{cl}(s)} \quad (3-28) \]

\[ G_{\text{load,T}}(s) = \frac{G_T(s)}{1 + G_{fw}(s) G_{cl}(s)} \quad (3-29) \]

where \( G_{sp}(s) \), \( G_{\text{load,s}}(s) \), and \( G_{\text{load,T}}(s) \) are the closed-loop transfer functions for a change in the setpoint, steam flowrate and primary coolant temperature, respectively. It should be noted that each transfer function has the same denominator.

### 3.4.2.3 Root Locus Analysis of Closed-Loop Transfer Function

The stability characteristics of the closed-loop response are determined by the poles of the transfer functions \( G_{sp}(s) \), \( G_{\text{load,s}}(s) \), and \( G_{\text{load,T}}(s) \). Because all three of these transfer functions have the same denominator, they each have the same poles. These are given by the solution of the equation:

\[ 1 + G_{fw}(s) G_{cl}(s) = 0 \quad (3-30) \]

Equation (3-30) is called the system’s characteristics equation because its roots define the system’s dynamic response. The roots of the characteristic equation depend on the controller dynamics and can be modified by the choice of controller type and controller gain. The proper selection of these quantities is the control engineer’s responsibility.
In order to analyze the behavior of the roots when the feedback gain is changed, a root locus plot is often used. This plot shows the roots of the closed-loop characteristic equation as a function of the feedback controller gain. With the aid of a digital computer, root locus plots can easily be obtained by using numerical techniques, such as the MATLAB Control System Toolbox, which was used in this research [G-2].

The initial phase of the study was to explain the response of the closed-loop transfer function when a single proportional feedback controller was incorporated. The proportional gain of this controller is denoted by symbol K. The root locus plots at different power levels (10 %FP, 50 %FP, and 100 %FP) are shown in Figures 3-36, 3-37, and 3-38 respectively. Grid lines that show the damping ratio and the undamped natural frequency are also drawn on the root locus plots. The damping ratio lines are drawn from 0 to 1 in steps of 0.1. Grid lines of the damping coefficient are radial while grid lines of the natural frequency are circles. The root loci start (K=0) at the poles of the open-loop transfer function and end (K = ∞) at the zeroes of asymptotes. Because the closed-loop transfer function has positive zeroes, the poles move to the right half of the s plane and the system becomes unstable as the feedback gain increases. In the root locus plot, the maximum feedback control gain which does not cause instability is easily obtained by calculating the feedback gain of the root on the imaginary axis. At 100 %FP, the maximum feedback control gain is 65.08 [kg.s⁻¹/%]. At 10 %FP, it is 4.575 [kg.s⁻¹/%]. Figure 3-39 shows the maximum feedback control gain at different power levels. The maximum feedback control gain rapidly drops because of the large inverse level dynamics effect that occurs as the operating power level decreases. Hence, in order to avoid instability, a very low feedback control gain should be used in the low power region. Compared with the maximum feedback control gain at 10 %FP (4.575 [kg.s⁻¹/%]), the nominal high power
Maximum Proportional Gain : $4.575 \left[ \frac{\text{kg/s}^{-1}}{\%} \right]$

Undamped Natural Frequency : $0.0229 \left[ \frac{1}{\text{s}} \right]$

Figure 3-36 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller ($T_1 = \infty$) at 10 %FP
Maximum Proportional Gain: 17.942 \left[ \frac{kg/s}{\%} \right]
Undamped Natural Frequency: 0.0747 \left[ \frac{1}{s} \right]

Figure 3-37 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller (T_1 = \infty) at 50 \%FP
Maximum Proportional Gain : 65.08 \text{\frac{kg/s^{-1}}{\%}}

Undamped Natural Frequency : 0.135 \text{\frac{1}{s}}

Figure 3-38 Root Locus Plot of the Closed Loop Transfer Function with Proportional Controller (T_1 = \infty) at 100 \%FP
proportional feedback control gain of the current steam generator level controller (16.59 
[kg.s\(^{-1}%/\)]) is large enough to cause system instability. Therefore, the current controller 
should use the lower value of the feedback control gain when operating at low power.

It should be noted that the curve of the maximum feedback control gain in Figure 
3-39 was calculated without integral control action. With integral control action, the 
maximum feedback control gain decreases as reset ratio (1/T\(_1\) where T\(_1\) is the level error 
integral time) increases. The feedback control gain should be set lower than its maximum 
value. For example, according to accepted controller tuning methods, such as the Ziegler-
Nicols method [L-4] the proper feedback control gain of PI type controller is calculated as 
follows:

\[
K_1 = 0.45K_{lm} \tag{3-31}
\]

where \(K_1\) is a tuned feedback control gain and \(K_{lm}\) is the maximum feedback control gain.

Figure 3-40 shows the undamped natural frequencies at different power levels. The 
Ziegler-Nicols tuning method recommends setting the integral time as follows.

\[
T_1 = \frac{2\pi}{1.2 \omega_n} \tag{3-32}
\]

where \(\omega_n\) is the undamped natural frequency. Therefore, the controller should be tuned to 
a long integral time, i.e. small reset ratio at low power.
Figure 3-39  Maximum Feedback Control Gain of Proportional Feedback Controller

Figure 3-40  Undamped Natural Frequencies vs. Power
Figure 3-41  Root Locus Plot of the Closed Loop Transfer Function with Proportional plus Integral Controller with $T_1 = 228$ s (Tuned by Ziegler-Nichols Method) at 10 %FP
Figure 3-41 shows the root locus plot of the closed-loop system at 10 %FP. The controller is of the PI type with reset time, selected by the Ziegler-Nicols tuning method, of 228 seconds. Integral control action eliminates any steady-state offset error. However, the elimination of the offset error usually comes at the expense of higher maximum deviations and a long, sluggish oscillatory response. If the feedback control gain is increased to produce a faster response, then the system becomes more oscillatory and perhaps unstable. As shown in this Figure, the maximum feedback control gain to avoid system instability decreases to $3.905 \text{[kg.s}^{-1}/\%\text{]}$. When compared to the maximum feedback control gain of the closed-loop system without integral control action, the maximum gain has decreased by 17.8 %.

Based on the Laplace domain analysis of a current single-element steam generator water level controller, it is concluded that the feedback control gain and error integral time should be adjusted as a function of the operating power in order for proper control performance to be achieved. Also, at low power, the controller must use a small feedback control gain and a long integral time. These degrade control performance but the alternative is instability.

### 3.4.3 Time Domain Analysis

Through the Laplace domain analyses, the control difficulties of the existing steam generator water level controller were examined. Time domain analyses, which entail the direct solution of the differential equations, were performed to confirm the Laplace domain analyses. The detailed nonlinear model described in Chapter Two was used to simulate steam generator water level.
The water level behavior was simulated after the feedwater flowrate was perturbed by a step change of 5.0 kg/s. Two different controller tuning constants were used as follows:

High power tuning: $K_l = 15.69 \text{ kg.s}^{-1}/\%$ and $T_l = 200 \text{ s}$

Low power tuning: $K_l = 2.377 \text{ kg.s}^{-1}/\%$ and $T_l = 200 \text{ s}$

Figure 3-42 shows the level response at 10 %FP when the steam generator level controller is tuned with two different values. If the controller is tuned with the smaller of the two gains, the level settles at the desired setpoint. However, the response is sluggish and oscillatory. The reason is that the damping ratio decreases as the feedback control gain increases as shown in Figure 3-36. The consequence of a decreasing damping ratio is that the level response changes from sluggish to faster-but-oscillatory and the overshoot and decay ratio of the closed-loop response both increase. Therefore, the system eventually displays instability. If the controller is tuned with the higher of the two gains, the system response is unstable. (Note: Because some physical property constants were out of the range of the simulation program's capability, the response was only calculated for 250 seconds after perturbation.) Hence, if the controller is even slightly off-tune from the optimum, the system response will be unstable as shown in Figure 3-43.

Figure 3-44 shows the level response at 100 %FP for the two gains. In both cases, the response is stable. However, the settling time is much longer when the smaller of the two gains is used. Therefore, in order to improve control performance at high power levels, it is better to tune the controller with the higher gain.
Figure 3-42  Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation
with Step Fashion. Current PI Controller and 10 %FP

Figure 3-43  Conventional Controller Tuning Effect During the Feedwater Flowrate
Perturbation Transients at 10 %FP
Figure 3-44  Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation with Step Fashion. Current PI Controller and 100 %FP

Figure 3-45  Steam Generator Level Response to 5 kg/s Feedwater Flowrate Perturbation with Step Fashion. (Current 3 Element PI Controller and 10 %FP)
Figure 3-45 shows the level response at 10 %FP of the three-element PI controller tuned with the higher gain. The flowrate mismatch error signal always gives the correct indication for the control action if the reset time for flowrate mismatch error is set to small value. Hence, this controller tuned $T_{iw} = 5$ s is stable even though tuned with higher gain. However, when $T_{iw} = 20$ s, this controller becomes unstable because the flowrate mismatch error cannot provide the correct control action immediately. It should be recalled that the uncertainty of the flowrate measurement during low power operation is too large to permit its use as a flowrate mismatch error signal in the controller. Hence, the option of using a higher gain three-element controller at low power does not exist.

### 3.4.4 Existing Control Problems

In summary, existing system generator level controllers have two types of problems. The first is to achieve a satisfactory means of control at low power. Specifically, in the low power region, the inverse response associated with shrink and swell effects causes control problems such as an oscillatory level response or instability. (Note: The instability problem is attributed to non-minimum phase properties.) As described in Section 3.4.2, physical systems that have an inverse response exhibit instability at higher feedback control gains. On the other hand, because the flow error signal always gives the correct indication for a control action, three-element controllers with high flow error feedback control gains can avoid system instability. However, uncertainty in flowrate measurements precludes the use of such controllers during low power operation.
The second control problem is that of tuning. If a level controller is tuned with a low feedback control gain and a long reset time, then the system response to an imposed perturbation will be stable. However, this approach degrades performance by causing a sluggish response. In addition to this, if the controller is off-tuned even slightly, its response is unstable. Stability and control performance are usually conflicting controller attributes, which must be compromised for a viable controller design. The problem in regard to steam generator level is that the proper tuning values for the feedback control gain and the integral time are functions of the operating power and their variation is quite large.
3.5 Proposed Controller

In this section, a new controller is proposed with the objective of solving the control problems associated with existing steam generator water level controllers. The intent is to achieve both excellent control performance and robustness at all power levels. Specifically, this new design permits the level feedback control gain to be tuned aggressively thereby improving performance and at the same time not incurring instability.

3.5.1 Previous Proposed Solutions to Inverse Response

Several solutions have been previously proposed to overcome the steam generator level control problems that are associated with inverse response. These are:

i) Modify the steam generation process to eliminate or reduce the inverse response phenomenon. The most important modification is to heat the feedwater during low power operation. This will reduce the vapor collapse that follows sudden increase in feedwater flowrate and hence greatly dissipate the inverse response [Ω-1]. This option is usually very expensive and often impossible because it requires retrofitting.

ii) Detune the PI feedback controller so that it is characterized by both an extremely low feedback control gain and a long reset time. This will, as described in the previous section of this thesis, avoid instability. However, it also means a severe degradation in control system performance.
iii) Enhance the existing PI control strategy by incorporating additional elements such as a feedforward term that is based on the mismatch between feedwater and steam flowrates. This is done for some current three-element controllers and/or five-element controller [T-4]. In theory, this can yield superior performance, but, in reality, it may not because a measurable disturbance variable must be available to implement a feedforward strategy and, as noted earlier, there is much uncertainty in flowrate measurements. Another possibility is to consider use of the wide range level measurement to compensate for shrink and swell effects at low power [B-7].

iv) Employ advanced or alternate control strategies. For example, Raju developed a fuzzy logic controller for drum boiler level [R-3]. In this controller, the hierarchical fuzzy control method is used. However, this controller requires that the derivative value of the level be measured together with other variable which are linear functions of the evaporator steam exit quality, downcomer flow, and evaporator rising mixture flow. The steam generator level derivative measurement is often too erratic to use as a control variable.

v) Implement a compensator that cancels the inverse response effect. There are several possibilities including: a model-based predictor [C-2], a semi-empirical compensator that uses the wide-range level measurement [B-7], or a dynamic compensator that employs a lead-lag compensator [S-1]. The advantage to the latter option is that no additional measured variables are required and implementation is relatively simple. The drawback is that a semi-empirical compensation method is needed. Also, the process dynamics must be accurately modeled in real time. This was until recently quite difficult.
The recent development of microprocessor-based controllers revived interest in compensator design. Any compensator, even non-linear ones, can be designed and easily implemented with present microprocessors. For example, Choi suggested the use of a detailed nonlinear model as the basis of a compensator [C-2]. The inverse model, which simulates steam generator characteristics given level and pressure measurements, was used to avoid the need for uncertain feedwater and steam flowrates measurements as control parameters. The idea was to compensate the measured level by means of the tube bundle mass change which can be estimated on-line from the inverse model. However, it turns out that the inverse model is unstable as described in Appendix B. Hence, it cannot be used as an on-line model-based compensator without additional development.

Another model-based compensator is derived in this research from the simplified transfer function that was described in Section 3.3.2 of this report. It can simulate steam generator level dynamics quite accurately if the system identification process is performed properly. The general concept of this compensator is based on the Smith's dead time predictor (dead time compensator) which is described in Appendix C. This compensator is relatively easy to implement as part of a microprocessor-based digital controller.

In this research, model-based inverse response compensators are designed for each perturbation parameter: feedwater flowrate, steam flowrate, and primary coolant temperature. The simplified transfer function is the basis of the compensator designs. The design approach and description of the proposed controller are addressed in the next section. In addition, the aforementioned detuning method of a current single-element PI controller is also considered. Control performance and stability are examined for both cases.
3.5.2 Feedwater Flowrate Compensator

3.5.2.1 Design Approach to Feedwater Flowrate Compensator

The Smith’s dead-time compensator, which is described in Appendix B, can be used to improve controller performance for most time-delayed processes. The general concept of a dead-time compensator can be used to cope with inverse response. This idea was originally proposed by Ienoya and Altpher [I-2]. Several other researchers have applied the concept to drum boiler level control [S-9,T-5].

The stability of a closed-loop system depends on the system response to the manipulated variable. In the case of steam generator level control, the manipulated variable is the feedwater flowrate. Hence, the level response to a perturbation in the feedwater flowrate must be stable. However, steam generator level dynamics are very complicated and, as noted earlier, level response to a feedwater flowrate change both exhibits inverse behavior and shows some initial oscillations. Therefore, for a compensator to be of benefit, it must offset these strong nonlinear responses and also maintain closed-loop system stability.

Compensation for both the inverse response behavior and the initial oscillation term can be obtained by placing a feedback compensator around the existing nominal PI controller. Figure 3-46 is a block diagram of the approach for a feedback-based compensator for feedwater flowrate. Modeling of the final control element is required to generate the compensation signal because the feedwater flowrate is taken from the controller so as to avoid using the measured value. Again, a perfect final control element is assumed for simplicity.
The measured signal is modified by a compensation signal, $L_c(s)$, which is the output of the feedwater flowrate compensator. The control input signal to the PI controller is expressed as follows:

$$L^*(s) = L_m(s) + L_c(s).$$  (3-34)

where $L^*(s)$ is the Laplace transform of the compensated signal that will be used as input to the PI controller and $L_c(s)$ is the Laplace transform of the compensation signal.

The compensation signal contains both an inverse response and a mass oscillation term. Hence, the compensator is most readily designed by using the transfer functions for those two terms except with opposite sign. Thus, from Section 3.3, we have:

- For the inverse response term: \[ \frac{G_{2f}}{\tau_{2fs} + 1} \]

- For the mass oscillation term: \[ -\frac{G_{3f}s}{(s + 1/\tau_{3fs})^2 + \omega^2}. \]

It is important that the compensation signal decay to zero under steady-state conditions. Otherwise, the controlled level would have a steady-state offset error. The mass oscillation term does die out at steady-state. However, because the inverse response transfer function consists of a first-order lag term, the compensation signal as a whole will not diminish at steady-state. To overcome this difficulty, Surgenor suggested a compensator which consists of an inverse response predictor and an impulse function [S-9]. The compensator for feedwater flowrate then has the following form:
\[ G_{cf}^* = \frac{G_{2f}}{\tau_{2f}s + 1} \cdot \frac{\alpha_f s}{\alpha_f s + 1} - \frac{G_{3f}s}{(s + 1/\tau_{3f})^2 + \omega^2} \] (3-35)

where \( G_{cf}^* \) is the transfer function of the feedwater compensator and \( \alpha_f \) is an adaptive parameter that determines the amount of compensation. If \( \alpha_f \) is set to zero, no compensating action is taken and the controller is the same as a conventional one. If \( \alpha_f \) is set to infinity, the inverse response is completely compensated. However, the difficulty that the compensation signal is not going to zero at steady-state, then returns. The first term in Equation (3-35) compensates for the inverse response. It consists of the inverse response predictor and acts to cancel the inverse response of the process and the impulse function which forces the compensation signal to zero with time constant \( \alpha_f \). Figure 3-47 shows the time behavior of the compensated level after a 5.0 kg/s feedwater flowrate increase in step fashion at 10 %FP. Curves are shown for several values of \( \alpha_f \). As \( \alpha_f \) increases, the inverse response is diminished but the steady-state offset error increases. Therefore, the value of the adaptive parameter in the feedwater compensator should be determined from a consideration of both stability and control performance. The detailed procedure for tuning this parameter is addressed Section 3.5.5.

The compensation signal is:

\[ L_c(s) = G_{cf}^* m_f(s) \] (3-36)

In order to calculate the compensation signal in Equation (3-36), the feedwater flowrate change must be known. The feedwater flowrate is the output of the digital PI controller.
under the assumption that the final control element responds perfectly. Therefore, no additional sensor is required to measure feedwater flowrate.

In order to calculate the compensation level in the time domain, two differential equations are obtained from Equation (3-35). These are:

\[
\tau_{2f}\alpha_f \frac{d^2 L_{ei}(t)}{dt^2} + (\tau_{2f} + \alpha_f) \frac{d L_{ei}(t)}{dt} + L_{ei}(t) = G_{2f}\alpha_f \frac{d m_f(t)}{dt}
\] (3-37(a))
\[
\frac{d^2 L_{\text{cm}}(t)}{dt^2} + \frac{2}{\tau_3} \frac{dL_{\text{cm}}(t)}{dt} + \left(\frac{1}{\tau_3^2} + \omega^2\right) L_{\text{cm}}(t) = G_3 \frac{d \dot{m}_f(t)}{dt}
\] (3-37(b))

where \(L_{ci}(t)\) and \(L_{cm}(t)\) are the compensation level terms for the inverse response and mass oscillation, respectively. The compensation level, \(L_c(t)\), is the sum of \(L_{ci}(t)\) and \(L_{cm}(t)\).

Equations (3-37) can be rewritten in matrix form as:

\[
\begin{bmatrix}
\frac{d}{dt} L_{ci}(t) \\
\frac{d}{dt} L_{cm}(t)
\end{bmatrix} =
\begin{bmatrix}
0 & 1 \\
-\frac{1}{\tau_2 \alpha_f} & -\frac{\tau_2 + \alpha_f}{\tau_2 \alpha_f}
\end{bmatrix}
\begin{bmatrix}
L_{ci}(t) \\
L_{cm}(t)
\end{bmatrix} +
\begin{bmatrix}
0 \\
\frac{G_2}{\tau_2}
\end{bmatrix}
\frac{d \dot{m}_f(t)}{dt}
\] (3-38(a))

or

\[
\begin{bmatrix}
\frac{d}{dt} L_{ci}(t) \\
\frac{d}{dt} L_{cm}(t)
\end{bmatrix} =
\begin{bmatrix}
0 & 1 \\
-\left(\frac{1}{\tau_3^2} + \omega^2\right) & -\frac{2}{\tau_3}
\end{bmatrix}
\begin{bmatrix}
L_{ci}(t) \\
L_{cm}(t)
\end{bmatrix} +
\begin{bmatrix}
0 \\
\frac{G_3}{\tau_3}
\end{bmatrix}
\frac{d \dot{m}_f(t)}{dt}
\] (3-38(b))

where \(L'_{ci}(t) = \frac{dL_{ci}(t)}{dt}\) and \(L'_{cm}(t) = \frac{dL_{cm}(t)}{dt}\)

3.5.2.2 Laplace Domain Stability Analysis

In order to study the stability of the closed-loop system associated with the feedwater flowrate compensator, Laplace domain analysis is performed again. The
Figure 3-48 Equivalent Block Diagram of Feedback Type Feedwater Flowrate Compensator
compensated controller consists of two closed loops. One is the compensating closed-loop and the other is the feedback control closed-loop. Figure 3-46 with its inner compensator loop can be redrawn to give an equivalent single loop as shown in Figure 3-48. In Figure 3-48, \( G_{\text{ser}}^* \) represents the equivalent series compensator and is expressed as follows:

\[
G_{\text{ser}}^*(s) = \frac{1}{1 + G_{\text{cl}}(s)G_{\text{cf}}^*(s)}
\]  

(3-39)

From Figure 3-48, the closed-loop transfer function is expressed as:

\[
G_{sp}(s) = \frac{G_{\text{fw}}(s) G_{\text{cl}}(s) G_{\text{ser}}^*(s)}{1 + G_{\text{fw}}(s) G_{\text{cl}}(s) G_{\text{ser}}^*(s)}
\]  

(3-40)

Upon substitution of Equation (3-39) into Equation (3-40), the closed-loop transfer function becomes:

\[
G_{sp}(s) = \frac{G_{\text{fw}}(s) G_{\text{cl}}(s)}{1 + G_{\text{fw}}(s) \left( G_{\text{fw}}(s) + G_{\text{cf}}^*(s) \right)}
\]  

(3-41)

The stability characteristics of the closed-loop transfer function are determined by its poles. Again, the response of a closed-loop transfer function is examined through a root locus plot when a single proportional feedback controller is incorporated. The root locus plots of the closed-loop system for two different adaptive parameters (10 seconds and 100 seconds) at 10 %FP level are shown in Figures 3-49 and 3-50, respectively. When the adaptive parameter is chosen to be 10 seconds, the maximum feedback control gain is 4.89
Figure 3-49  Root Locus Plot of the Closed Loop Transfer Function Associated with Compensator ($\alpha_f = 10$ s) at 10 \%FP
Figure 3-50  Root Locus Plot of the Closed Loop Transfer Function Associated with Compensator ($\alpha_f = 100 \, s$) at 10 %FP
[kg·s⁻¹%/]. This is increased by only 7.0% as compared to that for existing uncompensated controllers. If the gain is made any greater, the system is driven unstable. However, if the adaptive parameter is chosen to be 100 seconds, all roots of the characteristic equation are located in the left half plane of the Laplace domain and the closed-loop response associated with proportional controller is guaranteed to be stable.

Based on these results, it can be concluded that a compensator based on the simplified transfer function can mitigate the instability problem associated with controller tuning at low power. Also, the system is stable if an adaptive parameter in the feedwater flowrate compensator is chosen to be 100 seconds.

3.5.3 Load Parameter Compensators

3.5.3.1 Control Performance Index

Two important design goals for every controller are to maintain the maximum deviation as small as possible and, when perturbed, to return to the desired operating point in the shortest possible time. Unfortunately, these two performance goals lead to conflicting characteristics in a feedback controller. For example, the settling time increases when the overshoot error decreases. Therefore, controller performance often represents a balance between these conflicting characteristics. In order to compare different controller designs, it is often useful to compare performance indices of them.

There are two different kinds of control performance indices, simple performance indices and time-integral performance indices. The former represent some characteristic
features of the closed-loop response of the system such as overshoot, rise-time, settling-time, or decay ratio. In contrast, the latter represent the dynamic shape of the complete closed-loop response from perturbation until attainment of a new steady-state. Therefore, a time-integral performance index is based on the entire response of the process. There are several kinds of time-integral performance indices. In this research, the IAE (Integral of Absolute Value of the Error) and ISE (Integral of Square Value of Error) are used to evaluate the steam generator level controller. IAE and ISE can be calculated as follows:

\[
IAE = \int_0^{t_s} |\varepsilon(t)| \, dt
\]  
\[
ISE = \int_0^{t_s} (\varepsilon(t))^2 \, dt
\]

(3-42)  
(3-43)

where \(t_s\) is the settling time and \(\varepsilon(t)\) is the level error. In addition to these indices, the maximum level error is also often examined.

3.5.3.2 Design Approach of Load Parameter Compensators

Most previous work [S-9,T-5] on the control of systems that exhibit inverse response behavior focused on the stability of the closed-loop response. These efforts centered on the design of manipulated variable compensators. However, load parameters, such as steam flowrate and primary coolant temperature, also result in an inverse response. This is especially true for a steam generator level controller where the level setpoint remains
constant while the load changes. In that case, the steam generator level controller tries to eliminate the impact of the load changes and maintain the level at its setpoint. The resulting inverse response from the change in the load parameters can therefore also lead to a transitory feedback response in the wrong direction. Thus, even though compensation of the manipulated variable (feedwater flowrate) can guarantee stability, it cannot guarantee that controller performance will be significantly improved.

Figure 3-51 shows the trend in steam generator level after a power increase from 10 %FP to 15 %FP with a 5.0 %FP/minute ramp rate. The steam generator water level controller was compensated by a feedwater flowrate compensator. For purposes of comparison, the time behavior of the steam generator level when regulated by an existing PI controller that was tuned by the Ziegler-Nichols method is also shown in this figure. In the compensation case, the feedback control gain was set at 5.0 \( [\text{kg} \cdot \text{s}^{-1} / \%] \) which is a factor of 2.4 greater than that of the existing controller \( (2.1 [\text{kg} \cdot \text{s}^{-1} / \%]) \). The process remains stable even when the higher feedback control gain is used. However, in both cases, the control performance is almost the same. Table 3.4 summarizes the IAE and ISE for 1000 seconds after the start of the power perturbation. The feedwater compensator reduces the IAE and ISE by 40 % and 54 %, respectively. However, because a greater feedback control gain is used, the inverse response effects associated with load disturbances produce a bigger maximum error. Therefore, load parameter compensation is needed to improve control performance during transients that involve changes in demand.

The design of load parameter compensators is similar to that for feedwater flowrate compensation. Figure 3-52 is a block diagram of the load parameter compensators. The inverse response of the measured level is offset by using a model-based compensator. However, because load parameter compensation is achieved in an open-loop manner, the
Figure 3-51  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate

Table 3-4  Control Performance During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%s]</th>
<th>ISE [%^s]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>without Compensation (A) ( (K_i = 2.1 \text{ kg/s } / % \text{ and } T_i = 228 \text{ s}) )</td>
<td>166.2</td>
<td>53.4</td>
<td>12.7</td>
</tr>
<tr>
<td>with Feedwater Compensation (B) ( (K_i = 5.0 \text{ kg/s } / % \text{ and } T_i = 343 \text{ s}) )</td>
<td>100.2</td>
<td>24.6</td>
<td>14.6</td>
</tr>
<tr>
<td>Difference (B-A)/A [%]</td>
<td>- 39.7</td>
<td>- 53.9</td>
<td>15.0</td>
</tr>
</tbody>
</table>
Figure 3-52  Block Diagram of Load Parameter Compensators
resulting compensators differ from those developed for feedwater flowrate. In particular, the measured or estimated magnitude of the load parameter perturbation is required to avoid the need for steam flowrate measurements with their concomitant uncertainty. On the assumption that steam flowrate is proportional to the reactor neutronic power, steam flowrate can be estimated from these variables. These are the neutronic power, the feedwater flowrate, and the feedwater temperature. Thus,

\[ \dot{m}_{se} = \frac{Q_N}{h_g - h_{fw}} \]  

(3-44)

where \( \dot{m}_{se} \) is the estimated steam flowrate, \( Q_N \) is the measured neutron power, \( h_g \) is the steam enthalpy, and \( h_{fw} \) is the feedwater enthalpy.

For steam flowrate, a compensator based on the simplified transfer function is designed in the same form as the feedwater flowrate compensator. Thus,

\[ G_{cs}^* = \frac{G_{2s}}{\tau_{2s}s + 1} \cdot \frac{\alpha_s s}{\alpha_s s + 1} \]  

(3-45)

where \( G_{cs}^* \) is the transfer function of the feedwater compensator and \( \alpha_s \) is an adaptive parameter which determines the amount of compensation. If \( \alpha_s \) is set to zero, no compensating action is taken. Conversely, if \( \alpha_s \) is set to infinity, the inverse response is completely offset. This adaptive parameter must, of course, be determined from a tuning procedure.

207
For the primary coolant temperature compensator, the process transfer function of primary coolant temperature change is modified so that it decays to zero at steady-state. Equation (3-7) shows that the level response will not decay to zero if \( G_{1T} \neq G_{2T} \). Therefore, \( G_{1T} \) was taken as equal to \( G_{2T} \). Under that assumption, the primary coolant temperature compensator \( (G_{cT}^*) \) is:

\[
G_{cT}^*(s) = -G_{1T} \left( \frac{1}{(1 + \tau_{1T} s)} - \frac{1}{(1 + \tau_{2T} s)} \right)
\]  

(3-46)

Because the compensation factor for primary coolant temperature decays to zero at steady-state, an impulse function that forces such decay is not required. The compensated levels therefore are:

\[
L_{cs}(s) = G_{cs}^*(s) \dot{m}_s(s)
\]  

(3-47)

\[
L_{cT}(s) = G_{cT}^*(s) T_p(s)
\]  

(3-48)

The corresponding time differential equations are:

\[
\tau_{1s} \alpha_s \frac{d^2 L_{cs}(t)}{dt^2} + (\tau_{1s} + \alpha_s) \frac{dL_{cs}(t)}{dt} + L_{cs}(t) = -G_{1s} \alpha_s \frac{dm_s(t)}{dt}
\]  

(3-49)

\[
\tau_{1T} \tau_{2T} \frac{d^2 L_{cT}(t)}{dt^2} + (\tau_{1T} + \tau_{2T}) \frac{dL_{cs}(t)}{dt} + L_{cs}(t) = -G_{1T} (\tau_{2T} - \tau_{1T}) \frac{dT_p(t)}{dt}
\]  

(3-50)
Equations (3-49) and (3-50) can be rewritten in matrix form as:

\[
\frac{d}{dt} \begin{bmatrix} L_{cs}(t) \\ L_{cs}(t) \end{bmatrix} = \begin{bmatrix} 0 & 1 \\ -\frac{1}{\tau_{1s}} & -\frac{\tau_{1s} + \alpha_s}{\tau_{1s}} \end{bmatrix} \begin{bmatrix} L_{cs}(t) \\ L_{cs}(t) \end{bmatrix} + \begin{bmatrix} 0 \\ -\frac{G_{1s}}{\tau_{1s}} \end{bmatrix} \frac{dm_{s}(t)}{dt} \tag{3-51}
\]

and

\[
\frac{d}{dt} \begin{bmatrix} L_{cT}(t) \\ L_{cT}(t) \end{bmatrix} = \begin{bmatrix} 0 & 1 \\ -\frac{1}{\tau_{1T}\tau_{2T}} & -\frac{\tau_{1T} + \tau_{2T}}{\tau_{1T}\tau_{2T}} \end{bmatrix} \begin{bmatrix} L_{cT}(t) \\ L_{cT}(t) \end{bmatrix} + \begin{bmatrix} 0 \\ -\frac{G_{1T}(\tau_{2T} - \tau_{1T})}{\tau_{1T}\tau_{2T}} \end{bmatrix} \frac{dT_{p}(t)}{dt} \tag{3-52}
\]

where \( L_{cs}(t) \) and \( L_{cT}(t) \) are the time derivatives of the compensated levels for steam flowrate and primary coolant temperature, respectively.

### 3.5.4 Feedforward Control

The feedback level controller does not 'know' what the feedwater flowrate should be for any given set of conditions. Hence, it changes the feedwater flowrate until the compensated level and level setpoint are in agreement. However, as a practical matter, the inverse response that is created by a steam flowrate disturbance is not completely compensated, and, as a result, the level is not perfectly controlled. Rather it remains nearly
constant until the inversion time which is defined as the time required for level to cross its steady-state value. This situation can be rectified through the use of feedforward control.

Feedforward action is added to improve control system performance. For the case at hand, it entails adding the change in the estimated steam flowrate directly to the PI controller's output. Figure 3-53 shows the steady-state mass inventory of a steam generator as a function of power. The mass inventory decreases as power increases because more vapor exists at higher power. Therefore, the mass inventory should be offset

Figure 3-53  Steady State Steam Generator Mass Inventory vs. Operating Power
when power increases and supplemented when power decreases. Dynamic lag compensation is used for this purpose with the feedforward control output expressed as:

\[
\hat{m}_{ff}(s) = \frac{1}{\alpha_{ff}s + 1} \hat{m}_{se}(s)
\]  

(3-53)

where \(\hat{m}_{ff}(s)\) is the Laplace transform of the feedforward controller output and \(\alpha_{ff}\) is the time constant for the lagged response. It should be chosen with care so as to achieve best possible control performance.

Figure 3-54 shows the overall schematic of the proposed controller. It consists of a PI controller and three different compensators designed to offset the inverse response associated with feedwater flow, steam flow, and primary coolant temperature.

### 3.5.5 Controller Tuning

The proposed controller has five adjustable parameters. These are the feedback control gain and the reset time of the PI controller \((K_f \text{ and } T_r)\), the adjustable parameters in the inverse response compensators \((\alpha_f \text{ and } \alpha_g)\), and the time constant of the lag term in the feedforward controller \((\alpha_{ff})\). In order to achieve superior control performance, the proposed controller must be properly tuned. Optimum tuning for these control parameters depends not only on control performance criteria but also on the type of disturbance that is encountered. For example, a tuning action that minimizes IAE for a step change in load will not minimize IAE for a ramp load change. Also, a tuning that minimizes IAE for a step
change in load will not minimize ISE for the same transient. Optimum tunings also vary with the nature of the process.

There are a variety of ways to conduct an optimum tuning of a PI feedback controller. These include semi-empirical and theoretical methods as well as trial and error. There are two semi-empirical methods, the Ziegler-Nichols method (described in Section 3.4) and Cohen-Coon method [O-2]. To use the Ziegler-Nichols method, the maximum feedback control gain of the closed-loop system must be known. However, because the compensator renders the closed-loop process associated with the proportional controller is stable, the Ziegler-Nichols method cannot be applied to the proposed controller. The Cohen-Coon method is a process reaction method. In this method, the process is approximated using capacity and deadtime terms. If the process differs from this approximation, the Cohen-Coon method cannot be applied, either. A further complication is that because several compensators are involved, the proposed controller is too complex to be tuned by theoretical methods.

In this research, the time-integral performance criteria, IAE and/or ISE, were used to identify optimum tuning parameters. For the feedback control gain and reset time ($K_i$ and $T_i$), a simple criterion (0.316 decay ratio) was used. The feedback control gain was set at 5.0 [$\text{kg} \cdot \text{s}^{-1}/\%$] and the reset time at 343 seconds at 10 %FP. These settings were used over the entire power region. In addition, a feedback control gain of 15.69 [$\text{kg} \cdot \text{s}^{-1}/\%$] and a 343 second reset time were used to demonstrate stability.

Figure 3-55 shows the time behavior of steam generator level when $\alpha_{cf}$ and $\alpha_{cs}$ were set at 10, 100, and 1000 seconds respectively. As shown in this figure, when both of these adjustable parameters were set to 100 seconds, the best control performance was
achieved. For the lag time constant in the feedforward controller, a value of 50 seconds was used. It is time-consuming to identify the optimum tuning parameters by trial and error. Therefore, it is important to realize that the values given here for the adjustable parameters may not be the best possible ones.

Figure 3-55  The Effect of Amount of Inverse Response Compensation During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate
3.6 Evaluation of Proposed Controller

In this subsection, studies in which the proposed controller was evaluated by application to different transients are reported. The detailed nonlinear simulation model described in the previous chapter was used to simulate level response. A single power module with a single steam generator simulation option was used to evaluate the controller’s performance for feedwater flowrate perturbations and power transients. Four power modules were used to study the controller’s performance for unbalanced load transients in the multi-modular power plants. The performance of the steam generator level controller was evaluated at various power levels by comparing the IAE, ISE, and the maximum level error. The controllers studied included both the detuned, single-element PI controller and the compensated controller that was developed here. The latter was used to control both the single-module plant and the four-module, multi-modular plant.

3.6.1 Feedwater Flowrate Perturbation Transients

The reference transient studied was a 5.0 kg/s step increase in feedwater flowrate from steady-state with core and turbine power held constant. The objective of the controller was manipulate the feedwater flowrate so as to maintain the desired steam generator level. The conventional, single-element PI controller was tuned by the Ziegler-Nichols method while the base case compensated controller developed here was tuned with a 0.316 decay ratio and a reset time of 343 seconds (other constants were also used with the compensated controller to study variations).
Figure 3-56 shows the time behavior of the steam generator level for 1000 seconds following the reference transient. The conventional, single-element PI controller stabilized the level but its response showed some oscillations. However, the controller designed here both compensated for inverse response effects and provided a less oscillatory response. Also, it kept the level error at a smaller value throughout the transient.

Figure 3-56 also shows the level response at 10 %FP when the compensated controller was tuned with a gain of 15.69 [kg·s⁻¹/%]. Reset times of both 200 seconds and 343 seconds were used. Both cases show stable level trends. When tuned with a higher gain, the controller not only guaranteed stability but also performed better. It was decided that, even though a high feedback control gain can improve controller performance during feedwater flowrate perturbations, the feedback control gain should be set at 5.0 [kg·s⁻¹/%] for power ramps (up or down) transients because in power transients, imperfect steam flowrate compensation might degrade performance as a result of small errors in the estimated steam flowrate.

Table 3-5 summarizes the results of the feedwater flowrate perturbation transients. The compensated controller that was designed here improved control performance in all cases. It reduced the IAE by 54.4 % when the gain was set to 5.0 [kg·s⁻¹/%] and, when the gain was set at 15.69 [kg·s⁻¹/%], it reduced the IAEs by 80 % to 85 %. The ISE and maximum error were also reduced significantly with the amount ranging from a factor of 2.5 to 50.
Figure 3-56  Steam Generator Level During the Feedwater Flowrate Perturbation Transients at 10 %FP

Table 3-5  Control Performance During the Feedwater Flowrate Perturbation Transients at 10 %FP

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%s]</th>
<th>ISE [%^2.s]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) without Compensation (K_i = 2.1 kg/s /% and T_i = 228 s)</td>
<td>30.93</td>
<td>2.07</td>
<td>2.86</td>
</tr>
<tr>
<td>b) with Compensation (K_i = 5.0 kg/s /% and T_i = 343 s)</td>
<td>14.08</td>
<td>0.42</td>
<td>1.17</td>
</tr>
<tr>
<td>c) with Compensation (K_i = 15.69 kg/s /% and T_i = 343 s)</td>
<td>4.77</td>
<td>0.05</td>
<td>0.44</td>
</tr>
<tr>
<td>d) with Compensation (K_i = 15.69 kg/s /% and T_i = 200 s)</td>
<td>4.05</td>
<td>0.04</td>
<td>0.41</td>
</tr>
</tbody>
</table>
3.5.2 Power Transients

Figure 3-57 shows the steam generator level response during a power increase from 10 \%FP to 15 \%FP at a ramp rate of 5.0 \%FP/minute. The power increase required one minute and the power was then to be maintained at 15 \%FP. Both a conventional single-element PI controller tuned by the Ziegler-Nichols method and the compensated controller developed here were used. The latter showed better control performance and a more gradual transition to the new equilibrium. The conventional PI controller did yield a stable response but it was more oscillatory.

Table 3-6 summarizes the time integral control performance indices for the runs. The compensated controller significantly improved control performance in all cases. Specially, it reduced the IAE and ISE by one-third and one-sixth, respectively. The maximum error was also reduced by 40 \% when feedback gain was set to 5.0 \[\text{kg} \cdot \text{s}^{-1}/\%\]. However, higher feedback control gains lead to bigger maximum level errors.

Figure 3-58 shows the time behavior of steam and feedwater flowrates. The compensated controller increased the feedwater flowrate gradually until it reached steady-state. In contrast, the conventional controller caused oscillations and at one point decreased the feedwater flowrate to zero because of inverse response effects.

Figure 3-59 shows the steam generator level response during a power decrease from 20 \%FP to 15 \%FP at a rate of 5.0 \%FP/minute. Power was then to be maintained at 15 \%FP. Table 3-7 summarizes the time integral control performance index. Again, the compensated controller significantly improved performance.
Figure 3-57  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate

Table 3-6  Control Performance During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%s]</th>
<th>ISE [%^2s]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) without Compensation, 3 Element PI</td>
<td>166.2</td>
<td>53.4</td>
<td>12.67</td>
</tr>
<tr>
<td>(Ki = 2.1 kg/s /% and Ti = 228 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>b) with Compensation</td>
<td>46.3</td>
<td>7.7</td>
<td>7.85</td>
</tr>
<tr>
<td>(Ki = 5.0 kg/s /% and Ti = 343 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>c) with Compensation</td>
<td>42.1</td>
<td>11.7</td>
<td>14.88</td>
</tr>
<tr>
<td>(Ki = 15.69 kg/s /% and Ti = 343 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Figure 3-58  Steam and Feedwater Flowrate During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate

Figure 3-60 shows the steam generator level response during a power ramp from 95 %FP to 100 %FP at a rate of 5.0 %FP/minute. Two feedback control gains (5.0 \( [\text{kg} \cdot \text{s}^{-1/\%}] \) and 15.69 \( [\text{kg} \cdot \text{s}^{-1/\%}] \)) were used. A three-element conventional PI controller was also used. As shown in this figure, the proposed controller provided superior performance at this high power. IAE and ISE were only 7.52 \( [\% \cdot \text{s}] \) and 0.15 \( [\%^2 \cdot \text{s}] \), respectively.
Figure 3-59  Steam Generator Level During Power Ramp Transients from 15 %FP to 10 %FP at a 5.0 %/minute Ramp Rate

Table 3-7  Control Performance During Power Ramp Transients from 15 %FP to 10 %FP at a 5.0 %/minute Ramp Rate

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%.s]</th>
<th>ISE [%^2.s]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) without Compensation</td>
<td>173.9</td>
<td>59.6</td>
<td>14.98</td>
</tr>
<tr>
<td>(Ki = 2.1 kg/s /% and Ti = 228 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>b) with Compensation</td>
<td>65.2</td>
<td>9.1</td>
<td>6.88</td>
</tr>
<tr>
<td>(Ki = 5.0 kg/s /% and Ti = 343 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>c) with Compensation</td>
<td>64.8</td>
<td>9.8</td>
<td>7.49</td>
</tr>
<tr>
<td>(Ki = 15.69 kg/s /% and Ti = 343 s)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Figure 3-60  Steam Generator Level During Power Ramp Transients from 95 %FP to 100 %FP at a 5.0 %/minute Ramp Rate

Table 3-8  Control Performance During Power Ramp Transients from 95 %FP to 100 %FP at a 5.0 %/minute Ramp Rate

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%s]</th>
<th>ISE [%²s]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) without Compensation, 3 Element PI (K_i = 15.69 kg/s /% and T_i = 200 s, K_w = 1 kg/s / kg/s and T_i = 200 s)</td>
<td>15.52</td>
<td>0.46</td>
<td>1.38</td>
</tr>
<tr>
<td>b) with Compensation (K_i = 5.0 kg/s /% and T_i = 343 s)</td>
<td>7.52</td>
<td>0.15</td>
<td>1.40</td>
</tr>
<tr>
<td>c) with Compensation (K_i = 15.69 kg/s /% and T_i = 343 s)</td>
<td>7.26</td>
<td>0.21</td>
<td>1.66</td>
</tr>
</tbody>
</table>
The above simulations showed that, even if the compensated controller uses only a single element PI controller, its control performance is adequate for high power operation. Therefore, the proposed compensated controller can be used over the entire power region and flowrate measurements are not needed.

### 3.6.3 Application to the Multi-Modular Power Plant

The compensated controller was applied to steam generators configured in a PWR-type multi-modular power plant. The reference transient was to increase the module #1 power from 10 %FP to 15 %FP while the other modules remained at 10 %FP. Turbine power was increased from 10 %FP to 11.25 %FP. The feedback control gain was set at $5.0 \, \text{[kg}\cdot\text{s}^{-1}/\%\text{]}$ and the reset time at 343 seconds. Figure 3-61 shows the level of each steam generator during the reference transient. The module #1 steam generator level is oscillatory with the amplitude of the oscillations growing in time. Those of the other modules also show slowly growing oscillations. These oscillations in level are the result of the unbalanced power operation. Because the power was increased for only one module, the pressure balance among the steam generators and the main steam line header was disturbed with the result that steam flowrate oscillated as shown in Figure 3-62. The steam flowrate estimator uses the reactor's neutronic power as its input. The estimator could not predict the steam flowrate oscillations in a given steam generator because the neutronic power is a property of the system as a whole. If power in all four modules was increased at same rate, then the pressure balance among the steam generators and the main steam line header would be maintained and the time behavior of the steam generator level would be stable as shown in Figure 3-63. Therefore, in unbalanced power operation, a different and more restricted steam generator controller tuning is required. Figure 3-64 shows each
Figure 3-61  Steam Generator Level During Power Ramp Transients from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate on Only Module #1

Figure 3-62  Steam Flowrate During Unbalanced Power Transient from 10 %FP to 15 %FP at a 5.0 %/minute Ramp Rate on Only Module #1
Figure 3-63  Steam Generator Level During Balanced and Unbalanced Power Transients

Figure 3-64  Steam Generator Level of Module #1 During Unbalanced Power Transients from 10%FP to 15%FP at a 5.0%/minute Ramp Rate on Only Module #1
steam generator's level when the feedback control gain is set at \(2.0 \text{[kg}\cdot\text{s}^{-1}]/\text{%/s}]\). In this case, the levels remain stable. Figure 3-65 shows the steam flowrates from each steam generator. These are stabilized at the desired point.

Other unbalanced power transients were also simulated. For example, module #1 power was increased from 15 %FP to 20 %FP and module #2 power was decreased from 15 %FP to 10 %FP both at a ramp rate of 5.0 %FP/minute. Power in the other two modules was held at 15 %FP. The turbine power was also maintained at 15 %FP. The feedback control gain of the compensated controller was set to \(2.0 \text{[kg}\cdot\text{s}^{-1}]/\text{%/s}]\). Figure 3-66 shows the response of each steam generator's level during the above transient. As would be expected, the module #1 steam generator shows level swelling and module #2 shows level shrink. However, the compensated controller stabilized the level in both cases.

In summary, the proposed controller was successfully used for steam generator level control in PWR-type, multi-modular power plants and it showed reasonable performance for unbalanced load operation provided that the controller is tuned with a low feedback control gain. Additional simulation results from multi-modular power plant transients are presented in the next chapter of this report.
Figure 3-65  Steam Flowrate During Unbalanced Load Transient when PI Controller is Tuned to Lower Feedback Control Gain (2.0 kg/s/%)

Figure 3-66  Steam Generator Level During Unbalanced Power Transient (Module #1: from 15 %FP to 20 %FP, Module #2: from 15 %FP to 10 %FP and Module #3 and #4: Stay at 15 %FP)
3.6.4 Sensitivity Study of the Compensation Parameters

The controller developed here adopts inverse-response compensation techniques which are derived from a transfer function model. Even the simplified form of the model that was used here has many undetermined parameters which must be identified through numerical experiments. These studies are important because the controller's overall performance depends on the accuracy of the simplified transfer function parameters.

For the sensitivity study, it was assumed that the compensator was designed based on an incorrect model. An error of 50 % was either added or subtracted to all the correctly calculated parameters of Equations (3-5) to (3-7). The reference case was a balanced power ramp from 10 %FP to 15 %FP at a ramp rate of 5.0 %FP/minute. The feedback gain was set at $5.0\left[\text{kg}\cdot\text{s}^{-1}/\%\right]$ and the reset time at 343 seconds. It is important to recall that a conventional, single element PI controller results in a growing oscillation in the steam generator level response if feedback gain is set greater than $4.0\left[\text{kg}\cdot\text{s}^{-1}/\%\right]$.

Figure 3-67 shows the time behavior of the steam generator level during the reference transient. Use of the controller with the inaccurate parameters resulted in stable control of the steam generator level. However, its performance was, as would be expected, degraded. In particular, when compared to the level response of the correct controller, it was more oscillatory. However, it did settle out quickly when a -50 % error was imposed on the compensator design. The reason is that, while shorter time constants do result in a more imperfect compensation, the erroneous compensating signal itself dies out more rapidly.
Table 3-10 summarizes the overall results. The incorrectly designed compensators increased IAE by 12% ~ 34% and ISE by 27% ~ 45%. The maximum level error was also increased. This was especially true when a -50% error was imposed on the compensator design. In that case, the maximum level error increased by 32% because of the imperfectness of inverse response compensation. However, the overall level trend is not that of a stable process.

Based on these results, it was concluded that the compensated controller that was designed here was robust to errors that occurred during system identification. Specially, even if the compensation was based on a somewhat incorrect transfer function model, the controller stabilized level disturbances with its performance being only slightly degraded.

Table 3-9  Control Performance During Power Ramp Transients When Compensators are Designed Based on Incorrect Parameters

<table>
<thead>
<tr>
<th>Cases</th>
<th>IAE [%s]</th>
<th>ISE [%s^2]</th>
<th>Max Error [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) Exact Parameters (A)</td>
<td>46.3</td>
<td>7.7</td>
<td>7.85</td>
</tr>
<tr>
<td>b) + 50% Error on Parameters (B)</td>
<td>61.9</td>
<td>9.8</td>
<td>7.93</td>
</tr>
<tr>
<td>Difference (B-A)/A [%]</td>
<td>(34%)</td>
<td>(27%)</td>
<td>(1%)</td>
</tr>
<tr>
<td>c) - 50% Error on Parameters (C)</td>
<td>54.7</td>
<td>11.2</td>
<td>10.35</td>
</tr>
<tr>
<td>Difference (C-A)/A [%]</td>
<td>(12%)</td>
<td>(45%)</td>
<td>(32%)</td>
</tr>
</tbody>
</table>
Figure 3-67  Steam Generator Level During Power Ramp Transients When Compensators are Designed Based on Incorrect Parameters
3.7 Chapter Summary

Control problems associated with existing U-tube steam generator water level control systems were reviewed. Steam generator water level dynamics were examined and a simplified existing transfer function model was improved by adding the primary coolant temperature perturbation. The parameters of this revised simplified transfer function model were identified by numerical experiments using a detailed non-linear simulation model.

A robust U-tube steam generator water level control system was proposed to replace the current, single-element PI level controller. The proposed controller should be of benefit in the operation of U-tube steam generators that exhibit the well known ‘shrink and swell’ effects. Improved steam generator level control performance is obtained through the use of a model-based compensation technique. Three different types of compensators, one for feedwater flowrate, steam flowrate, and primary coolant temperature, were developed. These compensators were designed from a simplified transfer function model for the purpose of offsetting inverse response behavior. Feedforward control was also applied to improve control performance. Finally, in order to avoid the need to use an uncertain measured steam flowrate, a steam flowrate estimator was designed. It uses the measured neutron power, steam generator pressure, and feedwater temperature to approximate the actual steam flowrate.

The proposed controller ensures stability at both low and high power even when tuned with a high feedback control gain. Also, because it does not use measured flowrates, it is robust to flowrate measuring errors. Simulation studies of various transients show that the use of this new controller greatly reduces the effect of inverse response and significantly improves the controllability of steam generator level. The proposed controller
can be applied to the operation of steam generators in PWR-type, multi-modular power plants. In this case, continued use of a high feedback control gain causes oscillatory behavior because of steam flow rate oscillations between each steam generator and the main steam line header. The compensators do not correct for this because they utilize each reactor's neutronic power cannot reflect changes in steam flow split that occur during unbalanced maneuvers. However, if the proposed controller is tuned with a low feedback control gain, it displays acceptable control performance during operations with unbalanced loads. The proposed controller also exhibits robustness to errors in the parameters of the simplified model.
Chapter Four

Power Control in PWR-Type Multi-Modular Power Plants

4.1 Introduction

4.1.1 General

The operation of a multi-modular power plant should allow for unbalanced operation in which each module operates at a different power level. This is necessary to provide a high capacity factor because while one power module is shutdown, the others can continue to supply steam to the turbine generator. In fact, this mode of operation will probably be used to permit refueling without the need for taking the entire plant off-line. This in turn means that each module in a multi-modular plant will be routinely operated at a different load factor so as to stagger the times at which individual cores must be refueled.
The operation of each reactor at a different load creates a complex control problem because all power modules supply steam to a common header and, the steam flowrate from each individual module depends on both the hydraulic conditions in that module and on the pressure in the common steam line header. Thus, conditions in each steam generator and the common steam line header must be coordinated to allow each power module to supply a desired fraction of the total plant load.

If conventional control methods (PWR-type analog controllers and licensed operator supervisory control) are to be employed, then there will be a tremendous number of control signals and operating parameters to be monitored and processed. Each power module will require its own complete control room with the usual complement of operators. The resulting construction and operating costs could make a multi-modular power plant prohibitively expensive. New control techniques that rely on digital technology can avoid these costs by permitting use of a single control room. The resulting control system would be computer-based and operation would be highly automated. In this case, the human operator's role would be that of a well-informed supervisor and strategist.

At present, the direct digital control of nuclear power plants is not practiced in the United States. Regulations require that any system which is needed for the shutdown of the plant or which is associated with safety be subject to exhaustive quality control tests. The result is that only proven technology is employed. In the past, this has meant that only hard-wired analog devices could be used. This situation is slowly changing as is illustrated by the approval of digital feedwater systems in several power plants and by digital control experiments conducted at the Massachusetts Institute of Technology's research reactor, MITR-II. Also, one method for the control of neutronic power via a digital computer has
been licensed by the U.S. Nuclear Regulatory Commission for use on the MITR-II [B-1, B-2].

If properly designed and implemented, a digital control system may enhance safe operation. Nevertheless, concerns exist about the digital option. One of the advantages to considering digital control for multi-modular power plants is that those plants can be made passively safe. The presence of the passive protection features that are planned for multi-modular power plants should make automatic control strategies more acceptable to both licensing authorities and the public. Also, the inherently safe characteristics of these plants should lower demand on the control system.

This Chapter presents the development of closed-loop digital strategies for the control of power in PWR-type multi-modular plants. The goal of these strategies is to achieve a balance between the total electric power demand from the grid and the need to distributes load unevenly among the plant's modules. In that regard, a multi-tiered (or hierarchical) approach is used that includes a plant power controller for the first tier and module power controllers at the lower tiers. A general concept for a fault-tolerant multi-tiered power control strategy is presented.

### 4.1.2 Power Control System Functions

The basic function of a power plant is to supply electrical energy to the distribution network on demand. To meet this load, the nuclear power plant must generate heat in proportion to the demand and the nuclear steam supply system (NSSS) must respond with the correct, low-moisture, steam flow to the turbine. This is normally accomplished
through the coordinated action of a number of subsystems. These subsystems provide for the control of disturbances arising from abnormal conditions and for the control of processes that maintain the plant in an economical operating condition. Figure 4-1 shows these subsystems for current PWRs [M-2]. Major subsystems of an NSSS control system are as follows:

i) **Power Control System:** The power control system automatically adjusts the reactor power and power distribution through control rod motions, changes in boron concentration, and control of the coolant (moderator) temperature. Subsystems of the power control system include the rod control system, power distribution control system, boron control system, and remote dispatch system interface.

ii) **Reactor Pressurizer Control System:** This control system has two functions. First, it keeps the pressure of the primary coolant system within desired limits through operation of the pressurizer heaters, spray valves, and relief valves. Second, this system adjusts the mass inventory of the primary coolant system (i.e., pressurizer level) by balancing the charging flow against the letdown flow.

iii) **Steam Bypass Control System:** For a large step load decrease or a reactor trip, the steam bypass control system removes the sensible heat stored in the primary coolant system with steam dumped to the condenser or atmosphere as required. When a load rejection occurs, steam will be dumped if the difference between the programmed average reactor coolant temperature and the actual average temperature exceeds a predetermined amount. In that case, a signal will actuate the steam dump so as to maintain the average reactor coolant
temperature within a designated range until a new equilibrium condition is attained.

iv) **Feedwater Control System:** Automatic feedwater control is provided so that the full load range from zero to full power is covered by means of the steam generator level control system and the feedwater pump speed control system. A detailed description of these systems was given in Chapter Three.

This research concerned only with the control of power in PWR-type multi-modular plants with most of the effort focused on the rod control system. In addition, the plant power control system, which allocates the load demand among each power module, is also designed. Given the total load to be supplied, the power control system divides that load in some specified way among the active modules. Thus, with the load demand as input, the power control system supplies the requisite total steam flow while both observing constraints on the use of each module and minimizing perturbations to those modules.

The complete design of a digital power controller for a PWR-type multi-modular power plant requires that the control strategy, control rods, sensors, computer, and computer software be addressed. However, only the first of these is considered here; the others are beyond the scope of this work scope. It was assumed that the control rod reactivities of the PWR-type multi-modular reactor are same as those of current PWRs.
Figure 4-1  PWR NSSS Integrated Plant Control System [M-2]
4.2 Power Control System of Nuclear Power Plant

Control principles and strategies that have proven to be effective in other types of power plants can be utilized to design the power control system of a PWR-type multi-modular power plant. In order to identify relevant techniques, a survey of state-of-the-art power control technologies in other types of power plant was performed. In this regard, existing PWR, CANDU, and PRISM power control systems were reviewed. The first of these was selected because this system is in widespread use. The second was examined because CANDU plants were the first to incorporate digital controls. The third was chosen because it represents an inherently safe multi-modular design. In addition, control techniques developed at MIT for reactors were also reviewed.

The digital power controller described here for a PWR-type multi-modular power plant was designed based on the basis of the above power control technologies and the physical behavior unique to multi-modular plants, as described in Section 4.3 of this report.

4.2.1 PWR

The subsystems of a current-generation PWR power control system consist of the rod control system, power distribution system, boron control system, and remote dispatch system interface. The rod control system automatically adjusts the power level of the reactor to match the power demanded by the turbine. Control rod movement is determined from the reactor coolant temperature needed to meet the turbine power demand, the rate of change of the neutron power, and the measured reactor coolant temperature.
4.2.1.1 PWR Reactivity Control Devices

Reactivity control is provided by both the neutron-absorbing control rods and the soluble chemical absorber (boric acid) that is dissolved in the reactor coolant. The control rods, which are cylindrical in shape, are located within channels in place of fuel rods in certain fuel element assemblies. Boron carbide or a silver-indium-cadmium alloy is used. Also, stainless steel tubes that encapsulate a hafnium absorber rod are sometimes used.

Control rod drives are mechanically driven and, depending on the vendor, are either infinitely positionable (Babcock & Wilcox) or are moved in a series of discrete steps (Westinghouse and Combustion Engineering) [L-2]. All rod drives incorporate a magnetic device which is deenergized for reactor scram. The stepped type of rod drive has a series of switches that indicate position at every step. The infinitely-positionable rod drive has a continuous position indicator that follows the rod position at all times. These rods employ roller nuts to provide continuous motion. In the Westinghouse PWR, full-length control rods provide reactivity control for power defect compensation (reactivity changes because of the temperature change with power), for rapid shutdown, for reactivity changes because of coolant temperature changes in the power range, and for reactivity changes that result from void formation.

The boric acid concentration is varied to compensate for slow reactivity changes such as fuel depletion and fission product buildup as well as zero power reactivity changes, reactivity changes produced by intermediate-term fission products (xenon and samarium), and burnable absorber depletion.
4.2.1.2 PWR Power Control Strategy

Control of reactor thermal power and primary coolant temperature for a PWR operation above 15 % FP is accomplished either manually or by an analog automatic controller. The latter uses the current and desired power as well as dynamic temperature behavior to generate and implement control actions. The control output is the rod speed and direction. The analog controller is designed to handle (i.e., without causing a reactor trip) the following PWR operational transients:

i) A ten percent (10% FP) step change in demanded load,

ii) A five percent per minute (5.0 %FP/minute) ramp change in demand, and

iii) A 100 %FP step electrical load decrease with steam dump operation.

Current-generation PWRs are primarily controlled on average primary coolant temperature (T\text{ave}) if operating above 15 %FP. Various reactor coolant temperature programs are used to meet the needs of different PWR power plants. The two extremes of these programs are the 'constant coolant temperature' and 'constant steam pressure' programs.

The constant T\text{ave} approach, shown in Figure 4-2, is desirable for the design of the primary plant because it keeps the reactor coolant system water volume nearly constant and that in turn minimizes the requirements on pressurizer size. This type of temperature program also reduces the need for reactivity control because the feedback reactivity associated with moderator temperature changes is also minimized. The major disadvantage of the constant T\text{ave} program occurs in the design of the secondary plant because large steam pressure changes occur whenever the power is cycled from 0 %FP to 100 %FP.
Figure 4-2 The Constant Primary Coolant Average Temperature Control Program

A constant steam pressure program, shown in Figure 4-3, is desirable from the viewpoint of secondary plant design. However, it results in a large primary coolant temperature change. Hence, the reactivity control system must be able to handle the feedback reactivity caused by changes in the moderator temperature. Sizing of the pressurizer is also complicated because of the need to compensate for the widely varying reactor coolant volume. Another disadvantage is that with a constant steam pressure program, the primary coolant temperature increases as power increases. Therefore, to keep the primary coolant temperature from reaching an undesirable condition, an upper limit must be placed on it.
A compromise between these two temperature programs is the 'sliding $T_{ave}$ program'. This program blends the ideas of the constant $T_{ave}$ and the constant steam pressure programs in order to optimize the pressurizer size, secondary plant design, and reactivity control system design. Also, it seeks to limit the coolant temperature rise at high power.

Figure 4-3 The Constant Steam Pressure Control Program
4.2.1.3 Analog PWR Controllers

Analog controllers are used in existing PWRs to control the average coolant temperature and the reactor power. In this section, an existing PWR power controller is described. The controller is from a Westinghouse PWR and this description is based on the references [S-10,M-2,N-1].

Figure 4-4 is a block diagram of an existing PWR temperature controller. The temperature is controlled either manually by operator action or automatically by the reactor controller. This controller generates a control rod speed and direction signal in response to two error signals. The demands input to the controller are the reference primary coolant temperature, $T_{\text{ref}}$, and first stage turbine impulse pressure (an indication of turbine load).

The reactor controller consists of two error signal channels and a speed controller as shown in Figure 4-4. The two channels used to generate the total error signal are:

i) the deviation of the auctioneered (highest) primary coolant temperature ($T_{\text{ave}}$) from the programmed average temperature ($T_{\text{ref}}$), and

ii) the rate of change of the mismatch between the turbine load and power generated in the nuclear reactor.

The average temperature channel receives the auctioneered (highest) loop $T_{\text{ave}}$ from the reactor coolant system temperature instrumentation. The signal is first modified by a lead-lag compensator to correct for slow temperature response and then compared to the reference temperature, $T_{\text{ref}}$. The turbine impulse pressure is linearly proportional to turbine
load and is used in the load maps to determine $T_{ref}$. The resultant temperature error signal is then combined with the power mismatch signal.

Figure 4-4  Block Diagram of an Existing PWR Average Coolant Temperature Controller [N-1]
The second error signal is a power rate mismatch signal which is obtained by comparing the rate-of-change of turbine power with that of the neutronic power. This error signal provides fast response to a change in load by detecting a mismatch in the primary heat balance. The power rate mismatch error signal is sent through nonlinear and variable gain units which convert the error in power to an equivalent temperature error so that it may be summed with the average temperature error and sent to the control rod speed and direction program. The nonlinear gain unit causes larger changes in load to have larger effects. The variable gain unit imposes a higher gain at low power levels and a lower gain at high power thereby, enabling the power rate mismatch error channel to provide adequate control at low power levels as well as stable operation at high power.

It is important to note that the power rate mismatch error signal is zero at steady-state because it is based on a rate-of-change. Thus, this error signal is zero during steady-state operation even though the neutronic power and turbine load may disagree. During steady-state operation, the temperature error signal provides fine control to maintain $T_{ave}$ within the desired band.

The total error signal is sent to the rod controller. Based on the magnitude and sign of the total error signal, this controller determines the appropriate control rod speed and direction as shown in Figure 4-5. Rod motion begins at $\pm 0.56 \, ^\circ\text{C}$ at eight steps per minute (spm). The lockup of $\pm 0.28 \, ^\circ\text{C}$ prevents system oscillation. At an error of $\pm 1.67 \, ^\circ\text{C}$ error, the speed begins to increase linearly, so that at an error of $\pm 2.83 \, ^\circ\text{C}$ the rod speed is at its maximum, 72 spm (steps per minute). The polarity of the error signal determines the direction of rod motion. If $T_{ave}$ is less than $T_{ref}$, the rod motion is outward in order to increase power and primary coolant temperature. Conversely, if $T_{ref}$ is less than $T_{ave}$, the rod motion is inward in order to reduce power and primary coolant temperature.
The rod speed and direction signal is then sent to the control rod logic cabinet which selects the proper control rod bank to be moved. Before the signal for control rod movement is finally implemented, a series of interlocks that block outward rod motion under certain plant conditions are checked. These interlocks are designed to prevent an uncontrolled power escalation that could lead to a reactor trip.

Figure 4-5    Typical PWR Control Rod Speed Program [M-2]
4.2.1.4 Performance of Analog PWR Controllers

There are two major factors which can degrade the control performance of existing PWR analog controllers. First, as explained previously in Section 4.2.1.3, existing PWR power control is achieved by regulating the primary coolant temperature. Hence, the major controlled variable is not reactor neutronic power but primary coolant temperature. It is true that neutronic power measurements are used to generate the power rate mismatch error signal. However, in that case, it is the rate of change that is used and not the absolute value. In fact, the actual neutronic power measurement is only used in the rod motion interlock logic. For example, if neutronic power is greater than 103 %FP, rod motion is blocked. Given only one manipulated variable (reactivity change by either control rod motion or boron concentration adjustment), two independent control variables (reactor neutronic power and primary coolant average temperature) cannot be controlled satisfactorily. A second factor which can degrade the performance of existing controllers is that coolant temperature reacts slowly to control action because of the time delay involved in both the transfer of heat from fuel to coolant and the loop circulation time. Finally, it should be noted that even though temperature-measuring signals are often compensated by dynamic circuits, the nonlinearities associated with time delays cannot be completely offset.

Reference [C-1] provides a series of CABRAL-IPM simulations performed with a current analog automatic power control system. The following results were obtained. If the controller is tuned for optimal operation under a specific condition, the controller performs well. However, it requires a different tuning for each situation. Hence, if plant operating conditions stray from the initial tuning conditions, then the controller’s performance will be degraded, and an overshoot in $T_{ave}$ or reactor neutronic power may occur.
4.2.2 CANDU

Canada has used digital computers for control and display since the CANDU (CANadian Deuterium Uranium) reactor was first developed in 1966 [H-3]. The CANDU computer control system is used for many purposes including power control, pressure control of the primary heat transport system, steam drum pressure control, data display, and alarm annunciation. Allocating these functions to the computer resulted in a 40% reduction in the meters and recorders in the control room. In this Section, a brief review of the CANDU digital power control system is presented and it is contrasted with the power control logic of existing PWR analog controllers. Additional information is given elsewhere [A-4]

4.2.2.1 CANDU Reactivity Devices

These reactors are cooled and moderated by heavy water. Light water has a significantly higher absorption cross-section for neutrons than does heavy water. Hence, light-water filled cylinders are used to control a CANDU reactors. The system is termed 'zone control'. Zone control is the primary means of reactivity and power control in CANDU reactors. It consists of fourteen independently controllable compartments which are filled (or drained) with light water. Change in the average level of all zones is used for reactivity control while differential level changes in individual compartments are used for spatial power distribution control. The layout of the zone control units and their detectors is shown in Figure 4-6. The total reactivity worth of all fourteen zone control system units is only 0.0075 $\Delta k/k$. The water level (proportional to reactivity) in each unit is adjusted via
a valve that controls the flow of light water into the cylinders. Because the out-flowrate is constant, the light water level can be adjusted by in-flowrate changes.

The zone control system is the primary reactivity device. However, situations may arise where reactivity variations exceed the capability of that system. In these cases, adjuster rods and/or mechanical control absorbers are used to maintain a reactivity balance. Adjuster rods are gray absorbers that are symmetrically located in the core and which provide flux flattening, positive reactivity shim, and xenon override. These are normally fully inserted in the core for flux shaping. They can be withdrawn in banks to provide positive reactivity. Mechanical control absorbers are used to provide negative reactivity that is greater than the capability of the zone control system. The layout of the adjuster rods and mechanical control absorbers is also shown in Figure 4-6.

### 4.2.2.2 CANDU Power Control Strategy

The CANDU power control system, which is called a ‘reactor regulating system’, provides automatic control of reactor power to a given setpoint at any power level between $10^{-5}$ %FP and 100 %FP. It can maneuver reactor power at controlled rates between any two power levels. Two different power ramp rates are observed:

1. 0.3 %FP per second above 25 %FP, and
2. 1.2 % of present power per second at 25 %FP, and below.

By means of a cutback, the reactor power can be reduced at a controlled rate if any one of a given set of parameters exceeds a predetermined operating limit.
Figure 4-6  Layout of CANDU Reactivity Devices [A-4]
The CANDU power control system relies on stable feedback control of the neutron flux. It does not use the primary coolant temperature. The reactor power setpoint is determined by a demand power routine that is either obtained from operator or specified by the steam generator pressure control program. Reactor power is estimated from flux measurements that are obtained from either ion chambers (low power range) or platinum flux detectors (high power range). Upon comparison of the power setpoint and the estimated reactor power, the CANDU power control system adjusts reactivity so as to provide the desired reactor power.

4.2.2.3 CANDU Digital Controller

The block diagram of the CANDU power control system is shown in Figure 4-7 [A-4]. As shown in this figure, it consists of a cascade control system with the demand power setpoint for the zone controllers determined by the steam generator pressure control system. The demand power routine computes the reactor neutronic power setpoint and flux rate setpoint with the method of determination dependent on the mode of operation. Flux setpoint demand can originate from three sources:

- The boiler pressure control program,

- The operator, via a keyboard, and

- The setback routine.

The flux setpoint is rigidly limited to less than 100 %FP. A deviation limit facility also limits the setpoint from exceeding the current power by a factor which varies from 1.05
when above 25 %FP to 1.28 when below 5 %FP. This is done in order to prevent high flux rates that could result if the setpoint were much higher than the actual power.

The demand power routine calculates the reactor power error (E_p) based on logarithmic values of the flux power setpoint (P_{set}), the rate setpoint (R_{set}), the measured power (P), and the measured rate (R). Thus,

\[ E_p = K_B (P - P_{set}) + K_D (R - R_{set}) \]  \hspace{1cm} (4-1)

where \( K_D = 0.5 \) and \( K_B \) varies from 1.0 (above 25 %FP) to 0.2 (below 5 %FP). That is, rate control dominates at low power to give better stability.

Equation (4-1) generates the global reactor power error which addresses total power production. Spatial control is also an objective and is achieved by combining the spatial zone power tilt with the global power error. Thus, combined individual zone power errors are sent to the light-water flow controllers for the individual zone control system units. Figure 4-8 shows the light-water flowrate as a function of the power error. The zone control system unit is drained at its maximum rate for power errors less than -3.0 %, and filled at its maximum rate for power errors greater than +3.0 %.

The controlled variable in a CANDU power control system is the reactor neutronic power while that in a PWR analog power controller is the primary coolant temperature. Hence, the problems associated with the time delay, between a reactivity adjustment and the response of the controlled variable, are less of a problem in the CANDU design.
4.2.3 PRISM

4.2.3.1 PRISM Power Control System

The PRISM power control system is a supervisory, hierarchical system. The description of the PRISM controller given here follows that of several references [O-3,D-1, R-1, R-2]. Figure 4-9 shows the structure of the still-developing PRISM power control system [O-3]. The philosophy of operation for each supervisory module is the same. Namely, its child nodes are to be considered as a plant to be controlled so that the goals
specified by the parent node are achieved. Each supervisory module will embody strategies
to generate a demand distribution, monitor plant performance, select control actions from a
set of available control laws, diagnose the need for and implement controller gain
adjustments, and avoid operation near safety and administrative limits.

At the lowest level of the control hierarchy are automatic controllers that interface
directly to the plant actuators. As shown in Figure 4-9, a distributed control approach for
each reactor module and turbine has been chosen. Four automatic controllers are
implemented in each module including one for reactivity, primary flow, secondary flow,
and steam drum level. One turbine controller is implemented in each power block. A
variety of control laws and configurations are under development for each controller.
Some candidates are:

i) **Reactivity controller**: The reactivity control system monitors reactor power and
controls the rod positions in order to attain the demanded neutronic power.
This system manipulates rod positions to adjust the module’s neutronic power
or primary hot leg sodium temperature to match the values demanded by the
power block’s controllers. For this purpose, the model-based automatic
controllers that are described in Section 4.2.3.3, are used.

ii) **Primary controller**: This controller adjusts the primary loop sodium flowrate in
order to achieve a demanded sodium temperature at the outlet of the core. In
this regard, it is important to note that the PRISM power control system differs
from the others that were previously described in that it uses an additional
manipulated variable (sodium coolant flowrate) to control the coolant
temperature. This controller employs conventional PID control laws.
Figure 4-9  Supervisory Control System for Multi-Modular ALMRs [O-3]
iii) **Secondary controller:** This controller regulates the intermediate heat exchange loop sodium flowrate to satisfy a demand on the sodium temperature at the outlet of the intermediate heat exchanger. This controller uses conventional PID control laws.

iv) **Steam drum controller:** This controller adjusts feedwater flowrate to satisfy a demand on the steam drum water level. Conventional PID control laws are also used here.

v) **Turbine controller:** This controller varies the steam flowrate to satisfy a demand on the steam header pressure or the turbine power. Conventional PID control laws are used.

### 4.2.3.2 PRISM Model-Based Reactivity Controller

For reactivity control, a combination of model-based and conventional PID (Proportional-Integral-Derivative) action is used [R-2]. For the model-based portion, the point kinetics equations (Equations (2-1) and (2-2)) are used with reactivity (ρ) taken as a function of the rod position (urod), fuel temperature (Tf), and coolant temperature (Tc).

\[
\rho = \rho (urod, Tf, Tc) \tag{4-2}
\]

The fuel and coolant temperatures are given by the relations:

\[
\dot{T}_f = \alpha N + e (T_f - T_c) \tag{4-3}
\]
\[ \dot{T}_c = \gamma (T_f - T_c) - \varphi (T_c - T_c^o) \] (4-4)

where \( \alpha, \varepsilon, \gamma, \) and \( \varphi \) are constants which represent the heat conversion ratio from the fission reaction and the heat transfer coefficients respectively. The precursor concentration and fuel temperature can be estimated from the measured neutron flux and the coolant temperature at the core outlet.

The specified demands are the neutron power and its rate-of-change. A set of equations necessary to compute the control rod position is therefore:

\[ C' = \frac{\beta}{\Lambda} N_m - \lambda C' \] (4-5)

\[ T'_f = \alpha N_m + \varepsilon (T'_f - T_{cm}) \] (4-6)

\[ u_{mb} = \left[ \left( D_n - \lambda C' \right) \frac{\Lambda}{D_n} - \rho \left( T'_f, T_{cm} \right) + \beta \right] \frac{1}{\rho_c} \] (4-7)

where \( u_{mb} \) is the model-based control action,

\( C' \) is the estimated precursor concentration,

\( N_m \) is the measured neutron power,

\( T'_f \) is the estimated fuel temperature,
$T_{cm}$ is the measured coolant temperature,

$D_n$ is the demand neutron power,

$\rho_f$ is the feedback reactivity because of fuel and coolant temperature change, and

$\rho_e$ is the estimated reactivity.

In order to compensate for inaccuracies in the model of the plant dynamics, the model-based control action is complemented by a conventional PID controller. The PID control action ($u_{pid}$) is represented as follows:

$$u_{pid} = K_p e + K_i \int e \, dt + K_d \frac{de}{dt} \quad (4-8)$$

where $e$ is the power error $(N_m - D_n)$ and $K_p$, $K_i$, and $K_d$ are gain constants. Thus, the resulting net control action is:

$$u_{rod} = u_{mb} + u_{pid} \quad (4-9)$$

where $u_{rod}$ represents the control output of the reactivity controller.

### 4.2.4 Reactivity Constraint Approach

The 'reactivity constraint approach' was derived by Bernard and used by him for the supervisory control of neutronic power on both the 5-MWt MIT Research Reactor and
the Annular Core Research Reactor that is operated by Sandia National Laboratories [B-1, B-2]. The reactivity constraint approach is based on the principle that the effects of delayed neutrons can be offset by an induced change in their prompt counterparts. This principle was implemented by limiting the net reactivity present in the reactor core to the amount that can be offset by reversing the direction of the control rods before a predetermined safety limit is reached. In this control concept, two different times are defined. These are the 'available time' ($\tau_A$), which is the time to reach the safety limit, and 'the required time' ($\tau_R$), which is the time required to reduce the reactivity to the controllable level. The constraint is then originated by checking the two times, to make certain that the required time is always less than or equal to the available time. Thus,

$$\tau_R \leq \tau_A .$$

Equation (4-10) is a supervisory constraint. It is used in conjunction with a control law that determines rod movement. If the action of the control law does not violate the constraint, it is implemented. If not, that action is overridden. Cabral [C-1], and later Aviles [A-2], extended the idea to PWRs. In the Cabral controller, the average reactor coolant temperature (instead of the neutron power) was selected as the major control parameter. Figure 4-10 shows the logic diagram of Cabral's proposed controller that is based on the reactivity constraint approach. First, a tentative control rod movement is calculated. The criteria used for determining the rod motion is that the average coolant temperature be maintained as desired. The appropriate control action is found from the previously described conventional rod speed program (without an error band). To calculate the required time, the total reactivity and multi-group precursor decay parameter are estimated through use of an extended Kalman filter. The available time is calculated based
on measurements of the rate of approach to the safety limit(s). Then, the reactivity constraint (Equation (4-10)) is checked. If the constraint is satisfied, the tentative rod motion is converted to a control rod speed and applied to the reactor. If the constraint is not satisfied, reversal of control rod motion is required to maintain the constraint. Figure 4-11 is the block diagram of PWR digital controller proposed by Aviles [A-2]. A more detailed description of the digital controller is given in references [B-2,C-1,A-2]. This controller is one of several that were considered here for inclusion in the multi-modular control system.
Figure 4-10 Logic Diagram of Digital Controller for PWR Average Coolant Temperature Controller Proposed by Cabral [C-1]
Figure 4-11 Logic Diagram of Digital Controller for PWR Controller Proposed by Aviles [A-2]
4.3 Operational Characteristics of a PWR-Type Multi-Modular Power Plant

The design of both digital and analog controllers should account for the operational characteristics of the associated power plant. In this section, the thermal hydraulic characteristics of PWR-type multi-modular power plants are described. The operating strategies and operational modes that will be adopted for use in the power control system are also explained.

4.3.1 Operational Characteristics

A PWR-type multi-modular power plant will be comprised of several steam generating power modules with each module consisting of one small PWR reactor core and one steam generator. Each of these modules is in turn a dual cycle plant with a closed, pressurized, single-loop primary coolant system and a steam-generating secondary system. Each former is physically-independent, closed loop while the latter are interdependent because each steam generator supplies steam to a single turbine-generator through a common main steam line header. Feedwater is also supplied to each steam generator through a common header. Thus, even though the primary coolant system is physically independent, its thermal-hydraulic characteristics are interdependent because of feedback from the secondary system. The reality is that this interdependence makes it quite difficult to design a power control system for a multi-modular power plant.
Reactivity is directly affected by the coolant system in that a variation in temperature or pressure will alter the neutron balance in the core. The largest effect is usually that of the coolant temperature and the temperature coefficient of reactivity is an important safety and control feature. Changes in coolant temperature alter the density of the water and hence the neutron moderation and leakage. The temperature of the coolant/moderator, and hence fuel temperature, can be indirectly affected by changes in turbine load. Such changes may also alter the pressure of the primary system which in turn also alters reactivity by changing the coolant density. However, this pressure effect is slight because the density of water varies little with pressure and because the primary system pressure is kept constant by the pressurizer during normal operation.

As mentioned in Section 2.1, the steam flowrate from each power module depends on both the pressure in the module’s steam generator and that in the common steam line header. Unless these pressures are properly coordinated, variations in steam flowrate may develop. Furthermore, because steam generator pressure depends on the primary coolant temperature, that quantity should be regulated to maintain the desired steam flowrate from each steam generator. Therefore, pressure control on the secondary side of modular plant’s steam generators depends on the control of the primary coolant loop’s temperature.

Another important operational requirement imposed on multi-modular power plants is that it should be possible to operate each module at a different load. Because of reactor safety and economics, all modules may not operate at the same power level. For example, in order to stagger the refueling of each module’s reactor, the fraction of the load obtained from each module may be different. This mode of operation can improve the capacity factor of a multi-modular power plant by, for example, permitting a module that has been off-line for maintenance to operate at a power level higher than that of the other modules.
and thereby maintain core burnup for all modules in accordance with the refueling schedule. This mode of operation is referred to as 'unbalanced load' operation. It is, of course, necessary that the pressure at the inlet of the main steam line header be the same even though each module operates at different load. Otherwise, an extra valve should be introduced or the steam will not flow from each module at the proper rate.

As mentioned in Section 2.3.5, it was assumed that the total pressure loss through a steam line is proportional to the square of the steam flowrate. Thus, as shown in Figure 4-12, the pressure loss through the steam line is function of module power. If the main steam line header pressure is specified, then the steam generator pressure and hence the primary coolant temperature of each module can be determined as function of its power.

![Pressure Loss vs Module Power](image)

Figure 4-12   Steady State Pressure Difference between Steam Generator and Main Steam Line Header
4.3.2 Power Control Program

Most PWR power plants utilize a sliding $T_{ave}$ control program that is a compromise between constant $T_{ave}$ and constant steam pressure control. Figure 4-13 is a schematic of a sliding $T_{ave}$ program. The characteristic feature of a sliding $T_{ave}$ control system is that reactor power is adjusted to maintain a programmed non-constant $T_{ave}$. Specifically, $T_{ave}$ is programmed to increase and steam pressure is programmed to decrease when turbine load is increased. The power control system will withdraw the control rods to increase reactor power and maintain $T_{ave}$ at the programmed value. The temperature difference between $T_{ave}$ and the steam generator saturation temperature increases as the programmed

Figure 4-13  Sliding Primary Coolant Average Temperature Control Program
T_{ave} rises in order to cause the right steam generator heat transfer. The steam generator saturation temperature decreases. This control program produces acceptable steam conditions over the entire power range while both requiring less rod motion and maintaining a lower hot leg temperature than would a constant steam pressure program.

In this research, it is assumed that a PWR-type multi-modular power plant uses a sliding T_{ave} control program as shown in Figure 4-13. If all modules are operated at same load, then there will be no difference in the secondary side thermal hydraulics between a PWR-type multi-modular power plant and a current-generation multi-loop PWR. However, if each module is operated at a different load, power control is more complex. In order to illustrate this complexity, consider a multi-modular power plant that consists of two modules. Figure 4-14 shows T_{ave} and steam generator saturation pressure as a function of module power. If all modules are operated at Q_1, the pressures of both steam generators are P_{sg,1} and that of the main steam line header is P_{h,1}. On the other hand, suppose module #1 and #2 are operated at Q_1 and Q_2 respectively where Q_1 is greater than Q_2. Under this condition, the situation is quite different. The primary coolant temperature of module #1 follows the sliding T_{ave} control program. Hence, module #1 primary coolant temperature is adjusted to maintain T_{ave,1} and the pressures of module #1’s steam generator and main steam line header are P_{sg,1} and P_{h,1} respectively. The pressure in the module #2 steam generator should be maintained at P_{sg,2}^* so as to satisfy the pressure balance between the steam generator and main steam line header. (Note: The header pressure is the same for all steam generators. Differences exist between pressure in each steam generator because of pressure losses in the piping that connects to the main header. The magnitude of these losses depends on the steam flowrate and hence the module power. If it were not for these losses, all steam generators would have the same pressure.) As a result, module #2’s
temperature cannot be controlled to maintain $T_{ave,2}$. In reality, the primary coolant temperature of module #2 should be controlled to be $T_{ave,2}^*$ instead of $T_{ave,2}$ so as to maintain the needed pressure balance. Therefore, the sliding $T_{ave}$ control program can only be applied to the highest-power module reactor in a multi-modular power plant. Thus, the primary coolant temperatures of the lower-power modules depend on their own module's power as well as on the power of the most heavily loaded module. In contrast, the primary coolant temperature of the highest-power module depends on only that module's power.

![Diagram showing temperature and pressure relationships](image)

Figure 4-14  Different Load Operation under Sliding Average Temperature Control Program
Figure 4-15  PWR-Type Multi-Modular Power Plant Operation Map (Steady-State Average Primary Coolant Temperature)

Figure 4-16  PWR-Type Multi-Modular Power Plant Operation Map (Steady State Steam Generator Pressure)
Figure 4-15 shows, in the curve labeled 'High', the primary coolant temperature of the highest-power module as the function of module power. As shown in this figure, the temperatures for other modules do not form simple curves as do those for a current-generation PWR sliding $T_{ave}$ program. Even though the power in the lower-power modules is kept constant, their primary coolant temperatures will require adjustment if that of the highest-power module is changed. This fact gives an extra requirement for designing the power control system for a PWR-type multi-modular power plant. Figure 4-16 shows the corresponding steam generator pressure as a function of the power in the highest-power module.

4.3.3 Operating Strategies and Operational Modes

4.3.3.1 Operating Strategies

When a request for a new target turbine power level is received from the load dispatcher or by the reactor operator, there are several possible ways to allocate demand. For example, for PRISM demand allocation, four different strategies are implemented [O-3]. These strategies are equal load, equal change, extreme first, and a preset value operating.

In this research, similar demand allocation strategies are proposed for PWR-type multi-modular power plants. These include:
i) **Equal Amount Change:** The required change in turbine load is divided by the number of available power modules. Each available power module is directed to change by the same amount.

ii) **Extremes First Change:** The lowest power module in the case of an increase, or the highest in the case of a decrease, is changed first, then the next, etc., until the desired target is reached.

iii) **Preset Operation in any Power Module:** In this operating strategy, the operator can set the desired power of any module at any time and the other modules can be operated with either of the two previously mentioned strategies. In this operational mode, swing operation is possible. That is, the specified module’s power is changed to the desired value and other modules compensate so that there is no net change in the total power (i.e., the turbine load remains constant).

The operator can select the appropriate operating strategy at any time through the power plant controller which then allocates the power demand among the modules according to the chosen strategy.

### 4.3.3.2 Operational Modes

Electric power demand changes daily, with the largest demand occurring during the daylight hours, and the lowest during the late night and early morning hours. Power stations connected to a grid should have the capability to meet these varying demands and thereby prevent cutbacks in service. Large nuclear power plants have been used mostly for
base-load operation. In this mode, the operator determines the output of the electric-generating plant. Fossil-fueled plants are generally operated in a load-following mode. This has made economic sense in most grids because nuclear fuel costs are much less than those of their fossil counterparts. However, as the amount of installed nuclear generating capacity increases, the capability for nuclear plants to operate in a load-following or frequency-control mode will become increasingly important. If a nuclear power plant is assigned to daily load-following, the power demand variation is so slow that plant output can be determined by the operator. This operating approach is therefore almost the same as base-load operation and is referred to as 'reactor leading'. However, if a nuclear power plant is assigned to a fast-varying mode of load-following such as frequency control, plant operation is controlled by the turbine. This mode is termed 'turbine-leading'. Thus, a newly designed controller should have the flexibility to operate in either a turbine-leading or a reactor-leading mode.

CANDU reactors have operated under digital control for several decades. Two modes of operation, normal and alternate, are practiced [A-4]. In the normal mode, the turbine load setpoint is maneuvered towards the turbine target load at the maximum permissible rate calculated on the basis of turbine metal temperature. Steam generator pressure is controlled principally by maneuvering reactor power to follow turbine load. In the alternate mode, the turbine is used for control of steam generator pressure. Reactor power is kept at an operator-specified setpoint.

Similar operational modes are specified for the PRISM power control system which will use a digital control [O-3]. The two main modes are demand-following (turbine-lead) and base-operation (reactor-lead). In addition, there is a fast runback option that is equivalent to a setback on a CANDU. In the reactor-leading mode, reactor power is
controlled to an operator-specified value in a same manner similar to the same as alternate CANDU mode. In the turbine-leading mode, the turbine admission valve controls the steam flow and hence the power supplied to the turbine. Reactor power is adjusted automatically to match the turbine power.

In this research, two operational modes were selected for a PWR-type multi-modular power plant. The design of the power control system is required to handle both modes and to provide the capability for a smooth transition between these modes. The modes selected for study here are:

i) **Specified Power Demand Mode**: This mode is similar to both the alternate CANDU mode and the reactor-leading mode for PRISM. In this mode, the operator inputs a demand forecast and the power controller follows that forecast as a specified demanded power. This operational mode is preferred whenever the plant is allocated to base-load operation.

ii) **Arbitrary Power Demand Mode**: In this mode, the turbine power follows the demanded load as specified by the load dispatcher. The other controllers then adjust the reactor parameters to their desired setpoints. This mode is preferred whenever the plant is assigned to either grid network frequency-control or fast-changing load-following.

The feature that distinguishes between these two operational modes is that the future load change is known for the specified power demand mode and not for the arbitrary power demand mode. As a result, two different plant power control system designs are needed. A more detailed description of the plant power control controller is given in Section 4.4.
4.4 Proposed Power Control System

The structure and logic of the power control systems used in current-generation nuclear power plants were reviewed in Section 4.2 and the operational characteristics of a PWR-type multi-modular power plant were described in Section 4.3. Even though a PWR-type multi-modular power plant consists of PWR reactors, its overall operational characteristics are quite different from those of current-generation PWRs. Hence, existing power control systems cannot be applied directly to PWR-type multi-modular power plants. However, some general concepts can be adopted. These include sliding $T_{\text{ave}}$ control from PWRs, digital control concepts and cascade control of neutron power from CANDUs, hierarchical control structures of PRISM, and reactivity constraints approach from work done previously.

In this section, a closed-loop, digitally implemented, multi-tiered power controller developed for a PWR-type multi-modular power plant is presented.

4.4.1 Power Control System Design Approach

4.4.1.1 Power Control System Design Goals

The proposed PWR-type multi-modular plant power control system is designed to achieve the following goals. First, it should provide automatic control of reactor power to a given setpoint at any power level from startup to full power operation. Moreover, it must do this during both transient and steady-state conditions without violating any operational constraints on power or safety limits. Second, it should perform properly under any
operational mode or any operating strategy. Specifically, it should regulate the module power and steam flowrate from each module at the desired fraction during both transient and steady-state operation. Third, it should monitor module power and maintain it at the desired level. Also, it should reduce the module power if that power should ever exceed predetermined limits.

The proposed power controller is intended to control power ramps at rates up to a maximum of five percent per minute (5.0 %FP/minute) in any active module.

Finally, it should be remembered that in nuclear power plants, the purpose of the power control system is to maintain the power at the desired level. Its function is quite different from that of the reactor safety and reactor protection systems, which will continue to be hard-wired, analog processes. An automatic controller should never pose a challenge to the safety system.

4.4.1.2 Control Action

Figure 4-17 illustrates the general features of a PWR-type multi-modular power plant control system with emphasis on locations from which control information and signals might be obtained and where control action might be applied. The principal controlled variables are reactor neutronic power, primary coolant temperature and pressure, steam generator pressure and level, and turbine power. The principal manipulated variables are the control rods (or chemical shim) that vary reactivity and hence power, the primary coolant circulation pump speed that varies coolant temperature, the pressurizer heater or spray system that maintains primary coolant pressure, the feedwater flowrate that controls
Figure 4-17  Control Action and Controlled Variables of PWR-Type Multi-Modular Power Plant
steam generator level, and the steam throttle valve that adjusts steam flowrate to the turbine-generator and hence determines turbine power. Therefore, if a PWR-type multi-modular power plant consists of N power modules, the required sub-control systems are:

i) N power and temperature control systems,

ii) N pressurizer control systems (primary coolant system pressure),

iii) N steam generator level control systems, and

iv) One steam control system (turbine load and steam dump control system).

The ideal design approach would be for all the above subcontrol systems to be designed simultaneously based on a MIMO (Multiple Input and Multiple Output) control philosophy. In reality, this would be almost impossible because the power plant dynamics are too complicated. Therefore, each subcontrol system is designed independently based on a SISO (Single Input and Single Output) control design philosophy.

In this research, it was assumed that the pressurizer and steam control systems work perfectly. Hence, their design is not addressed here. The steam generator level control system was addressed in Chapter Three. Thus, the effort here is focused on the power and temperature control system.

Several of the principal controlled variables are not completely independent. For example, if all module neutronic powers and one of the other variables such as primary coolant temperature or steam generator pressure are known, then all primary temperatures or steam generator pressures can be determined automatically. Given that the reactor neutronic power should match the turbine load at steady-state, there is, in reality, only one principal independent variable. It is either the coolant temperature or the steam generator
pressure. This fact creates differences among the PWR, CANDU, and PRISM power controllers. In a PWR power control system, the primary coolant temperature is the principal controlled variable and the reactor neutronic power is not controlled directly. Instead, it is adjusted only to correct for a deviation in the primary coolant temperature from that demanded. Hence, the reactor neutronic power may overshoot or undershoot during transients. In a CANDU power control system, the important controlled variable is the steam generator pressure. The demand power routine (steam generator pressure control) sets the demanded reactor neutronic power and then, the reactor power control system adjusts reactivity to maintain that demanded power. In PRISM, two independent controllers are installed for the two main controlled variables. These are a reactivity controller for reactor neutronic power and a primary coolant flowrate controller for primary coolant temperature.

4.4.2 Multi-Tiered Control System

There are a number of ways to equip a multi-modular power plant with a digital control system. In this research, a multi-tiered approach was designed as shown in Figure 4-18. Its structure is similar to the hierarchical supervisory controller for PRISM. However, the details of the individual controllers and their roles differ from those of PRISM.

The proposed power control system consists of a plant power controller, module power controllers, and a turbine controller. Also, control rod and steam generator level controllers are installed in each module. These provide the actuator signals and hence function at the lowest tier of the hierarchical controller. The proposed controller can control
Figure 4-18  The Schematic of the Proposed Multi-Tiered Power Control System
the power in any operational mode and under any operating strategy. In the arbitrary power demand mode, the controller would receive an electric load demand signal from the load dispatcher. This signal would specify the load demand to be carried by the turboelectric generator. The load demand would then be divided among the active modules according to the on-line operating strategy. At the same time, another signal would be sent to the turbine controller to adjust the throttle valve. In the specified power demand mode, the load demand signal would originate with the plant operator who is in charge of the control console. Other control signal activities would be the same as those for the arbitrary power demand mode.

The plant and module controllers will regulate overall plant status as well as provide power control. However, this research focuses on the power control action of these two controllers. Hence, as used here, the term 'power' in the plant and module controllers refers only to the power control action of those controllers.

Detailed descriptions of each power controller are given in the following subsections.

**4.4.3 Plant Power Controller**

The major role of the plant power controller is the allocation of load among the on-line modules. The plant power controller assigns the load change only to on-line modules. The assignment is in accordance with the selected operational mode and operating strategy. The status of each module is tracked and a power demand is not transmitted to an inactive or unavailable module. The turbine power demand signal is also sent to the turbine power
controller. In addition to these roles, the controller may also have other functions such as safeguard and event response.

Figure 4-19  Relationship between Plant Power Controller and Other System
Figure 4-19 shows the relationship between the plant power controller and the other subsystems. At any time the operator may select the operational mode and operating strategy that are to be used. The plant power controller receives the load demand from the load dispatcher if in the arbitrary power demand mode or from the operator if in the specified power demand load. It also receives the operational status of each module from the individual module power controllers. Several predetermined rules including ones for demand power allocation under each specific operational mode and operating strategy are encoded in the plant power controller's software. In addition, safeguard rules such as the procedure to be followed should one of the modules suddenly become unavailable would also be encoded.

As mentioned in Section 4.4.1.2, the reactor neutronic power and primary coolant temperature are the principal controlled variables and two independent actuators are required for their control. PRISM uses control rods (reactivity) and primary coolant pump speed (coolant flowrate) for this purpose. However, both current PWRs and PWR-type multi-modular power plants have only one possible actuator (control rods) because their primary coolant pumps are operated at a fixed speed. Therefore, except under special circumstances, it is not possible to cause both the reactor neutronic power and primary coolant temperature to track a demanded trajectory. Usually, the reactor power is allowed to deviate from its desired power trajectory during power increases and decreases.

It was found that a demanded reactor neutronic power and primary coolant temperature trajectory could be achieved by manipulating the reactor neutronic power and turbine power dynamically. The term ‘dynamic manipulation’ means that the demanded reactor neutronic and turbine power are determined as function of time. That is, their desired time-dependent trajectories are updated at every sampling interval to minimize the
reactor neutronic power and the primary coolant temperature error during power increases or decreases.

Figure 4-20 (a) illustrates the above power and temperature control problem. If the reactor neutronic power and turbine power are increased at the same rate and at the same time, then the primary coolant temperature remains constant. The temperature rise across the core, which is the difference between the hot leg and cold leg temperatures, increases in proportional to the power level. The rod controller will try to follow a sliding $T_{ave}$ control

![Diagram](image_url)

(a) Without Time Delay Control  (b) With Time Delay Control

Figure 4-20  Illustration of the Requirement of Time Delay Control
program and, because this requires a higher $T_{ave}$ at higher power, the reactor neutronic power should be allowed to overshoot until the desired primary coolant temperature is achieved. The neutronic power would then be cut back to the desired setpoint. Thus, extra energy is required to increase the primary coolant temperature. If the reactor neutronic power and turbine power are manipulated on different trajectories, such as one in which the reactor neutronic power is increased first and sometime later the turbine power is increased, then the primary coolant temperature trajectory can follow its demanded trajectory without causing the neutronic power to overshoot its setpoint. This is illustrated in Figure 4-20(b). Even though this concept cannot provide complete trajectory control for both the reactor neutronic power and primary coolant temperature at the same time, it can minimize error in those two variables and hence complete the transient more rapidly. Therefore, this control concept is used for power demand allocation. However, it is difficult to apply this concept to an actual power plant, especially a PWR-type multi-modular plant, because the magnitude of the time delay and whether it is a reactor power or turbine power delay will depend on the operational mode and operating strategy. Figure 4-21 shows the possible paths to raise module power when turbine power is increased in a PWR-type multi-modular power plant.

i) Path 1: Raise plant power using all modules. In this case, the primary coolant temperature of each module must follow either path 1 (in the highest-power module) or 1' (in the lower-power module) as shown in Figure 4-21. After the power increase, the primary coolant temperatures of all modules are increased. Thus, in this case, module power should be increased first and turbine power follow sometime later. This path is the same as that used for the operation of current-generation PWRs.
- Path 1: Raise plant power with all modules.

- Path 2: Raise plant power with highest power module from same initial load.
  a: One module:
  b: Two modules:
  c: Three modules:

- Path 3: Raise plant power with lower power module from different initial load.

Figure 4-21  Demand Allocation Methods
ii) **Path 2:** Raise plant power using the highest-power module and several of the lower-power modules from same initial power level. In this case, there are three possible paths. One module (a), two modules (b), or three modules (c) could be used to raise power. For example, consider a power transient in which all four modules are initially operated at 95 %FP and module #1’s power is increased from 95 %FP to 100 %FP while those of modules #2, #3, and #4 are fixed at 95 %FP by the operator or the controller. The primary coolant temperature of all modules is initially at point ‘a’ in Figure 4-21. After the transient, the primary coolant temperature of module #1 has increased to point ‘b’ while those of module #2, #3, and #4 have decreased to point ‘c’ as shown in Figure 4-21. The net result is that even though plant power increases, turbine power should be increased first and module power follow sometime later.

iii) **Path 3:** Raise plant power using the lower-power modules while power in the highest-power module is kept constant. Because the highest-power module’s power remains constant, so does its coolant temperature. However, the primary coolant temperature(s) of the low-power module(s) must be increased. Hence, module power increases first and the turbine power follows later.

The needed duration of the static time delay and the appropriate mode (module power delay or turbine power delay) can be calculated from the change in the internal energy of the entire plant. Allowance should be made for the internal energy change of the fuel rods, primary coolant, and steam generator inventory. The result is shown in Figure 4-22 where a positive delay implies that module power is increased first and turbine power later. As shown in this figure, the time delays imposed by the fuel and coolant system
internal energy changes are positive because, as expected, the coolant temperature increases as power increases. Unfortunately, these static time delays can not be used directly because the time constants characteristic of each component's internal energy change vary widely with the range going from seconds in fuel rods to around a half-minute for the coolant temperature and several minutes for a steam generator. Therefore, in this research, the time delays given in Table 4-1 were adopted. These were chosen empirically to give the best control performance for both neutronic power and primary coolant temperature trajectories. These figures are used for both power increases and decreases.
Table 4-1 Duration of Time Delays for Module Power and Turbine Power Demand Allocation

<table>
<thead>
<tr>
<th>Operating Paths</th>
<th>Time Delay (s)*</th>
</tr>
</thead>
<tbody>
<tr>
<td>Path 1</td>
<td>10</td>
</tr>
<tr>
<td>Path 2</td>
<td></td>
</tr>
<tr>
<td>- One Module</td>
<td>-20</td>
</tr>
<tr>
<td>- Two Modules</td>
<td>0</td>
</tr>
<tr>
<td>- Three Modules</td>
<td>10</td>
</tr>
<tr>
<td>Path 3</td>
<td>20</td>
</tr>
</tbody>
</table>

* Note: Positive time delay implies that module power is increased first and turbine power later

4.4.3.1 Specified Power Demand Mode

When the specified power demand mode is activated, turbine power follows a predetermined trajectory. In this operational mode, the time delay concept can be applied to the plant power controller. An example of demand allocation between module and turbine power is shown in Figure 4-23. As shown in this figure, the module power is increased first and the turbine power later. If the duration of the time delay is changed during the transient such as described in Table 4-2 (Path 3 changes to Path 2 (a)), the turbine power demand increase stops until the duration of the new time delay is satisfied. The turbine power demand then increases again. This procedure is shown in Figure 4-24.
Figure 4-23  Demand Allocation in the Specified Demand Power Operation Mode-I

Figure 4-24  Demand Allocation in the Specified Demand Power Operation Mode-II
Table 4-2  Example of Demand Power Allocation

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Module Power</th>
<th>Turbine Power (% FP)</th>
<th>Operating Strategy</th>
<th>Demand Allocation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td># 1 # 2 # 3 # 4</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Initial Power</td>
<td>95 95 95 92.5</td>
<td>94.375</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td></td>
<td>95 95 95 95.0</td>
<td>95.0</td>
<td>Extreme First</td>
<td>Path 3</td>
</tr>
<tr>
<td>Final Power</td>
<td>95 95 95 97.5</td>
<td>95.625</td>
<td>Fixed Operation</td>
<td>Path 2 (a)</td>
</tr>
</tbody>
</table>

4.4.3.2 Arbitrary Power Demand Mode

When the arbitrary power demand mode is activated, turbine power is adjusted as specified by the load dispatcher. In this operational mode, the time delay concept cannot be applied to the plant power controller because future turbine power levels cannot be predicted. Thus, module and turbine power are both increased at the same time and the module power may overshoot or undershoot its equilibrium value for some time. However, if lead-lag dynamic compensation is applied, the duration of the overshoot or undershoot can be reduced. Values of the lead time constants used here are given Table 4-1. A lag time constant of 8.0 seconds is used for all paths. Thus, when the turbine power is delayed (module power increases first), the demanded module power is modified by a positive lead-lag compensator. An example of module and turbine power demand allocation is shown in Figure 4-25. The module and turbine power are increased at the same time but the module power increases faster because of the lead-lag compensation. Figure 4-26 shows the module and turbine power demand allocation for the transient described in Table 4-2.
Figure 4-25 Demand Allocation in the Arbitrary Power demand Operation Mode-I

Figure 4-26 Demand Allocation in the Arbitrary Power Demand Operation Mode-II
4.4.4 Module Power Controller

The module power controller has several important roles including transmission of the demanded module power to the rod controller and supervision of the module power. The latter entails preventing the rod controller, which forms the lowest tier of the hierarchical controller, from implementing control rod motion that would place the reactor in an undesirable region such that the reactor safety system might be challenged or an operating limit (103 %FP) exceeded. Thus, the supervisory controller monitors the module power and overrides the rod controller when violation of operating limits might be possible.

For supervisory purposes, the 'reactivity constraint method' developed by Bernard [B-1] is utilized by the module power controller. As explained in Section 4.2.4, a reactivity constraint is not a control law but rather a supervisory rule. In this section, a more detailed explanation of the reactivity constraint method for neutronic power is given.

4.4.4.1 Reactivity Constraint Approach for Module Power

The reactivity constraint approach is intended for use during transients. Its objective is to ensure that sufficient time is available to restore a reactor to equilibrium conditions before a limit, such as the demanded power, is attained. The non-linear, time-delayed nature of reactor dynamics makes such an approach necessary. Specifically, the rate of rise of reactor power is a function of both the prompt and delayed neutron populations. The former is proportional to the power level while the latter is a function of the power history. Hence, the former is directly controllable and the latter is not. The
reactivity constraint concept, as applied to neutronic power, is based on the principle that the effects of delayed neutrons can be offset by an induced change in the prompt neutron population. The concept is given mathematical meaning through use of either the standard or alternate form of the dynamic period equation [B-2,A-2]. That equation gives the instantaneous reactor period in terms of the reactivity, its rate of change, and the rate of redistribution of the delayed neutron precursor groups. The alternate form of the dynamic period equation is:

$$
\tau(t) = \frac{\bar{\beta} - \rho(t)}{\dot{\rho}(t) + \lambda_e(t)\rho(t) + \sum_{i=1}^{I} \beta_i \left( \lambda_{i} - \lambda_e(t) \right)}.
$$

(4-11)

where $\tau(t)$ is the dynamic period, $\rho(t)$ is net reactivity, and $\dot{\rho}(t)$ is the rate of change of the net reactivity. Prompt effects have been ignored in Equation. The alternate multi-group decay parameter $(\lambda_e(t))$ is defined as follows:

$$
\lambda_e(t) \equiv \frac{\sum_{i=1}^{I} \lambda_{i}^2 C_i}{\sum_{i=1}^{I} \lambda_{i} C_i}.
$$

(4-12)

The net reactivity and the multi-group decay parameter cannot be directly measured, but several methods are available for estimating these parameters. One method for reactivity is inverse kinetics [S-11,Z-1]. To suppress the noise associated with the neutron power measurements needed to implement inverse kinetics, an extended Kalman filter [C-1] or
other signal smoothing technique is used. A description of the technique to estimate the net reactivity and the multi-group decay parameter is given in next subsection.

From the dynamic period equation, the 'available time' (τₐ), and the 'required time' (τᵣ) were derived [B-1]. In this research, the 'sufficient reactivity constraint' [B-2] is used. The relation limits the delayed neutron contribution to the reactivity to an amount that can be removed by the control device before the desired power is achieved. The required and available times are:

$$\tau_R = \frac{\lambda_e(t)}{\left| \dot{\rho}(t) \right|}$$  \hspace{1cm} (4-13)

$$\tau_A = \tau(t) \ln \left( \frac{P_{\text{safe}}}{P(t)} \right)$$  \hspace{1cm} (4-14)

where $P_{\text{safe}}$ is the power limit that is not to be exceeded during the transient and $P(t)$ is the current power. The constraint to be satisfied is:

$$\rho(t) - \left| \frac{\dot{\rho}(t)}{\dot{\rho}(t)} \right| \frac{\lambda_e(t)}{\left| \dot{\rho}(t) \right|} \leq \tau(t) \ln \left( \frac{P_{\text{safe}}}{P(t)} \right)$$  \hspace{1cm} (4-15)

If Equation (4-15) is always satisfied, the reactor's neutronic power cannot increase beyond the allowed limit as the result of an improper control action.
4.4.4.2 Reactivity and Multi-Group Decay Parameter Estimator

Reactivity can be estimated via either a reactivity balance or inverse kinetics. The latter was chosen here.

The inverse kinetics model starts with the point kinetics relations as given by Equations (2-1) and (2-2).

\[
\frac{dT(t)}{dt} = \frac{\rho(t) - \bar{\beta}}{\Lambda} T(t) + \sum_{i=1}^{I} \lambda_i C_i(t)
\]

(2-1)

\[
\frac{dC_i(t)}{dt} = \frac{\bar{\beta}}{\Lambda} T(t) - \lambda_i C_i(t), \quad i = 1, 2, ..., I
\]

(2-2)

The prompt-jump approximation, in which the amplitude function is assumed to be in equilibrium with the delayed neutron precursors at all times, is made by assuming

\[
\frac{dT(t)}{dt} = 0
\]

(4-16)

This is appropriate for \( \rho << \bar{\beta} \) which is the case here. Upon combining Equations (2-1), (2-2), and (4-16) and doing some algebra, the following expression is obtained for reactivity:
\[ \rho(t) = \beta - \frac{\sum_{i=1}^{I} \lambda_i C_i(t)}{T(t)} \]  

(4-17)

The solution of this equation requires knowledge of the precursor concentrations. This is done by integrating Equation (2-2) analytically with the amplitude function taken to be some predetermined function of time. For example, it might be assumed to be constant, linear or exponential, over each sampling interval. This done to avoid the need for simultaneous solution of both the neutron and precursor kinetics equations. The former is quite stiff and, by assuming a shape for the amplitude function, its solution is decoupled from that of the precursors. Hence, a sampling interval of reasonable length can be utilized. In this research, it is assumed that the amplitude function within a small time interval \( t \in [t^n, t^{n-1}] \) is constant. Under this assumption, the delayed neutron precursor concentrations can be calculated as follows.

\[ \hat{C}_i^n = e^{-\lambda_i \Delta t} \hat{C}_i^{n-1} + \frac{\beta_i}{\Lambda} T^n \left(1 - e^{-\lambda_i \Delta t}\right) \]  

(4-18)

where superscript \( n \) denotes the \( n \)-th time step, \( \hat{C}_i^n \) is the calculated \( i \)-th delayed neutron precursor group concentration at \( n \)-th time step, \( T^n \) is the measured amplitude function, and \( \Delta t = t^n - t^{n-1} \). Additional information is given in [Z-1].

The initial conditions for the inverse kinetics model are obtained by assuming that the reactor is in a steady-state condition \( \left( \frac{dT(t)}{dt} = 0 \right. \) and \( \rho(t) = 0 \) and that the delayed
neutron precursors are in equilibrium. Then, given some initial amplitude function $T(0)$, the initial effective precursor concentrations are:

$$C_i(0) = \frac{\beta_i}{\Lambda \lambda_i} T(0), \quad i = 1, 2, \ldots, I. \quad (4-19)$$

From Equation (4-17), (4-18) and (4-12), the reactivity and the multi-group decay parameter can be calculated at the $n$-th time step as follows, respectively:

$$\hat{\rho}^n = \beta - \frac{\Lambda \sum_{i=1}^{I} \lambda_i \hat{C}_i^n}{T^n} \quad (4-20)$$

$$\lambda_e(t) = \frac{\sum_{i=1}^{I} \lambda_i \hat{C}_i^n}{\sum_{i=1}^{I} \lambda_i \hat{C}_i^n} \quad (4-21)$$

where $\hat{\rho}^n$ is the estimated reactivity at the $n$-th time step.

### 4.4.4.3 Smoothing Technique for Measured Data

The measurement of data generally involves instrumentation noise and random signal fluctuations. Because these effects can impair the estimate of reactivity and the
multi-group decay parameter, a signal smoothing technique is required. Several methods are available including an extended Kalman filtering method [C-1], the moving average method [K-5], and the dynamic least squares fit method [K-6]. In this research, the most simple signal smoothing method, the moving average technique is used.

The concept underlying the moving average method is the performance of an averaging calculation over a fixed number of data points spaced at equal time intervals. If a total of \( N \) data points are to be used, then the set selected consists of the data point for the current time step and those for the \((N-1)\) previous time steps. Thus, the average is constantly ‘moving’ because the set of data points used in the average calculation moves forward in time as time increases. The estimate of the mean or average at time \( t^0 \) is the ordinary average value of the measured data and is expressed as follows:

\[
\overline{X}^n = \frac{1}{N} \sum_{j=0}^{N-1} X^{N-j}
\]  

(4-22)

where \( \overline{X}^n \) is the estimate of the measured data, \( X^{N-j} \) is an individual measurement, and \( N \) is the total number of measurements.

This method is applied to smooth all of the measurements required in the model-based controller including the neutron power and primary coolant temperature. Figure 4-27 shows the estimated reactivity without signal smoothing and with a \( \pm 0.1 \%\) FP random error imposed on the neutron power measurements. The estimated reactivity is too erratic to be used in the model-based controller. Figure 4-28 shows the estimated reactivity with smoothing. Ten data points \((N=10)\) are used. Compared with the previous result, signal
Figure 4-27  Estimated Reactivity when 0.1 %FP White Noise is Impoised on the Measurements

Figure 4-28  Estimated Reactivity when Moving Average Signal Smoothing Method is Applied (0.1 %FP White Noise)
smoothing using the moving average method gives the better performance. A further advantage of the moving average method is that the algorithm is simple to implement. A drawback is that it reacts slowly to changes until all previous measurements have moved out of the set of data points used in averaging the data.

### 4.4.5 Rod Controller

The rod controller is the bottom level controller in that it interfaces directly to the plant signal and control elements. Its most important function is to position the control rods so as to achieve the demanded module power and thereby cause the module power to follow the demanded trajectory. Thus, the control laws for the rod controller should be designed and developed to maneuver the system along the demanded module power trajectory that is determined by the plant power controller.

#### 4.4.5.1 Model-Based Rod Controller

A number of control laws have been developed for the trajectory-tracking of linear systems and some for non-linear systems. Lau selected several control laws such as feedback, pure feedforward, period-generated, and variable structure control and evaluated their performance through experiment on the 5 MWt MIT Research Reactor [L-5]. He concluded that period-generated control gave the best performance.

Period-generated control is a model-based technique that was developed at the MIT Nuclear Reactor Laboratory in conjunction with Sandia National Laboratories for the
purpose of adjusting nuclear reactor power in a very rapid yet safe manner [B-8]. Figure 4-29 is a schematic of the technique [B-8]. Period-generated control is of particular benefit if it is desired that the reactor neutronic power conform to a certain trajectory.

The principal steps in the control method are to determine a demanded inverse period, compute the rate of change of reactivity (the actuator signal) needed to generate that inverse period, and then apply the calculated rate of reactivity change to the actual system. So doing should cause the reactor's power to rise or fall on the desired trajectory. In order to implement period-generated control, an error signal is first defined in terms of the demanded and observed power levels and then a conventional proportional-integral-derivative feedback expression is used to calculate a demanded inverse period which will either maintain the reactor power on the specified trajectory or, if a deviation exists, restore it to that trajectory. The error signal is expressed as:

\[ e(t) = \ln \left( \frac{n_d(t + j\Delta t)}{n(t)} \right) \]  

(4-23)

where \( n_d(t) \) is the demanded trajectory, \( n(t) \) is the observed trajectory, and \( j \) is a positive integer. It has been shown that this error signal is the sum of a feedforward action from the demanded period and a proportional action from the quotient of the demanded and observed system outputs [B-8]. It has also been shown that the value of \( j \) should be at least 2 in order to ensure stability against oscillations [B-8]. Because period-generated control uses the future demanded power to calculate the present error signal, this control law can not be applied when operating in the arbitrary power demand mode. Therefore, instead of using a demanded period that is calculated from the above error signal, the period associated with
Figure 4-29 Period-Generated Control as Applied to Trajectory Tracking of Reactor Power [B-8]
the desired power trajectory or latest arbitrary power information is used in this research. That period is expressed as follows:

$$\omega_d = \frac{\dot{D}}{D}$$  \hspace{1cm} (4-24)

where $\dot{D}$ is the time derivative of the demanded power trajectory and $D$ is the demanded power.

The next step in implementing period-generated control is to obtain an appropriate model and thereby relate the demanded inverse period to the actuator signal which is the required rate of change of reactivity. The needed model is readily obtained by rearranging terms in the dynamic period equation. So doing yields:

$$\dot{\rho}_c(t) = (\bar{\beta} - \hat{\rho}(t))\omega_d(t) - \lambda_e'(t)\hat{\rho}(t) - \sum\beta_i(\lambda_i - \lambda_e(t)) - \hat{\rho}_f(t)$$  \hspace{1cm} (4-25)

where $\dot{\rho}_c(t)$ is the required rate of change of the control rod reactivity and $\dot{\rho}_f$ is the rate of change of reactivity associated with temperature-induced feedback from the reactor's fuel and coolant. Equation (4-25) requires that the total reactivity, the multi-group decay parameter, and the feedback reactivity be estimated. The first two of those quantities are determined as described in Section 4.4.4.2. The method for estimating the latter is described in Section 4.4.5.3.

The model-based controller used in this research differs slightly from the period-generated controller that was developed at MIT. Even though the controller developed here uses Equation (4-25), it is actually a form of feedforward control because no use of
feedback is made in calculating the demanded inverse period (Equation (4-24)). Therefore, this controller should be complemented with a conventional feedback controller to compensate for inaccuracies in the model of the plant dynamics. This feedback control action is addressed in the next section.

4.4.5.2 Feedback Rod Controller

As previously mentioned, the model-based controller used to determine rod motion is of the feedforward type. Hence, it must complemented with a proper feedback controller in order to compensate for modeling errors. In addition, conventional feedback is also necessary because the module power demand is not perfectly manipulated and hence the primary coolant temperature will not follow exactly the desired sliding $T_{ave}$ control program. For feedback control action, the rod speed and direction program of current-generation PWRs is used.

In this research, two different feedback control algorithms are implemented. The first is used for the highest-power module. It entails computing the module power and primary coolant temperature errors. The latter is converted to an equivalent module power error as follows:

$$\varepsilon_e = \varepsilon_p + K_{Tp} \varepsilon_T$$  \hspace{1cm} (4-26)

where $\varepsilon_e$ is the equivalent total error,

$\varepsilon_p$ is the module power error,
ε_T is the primary coolant temperature error, and

K_{TP} is the conversion constant (e.g., 1.0 %FP/°C).

The feedback controller generates a control rod speed and direction signal in response to the combined error signal.

The second is used for the lower-power modules. The error in module power ratio to the highest-power module is computed for feedback control. This error is:

\[ ε_e = \frac{N_d^*}{N_m^*} N_m - N_m \]  

(4-27)

where N_m and N_d are the measured and demanded module power and N_m^* and N_d^* are the measured and demanded power of the highest-power module.

Figure 4-30 shows the rod speed and direction controller used for feedback control action. The rod speed is proportional to the power error and its maximum is the same as that of current-generation PWRs, namely 19.05 mm/s.

4.4.5.3 Feedback Reactivity Estimator

The magnitude of the neutron flux affects heat and xenon production. Consequent fuel and moderator temperature changes as well as variations in xenon concentration affect reactivity. The latter is not considered here because xenon concentration change slowly. Thus, the feedback reactivity from fuel and coolant temperature changes are considered.
As explained in Chapter Two, fuel and coolant feedback reactivity changes can be calculated by using temperature coefficients as given in Equations (2-4) and (2-5). Thus, the fuel and coolant temperature must be known in order to estimate feedback reactivity. However, fuel temperature cannot be measured and hence it must be estimated.

The average temperature of the fuel can be expressed as a function of module power and primary coolant temperature using the model that was described in Chapter Two.
4.4.5.4 Composite Design of Rod Controller

The rod controller consists of the feedforward controller and an associated feedback controller. The latter's function is to compensate for both inaccuracies in the model of the plant dynamics and for the imperfect demand allocation of plant power controller. Thus, the resulting control action is:

\[ u_{rod} = u_{mb} + u_{fb} \]  \hspace{1cm} (4-28)

where \( u_{rod} \) represents the control output of reactivity controller, \( u_{mb} \) is the model-based control action, and \( u_{fb} \) is the feedback control action.

For feedback control, two different actions are used. Module power and primary coolant temperature error feedback are used in the highest-power module and feedback of the module power ratio to the highest-power module is used for the low-power modules. Figure 4-31 shows the schematic of the rod controller.
Figure 4-31 Schematic of Proposed Rod Controller
4.5 Evaluation of Proposed Controller

In this section, the performance of the proposed power control system is demonstrated by simulating normal operational transients that are associated with a PWR-type multi-modular power plant. The plant simulation program, that was developed as part of this research and which was described in Chapter Two, is used as the simulation tool. The power control system evaluation is performed only for operational transients such as power increases or decreases under the various operational modes and operating strategies. The automatic control of the module power and primary coolant temperature is the major concern in this evaluation. Also, maintenance of the desired steam flowrate from the individual steam generators is an important issue whenever the individual modules are operated at different loads. Finally, as mentioned in Chapter Three, the performance of the steam generator level controller is also shown in these evaluations.

This section of the report is organized as follows. First, the operational transients are described. Next, each level controller is evaluated independently. Finally, the performance of the composite power control system is evaluated.

4.5.1 Operational Transients

Only transients that are encountered during normal operation are considered here for the evaluation of the power control system. These include 5 %FP per minute ramp changes in module powers and a corresponding turbine power change. Some slower transients are
also simulated. All simulations assume a PWR-type multi-modular power plant that consists of four identical modules as described in Section 2.2. The conditions imposed for all transients are:

i) Primary coolant loop flowrate of each power module taken as constant at 4453 kg/s.

ii) Primary coolant system pressure of each power module considered constant at 15.51 MPa.

iii) Feedwater temperature assumed to be a function of turbine power as shown in Figure 2-16.

iv) Charge and letdown flowrate assumed to be zero for all transients so as to isolate the power control system’s performance from the effects of changes in boron concentration and other parameters.

v) Boron concentration in the makeup flow assumed to be zero for these evaluations.

vi) Dead band for feedback control of the rod controller is set at ±0.5 %FP.

vii) The steam generator level controller used is the model-based one developed in this research and described in Chapter Three.

The secondary plant model in the simulation program developed here does not include the turbine, condenser, steam throttle valve, and steam dump valves. As a result, the load cannot be simulated exactly and hence the steam flowrate from the main steam line header cannot be calculated as a function of electric load demand, either. However, the quantity needed for the simulations because is a boundary conditions in the main steam line
header model. In this research, as mentioned previously, it was assumed that the turbine controller works perfectly. This allows calculations of the steam flowrate from the main steam line header. Specifically, it is assumed that the power withdrawn from the main steam line header is always equal to the turbine power that generator power is proportional to the turbine power. Under this assumption the steam flowrate is:

\[
\dot{m}_s = \frac{Q_{TB}}{h_g - h_{fw}}
\]  

(4-29)

where \(Q_{TB}\) is the turbine power and \(h_g\) is the saturated vapor enthalpy of steam generator inventory.

Control rod movement is important in the model-based controller because precise reactivity adjustments are required. Thus, it is assumed that the control rods of a PWR-type multi-modular power plant can be moved continuously and at variable speed. However, because some of current-generation PWR control rods move in discrete steps, the effect of the discrete rod movement is also evaluated.

The proposed controller may be sensitive to measurement noise because it uses the measured rate of change of neutronic power as one of its inputs. Accordingly, the effect of measurement noise on reactor neutronic power, primary coolant average temperature, primary coolant flowrate, and primary coolant system pressure is considered. The following random sensor noise is assumed for the simulations:

- Neutronic power measurement: \(\pm 0.1\%FP\)
- Primary coolant average temperature: \(\pm 0.1^\circ C\)
- Primary coolant system pressure and flowrate: ±0.1% of their nominal values.

It is assumed that all sensors and control elements work perfectly and without time delay because the sensor and control rod movement dynamic models are not included in the plant simulation program. The initial plant conditions assume xenon equilibrium. Decay heat is not included in the simulation. The time step sizes used for plant simulation and controller sampling time is 0.1 seconds unless discrete rod movement is included. Such movement requires approximately three quarter of a second for each complete step. Hence, for transients involving discrete rod movement, a controller sampling time step size of 0.8 seconds is used.

4.5.2 Evaluation of Rod and Module Supervisory Controller

As discussed earlier, the composite power control system has a multi-tiered structure. Accordingly, those controllers at the lowest level of the hierarchy are evaluated first. These are the rod and module supervisory controllers. In order to evaluate these controllers properly it is important to understand the design philosophy of the composite controller. The overall objective is to maintain the desired primary coolant temperature and hence, the desired steam flowrate fraction as specified. To this end, the composite controller generates demanded control signals for each module that will result in proper control of both module power and temperature. However, if these two objectives are in conflict, the control of temperature has precedence. For example, in the arbitrary demand power operational mode, model power is allowed to overshoot its target power so as to maintain temperature and hence steam flowrate. Under these circumstance, the supervisory
control action of each module's power controller becomes important because it is that supervisory action that limits module power to a safe envelope of conditions.

4.5.2.1 Evaluation of Rod Controller

The purpose of the rod controller is to move the control rods so that module power will follow a given power trajectory. The rod controller consists of the model-based feedforward control law and the feedback control law. In the highest-power module, the feedback control signal is computed from combination of the power and primary coolant temperature errors. However, in the low-power modules, the feedback control signal is computed from the ratio of power error to that of the module with the highest power. It should be noted that the performance of this model-based controller will depend on the reactor kinetic parameters used in the model, including the delayed neutron precursor decay constants and yield fractions. In this evaluation, it is assumed that all reactor kinetic parameters used in the controller model are appropriate.

The simulated transient that is used to evaluate the rod controller's performance is a simple power ramp. Module power is increased from 95 %FP to 100 %FP at a ramp rate of 5.0 %FP/minute. It is assumed that the control rods move continuously to produce the demanded module power.

Measurement noise is not considered for the initial studies. Figure 4-32 shows the demanded and observed power trajectories. As shown in this figure, the module power follows the desired trajectory almost exactly. If noise levels of ±0.1 %FP in the neutron power and ±0.1 °C in the temperature measurement are randomly imposed, then the
power trajectory shows a somewhat erratic behavior as indicated in Figure 4-33. However, the overall behavior is as expected because the imposed measurement error is randomly distributed. Figure 4-34 shows the power trajectories when the measurement signals are smoothed by use of the moving average method with ten data points. In this case, the power fluctuations are significantly reduced.

Based on these and other similar results, it can be concluded that the proposed rod controller does cause the module power to follow a demanded trajectory. Also, if measurement errors degrade performance, signal smoothing techniques can reduce power fluctuations significantly.

Figure 4-32  Module Power without Measurement Noise
Figure 4-33  Module Power with Measurement Noise

Figure 4-34  Module Power with Signal Smoothing Technique
4.5.2.2 Evaluation of Module Power Controller

As mentioned in Section 4.4.4, the arbitrary power demand operational mode allows module power to overshoot the target during power transients in order to maintain the desired primary coolant temperature and hence, the desired steam flowrate fraction. However, module power should be strictly prevented from exceeding safe operational power limits. Otherwise, the reactor protection system will be challenged. Therefore, the supervisory control action of the module power controller is important for safe operation because it overrides the rod controller.

In order to evaluate the supervisory module controller and its capability to maintain module neutron power within a safe limit, it is assumed that the rod controller continuously withdraws one control rod bank at its maximum speed when the module is at 100 %FP.

Figure 4-35 shows the time behavior of the module power during the above transient. The reactivity constraint is first violated about 4 seconds after the start of the power increase. Beginning at this time, the module controller recognizes that it must begin withdrawing positive reactivity from the core in order to avoid a violation of the safe operational power limit. Hence, the module controller interrupts the rod controller and inserts the control rod at full speed during the next control time step. This process is repeated and the module power controller successfully completes its task of maintaining the module power below the operational power limit.
Figure 4-35  Module Power When the Module Power Controller Supervises the Power Increase

4.5.3 Evaluation of Composite Power Control System

The power control system is designed to achieve proper control given demanded ramp changes in module and turbine power. A ramp rate of five percent per minute (± 5.0 %FP/minute) is typically the maximum for changes in module power. The maximum rate of turbine power change depends on the number of the active modules that can be allocated by the plant power controller. The operational status, active or inactive, of each module is determined by either the operator or the plant power controller based on the operating strategy.
This section presents the evaluation results of the composite power control system during operational power transients. Various operational modes and operating strategies are considered. Emphasis is placed on operation with unbalanced loads. The transients are identified by case number in the following sections.

4.5.3.1 Case 1: Power Increase in the Specified Power Demand Mode

Case 1 is a power increase in the specified power demand mode. Initially, each module's power level is different. The case study involves raising the power of each module to 100 %FP with different operating strategies. The power increase sequence and the active operating strategies are given in Table 4-3. Initially, module #1 is the highest-power module. The others are all different power levels. During the first phase, it is assumed that the active operating strategy is 'equal amount' change and that all modules are active. Hence, all module powers are increased by 5 %FP at a 5.0 %FP/minute ramp rate. Module #1 then reaches 100 %FP and this module cannot remain active any longer. It is assumed that the operating strategy is changed to 'extreme first'. Under this strategy only module #4 is active because it is currently the lowest power module. The maximum turbine power ramp rate is now reduced to 1.25 %FP/minute because only one module is active. If a higher ramp change in load is demanded, the plant power controller should either reject the load input or change the operating strategy. For example, the 'equal amount' strategy with module #1's power held constant will yield a maximum turbine power ramp rate of 3.75 %FP/minute. The second phase continues until module #4 attains 90 %FP. Both it and module #3 are now equal in power. Both module #3 and #4 are therefore now active. This portion of the transient is designated as the third phase. It continues until the these
low-power modules all attain 95 %FP. The fourth phase starts with all three modules being used to increase power.

The demand allocation method depends on the operating strategy and operational mode. Table 4-3 also gives the module and turbine demand power allocation methods.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Module Power</th>
<th>Turbine Power (% FP)</th>
<th>Operating Strategy</th>
<th>Demand Allocation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td># 1</td>
<td># 2</td>
<td># 3</td>
<td># 4</td>
</tr>
<tr>
<td>Initial</td>
<td>95</td>
<td>90</td>
<td>85</td>
<td>80</td>
</tr>
<tr>
<td>1</td>
<td>100</td>
<td>95</td>
<td>90</td>
<td>85</td>
</tr>
<tr>
<td>2</td>
<td>100</td>
<td>95</td>
<td>90</td>
<td>90</td>
</tr>
<tr>
<td>3</td>
<td>100</td>
<td>95</td>
<td>95</td>
<td>95</td>
</tr>
<tr>
<td>4</td>
<td>100</td>
<td>100</td>
<td>100</td>
<td>100</td>
</tr>
</tbody>
</table>

Figure 4-36 shows the module powers and turbine power. During the first phase, module power is increased first and ten seconds later turbine power starts to increase. Figure 4-37 presents the primary coolant average temperature of each module and the reference primary coolant average temperature of the highest-power module. The primary coolant average temperature of the highest-power module cannot follow the desired trajectory exactly because the time delay constant used in the plant power controller is not
Figure 4-36  Module and Turbine Power (Case 1 Transient)

Figure 4-37  Primary Coolant Average Temperature (Case 1 Transient)
Figure 4-38  Equivalent Control Rod Position (Case 1 Transient)

Figure 4-39  Steam Flowrates from Individual Steam Generators (Case 1 Transient)
Figure 4-40  Steam Generator Level (Case 1 Transient)

Figure 4-41  Steam Generator Pressure (Case 1 Transient)
known precisely. However, even though the primary coolant average temperature is not a controlled variable except in the highest-power module, all primary coolant average temperatures track their desired trajectories within the allowed error band (±0.5 °C). Figure 4-38 shows the control rod positions. The control rod position of module #1 remains constant after the power increase. This means that no feedback control action was performed to adjust the primary coolant temperature. Also, as noted, the primary coolant average temperature of the highest-power module stayed within the allowed error band. Therefore, it can be concluded that the plant power controller achieves the desired primary coolant average temperature trajectory through proper module and turbine power demand allocation.

Figure 4-39 shows the steam flowrates from the individual steam generators. The steam flowrates share the load without any oscillation during either transient or steady-state conditions. Figure 4-40 shows the steam generator water levels. As expected, the level of each module shows the effects of swell when module power is increased. However, the proposed model-based steam generator level controller maintains level successfully. Figure 3-41 shows the steam generator pressures.

### 4.5.3.2 Case 2: Power Increase in the Arbitrary Power Demand Mode

Case 2 is the same transient as described in Table 4-3 except that the operational mode is changed. In this case, the arbitrary power demand mode is used for both module and turbine power demand allocation. Figures 4-42 through 4-47 present the results of this evaluation.
Figure 4-42 shows the module powers and the turbine power. In this operational mode, the demanded module power is adjusted by a lead-lag dynamic compensator so that more energy is generated by the module than extracted by the turbine. Thus, the module power is allowed to overshoot the desired power. However, because the module power is supervised by the module power controller, it is guaranteed that module power will not violate the operational safe power limit. Figure 4-43 shows the primary coolant average temperature of each module and the reference primary coolant average temperature of the highest-power module. The primary coolant average temperature of the highest-power module stays within the allowed error band of ±0.5 °C. The other simulation results such as steam flowrate from each steam generator (Figure 4-45), generator level (Figure 4-46), and steam generator pressure (Figure 4-47) are similar to the previous ones. All parameters follow their desired trajectory and remain stable upon completion of the transient.

The proposed power control system also shows superior control performance in the arbitrary power demand mode.

4.5.3.3 Case 3: Power Decrease in the Specified Power Demand Mode

Case 3 involves a power decrease in the specified power demand mode. Initially, each module is at a different power level. The transient entails lowering all module powers to 80 %FP with varying operating strategies. The power decrease sequences and the operating strategies are given in Table 4-4. During the first phase, the operating strategy is 'extreme first'. Hence, power in the highest-power module, in this case module #1, is decreased first because only that module is active in the 'extreme first' strategy. Once
module #1's power reaches 95 %FP, module #2 is also activated because its power has become equal to that of module #1. This approach continues until all modules have attained the same power level. An 'equal amount' strategy is then adopted.

Figures 4-48 through 4-51 present the results of this evaluation.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Module Power</th>
<th>Turbine Power (% FP)</th>
<th>Operating Strategy</th>
<th>Demand Allocation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td># 1  # 2  # 3  # 4</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Initial</td>
<td>100  95  90  85</td>
<td>92.5</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>1</td>
<td>95   95  90  85</td>
<td>91.25</td>
<td>Extreme First</td>
<td>Path 3</td>
</tr>
<tr>
<td>2</td>
<td>90   90  90  85</td>
<td>88.75</td>
<td>Extreme First</td>
<td>Path 3</td>
</tr>
<tr>
<td>3</td>
<td>85   85  85  85</td>
<td>85.0</td>
<td>Extreme First</td>
<td>Path 3</td>
</tr>
<tr>
<td>4</td>
<td>80   80  80  80</td>
<td>80.0</td>
<td>Equal Amount</td>
<td>Path 1</td>
</tr>
</tbody>
</table>

4.5.3.4 Case 4: Power Decrease in the Arbitrary Power Demand Mode

Case 4 is identical to the transient described in Table 4-4 except that the arbitrary power demand mode is used for both module and turbine power demand allocation. Figures 4-52 through 4-55 present the results of this evaluation.
Figure 4-42  Module and Turbine Power (Case 2 Transient)

Figure 4-43  Primary Coolant Temperature (Case 2 Transient)
Figure 4-44  Control Rod Position (Case 2 Transient)

Figure 4-45  Steam Flowrates from Individual Steam Generators (Case 2 Transient)
Figure 4-46  Steam Generator Level (Case 2 Transient)

Figure 4-47  Steam Generator Pressure (Case 2 Transient)
Figure 4-48  Module and Turbine Power (Case 3 Transient)

Figure 4-49  Primary Coolant Temperature (Case 3 Transient)
Figure 4-50  Steam Flowrates from Individual Steam Generators (Case 3 Transient)

Figure 4-51  Steam Generator Level (Case 3 Transient)
Figure 4-52  Module and Turbine Power (Case 4 Transient)

Figure 4-53  Primary Coolant Temperature (Case 4 Transient)
Figure 4-54  Steam Flowrates from Individual Steam Generators (Case 4 Transient)

Figure 4-55  Steam Generator Level (Case 4 Transient)
4.5.3.5 Case 5: Power Transient When Demand Allocation Method is Changed

Cases 1 through 4 involved power increases or decreases under various operational modes and operating strategies. However, in these cases, the designated highest-power module was not changed during the power ramp and hence the demand allocation was not changed either. Case 5 involves a transient in which the highest-power module is kept at the initial power and the low-power modules are active. During the power ramp, the designated highest-power module is changed as is the demand allocation method. Table 4-5 gives the power increase sequence and operating strategies for this transient.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Module Power</th>
<th>Turbine Power (% FP)</th>
<th>Operating Strategy</th>
<th>Demand Allocation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>#1</td>
<td>#2</td>
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<td>#4</td>
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<td>Initial</td>
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<td>92.5</td>
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<td>2</td>
<td>100</td>
<td>100</td>
<td>95</td>
<td>97.5</td>
</tr>
</tbody>
</table>

Initially, the plant is at steady-state and module #1 is the highest-power module. Hence, demand allocation path 3 is applied when modules #2, #3, and #4 ramp up. Thirty seconds into the power increase, module #4's power reaches 95 %FP and it is then
designated as the highest-power module. Therefore, the demand allocation rule is changed from Path 3 to Path 2 (c) for the remainder of the power ramp. As given in Table 4-1, the magnitude of the time delay of Path 3 is +10 seconds while that for Path 2 (c) is -10 seconds. Hence, the plant power controller first raises module power under the demand allocation rule of Path 3. Once module #4’s power reaches 95 %FP, this module is designated as the highest-power module. The plant power controller then holds module power at current levels until a new demand allocation rule, that of Path 2 (c), is satisfied. During phase 2, a similar procedure is repeated.

Figures 4-56 through 4-59 present the results of this evaluation. Figure 4-56 shows the module and turbine power. Note that during both the first and second power ramps, the turbine power is increased continuously while the module powers are first increased, then held constant, and then increased again. This behavior occurs because the module power increases first under the demand allocation rule of Path 3 and then later under the demand allocation rule of Path 2 (c). The primary coolant average temperature (Figure 4-57), steam flowrates (Figure 4-58), and steam generator levels (Figure 4-59) all show good control performance.

Figures 4-60 through 4-63 show the evaluation results for the transient described in Table 4-5. In this case, the arbitrary power demand mode is used. Under this operational mode, only the value of the leading time constant is changed when the demand allocation rule is switched.
Figure 4-56  Module and Turbine Power in Specified Power Demand Operational Mode (Case 5 Transient)

Figure 4-57  Primary Coolant Temperature in Specified Power Demand Operational Mode (Case 5 Transient)
Figure 4-58  Steam Flowrates from Individual Steam Generators in Specified Power Demand Operational Mode (Case 5 Transient)

Figure 4-59  Steam Generator Level in Specified Power Demand Operational Mode (Case 5 Transient)
Figure 4-60  Module and Turbine Power in Arbitrary Power Demand Operational Mode (Case 5 Transient)

Figure 4-61  Primary Coolant Temperature in Arbitrary Power Demand Operational Mode (Case 5 Transient)
Figure 4-62  Steam Flowrates from Individual Steam Generators in Arbitrary Power Demand Operational Mode (Case 5 Transient)

Figure 4-63  Steam Generator Level in Arbitrary Power Demand Operational Mode (Case 5 Transient)
4.5.3.6 Case 6: Low Power Transient with Slow Ramp Rate

As previously mentioned, the magnitude of the time delay depends on the power. The static time delays, which are shown in Figure 4-22, are different from those used in the plant power controller. The latter are given in Table 4-1. The steam generator makes an especially large contribution to the static time delay at low power. The power increase described in Table 4-6 was chosen for study in order to illustrate this effect. All module powers are increased by 5.0 %FP on a 1.0 %FP/minute ramp rate.

Figures 4-64 through 4-67 shows the results from this simulation. Figure 4-65 depicts the time behavior of the primary coolant temperatures for each module. The primary coolant temperature follows the demanded trajectory during the early stages of the transient, but departs from that trajectory at about 150 seconds. The reason for the departure is that at about 150 seconds, the feedback effect on temperature from the steam generator pressure is dominant. Nevertheless, the primary coolant temperature always remains within the allowed error band. Hence, the steam flowrates do share the load in the desired manner as shown in Figure 4-66. Figure 4-67 shows the steam generator level response. As expected, the level perturbation in the lowest-power module is the largest.

Table 4-6 Power Increase Sequence for Case 6

<table>
<thead>
<tr>
<th>Phase</th>
<th>Module Power #1</th>
<th>Module Power #2</th>
<th>Module Power #3</th>
<th>Module Power #4</th>
<th>Turbine Power (% FP)</th>
<th>Operating Strategy</th>
<th>Demand Allocation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial</td>
<td>30</td>
<td>25</td>
<td>20</td>
<td>15</td>
<td>22.5</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>1</td>
<td>35</td>
<td>30</td>
<td>25</td>
<td>20</td>
<td>27.5</td>
<td>Equal Amount</td>
<td>Path 1</td>
</tr>
</tbody>
</table>
Figure 4-64  Module and Turbine Power in Specified Power Demand Operational Mode (Case 6 Transient)

Figure 4-65  Primary Coolant Temperature in Specified Power Demand Operational Mode (Case 6 Transient)
Figure 4-66  Steam Flowrates from Individual Steam Generators in Specified Power Demand Operational Mode (Case 6 Transient )

Figure 4-67  Steam Generator Level in Specified Power Demand Operational Mode (Case 6 Transient )
4.5.4 Discrete Control Rod Movement

A model-based digital rod controller requires precise reactivity adjustments in order to achieve proper trajectory-tracking of neutronic power. Therefore, it is desirable that it be possible to move the control rods continuously and at variable speed so as to implement the required reactivity adjustment. However, in some commercial PWRs, control rods are moved in discrete steps. Discrete rod movement in large steps can provide only crude reactivity adjustment and hence, the accurate trajectory-tracking of reactor power may be jeopardized and overall power control performance may be degraded. These possibilities are evaluated in this section. By way of introduction, current PWR discrete control rod movement is briefly described.

Figure 4-68  Control Rod Drive Assembly [S-10]
Figure 4-68 shows a Westinghouse PWR control rod drive mechanism [S-10]. The control rod is held by movable gripper latches. Movement is accomplished as follows. First, the upper, or lift, coil is energized. As a result, the entire movable gripper latch assembly, with the drive rod and attached rod cluster control assembly, is raised 15.88 mm. Next, the stationary gripper coil is energized. As a result, the stationary gripper latches and then picks up the drive rod load from the movable gripper latches. It pulls the rod up by approximately 1.59 mm. The movable gripper coil is then deenergized and the movable gripper latches pivot out. Finally, the lift coil is deenergized and the movable gripper latch assembly is lowered to its normal position. This sequence results in raising the control rod by 5/8 inch or one step. The sequence is accomplished in approximately 0.75 second. By varying the time interval between steps, the rod speed can be varied from 8 to 72 steps per minute. It is important to note following:

i) The control rods move at a single speed of 21.0 mm/s,

ii) One step requires 0.75 second, and

iii) Control rod speed between the steps is zero.

In this research, it was assumed that one step requires 0.8 seconds. This figure was chosen because it was compatible with the simulation code’s sampling interval of 0.1 second. A rod speed of 19.5 mm/s was also assumed.

Time plots of control rod speed and the resulting reactivity are shown in Figures 4-69 and Figure 4-70 respectively. When the control rods are withdrawn, the reactivity first increases and then decreases because of feedback effects. The resulting reactor power profile is saw-toothed in shape as shown in Figure 4-71. Hence, even though the model-based controller generates an appropriate rod signal, the observed power does not exactly
follow the demanded trajectory because of the effect of discrete control rod movement. However, if the saw tooth effect is discounted, power does, on average, follow the desired trajectory.

Figure 4-72 shows the control rod velocity for a simulation in which module power was increased from 95 %FP to 100 %FP with the control rods moving in discrete steps. Control rod chattering is observed during the increase. The rod controller inserts the rod immediately after withdrawal and then withdraws it again. This chattering occurs because of the rapid power increase when the control rod is withdrawn and the rod insertion that results from any error in the measured power. Figure 4-73 shows the reactor power while the control rod is chattering.

The transient described in Table 4-3 was repeated with discrete control rod movement and was designated Case 7. Figures 4-74 through 4-77 present the results of this evaluation. As shown in Figure 4-74, the module powers are jagged during the power ramp. However, the other parameters including primary coolant average temperature (Figure 4-75), steam flowrate (Figure 4-76), and steam generator level (Figure 4-77) do not show this effect because they are a function of the integral of change in power. The integration process has a smoothing effect.
Figure 4-69  Effect of Discrete Rod Movement on Control Rod Speed

Figure 4-70  Effect of Discrete Rod Movement on Reactor Reactivity Where is Presence of Feedback Effects
Figure 4-71  Module Power When Control Rod Moves in Discrete Steps

Figure 4-72  Control Rod Chattering When Control Rod Moves in Discrete Steps
Figure 4-73 Module Power When Control Rod is Chattering

Figure 4-74 Module Power (Case 7 Transient)
Figure 4-75  Primary Coolant Temperature (Case 7 Transient)

Figure 4-76  Steam Flowrates from Steam Generators (Case 7 Transient)
Figure 4-77  Steam Generator Level (Case 7 Transient)
4.6 Chapter Summary

In this chapter, the power control system of a PWR-type multi-modular power plant was developed and its control performance was evaluated through simulation of various power increases and decreases.

The main objective of the power control system is the automatic regulation of reactor power to a given setpoint at any power level from startup to full power. The controller must function under both transient and steady-state conditions without violating any safety limits. Also, it should give satisfactory control performance under any operational mode or any operating strategy. For these design goals, a multi-tiered, closed-loop digital power control system was proposed. Each level of this hierarchical controller was developed from knowledge of power controllers in current-generation nuclear power plants in concert with the operational characteristics of proposed multi-modular power plants.

The power control system developed here consists of three different controllers. These are the plant power controller, and the module power controller, the rod controller. Also needed, but not addressed in this research, is a turbine steam controller and a primary system pressurizer controller for each module. Each controller functions at a different level within the overall hierarchy and each has its own unique purpose. The plant power controller supervises the overall plant and allocates demand based on predeveloped rules that depend on the active operational mode and operating strategy. Several demand allocation rules were developed, all with the objective of achieving proper control of primary coolant temperatures during power transients. This controller also provides the man-machine interface in that operators can select the operational mode and operating
strategy at any time. The module power controller supervises transients so as to ensure that module power always stays within a predefined envelope of safe conditions. For this role, the reactivity constraint approach is applied. The rod controller interfaces directly to the plant signal and control elements. Its most important roles are to position the control rod so as to achieve the demanded module power and to cause module power to follow a demanded trajectory. Model-based feedforward action was combined with feedback to fulfill this role.

The performance of the proposed power control system was demonstrated by simulating various operational transients on a PWR-type multi-modular power plant. The simulation program was PMSIM which was developed in this research and which is described in Chapter Two. The major concerns in these evaluations were the automatic control of module power and primary coolant temperature and the proper division of steam flowrate among the individual steam generators when operating with unbalanced loads. All evaluation results showed satisfactory control performance. In particular, the primary coolant temperature followed the desired trajectory within allowed error bands and, hence, the steam flowrates shared their desired fraction without any unsatisfactory oscillation among modules.
Chapter Five

Summary, Conclusions, and Recommendations

5.1 Summary

This research had three primary objectives. One was to develop and evaluate an integrated plant simulation program for a PWR-type multi-modular power plant. The resulting program is capable of simulating normal operational transients in multi-modular plants, especially ones involving operation with unbalanced loads. The program models the reactor core and both the primary and secondary coolant loops of each module as well as the common main steam line header. This computer simulation program is named PMSIM for PWR-type Multi-modular power plant SIMulation program. PMSIM has been used both to obtain physical information on PWR-type multi-modular power plants and to verify a proposed controller’s performance. The second objective of this research was to develop and evaluate a robust, digital, closed-loop steam generator level controller. The as-designed controller ensures satisfactory automatic control of steam generator water level in both PWR-type multi-modular power plants and existing PWR plants. As a part of this
objective, a predictive display for use as an operator aid in the manual control of steam
generator level was developed. The third objective of this research was to develop and
evaluate a closed-loop, digital power control system for a PWR-type multi-modular power
plant. The as-designed controller both assures that no automated action results in a
challenge to the safety system and allows different modules of the multi-modular plant to
operate at different power levels thereby accommodating differing fuel depletion rates.

5.1.1 Summary of Simulation Program Development

The simulation program developed here for a PWR-type multi-modular power plant
can replicate the behavior of up to four power modules and their common main steam line
header. Included in the program are models of the reactor cores, fuel rods, primary coolant
loops, steam generators, common steam line header, and associated controllers. This
simulation program was designed for uses in control studies and it can simulate operational
transients including normal power increases and decreases. Models previously developed
by Kao [K-1], Cabral [C-1], Strchmayer [S-2], and Choi [C-2] were the starting point for
the development of this simulation program. The particular contribution of the research
reported here was to integrate subsystem models to form a whole plant simulation program,
to develop the model for the common main steam line header, and to develop the numerical
solution method for the multi-modular plant simulation program.

The reactor physics model is based on point kinetics with fuel and coolant thermal-
hydraulic feedback, fission product poisons, control rod motion, and chemical shim
included in the reactivity calculation. A single-fuel-rod heat transfer model was used to
represent the thermal-hydraulic behavior of the core. The thermal transport path starts at
the point of fission energy deposition within the solid fuel. It then proceeds through successive material layers including those of the fuel, gas at the interface of the fuel and cladding, and the cladding until reaching the interface with the light-water coolant. In order to simulate this process, a doubly-lumped-parameter model was used to calculate fuel and cladding temperatures. The property correlations for the fuel and cladding materials as a function of temperature were taken from THERMIT [K-4]

The thermal-hydraulic model for the primary loop includes control volumes for the reactor core, the reactor vessel upper plenum, the hot leg and steam generator inlet plenum, the steam generator tube bundle, the cold leg and steam generator outlet plenum, and the reactor vessel downcomer and lower plenum. The pressurizer is not modeled because it was assumed that primary system pressure was essentially constant under normal conditions of operation. Hence, the mass flowrate and pressure of the primary coolant system were specified as boundary conditions.

In order to design the steam generator level controller, a detailed simulation model that could analyze complicated steam generator level dynamics including 'shrink and swell effects' was needed. For this purpose, an existing steam generator model, that was developed by Strohmayer [S-2], served as the starting point for the steam generator secondary side simulation. This model is low-order, non-linear, and fast-running. Two salient features of the model are the incorporation of an integrated secondary recirculation loop momentum equation and retention of non-linear effects [S-2]. Also, this model had been validated over a wide range of steady-state and transient conditions by comparing results calculated with the model to both experimental data and other calculated results. Choi had modified this model to improve the simulation of the shrink and swell effects [C-2]. In PMSIM, a further modified version of this simulation program was used.
Specifically, the following changes were made for multi-modular plant simulation. First, the steam generator simulation model was combined with the primary side and main steam line header models. Second, its capacity was expanded to simulate up to four steam generators at the same time. Third, steam flowrates from each steam generator are determined from the main steam line header model. Originally, these flowrates had been specified as boundary conditions.

The main steam line header model included momentum conservation equations because the steam flowrates from each steam generator to the main steam line header depend on the hydraulics between the power modules and the main steam line header. Also, this model incorporated the moisture content at the main steam line header because that quantity is important to turbine operations. Turbine-generator and condenser models were not included in the simulation program. Steam flowrate from the main steamline header was calculated on the basis of energy conservation, namely that energy withdrawn is always proportional to the turbine load.

The simulation program can reproduce typical PWR steady-state operational data. Verification tests in which a symmetric response of the associated modules was observed were used to determine that this program can simulate a PWR-type multi-modular power plant.

### 5.1.2 Summary of Steam Generator Level Controller

The control system for steam generator water level is of major importance from the perspective of decreasing reactor unavailability. In a multi-modular plant, such a controller
is of particular importance because the need to stagger refuelings will make it necessary to operate with unbalanced loads. Hence, the steam flowrate from each steam generator may be different. As a result, a digital non-linear controller may be needed to maintain water level within allowed tolerances and to ensure satisfactory control performance under any circumstances.

Control problems associated with existing U-tube steam generator water level controllers were reviewed. Inverse response characteristics of steam generator water level were examined and a simplified transfer function model was improved by adding the effect of primary coolant temperature perturbations. The parameters in this simplified transfer function model were determined though the performance of numerical experiments using a detailed non-linear simulation model.

Based on knowledge of existing analog steam generator level controller problems and also on an understanding of steam generator level dynamics, a robust U-tube steam generator water level control system was designed to replace current, single-element PI level controllers. The intent was to achieve both excellent control performance and robustness at all power levels. Specifically, this new design permits the level feedback control gain to be tuned aggressively thereby improving performance and at the same time not incurring instability. Model-based inverse response compensators were designed for each perturbation parameter including feedwater flowrate, steam flowrate, and primary coolant temperature. These compensators were designed from a simplified transfer function model for the purpose of offsetting inverse response behavior. In order to make the compensation signals decay to zero under steady-state conditions, each compensator was combined with an inverse response predictor and an impulse function. For the compensator design of load parameters, a steam flowrate estimator was developed in order
to avoid the need to use an uncertain measured steam flowrate. This estimator uses measured neutron power, steam generator pressure, and feedwater temperature to approximate the actual steam flowrate. Finally, feedforward action was added to improve control performance.

The performance of the proposed controller was demonstrated through a wide range of operation including power adjustments on a multi-modular power plant. Improved steam generator level control performance was obtained through the use of model-based compensation techniques that were described above. The proposed controller is stable for PWR operation at both low and high power even when tuned with a high feedback control gain. Also, because it does not use measured flowrates, it is robust to flowrate measurement errors. Simulation studies of various transients also showed that the use of this new controller greatly reduced the effect of inverse response and significantly improved the controllability of steam generator level.

The proposed controller can be applied to the operation of steam generators in PWR-type, multi-modular power plants. In this case, continued use of the high feedback control gain causes oscillatory behavior because of steam flowrate oscillations between each steam generator and the main steam line header. The compensators do not correct for this because they utilize the reactor's neutronic power which reflects the total steam flowrate from the entire plant and not that from an individual part of a given module. However, if the proposed controller is tuned with a low feedback control gain, the control performance is acceptable during operation with unbalanced loads. The proposed controller also exhibits robustness to errors in the parameters of the simplified model.
5.1.3 Summary of Power Control System

The operation of a multi-modular power plant with unbalanced loads creates a complex control problem because each power module supplies steam to a common header and the steam flowrate from each module depends on both the hydraulic conditions in that module and the pressure in the common steam line header. Thus, conditions in each steam generator and the common header must be coordinated so as to allow each power module to supply a desired fraction of the total plant load.

Control principles and strategies that have proven effective in other types of power plants can be utilized in part to design the power control system of a PWR-type multi-modular power plant. In order to identify relevant techniques, a state-of-the-art survey of such technologies was conducted. In this regard, existing PWR, CANDU, and PRISM power control systems were examined. In addition, new reactor control technologies that have been developed at MIT were reviewed. A sliding $T_{ave}$ program was possible only in one module and here was adopted for the highest-power module. Strategies for the other modules were developed as part of the research reported here. The thermal-hydraulic characteristics of PWR-type multi-modular power plants were analyzed and an operating map was generated. Operating strategies and operational modes for use in the power control system were then enumerated.

The proposed control system has several objectives. First, it should provide automatic control of reactor power to a given setpoint at any power level from startup to full power operation. Moreover, it must do this during both transient and steady-state conditions without violating any operational power constraints and/or safety limits. Second, it should give proper control performance under any operational mode or operating
strategy. Specifically, it should regulate the module power and steam flowrate from each module at the desired fraction during both transient and steady-state operation. Third, it should monitor module power and maintain it at the desired level. Also, it should reduce the module power if that power should ever approach a predetermined limit. For these design goals, a multi-tiered, closed-loop digital power control system was proposed. Each level of this hierarchical controller was developed from knowledge of power controllers in current-generation nuclear power plants in concert with the operational characteristics of proposed multi-modular power plants.

Two operational modes were defined and studied. In the arbitrary power demand mode, the controller would receive an electric load demand signal from the load dispatcher. This signal would specify the load demand to be carried by the turbine-electric generator. The load demand would then be divided among the active modules by means of an on-line operating strategy. At the same time, another signal would be sent to the turbine controller to adjust the throttle valve. In the specified power demand mode, the load demand signal would originate with the plant operator in charge of the control console. Other control signal activities would be the same as those for the arbitrary power demand mode.

The power control system developed here consists of three different controllers. These are the plant power controller, the module power controller, and the rod controller. Also needed, but not addressed in this research, is a turbine steam controller. Each controller functions at a different level within the overall hierarchy and each has its own unique purpose. The plant power controller supervises the overall plant and allocates demand based on predeveloped rules that depend on the active operational mode and operating strategy. Several demand allocation rules were developed, all with the objective of achieving proper control of primary coolant temperatures during power transients. This
controller also provides a man-machine interface in that operators can select the operational mode and operating strategy at any time. The module power controller supervises transients so as to ensure that module power always stays within a predefined envelope of safe conditions. For this role, the reactivity constraint approach is applied. The rod controller interfaces directly to the plant signal and control elements. Its most important functions are to position the control rods so as to achieve the demanded module power and to cause module power to follow a demanded trajectory. Model-based feedforward action was combined with feedback for this purpose.

The performance of the proposed power control system was demonstrated by simulating various operational transients on a PWR-type multi-modular power plant. The simulation program was PMSIM that was developed as part of this research. The major concerns in these evaluations were the automatic control of module power and primary coolant temperature and the proper division of steam flowrate among the individual steam generators when operating with unbalanced loads. All evaluation results showed satisfactory control performance. In particular, the primary coolant temperature followed the desired trajectory within allowed error bands and hence, the steam flowrates shared their desired fraction without oscillation among modules.
5.2 Conclusions

Conclusions based on the achievements of this research are made here regarding the simulation program development, the steam generator level controller, and the power control system design.

5.2.1 Conclusions on Simulation Program

The integrated plant simulation program that was developed here can reproduce the characteristics of a PWR-type multi-modular power plant that consists of up to four modules. Moreover, it can do this about three times faster than real-time on a VAX station-II. According to reference [A-2], this mini-computer's relative speed is slower than that of other VAX stations or Apollo minicomputers. This means that this simulation program can be used as part of the simulator for a PWR-type multi-modular power plant.

The numerical solution method used in PMSIM was proven to provide a stable solution during both steady-state and transient conditions. A unique feature of PMSIM is that it includes both a steam generator secondary side and a main steam line common header model and hence can simulate the operating characteristics of modular power plants under unbalanced load conditions.

The simulation program can reproduce typical PWR steady-state operational data. Verification tests in which a symmetric response of the associated modules was observed were used to determine that this program can simulate a PWR-type multi-modular power plant.
5.2.2 Conclusions on Steam Generator Level Controller

The simplified model can predict steam generator water level response for the cases of feedwater, steam flowrate, and primary coolant temperature perturbations provided that proper identification of the parameters used in that model has been made. Therefore, this model can replace detailed nonlinear models in model-based steam generator level controller designs. This is an advantage because the model developed here uses a simple numerical calculation routine while a detailed, nonlinear one requires a complicated computer program for its solution.

It is possible to mitigate or eliminate the stability constraints imposed on the level feedback control gain of single-element PI controllers by using a feedwater flowrate compensator that is designed on the basis of the simplified transfer function model. In the digital controller developed here, the use of uncertain flowrate measurements is avoided. Thus, the proposed steam generator level controller is robust to feedwater flowrate measurement.

It was found that inverse response behavior such as shrink and swell that results from changes in steam flowrate and primary coolant temperature could be mitigated through the use of compensators that were designed using the simplified transfer function model. However, because these parameters do not form a closed-loop, estimators or measurement sensors were needed. Finally, in order to maintain insensitivity to errors in steam flowrate measurement, a steam flowrate estimator was needed. It was designed by using neutron power measurements.
The proposed controller provides stability and proper control performance for PWRs over the entire power range. Also, if it is tuned with a low feedback control gain, it displays acceptable control performance during operation with unbalanced loads in multi-modular power plants. The proposed controller also exhibits robustness to errors in the parameters of the simplified model.

5.2.3 Conclusions on Power Control System

A sliding $T_{ave}$ program can only be applied to the highest-power module in a PWR-type multi-modular power plant because the steam flowrate from each module depends on both the hydraulic conditions in that module and on the pressure in the common steam line header. As a result, the temperature in the lower power modules can not be controlled to the same program during unbalanced operation. Specifically, all modules must have the same steam pressure at the inlet of the common main steam line header in order for steam to flow to that header. If all modules have a common steam pressure and yet each supplies a different fraction of the load, then each must operate at a different temperature.

It was found that a the multi-tiered controller structure would be effective for the design of a power control system for a PWR-type multi-modular power plant. In particular, a special demand allocation method is required to maintain the desired primary coolant temperature and hence, the desired steam flowrate fraction. For this purpose, dynamic demand allocation is used to cause these parameters to follow their demanded trajectories. As a result, a hierarchical control approach becomes important because supervisory action may be needed to restrict module power to a safe envelope of conditions.
All evaluation results showed satisfactory control performance. In particular the primary coolant temperature followed the desired trajectory within the allowed error bands and hence, the steam flowrates shared their desired fractions without any oscillation among modules.
5.3 Recommendations for Future Work

Recommendations for future work are enumerated here in regard to both the plant simulation program and the development of the control system.

5.3.1 Recommendations for Plant Simulation Program

The simulation program developed here is limited to replication of normal power increases and decreases. The following is a list of recommendations for further refinement and application of the models developed in this research.

i) **Three-Dimensional Core Dynamics**: A point kinetics model and a single control volume loop model were used for the reactor neutronic power and average coolant temperature calculations. These models should be replaced by a three-dimensional neutronics model coupled with a three-dimensional core thermal-hydraulics model so as to allow analysis of the local neutron power distributions. This approach has already been done by Aviles for a PWR [A-2]. Thus, all that is needed is to expand the approach to multiple cores as exist in a modular plant.

ii) **Plant Simulator**: The plant model should be further improved so as to be suitable for use as a PWR-type multi-modular power plant simulator. The needed work entails developing models of pressurizers and primary pumps as well as ones of the steam dump, turbine, and condenser. Also, models of the control algorithms for those components will be required as part of the simulator.
iii) **Sensor and Actuator Dynamics:** These dynamics are important in the control system design because their features such as time delays have the potential to cause deterioration of the control system's performance. Hence, these dynamics should be taken into consideration in the simulator model.

iv) **Dynamic Graphic Display:** Interactive graphic display software should be incorporated in the man-machine interface so that the operator can visualize the simulated plant dynamics in either real time or on a faster-than-real-time basis.

### 5.3.2 Recommendations for Controller Development

Suggestions for future work in the integrated control area include the development of the steam generator level controller and the power control system.

i) **Steam Flowrate Estimator:** The performance of the steam generator level controller depends on the accuracy of the load parameter compensators. In this research, the steam flowrate estimator utilized measurements of the neutron power. However, it was found that the steam flowrate from each generator was not proportional to the neutron power during transients. This fact can degrade the performance of the level controller if the controller is not properly tuned. The steam flowrate estimator may be improved by using additional available system parameters such as pressure measurements in the steam generator and common header.

ii) **Demand Allocation Rules:** Demand allocation methods could be improved by implementing variable time delays during power increases and decreases. This
would require development of an adaptive method that determines the appropriate
time delay from the coolant system and steam generator dynamics.

iii) **Operation Procedure in Other Operational Transients:** In this research, normal
power increases and decreases were considered. Other transients should be
studied. For example, the plant should be able to continue functioning if one
module were to be scrambled. Demand allocation methods should be developed to
handle this situation. These should include control (and hence modeling of) the
steam dump because the steam generator pressure may be adjusted temporarily by
means of the steam dump control system in the event of single module trip.

iv) **Local Power Distribution Control:** The local power distribution in the individual
modules may be important because of reactor operational limits. For example, the
departure from nucleate boiling ratio (DNBR) strongly depends on the local power
distribution. Therefore, the module power controllers should incorporate a
supervisory function to monitor those operating limits. For this task, a three-
dimensional neutronics and a core thermal-hydraulics model are necessary.

v) **Sensor Dynamics:** The sensor dynamics of the various controlled variables should
be taken into consideration during the design stage of the controller because time
delays associated with sensor activation can degrade control system performance.

vi) **Evaluations:** Further evaluations of the proposed controller should be made by
performing composite tests.
Appendix A

Steam Generator Water Level Predictive Display Program

A.1 Introduction

Unplanned reactor trips because of steam generator level instability have contributed significantly to the unavailability of PWRs. Steam generator water level control is complicated by the thermal reverse effects known as 'shrink and swell.' The performance of current automatic controllers for steam generator water level control is often found to be unsatisfactory during low power operation. This in turn may require that an operator take manual control to avoid reactor trips. However, such action is itself not without difficulty because system interactions, the delay between control demand and response, and the counterintuitive steam generator water level dynamics combine to complicate the operator's task. To deal with these complexities, an operator should understand how steam generator parameters respond to control actions. A model-based predictor with appropriate displays
may improve an operator's capability to control level by showing the consequences of a particular control action prior to actually having to implement it.

The concept of providing predictive displays as an aid to those responsible for controlling complex processes is not new. Early applications of the approach concerned the diving controls for submarines and the landing system for Apollo spacecrafts [K-7]. More recently, the technique has been utilized for air and railroad traffic control [E-1,H-4]. Relative to the nuclear industry, the use of predictive display technology as an operator aid for steam generator level in PWRs has been previously suggested though not implemented [V-1]. The motivation for the work reported here on the design of a predictive display for steam generator level was a successful study that had been previously performed at MIT on the provision of predictive information on neutronic power to reactor operators [L-6]. Five displays that provided the operator with various combinations of derivative, current, and predictive information were developed and evaluated through actual use on the 5 MWt MIT Research Reactor, MITR-II. Operators had to be trained to use the predictive information. However, once they understood it, most reported its availability to be of benefit. This was especially true if a power maneuver was to be performed with the control rods configured in other than their normal pattern. Given this success, it was decided to develop a similar display for steam generator level.

Currently, a steam generator operator's primary indications of the water inventory are the narrow and wide level indicators located in the steam generator downcomer. These indicators notify the operator of the height of the water in the steam generator downcomer. However, because these indications are susceptible to the counterintuitive effects of shrink and swell, it is often difficult for the steam generator operator to make the correct decision about how he or she should respond to keep the mass inventory of the steam generator
within the prescribed levels. The operator can adjust steam generator levels by changing the feedwater flowrate. This is done by changing the position of either the bypass or the main feedwater valves. During low power conditions (below approximately 15% of full power), the bypass valve is used because it is smaller than the main valve and, hence, more sensitive. Also, the relation between its position and flowrate is more linear than that of the main valve. This is especially important during low power conditions.

The method for avoiding operator-related, steam generator trips that has been most often implemented is the replacement of the operator with an automatic level controller. Traditionally, these level controllers have been analog systems that have proved inadequate during low power transients and have led to a return to manual control. Recent advances in non-linear digital steam generator control have produced some promising results. However, there may be a substantial lag before such controllers are implemented on a wide scale in the United States. Other methods for improving level control are therefore desirable.

A predictive display might fulfill this need by giving the steam generator operator an understanding of how his adjustments of feedwater flow will affect the narrow level some fifty seconds to a few minutes in the future. Such displays might reduce the frequency of operator-related trips by forewarning operators of the effects of shrink and swell, and hence removing their natural response to overcompensate for the movement in narrow level caused by transient phenomena such as bubble formation and collapse. In this Appendix, a program designated as Steam Generator Level Display Program (SGLDP) is reported.
A.2 Predictive Display Program

The SGLDP is a FORTRAN program that combines a steam generator water level simulation routine, a level display routine, and an operator interaction routine as shown in Figure A-1. This program works on any IBM-compatible personal computer that is equipped with a VGA or Hercules graphic board.

The steam generator water level simulation routine uses the mathematical model that was developed in Chapter Three of this report. The program projects the steam generator narrow gauge level signal for three cases: control valve being closed, control valve position maintained constant, and control valve being opened. The operator can select the speed at which the valve is to be opened or closed. The demanded reactor power is also displayed so that the operator can observe the correlation (or lack thereof) between power demand and change in anticipated level in the steam generator. The model gives the narrow-range steam generator level in the downcomer as a function of the steam and feedwater flowrates. Four terms are included. The first is a mass capacity term that reflects the net difference between the steam and feedwater flowrates. The second allows for shrink/swell effects associated with changes in feedwater flow. The third is similar except that it is for changes in steam flow. The fourth allows for short-lived mechanical oscillations that can be caused by the addition of feedwater to the generator. As was reported in Chapter Three, this model's accuracy was verified by comparison with a much larger and more rigorous model that had been benchmarked against plant data. Projections of up to 200 s are possible with a display update frequency one second.
Figure A-1  Block Diagram of SGLDP
Figure A-2 shows the predictive display for steam generator level. The upper portion shows the reactor power and the lower portion depicts steam generator level. Reactor power was initially at 10% of rated and it is being raised at 2.5% of rated per minute. Derivative information, the steam generator level for the previous 100 s, is shown together with the current level. Emanating from the current level are the three projections, each corresponding to a possible control option (valve opened at selected rate, held constant, or closed at selected rate). In the actual display, each option is shown in a different color. The advantage of this display is that an operator can visualize the effects of adjusting the position of the feedwater control valve before doing so. This capability should result in more reliable operation because even though operators are trained to and do understand the counterintuitive nature of shrink and swell, they may have difficulty quantifying those effects. Thus far, no trials of this display have been conducted either by simulation or in an actual plant. Additional information is given in [B-9].
Figure A-2  Steam Generator Level Display After Start of Power Ramp with Ramp Rate of 2.5% of Full Power Per Minute with Reactor Initially at 10% of Rated Power
Appendix B

Stability Analysis of Steam Generator Inverse Simulation Model

B.1 Introduction

A model-based steam generator water level controller which compensates for shrink and swell effects using the mass inventory change in the tube bundle was previously developed at MIT [C-2]. This model was designated as the SGLCP which stands for 'steam generator level control program.' The SGLCP used an inverse simulation model to evaluate the mass inventory in the tube bundle because that quantity is not directly measurable and because feedwater and steam flowrates are difficult to measure accurately at low power. Thus, the SGLCP differs from typical steam generator simulation models which estimate internal pressure, downcomer water level, and other state variables from the feedwater flowrate, steam flowrate, and primary side data. These more typical models are called 'forward simulation models.' However, because steam generator internal pressure and water level are measurable, it is possible to eliminate the steam and feedwater flowrates
from the required list of the steam generator simulation input variables and replace them with the measured steam generator internal pressure and water level. This modified steam generator model is what is meant by an 'inverse simulation model.' Its advantage relative to the forward-simulation type model is that it eliminates reliance on flowrate measurements that, as noted earlier, are not reliable at low power. The input data and state variables of the forward and inverse simulation models are summarized in Tables B-1 and B-2.

Table B-1  Input Variables and State Variables of Forward Simulation Model

<table>
<thead>
<tr>
<th>Input Data</th>
<th>Primary Side</th>
<th>Secondary Side</th>
</tr>
</thead>
<tbody>
<tr>
<td>- Primary Coolant Inlet Temperature</td>
<td>- Feedwater Flowrate</td>
<td></td>
</tr>
<tr>
<td>- Primary Coolant Flowrate</td>
<td>- Steam Flowrate</td>
<td></td>
</tr>
<tr>
<td>- Feedwater Temperature</td>
<td>- Feedwater Temperature</td>
<td></td>
</tr>
<tr>
<td>State Variables</td>
<td>- Temperature at the Inlet Plenum Outlet</td>
<td>- Internal Energy at the Bottom of Downcomer</td>
</tr>
<tr>
<td>- Temperature at the Tube Bundle Outlet</td>
<td>- Vapor Volume in the Steam Dome and Downcomer</td>
<td></td>
</tr>
<tr>
<td>- Temperature at the Outlet Plenum Outlet</td>
<td>- Void Fraction at the Tube Bundle Outlet</td>
<td></td>
</tr>
<tr>
<td></td>
<td>- Void Fraction at the Riser Outlet</td>
<td></td>
</tr>
<tr>
<td></td>
<td>- Pressure Inside Steam Generator</td>
<td></td>
</tr>
<tr>
<td></td>
<td>- Average Recirculation Flowrate</td>
<td></td>
</tr>
</tbody>
</table>
Table B-2  Input Variables and State Variables of Inverse Simulation Model

<table>
<thead>
<tr>
<th></th>
<th>Primary Side</th>
<th>Secondary Side</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Input Data</strong></td>
<td>- Primary Coolant Inlet Temperature</td>
<td>- Pressure inside Steam Generator</td>
</tr>
<tr>
<td></td>
<td>- Primary Coolant Flowrate</td>
<td>- Steam Generator Water Level</td>
</tr>
<tr>
<td></td>
<td></td>
<td>- Feedwater Temperature</td>
</tr>
<tr>
<td><strong>State Variables</strong></td>
<td>- Temperature at the Inlet Plenum Outlet</td>
<td>- Internal Energy at the Bottom of Downcomer</td>
</tr>
<tr>
<td></td>
<td>- Temperature at the Tube Bundle Outlet</td>
<td>- Void Fraction at the Tube Bundle Outlet</td>
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</tr>
<tr>
<td></td>
<td></td>
<td>- Average Recirculation Flowrate</td>
</tr>
</tbody>
</table>

Inverse simulation models are required for advanced digital control techniques such as feedforward and computed torque control [A-5]. In these methods of control, model-based calculations are combined with feedback to produce the actuator signal that will cause a system to move along a demanded path. Figure B-1 illustrates one of the more effective ways to combine the two. The observed system output is compared to that demanded. The resulting error signal is then used to modify the demanded trajectory so that the system will be restored to the desired output. This modified demanded trajectory is processed through an inverse model to obtain the appropriate actuator signal. The success of the technique depends in large measure on the accuracy and stability of the inverse model. In this report, a steam generator inverse simulation model is described and a stability analysis of that model is performed using eigenvalue analysis.
Figure B-1  Conceptual Idea for Model-Based Controller.
B.2 Steam Generator Inverse Simulation Model

The steam generator secondary side state equation can be expressed as a sixth order coupled differential equation of the form [R-4]:

$$A \frac{dx}{dt} = f$$  \hspace{1cm} (B-1)

where $A$ is the coefficient matrix of the differential equation, $f$ is the coefficient vector, and $x$ is a state vector whose elements are $U_0$, $V_v$, $<\alpha_t>$, $<\alpha_n>$, $P$, and $\bar{w}$. These are defined as:

- $U_0$ is the internal energy at the bottom of the steam dome downcomer,
- $V_v$ is the void volume in the steam dome downcomer,
- $<\alpha_n>$ is the void fraction at the tube bundle exit,
- $<\alpha_t>$ is the void fraction at the riser exit,
- $P$ is the saturation pressure inside the steam generator, and
- $\bar{w}$ is the average recirculation flowrate.

Upon solving for $f$, one obtains:

$$f = \text{Col}\{ \bar{w}(H_0 - H_r) + Q_B, \bar{w}(H_r - H_n) \}$$
\[ \bar{w}(H_n - H_k) - \bar{w}(H_{vs} - H_k), \]

\[ W_{fw}(H_{fw} - H_k) - W_s(H_o - H_k), \]

\[ W_{fw} - W_s, \]

- \( F \)

where symbols are defined as follows:

- \( W_{fw} \) is the feedwater flowrate,
- \( W_s \) is the steam flowrate,
- \( H_o \) is the enthalpy at the bottom of downcomer,
- \( H_n \) is the enthalpy at the tube bundle outlet,
- \( H_k \) is the enthalpy at non-boiling position,
- \( H_r \) is the enthalpy at the riser outlet,
- \( H_{fw} \) is the enthalpy of the feedwater,
- \( F \) is the pressure loss, and
- \( Q_B \) is the heat transferred from the primary to the secondary side.

The steam generator level dynamics are strongly nonlinear because the coefficient matrix \( A \) and the coefficient vector \( f \) are functions of the state variables. If the steam generator internal pressure and water level and their time derivatives are known, then feedwater and steam flowrates can be eliminated from Equation (B-1). Thus, Equation (B-1) can be
reduced to a fourth order differential equation by eliminating these two flowrates. Therefore,

\[
\mathbf{B} \frac{d}{dt}\mathbf{y} = \mathbf{g}
\]  

(B-2)

where \( \mathbf{y} \) is a new column vector of the form \( \text{Col}\{U_0, <\alpha_t>, <\alpha_n>, \text{ and } \overline{w}\} \) and the matrix \( \mathbf{B} \) and the vector \( \mathbf{g} \) are defined as:

\[
\mathbf{B} = \begin{bmatrix}
A_{11} & A_{13} & A_{14} & 0 \\
A_{21} & A_{23} & A_{24} & 0 \\
A_{31}^* & A_{31}^* & A_{34}^* & 0 \\
0 & 0 & 0 & A_{66}
\end{bmatrix}
\]

where \( A_{i,j} \) is an element of matrix \( \mathbf{A} \) in Equation (B-1) and

\[
A_{31}^* = A_{51} - \frac{A_{31}}{H_{v2}-H_k} - \frac{A_{41}}{H_{fw}-H_k}
\]

\[
A_{33}^* = A_{53} - \frac{A_{33}}{H_{v2}-H_k} - \frac{A_{43}}{H_{fw}-H_k}
\]

\[
A_{34}^* = A_{54} - \frac{A_{34}}{H_{v2}-H_k} - \frac{A_{44}}{H_{fw}-H_k}
\]

and
\[
g = \begin{bmatrix}
\overline{W}(H_0 - H_r) + Q_B - A_{15} \\
\overline{W}(H_r - H_n) - A_{25} \frac{dP}{dt} \\
\frac{\overline{W}(H_0 - H_k - H_n - H_k)}{H_{fw} - H_k} - A_{32} \frac{dV_v}{dt} - A_{35} \frac{dP}{dt}
\end{bmatrix}
\]

In order to use pressure and water level instead of feedwater and steam flowrates as inputs to the inverse simulation model, the time derivative of the pressure and the steam dome downcomer vapor volume \( V_v \) are required. These are obtained from successive measurements of the pressure and water level as follows:

\[
\frac{dP}{dt} = \frac{P^{n+1} - P^n}{t^{n+1} - t^n}
\]  
(B-3)

and

\[
\frac{dV_v}{dt} = \frac{V_{v}^{n+1}(L_{w}^{n+1}) - V_{v}^{n}(L_{w}^{n})}{t^{n+1} - t^n}
\]  
(B-4)

where \( L_w \) is the water level measurement, \( t \) is time, and the superscripts \( n \) and \( n+1 \) denote time step number. The saturated vapor volume corresponding to the water level measurement, \( L_w \), can be obtained from a knowledge of the steam generator geometry.

The other equations are the same as those of a forward model.
B.3 Stability Analysis of Steam Generator Inverse Simulation Model

Because the terms in Equation (B-2) are functions of the state variables of the column vector $y$, it is very difficult to analyze the stability of the inverse simulation model without first linearizing it about some operating point. Upon so doing, the analysis is valid for small perturbations about the operating point [S-2]. A stability analysis of the forward simulation model was performed in reference [R-4]. According to that result, the forward simulation model is stable. The analysis for the inverse model is given here.

First, linearize the state variable vector $y$ about an operating point $y_o$ such that

$$y = y_o + \epsilon$$

(B-5)

where $\epsilon$ is a small perturbation. Expand the right side of equation (B-2) using a Taylor series about the point $y_o$ to obtain:

$$B^{-1}g = B^{-1}g_{y_o} + J_{y_o}(y - y_o) + \text{higher order terms}$$

(B-6)

where $J_{y_o}$ is the Jacobian matrix of $B^{-1}g$ with respect to $y$ evaluated at $y_o$. That is:

$$J_{i,j} = \frac{\partial(B^{-1}g)}{\partial y_j}$$

(B-7)
If the perturbation is small enough, the higher order terms in Equation (B-6) can be neglected. Then, substitution of Equations (B-5) and (B-6) into Equation (B-4) yields:

\[ \frac{d\epsilon}{dt} = J_{yo} \epsilon . \]  

(B-8)

The stability properties of the linear system described by Equation (B-8) are directly linked to the eigenvalues of the matrix $J_{yo}$. In order to investigate the relationship between the stability of Equation (B-2) and the matrix $J_{yo}$, the matrix $J_{yo}$ is diagonalized as the transition matrix $P$. Thus,

\[ P^{-1} J_{yo} P = \Lambda \equiv \text{diag}\{\lambda_i\} \]  

(B-9)

where $\lambda_i$ is the eigenvalue of the Jacobian matrix $J_{yo}$. Next, define another vector $v$ such that

\[ \epsilon = P v . \]  

(B-10)

The substitution of Equation (B-10) into Equation (B-8) yields

\[ \frac{dv}{dt} = P^{-1} J P v = \Lambda v \quad \text{and} \]

(B-11)

from Equation (B-11)
\[ \frac{dv_i}{dt} = \lambda_i v_i. \]  

(B-12)

Thus, upon solving Equation (B-12), one obtains:

\[ v_i = v_{i0} \exp(\lambda_i t) \]  

(B-13)

As can be seen from Equation (B-13), the real part of every eigenvalue of the Jacobian matrix should be less than zero in order for the perturbation to die out.

As originally configured, the SGLCP used both forward and inverse calculations. However, for the control system under consideration here, an independent inverse model was essential. Accordingly, a stability analysis of this model was performed using data from an actual commercial nuclear power plant. The steady-state operating points about which linearizations were performed were 10% and 100% of full-power. The analysis was done as outlined above except that the Jacobian matrix was obtained by numerical differentiation because the coefficient matrix B was quite complicated.

The eigenvalues of the fourth order inverse simulation model are summarized in Table B-3. As shown in that table, the real parts of some eigenvalues are positive. This indicates that the fourth order inverse simulation model described by Equation (B-2) is not stable. That is, a small disturbance to the state vector will eventually cause the calculation to diverge. Figures B-2 to B-4 are from studies in which the inverse and forward simulation models were compared. Steady-state operation at 10 \%FP was assumed. A null transient in which all perturbation parameters (feedwater flowrate, steam flowrate, and primary coolant temperature) were kept constant, was selected. For the inverse simulation,
the exact pressure and level which were simulation results of the forward simulation, were used. The inverse model was used to calculate the internal energy at the bottom of the downcomer \(U_o\), the void fraction at the tube bundle outlet \(\alpha_r\), and the void fraction at the riser outlet \(\alpha_n\). As shown in these figures, the results of the inverse simulation grew exponentially as was expected from the eigenvalue analysis. This implies that the inverse simulation model is not stable and that it cannot be used as an on-line predictor in a model-based controller.
Table B-3  Eigenvalues of the Fourth Order Inverse Simulation Model

<table>
<thead>
<tr>
<th></th>
<th>100 %FP</th>
<th>10 %FP</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>-3.8734</td>
<td>-0.9333</td>
</tr>
<tr>
<td>2</td>
<td>-0.0166 + 0.3794i</td>
<td>0.0281</td>
</tr>
<tr>
<td>3</td>
<td>-0.0166 - 0.3794i</td>
<td>0.3369 + 0.388i</td>
</tr>
<tr>
<td>4</td>
<td>2.2206</td>
<td>0.3369 - 0.388i</td>
</tr>
</tbody>
</table>

* i = √-1

Figure B-2  Forward and Inverse Simulation Results (Internal Energy at the Bottom of Downcomer, 10 %FP Steady State)
Figure B-3  Forward and Inverse Simulation Results (Void Fraction at the Tube Bundle Outlet, 10 %FP Steady State).

Figure B-4  Forward and Inverse Simulation Results (Void Fraction at the Riser Outlet, 10 %FP Steady State)
Appendix C

Smith's Dead-time Compensator

If a physical process involves significant dead time such as a transport lag, a conventional feedback controller may provide a quite unsatisfactory closed-loop response. The problem is that a change in an input does not produce on immediate, corresponding change in an output. Hence, it is not possible to obtain any information for use as feedback. Control action may be incorrect because such actions will be based on previous instead of current measurements of the output. Therefore, the presence of dead time may be an important source of instability in a closed-loop response. In order to improve control of such processes, Smith suggested a compensation element connected around the primary controller [Smith].

Smith's compensator is based on the Smith principle which states that if a response satisfies the design criteria for the delay-free case, then the response to be designed for the delayed case should be the same except that is delayed by whatever dead time is involved.
Figure C-1  Schematic of the Control System with Smith's Dead-time Compensator

In order to obtain the delay-free signal, a model-based dead time compensator which predicts the delayed effect is used. Figure C-1 is a schematic of the Smith dead-time compensator. The measured signal, \( y(s) \), is compensated by the signal \( y'(s) \). This compensating signal is obtained from a simple local feedback loop that goes around the controller. It is called the Smith dead-time compensator. The control input signal, \( y^*(s) \) can be expressed as follows:

\[
y^*(s) = y(s) + y'(s)
\]

\[
= [G_c G e^{-ts} + (1 - e^{-ts}) GG_c] y_{sp}(s)
\]

\[
= G_c G y_{sp}(s)
\]

(C-1)
As shown by Equation (C-1), the strength of the Smith approach is that the delayed effect is canceled by the model. Thus, the controller’s input signal contains current and not delayed information. Figure C-2 shows the equivalent block diagram of the control system associated with Smith dead-time compensator. It is possible to eliminate entirely the undesired effect of dead-time provided that a perfect model of the process dynamics can be obtained. The model-based compensator offsets the deficient performance of the original system by altering its overall behavior so that the system behaves as desired.

![Equivalent Block Diagram of the Control System with Smith's Dead-time Compensator](image)

**Figure C-2** Equivalent Block Diagram of the Control System with Smith's Dead-time Compensator
References


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N-1. "Millstone Nuclear Power Station - Unit 3; Final Safety Report," Northeast Utilities, Hartford, CT.


