

**ANALYSIS OF SPACE REACTOR SYSTEM COMPONENTS:
INVESTIGATION THROUGH SIMULATION AND NON-NUCLEAR TESTING**

by

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This work is dedicated to my family.
To my father, for encouraging my pursuit of knowledge.
To my mother, for offering an unwavering stanchion of support.
To my husband, for his patience and understanding.

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TABLE OF CONTENTS

DEDICATION.....	ii
ACKNOWLEDGMENTS	iii
LIST OF TABLES	viii
LIST OF FIGURES	ix
INTRODUCTION.....	1
CHAPTER 1	5
MOTIVATION AND HISTORICAL REVIEW.....	5
1.1 U.S. Space Nuclear Reactor Programs	5
1.1.1 SNAP Reactor Program	6
1.1.2 Rover/NERVA: Space Nuclear Rockets.....	9
1.1.3 SP-100.....	11
1.1.4 NEPSTP/Topaz II	14
1.2 Soviet Space Nuclear Reactor Programs	16
1.3 Testing Approach for Flight Systems	19
1.4 Motivation for Current Space Reactor Research	20
CHAPTER 2	22
REACTOR THEORY AND ENVIRONMENT DEFINITION	22
2.1 Reactor Theory.....	22
2.1.1 Heat Pipe Cooled Reactors	23
2.1.1.1 SAFE-400	25
2.1.1.2 Theory of Heat Pipe Operation	28
2.1.2 Reactor Dynamics and Control.....	35
2.1.3 System Model: Point Reactor Kinetics	39
2.2 Control Theory: Model Based Predictive Control.....	44
2.3 Space Radiation Environment.....	47
2.3.1 Near-Earth Space	48

2.3.1.1 Trapped Particles	49
2.3.1.2 Galactic Cosmic Rays	54
2.3.1.3 Anomalous Cosmic Rays	55
2.3.1.4 Neutrons	56
2.3.1.5 Solar Cycle Effect on Particle Fields	58
2.3.2 Jovian System	62
CHAPTER 3	65
COMPUTATIONAL RESEARCH: MODEL DEVELOPMENT	65
3.1 Modeling the External Radiation Environment	65
3.1.1 Trapped Particle Models: AP8 & AE8	66
3.1.2 Ionizing Radiation Environment Models:	
CREME96	68
3.1.3 Model Accuracy: Comparison to Satellite Data	72
3.1.4 Jovian Models	73
3.2 Radiation Transport Models	75
3.2.1 Deterministic Methods: CREME96 Transport	
Module	76
3.2.2 Deterministic Methods: HZETRN	77
3.2.3 Statistical Codes: MCNP / MCNPX	80
3.2.3.1 Interaction Physics	82
3.2.3.2 Validation	84
3.3 Control Methodology	87
3.3.1 Reactivity Control	89
3.3.2 Expression of a Cost Function	90
3.3.3 Selection of a Control Variable	92
3.3.3.1 Drum Position, $\theta(t)$, Control	93
3.3.3.2 Angular Velocity, $\omega(t)$, Control	94
3.3.4 Optimization	96
CHAPTER 4	98
COMPUTATIONAL RESULTS	98
4.1 SAFE-400 Reactor Kinetics	98
4.1.1 Nominal Operation: Reactivity and Control	
Drum Motion	99
4.1.2 Off-Nominal Operation: Failed Control Drum	
Drive	103
4.2 Benchmarking MCNPX: Proton and Alpha Particle	
Transport	107
4.2.1 Incident Protons	109
4.2.2 Incident Alpha Particles	111
4.3 Secondary Neutron Production: SAFE-400 Reactor	113
4.3.1 Incident Protons	115
4.3.2 Incident Alpha Particles	116

4.3.3 Non-Isotropic Radiation Source	120
4.3.4 Jovian Environment	128
4.4 Reactor Control Methodologies.....	129
4.4.1 Control Drum Motion and Reactivity	130
4.4.2 Reactor Start-Up: Constant External Source	131
4.4.2.1 Drum Position, $\theta(t)$, Control	132
4.4.2.2 Angular Velocity, $\omega(t)$, Control.....	138
4.4.2.3 Cost Function Performance for Angular Velocity Control	141
4.4.2.4 Stability Analysis	144
4.4.3 Reactor Start-Up: Variable External Source.....	151
4.4.4 Future Controller Enhancements	153
CHAPTER 5.....	156
VIRTUAL REACTIVITY FEEDBACK EXPERIMENTS.....	156
5.1 Validation of Core Deflection Measurements	158
5.1.1 Set-up of Test Hardware	159
5.1.2 LGDT Measurement Configuration.....	164
5.1.3 CCD Measurement Configuration	166
5.1.4 Validation Results.....	168
5.2 Deflection / Reactivity Feedback Loop.....	180
5.2.1 Neutronic Analysis of Core Deflection.....	180
5.2.2 Point Kinetics Equations: Calculation of Eigenvalues & Eigenvectors	187
5.2.3 Determination of CCD Imaging System Resolution	189
5.2.4 Virtual Reactivity Test Procedure.....	192
5.2.5 Experimental Results	196
5.3 Discussion & Conclusions	209
5.4 Future Enhancements to Virtual Reactivity Feedback Control	210
CHAPTER 6.....	213
SINGLE CHANNEL TESTING FOR ANNULAR FLOW CHARACTERIZATION	213
6.1 Direct-Drive Gas Cooled Reactor.....	214
6.1.1 Test Article Description.....	215
6.1.2 Experiment Design & Test Matrix Development ...	218
6.1.3 Results & Discussion	220
6.1.4 Future Enhancements.....	224
6.2 SAFE-100a Heat Exchanger.....	225
6.2.1 Test Article Description.....	227
6.2.2 Experiment Design & Test Matrix Development ...	235
6.2.3 Results & Discussion	237

6.2.4 Future Enhancements.....	240
CHAPTER 7.....	243
HEAT PIPE OPERATION: EXPERIMENTAL AND COMPUTATIONAL ANALYSES.....	243
7.1 Steady-State Operation of a Sodium Heat Pipe	243
7.2 Transient Operation of a Sodium Heat Pipe	245
7.3 Results: Comparison of Computational and Experimental Analyses	253
7.4 Future Enhancements to Computational Heat Pipe Analysis.....	259
CHAPTER 8.....	261
CONCLUSIONS AND FUTURE WORK.....	261
BIBLIOGRAPHY	266

LIST OF TABLES

TABLE 1.1. Partial List of US Space Fission Programs That Have Not Resulted in Flight.	6
TABLE 2.1. SAFE-400 Heat Pipe Properties.	31
TABLE 2.2. Delayed Neutron Properties for Fast Fission of ^{235}U (Hetrick 1971).	43
TABLE 3.1. Reaction Thresholds for Incident Protons and Alpha Particles on Beryllium that Result in Neutron Production.	87
TABLE 4.1. Experimental and Calculated TTY for Protons Incident on Beryllium (Howard et al. 2001; Kim et al. 1966; Northcliffe and Schilling 1970).	111
TABLE 4.2. Weighting Coefficients for $\theta(t)$ Control.	133
TABLE 5.1. LGDT Manufacturer Specifications.	166
TABLE 5.2. CCD Imaging System and Caliper Measurements (Test #2).	176
TABLE 5.3. Calculated Effective Multiplication Factor at the Indicated Conditions.	184
TABLE 5.4. Fractional Power Levels Applied to Each Control Zone.	186
TABLE 5.5. Measured Core Deflection for During Initialization at 1 kW _t .	203
TABLE 6.1. Sample Test Matrix for Single Channel DDG Testing.	220
TABLE 6.2. Thermocouple Locations Along HX Flow Annulus.	234
TABLE 6.3. Single Channel HX Test Matrix at 1.0 MPa Inlet Pressure.	236

LIST OF FIGURES

FIGURE 1.1. SNAP-10A Reactor Module.	8
FIGURE 2.1. Schematic of the SAFE-100a Test Article with Heat Exchanger.	25
FIGURE 2.2. SAFE-400 Module Design.	26
FIGURE 2.3. Axial Cross Section of SAFE-400 Core for Control Drum Positions.	27
FIGURE 2.4. Illustration of Heat Pipe Operation.	29
FIGURE 2.5. Representation of MBPC Theory. Modified from (Morari 1994).	46
FIGURE 2.6. Approximate Trajectory of Trapped Particles Along Earth's Magnetic Field Lines.	50
FIGURE 2.7. Greater Than 10 MeV Integral Trapped Proton Flux. Figure included with permission from (ESA/ESTEC 2002).	50
FIGURE 2.8. Illustration of the East-West Effect for Trapped Protons.	52
FIGURE 2.9. Greater Than 1 MeV Integral Trapped Electron Flux. Figure included with permission from (ESA/ESTEC 2002).	54
FIGURE 2.10. Solar Particle Event, October 28, 2003.	62
FIGURE 2.11. Jovian Integral Trapped Proton Flux as a Function of Altitude.	63
FIGURE 3.1. Trapped Proton Environment at 500 km Altitude During Solar Maximum. Figure included with permission from (ESA/ESTEC 2002).	67
FIGURE 3.2. Trapped Proton Fluence in the Jovian System, as Measured by the Galileo Energetic Particle Detector.	74
FIGURE 3.3. Approximate Radiation Environment at the Jovian Moon Europa, as Compared to Near-Earth Orbit.	75
FIGURE 3.4. Representation of Thick Target Yield Geometry.	85
FIGURE 4.1. Effective Neutron Multiplication in the SAFE-400 Reactor Core as a Function of Control Drum Position.	100

FIGURE 4.2. Neutron Flux in the SAFE-400 Reactor Core, $k_{\text{eff}} = 0.9770 \pm 0.0002$.	102
FIGURE 4.3. Effect of a Failed Control Drum Drive on k_{eff} .	104
FIGURE 4.4. Neutron Flux in the SAFE-400 Reactor Core with One Failed Control Drum Drive; Remaining Control Drums Were Positioned at 60° .	105
FIGURE 4.5. Ion Range in Beryllium.	108
FIGURE 4.6. Proton Stopping Power and Neutron Production Cross Section Data for 2.3 – 5.4 MeV Protons Incident on Beryllium.	110
FIGURE 4.7. Alpha Particle Flux in Near Earth Space.	112
FIGURE 4.8. Proton and Alpha Particle Flux in Near-Earth Space, International Space Station Orbit (450 km, 51.6° inclination).	114
FIGURE 4.9. Proton and Alpha Particle Flux in Geosynchronous Earth Orbit During the Peak 5 Minute Flux of the October 1989 SPE.	114
FIGURE 4.10. Incoming Neutron Current Due to Isotropic Proton Bombardment of the SAFE-400 Reactor Core.	116
FIGURE 4.11. Experimentally Determined Thick Target Yield for Alpha Particles on Beryllium.	117
FIGURE 4.12. Experimentally Determined Thick Target Yield and Average Neutron Energy for Alpha Particles on Beryllium (Lone et al. 1981).	118
FIGURE 4.13. Calculated Neutron Yield Due to Alpha Source in Geosynchronous Orbit.	120
FIGURE 4.14. Isotropic Proton Bombardment of the SAFE-400 Reactor.	122
FIGURE 4.15. Illustration of the Axial Proton Source Configuration.	123
FIGURE 4.16. Illustration of the Planar Proton Source Configuration.	124
FIGURE 4.17. Identification of Cross Section Locations for Figures 4.18-20.	125
FIGURE 4.18. Neutron Flux in the SAFE-400 Reactor Core: Isotropic Proton Source.	126
FIGURE 4.19. Neutron Flux in the SAFE-400 Reactor Core: Axial Proton Source.	127
FIGURE 4.20. Neutron Flux in the SAFE-400 Reactor Core: Planar Proton Source.	127
FIGURE 4.21. Secondary Neutron Flux at the Surface of the SAFE-400 Reactor Core.	129

FIGURE 4.22. Reactivity as a Function of Control Drum Position, SAFE-400.	131
FIGURE 4.23. Control Dependent on Drum Position: $a = 0, b = 0$.	134
FIGURE 4.24. Control Dependent on Drum Position: $a = 0, b = 0$, Zoom Showing Slight Overshoot in Power.	135
FIGURE 4.25. Control Dependent on Drum Position: $a = 0, b = 10^{18}$.	136
FIGURE 4.26. Control Dependent on Drum Position: $a = 0, b = 10^{18}$, Demonstrating Minimal Overshoot in Power and Achievement of Goal in One Second.	136
FIGURE 4.27. Control Dependent on Drum Position: $a = 10^{18}, b = 10^{18}$.	136
FIGURE 4.28. Control Dependent on Drum Position: $a = 10^{18}, b = 0$.	137
FIGURE 4.29. Control Dependent on Drum Position: $a = 10^{18}, b = 10^{18}$, Application of Control Over Extended Time.	138
FIGURE 4.30. Sample Reactor Startup Control Program to 1 kW _t for $\omega(t)$ Controller.	140
FIGURE 4.31. Control Dependent on Angular Velocity of Control Drums; Instability Demonstrated for $a = 0$.	143
FIGURE 4.32. Control Dependent on Angular Velocity of Control Drums; Failure to Achieve Goal Power for $a = 1$.	144
FIGURE 4.33. Illustration of Distinct Regions of Operation for Reactor Start-Up Control.	145
FIGURE 4.34. Demonstration of Stable Controller Operation Given Two Control Regions: Power Rise (0 - 120 sec) and Steady State Operation (> 121 sec).	147
FIGURE 4.35. Stability Analysis of the Control Algorithm During Steady State Operation at 1 kW Fission Power.	149
FIGURE 4.36. Reactor Start-Up Control in the Presence of Source Perturbations.	152
FIGURE 4.37. Rate of Change of the Control Drum Angular Velocity.	155
FIGURE 5.1. SAFE-100 Prototype.	157
FIGURE 5.2. SAFE-100 Test Hardware Setup.	160
FIGURE 5.3. Power Hook-up for the Reconfigured SAFE-100 Tests.	161
FIGURE 5.4. Original and Refurbished Heater Elements for the SAFE-100.	162

FIGURE 5.5. Reconfigured SAFE-100 Control Zones.	162
FIGURE 5.6. Thermocouple Placement on SAFE-100 Core.	164
FIGURE 5.7. Setup of the LGDT System.	165
FIGURE 5.8. Photographs of the LGDT Setup Inside the Vacuum Chamber.	165
FIGURE 5.9. Setup of the Camera / Reactor Core Prototype.	167
FIGURE 5.10. Screen Shot Taken During Test Showing Three Axial Locations for Core Width Measurement.	168
FIGURE 5.11. Results of the Initial Deflection Measurement Validation Test.	169
FIGURE 5.12. LGDT Probe Ends Showing Epoxy Contamination Post-Test.	170
FIGURE 5.13. LGDT Calibration Test Setup.	171
FIGURE 5.14. LGDT Test Results at Room Temperature.	172
FIGURE 5.15. LGDT Test Results for a Heated Target.	173
FIGURE 5.16. Modified Core Support Structure to Allow Free Radial Expansion.	174
FIGURE 5.17. CCD Data and Caliper Measurements for Test 2 (Sampling Position 1).	175
FIGURE 5.18. CCD Data and Caliper Measurements for Test 3 (Diameter D2).	177
FIGURE 5.19. Measured Deflection and Average Core Temperature for Test 3.	178
FIGURE 5.20. Measured Core Deflection and Temperature at Vacuum Conditions.	179
FIGURE 5.21. Average Thermal Expansion Coefficient for Stainless Steel 316.	181
FIGURE 5.22. SAFE-400 Reactivity (SS316 Core) as a Function of Core Deformation.	183
FIGURE 5.23. Thermal Expansion of Stainless Steel 316 and Molybdenum Metal as a Function of Temperature.	184
FIGURE 5.24. Variation in SAFE-400 Reactivity as a Function of Core Temperature and Composition.	185
FIGURE 5.25. Normalized Power Profile for the Simulated SAFE-400 Reactor and Distribution of Power for the SAFE-100 Reactor Prototype.	186
FIGURE 5.26. SAFE-100 Control Zone Map.	186

FIGURE 5.27. Empirical Relation for Reactivity as a Function of Control Drum Position; Applied in Virtual Reactivity Experiment.	188
FIGURE 5.28. Incorporation of Spatial Averaging in CCD Deflection Measurements.	190
FIGURE 5.29. Distribution of One Second CCD Data.	191
FIGURE 5.30. Distribution of CCD Data Applying a Five Second Moving Average.	192
FIGURE 5.31. Virtual Reactivity Feedback Test Flow Chart.	193
FIGURE 5.32. Correction to the Applied Electrical Power at Each Measurable Core Deflection; Applied to Better Simulate an Operating Nuclear Reactor.	196
FIGURE 5.33. Measured Core Deflection During Initial Core Heat Up.	199
FIGURE 5.34. Measured Core Temperature During Initial Core Heat Up.	201
FIGURE 5.35. SAFE-100 Power for Virtual Reactivity Feedback Control (Test 1).	202
FIGURE 5.36. SAFE-100 Power: Miscalculation of Initial Conditions (Test 2).	204
FIGURE 5.37. SAFE-100 Prototype: Full Test to 20 kW _t With Shut Down (Test 2), Thermal Power and Virtual Control Drum Position.	205
FIGURE 5.38. SAFE-100 Prototype: Full Test to 20 kW _t With Shut Down (Test 2), Average Core Temperature and Radial Deflection.	206
FIGURE 5.39. SAFE-100 Prototype: Full Test to 10 kW _t With Shut Down (Test 3), Thermal Power and Virtual Control Drum Position.	207
FIGURE 5.40. SAFE-100 Prototype: Full Test to 10 kW _t With Shut Down (Test 3), Average Core Temperature and Radial Deflection.	208
FIGURE 5.41. SAFE-100a Core and Heat Exchanger Assembly.	211
FIGURE 6.1. DDG Single Channel Test Apparatus.	216
FIGURE 6.2. Breakout of Flow Path; Lower Configuration Fits Snugly Inside the Upper Tube Housing.	217
FIGURE 6.3. “Finned” Design for Maintaining Concentric Placement of the Heater Housing.	217
FIGURE 6.4. Orifice and Plug Designed to Hold Heater in Place at the Downstream End of the Flow Annulus.	217
FIGURE 6.5. Effect of Orifice on Channel Pressure Drop.	221

FIGURE 6.6. Comparison of the Measured and Calculated Pressure Drop for the DDG Flow Annulus.	223
FIGURE 6.7. SAFE-100a Heat Exchanger with Four Heat Pipe Modules.	226
FIGURE 6.8. Cross Section of the Proposed HPR-HX Assemblies.	227
FIGURE 6.9. Test Stand for the Single Channel Heat Exchanger Test Article.	228
FIGURE 6.10. Close Up Detail of Fabricated Part for HX Inner Flow Path.	229
FIGURE 6.11. Illustration of the HPR-HX Flow Channel.	230
FIGURE 6.12. Break-out of the HPR-HX Flow Channel.	231
FIGURE 6.13. Flow Channel Entrance/Exit Showing Positioning Tabs.	232
FIGURE 6.14. Thermocouple Placement at Flow Channel Exit.	233
FIGURE 6.15. Detailed Flow Channel Diagnostics.	234
FIGURE 6.16. Experimental and Calculated Pressure Drop for the HX Flow Annulus.	237
FIGURE 6.17. Flow and Wall Temperatures for the HX Flow Annulus.	239
FIGURE 6.18. Experimental and Calculated Pressure Drop for the HX Flow Annulus; Adjustment of the Coefficient in the Takase (2000) Correlation.	240
FIGURE 7.1. Steady State Operation of a SAFE-400 Heat Pipe.	244
FIGURE 7.2. General Component Layout for Heat Pipe Testing.	245
FIGURE 7.3. Illustration of Heat Pipe Module Instrumentation Layout.	246
FIGURE 7.4. Heat Pipe Module Test Set-Up.	247
FIGURE 7.5. Heat Pipe Modules Modeled by HPAPPX.	248
FIGURE 7.6. Three Node Radiation Network Applied for SAFE-100a Heat Pipe Module Analysis.	250
FIGURE 7.7. Input Power and Calculated Output Power for SAFE-100a Module 12.	254
FIGURE 7.8. Calculated Module 12 Temperatures: Heater, Fuel Tubes, Evaporator, and First Condenser Nodes.	255
FIGURE 7.9. SAFE-100a Test Module Configuration.	256

FIGURE 7.10. Calculated and Measured Module 12 Temperatures: Evaporator and First
Condenser Nodes. 257

FIGURE 7.11. Calculated and Measured Module 12 Temperatures: Condenser Nodes. 259

INTRODUCTION

Fission power can enable the accomplishment of ambitious space missions by providing rapid, affordable access to any point in the solar system. Despite the tremendous potential of space fission systems, the development and application of these systems has proven elusive. The United States has demonstrated the use of fission systems in the launch of the SNAP-10A reactor in 1965, but no other reactor development programs have led to in-flight testing. Although the former Soviet Union extensively used space fission systems, these were limited to Earth orbital missions and did not attempt any ambitious deep space missions. Near-term space fission missions must capitalize on the experience of earlier development programs in order to accomplish the development and launch of a flight system given a limited budget and within a limited time.

Terrestrial reactors commonly initiate start-up using a neutron source located within the reactor core. This source provides the neutrons required to initiate fission in fuel nuclides, beginning the process of neutron multiplication in the reactor core. Similar methods may be applied to a space reactor system. However, from a launch safety standpoint, it may be desirable to eliminate the neutron source from the design requirements. A reactor that has not been operated prior to launch is essentially non-radioactive. For the proposed 400 kW thermal (kW_t) heat pipe reactor design, which will be described more thoroughly in this dissertation, activity in the reactor core prior to startup includes approximately 85.5 spontaneous fission neutrons per second, 2.4×10^{11} alphas per second, and 9.0×10^9 decay gammas per second (Poston 2002). The necessity of an additional neutron source could complicate transport and storage of the system prior

to launch, as well as introduce additional regulations that must be met in order to gain launch approval.

Space vehicles are exposed to a significant radiation environment including protons, electrons, and atomic nuclei. Trapped proton energies can range from fractions of an MeV to GeV, trapped electron energies from a few tens of eV to a few MeV, and atomic nuclei in galactic cosmic rays can have energies ranging from hundreds of MeV to tens of GeV per nucleon. Some of these particles will pass through the reactor vessel with little to no effect because of their extremely high energies; however, other particles will interact with the materials in the reactor core and vessel, creating a cascade of secondary particles that may interact in the reactor fuel generating fissions, or that may interfere with reactor monitoring and control instrumentation.

Space systems are designed to protect the payload from the harmful effects of cosmic radiation. However, attempts to protect payload from high energy, high Z (atomic number) components of cosmic radiation may actually increase the measured dose inside the spacecraft due to the secondary radiation that is produced (Dudkin et al. 1996). Energetic atomic nuclei can undergo spallation in the spacecraft materials, producing a large number of secondary, lighter nuclear species that can more easily deposit their energy in the payload, whether that payload includes electronic systems, biological material, or human cargo.

For a reactor system in space, the natural radiation environment may play a dual role. External sources may provide a start-up source and enable shortened start-up times, but they may also hinder reactor operations in which the reactor neutron population is relatively low, including start-up, shutdown, or other transient maneuvers. Energetic protons, alpha particles, and other high Z ions interacting with reactor module materials can result in neutron production. If the number of neutrons produced by these interactions is significant, and these secondary neutrons are produced in the vicinity of the nuclear fuel, the neutron population in the reactor may be sufficient to allow start-up

without any additional neutron source. The start-up procedures assisted by the secondary neutrons may also significantly decrease the time required for system start-up dependent on spontaneous fission neutrons alone. Because each neutron has a finite probability of initiating a sustainable chain reaction, increasing the source neutron fluence increases the probability of starting a reactor at a specific point in time. On the other hand, the neutron flux induced by external radiation sources may generate significant power fluctuations during start-up and shutdown, which could complicate tracking these power transients.

Highly autonomous, intelligent control will be required for nuclear electric propulsion systems. Communication between the Earth and a deep-space probe can take over ten hours from when a signal is sent to when it is received. Because of this time lag, any reactor transients that occur must be addressed by the onboard control system. An autonomous control system should provide stable reactor control during reactor start-up and shutdown transients, during power transients, and during steady-state operation. To design a proper control system, one must account for potential fluctuations in the radiation environment that might result from changes in the vehicle location (e.g., altitude, latitude, or position in solar system) or to temporal variations in the radiation field, as may occur in the case of a solar flare.

This dissertation will discuss the various sources of radiation in the space environment and will assess the potential effects of this environment on reactor operations through computational analysis. A control methodology is then developed to provide stable, robust control in the presence of a constant or fluctuating external source. Finally, the development of a nuclear reactor for space applications requires a significant amount of testing prior to deployment of a flight unit. High confidence in fission system performance can be obtained through a series of non-nuclear tests, with the heat from nuclear fission simulated using electric resistance heaters. A set of experiments conducted on a prototypic heat pipe cooled reactor design and a direct gas cooled reactor design will be discussed. Tests include the measurement of core deformation due to

material thermal expansion and implementation of virtual reactivity feedback control to an electrically heated reactor core prototype; testing and characterization of a nominal heat exchanger flow channel for the heat pipe reactor prototype and a nominal flow channel for the direct gas cooled reactor prototype; and computational and experimental analysis of a liquid metal heat pipe during start-up and steady state operation.

CHAPTER 1

MOTIVATION AND HISTORICAL REVIEW

The potential of using nuclear fission in spacecraft power and propulsion systems to enable extensive exploration and development has been recognized for decades. However, despite the tremendous potential of space fission systems, their development and utilization has proven elusive. The first application of fission technology in space occurred nearly four decades ago when the United States launched the SNAP-10A reactor on April 3, 1965 (Schmidt 1988). Although numerous research and development programs aimed at utilizing fission systems in space have followed in the US, none has resulted in a flight system. More than 30 space fission systems have been flown by the former Soviet Union, but each of these was limited to earth-orbital missions and did not seek to extend their application to solar system exploration (Ponomarev-Stepnoi et al. 2000).

1.1 U.S. Space Nuclear Reactor Programs

Although several space reactor systems have been investigated by the United States, only the SNAP-10A resulted in a flight system. Since the 1950s, several research and development programs have been established in the United States to investigate the use of fission reactors in space. Various reactor designs have been evaluated either experimentally or in theoretical paper studies, including thermal, epithermal, and fast spectrum reactors. These designs have been coupled to a variety of reactor cooling and energy conversion systems including pumped liquid metal, pumped gas, in-core and

integrated radiator heat pipes, in-cell thermionics, thermoelectrics, Brayton conversion systems, and Stirling conversion systems. A selection of the space fission system programs are discussed, including the primary reasons for program termination.

A partial list of major US space fission programs that, for a variety of reasons, failed to result in a flight system is provided in Table 1.1 to demonstrate the interest in producing a fission power or propulsion system for space applications (Angelo and Buden 1985). In many instances, the reactor development program was prematurely terminated due the cancellation of the proposed mission. However, in many of those cases the reactor required by the mission was becoming too costly and taking too long to develop, partially contributing to the mission cancellation. Near-term space fission programs must capitalize on this experience, learning the lessons of the past to develop the technology for the future.

- | | |
|--|--|
| • Solid-Core Nuclear Rocket Program | • Flight Topaz |
| • Medium-Power Reactor Experiment (MPRE) | • DOE 40 kWe Thermionic Reactor |
| • Thermionic Technology Program (1963-1973) | Program Advanced Liquid Metal Cooled Reactor |
| • Space Nuclear Thermal Rocket Program | • Advanced Space Nuclear Power Program (SPR) |
| • SP-100 | • Multi-Megawatt Program |
| • SNAP-50 / SPUR | • Thermionic Fuel Element Verification Program |
| • High Temperature Gas Cooled Electric Power Reactor (710 Reactor) | • Air Force Bimodal Study |
| • SPAR / SP-100 | |

TABLE 1.1. Partial List of US Space Fission Programs That Have Not Resulted in Flight.

1.1.1 SNAP Reactor Program

One of the first investigations of nuclear power for space application was a program called Systems for Nuclear Auxiliary Power (SNAP). The SNAP reactor program is the first and only US-based space reactor program that has resulted in a flight test. Research for the SNAP program commenced in 1957 with the development of a

uranium – zirconium hydride (U-ZrH) thermal reactor system. The SNAP program resulted in construction and testing of six U-ZrH systems before it was terminated in the early 1970s.

The SNAP-10A remains the only fission-powered system ever flown by the United States. Pre-launch testing of the SNAP-10A reactor included thermal tests using electric heaters in a simulated space environment. To allow launch of a “cold, clean” reactor, which is necessary for safety considerations, no full-power nuclear tests were performed on the flight unit. All new, virgin SNAP reactors required neutron sources during fuel loading and performance of “zero-power” critical testing to determine control calibration. While zero-power critical tests accomplish verification of the reactivity worth of the core components, they do not result in significant fission product buildup, allowing for standard ground handling and launch procedures. Because each reactor can be slightly different despite being constructed with seemingly identical parts, each will have a slightly different critical configuration; these differences can be parsed out during zero-power critical tests performed on the ground (Schmidt 2003a). The SNAP-10A reactor that was flight tested was referred to as module FS-4. Early in 1965, the FS-3 reactor module commenced a 1-year full power, ground nuclear qualification test. The FS-3 module operated under vacuum conditions with no active control for 10,000 hours.

On April 3, 1965, SNAP-10A (FS-4) was launched from Vandenberg Air Force Base, California aboard an Atlas-Agena launch vehicle (Schmidt 1988). After achieving a stable orbit with an apogee of 1328 km and perigee of 1295 km, the reactor began operation approximately four hours after launch. Start-up was achieved by first driving two control drums to their “full-out” position (such that the neutron reflector material was adjacent to the core); the remaining two control drums were then slowly rotated in at a rate of one degree per 300 seconds. Criticality was achieved after approximately seven hours. During this time, heat shields remained in place around the reactor vessel to prevent the liquid sodium-potassium (NaK) coolant from freezing. Because there was no

mechanism provided to thaw the NaK, freezing would have prevented operation of the reactor cooling system. The U-ZrH thermal reactor aboard SNAP-10A produced 50 kW thermal (kW_t) power and was coupled to a thermoelectric power conversion system to produce 500 W electric (W_e) power. After six days of full power operation, the on-board automatic control system was deactivated by ground control. The reactor continued to operate using the inherent control provided by negative reactivity feedback. On May 16, 1965, after 43 days of operation, SNAP-10A was prematurely shut down due to an apparent failure of the voltage regulator in the Agena vehicle (Schmidt 1988). All data indicate that the reactor was operating normally at shutdown. Safety design features of the SNAP-10A prevented the reactor from being restarted using ground-based controls. Calculations indicate that by 1980, fifteen years after reactor shutdown, the fission product inventory had decayed to less than 100 Ci. One hundred years after shutdown in 2065, calculations indicate that the radioactivity level of the core will be less than 0.1 Ci. At present, SNAP-10A continues to safely orbit the Earth in its original 4000 year orbit (Schmidt 1988).

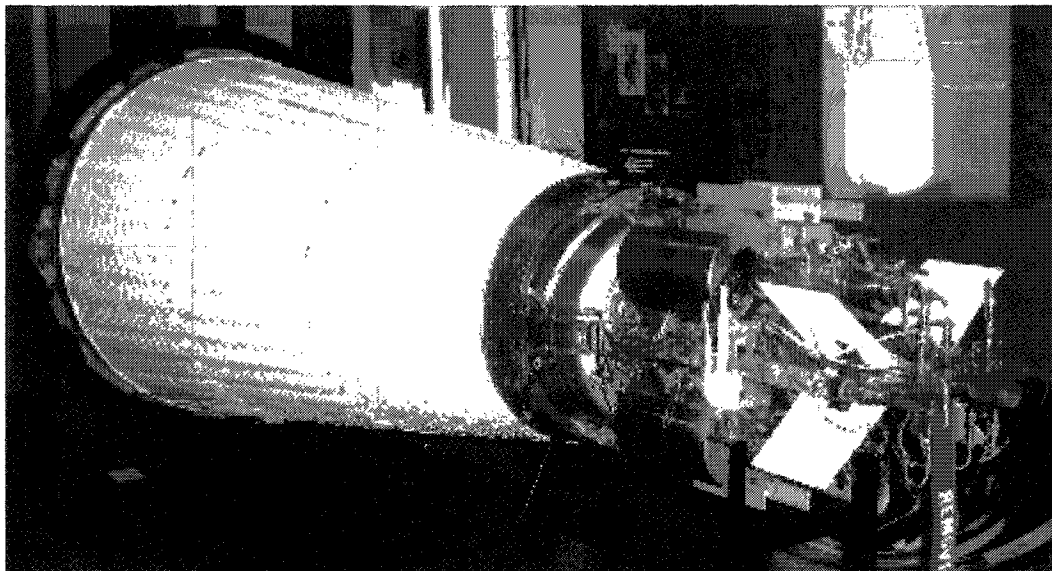


FIGURE 1.1. SNAP-10A Reactor Module.

Despite failure to maintain long-term operation, the SNAPSHOT mission that carried SNAP-10A (FS-4) to orbit was considered a technical success. The mission successfully demonstrated that a fission reactor could be launched and operated in the space environment, and that nuclear reactors could be safely operated in space without causing unnecessary radiation exposure to Earth's population or biosphere.

1.1.2 Rover/NERVA: Space Nuclear Rockets

The Rover nuclear rocket development program commenced in 1955. Also called the NERVA – Nuclear Engine for Rocket Vehicle Application – program, the Rover program considered the use of epithermal reactors to directly provide thrust by using the energy from nuclear fission to heat a hydrogen propellant. Although it was considered a technical success, the nuclear rocket program was a victim of changing national space program priorities and was terminated in 1973 before a flight demonstration could be made.

In a nuclear rocket, a propellant is directly heated to a very high temperature (e.g., 2400°-2700° K) as it flows through reactor coolant channels. The hot gas is then expanded through a nozzle to provide directional thrust (Spence 1968). The primary goals in the Rover program were to improve the specific impulse of a rocket by increasing the reactor outlet temperature and to lengthen the operating lifetime of the reactor. Potential applications of the Rover reactors included functioning as the second stage in a lunar flight, providing propulsion for a manned Mars mission, use in orbit-to-orbit transfer, or even functioning as the back-up for intercontinental ballistic missile (ICBM) propulsion (Koenig 1986). Because some of these potential applications could be supported by chemical rockets, which were more economical than the use of nuclear engines for rocket vehicle application, the nuclear rocket development program was prematurely terminated.

The Rover program demonstrated a number of reactor concepts over its approximate 20-year duration. During the course of the program, a series of reactors was built and tested at the Nuclear Rocket Development Station at Jackass Flats in the Nevada Test Site. The first in the series of research reactors tested in the Rover program were the Kiwi reactors, which were the first reactors to demonstrate the use of high temperature fuels and to operate with liquid hydrogen. The Kiwi test series culminated with a test of the Kiwi-B4E reactor, which operated at a thermal power level of 940 MW for 11.3 minutes with a coolant outlet temperature of 1890° K, and for an additional 95 seconds at 2005° K. The Kiwis were followed by the Nuclear Reactor Experiment (NRX) series and the Phoebus test series. These programs were developed with the intention of increasing the specific impulse of the rocket to as high as 825 seconds – twice the maximum specific impulse of a chemical propulsion system. The original objectives of the NRX test series were exceeded in the final test of the NRX-6 reactor, which operated at 1100 MW for 62 minutes with a coolant outlet temperature of 2200° K. The Phoebus-2A, which operated at 4000 MW for 12 minutes, with a peak power of 4080 MW, is touted as “the most powerful reactor ever built” (Koenig 1986). The final series of research reactors tested under Rover included the Pewee and Nuclear Furnace reactors. While these were lower power reactors, they demonstrated the capabilities of high temperature fuel elements.

The Rover/NERVA nuclear rocket development program fell victim to a changing emphasis in the space program after the Apollo lunar landings. The Saturn V launch vehicle was the primary vehicle planned for launch of the nuclear rockets under development in the 1960s. After work on the Saturn V launch vehicle was suspended in 1969, the US abandoned manned missions to Mars as a follow-on to the lunar landings, and high-energy space reactors no longer fit with the mission of the national space program.

1.1.3 SP-100

Interest in developing fission technology for space missions was revived in the early 1980s with the SP-100 program. In 1979, an order of magnitude increase in power requirements was anticipated for space applications. Proposed mission requirements spurred the development of a new generation of space reactors that could provide high power levels of 50-1000 kW_e over a lifetime of 7 to 10 years (seven year full power lifetime, ten year mission lifetime) with a relatively low power plant specific mass on the order of 20-30 kg/kW, designed to satisfy military and civilian requirements for future space missions. The proposed applications of an SP-100 type system indicated an optimal power level of 100 kW_e. In 1983, funding for SP-100 was provided by National Aeronautics and Space Administration (NASA), the Department of Energy (DOE), and the Defense Advanced Research Project Agency (DARPA) within the Department of Defense (DOD) to begin a study to identify the space power system that could meet these requirements (Buden 1994).

Phase I of the SP-100 program evaluated all possible space reactor power system technologies to determine which systems, if any, would be capable of meeting the basic system requirements and be ready for flight development by fiscal year (FY) 1992 without exceeding \$500M (FY 1983 dollars) for technology development. A study conducted by three independent, competitively selected contractors provided detailed analysis of the potential systems. A broad range of candidate reactor systems was considered, including liquid metal cooled, gas cooled, thermionic, and heat pipe cooled reactors, coupled in various combinations to thermionic, thermoelectric, Brayton, Rankine, and Stirling energy conversion systems. In August 1985, the SP-100 Steering Committee selected the only option that met all design, cost, and time requirements. The selected system was a fast spectrum reactor that utilized pin-type fuel elements, lithium coolant, and thermoelectric energy conversion. The project plan called for a five-year

development period, denoted as Phase II of the SP-100 program, to be completed by FY 1992 (Buksa et al. 1994).

Design goals led to the development of a fast spectrum reactor, utilizing highly enriched uranium nitride (UN) fuel (97%) and maintaining a large fuel inventory in a relatively small volume to prevent large reactivity changes with fuel burnup. SP-100 was designed to produce 100 kW_e (1.5 MW_t) utilizing thermoelectric power conversion. For the 100 kW_e design, reactor control was accomplished by twelve sliding, tapered reflector segments composed of beryllium oxide (BeO) housed in a niobium-1% zirconium (Nb-1% Zr) shell. Manipulation of the sliding reflectors controlled the neutron leakage from the reactor core. Material temperature limits and material integrity concerns drove the selection of the reactor operating temperature. Heat was transferred from the reactor core to the thermoelectric converter via six pumped lithium loops, with the liquid lithium circulated through the full system by thermoelectric-electromagnetic (TEM) pumps. Waste heat was then transported to the heat rejection subsystem which utilized a pumped liquid metal loop and potassium heat pipes to radiate excess heat to space.

The SP-100 reactor system was designed for modularity and scalability, where the reactor power level could be varied by modifying the number fuel pins, changing the number of thermoelectric converters, or using a different power conversion system. The scalability of the design allowed initial work to be performed on a smaller scale, with studies on 10-20 kW_e designs applicable to the planned 100 kW_e system with minor system modifications to accommodate the higher power output of the reactor. The early 20 kW_e designs achieved reactor control using a fixed radial reflector and a dual-functioning control and safety rod. Later, higher power designs employed the multi-component, moveable radial reflector for control of the reactor temperature and power level.

SP-100 was designed for launch and deployment from the Space Shuttle cargo bay into a high Earth orbit having an orbital lifetime of more than 300 years. Reactor

start-up was controlled by control drum operation, as indicated in the following procedure (Angelo and Buden 1985):

- (1) The control drums are ramped from shutdown to a position significantly below cold critical.
- (2) The drums are ramped through the cold critical condition very slowly, depending on the magnitude of the neutron source.
- (3) The system is switched to temperature control when heat pipe temperatures reach minimum controllable values.
- (4) The reactor power level is raised to increase the control temperature enough to permit normal operation.

In the context of the reactor start-up procedure, “very slow” movement of the sliding control drums is related to the strength of the neutron source, which governs the probability of the source generating a sustained chain reaction in a given time. Because this event is a function of probability and chance, if the control drums are moved too rapidly, a prompt supercritical configuration could be achieved before a neutron started a significant reaction chain. If the reactor is in a supercritical configuration when this occurs, it could suddenly jump to a high power with a time constant equal to the prompt neutron lifetime – a potentially dangerous, uncontrollable scenario. The published procedures do not indicate any requirement for an external start-up source. It is assumed that a sufficient neutron population was available from spontaneous fission to approach criticality given neutron reflection accomplished by proper control drum positioning. Shutdown procedures simply required the control drums to be positioned such that the neutron absorbing material was located adjacent to the reactor core.

Much like its predecessors, SP-100 was the victim of a changing political environment. When the administration changed in 1992, work related to space nuclear power and propulsion was no longer supported, and the research program shriveled along with research dollars. In FY 1994, the SP-100 development program was officially terminated due to the lack of a specific near-term mission.

Upon program cancellation, many of the SP-100 subsystems had not yet been fabricated or tested to validate the component lifetimes. However, some subsystem tests had been completed. It was presumed that these technology developments could be applied to various near-term space reactor power systems to accomplish challenging space missions, even though the technology that resulted from the program did not fulfill the mass and lifetime requirements that were the ultimate goal of the SP-100 program. Missions that could take advantage of the SP-100 technology might include serving as the power source for large commercial communications satellites, commercial radar surveillance of the Earth, or as the power source for manned Moon and Mars outposts and bases. The nuclear electric propulsion aspect of this technology could power active-sensing spacecraft for outer planetary orbiters, establish the capability for spacecraft rendezvous with comets or asteroids, or allow the completion of a Jupiter “Mini Grand Tour” (Bukša et al. 1994). Technology issues that still needed to be overcome to make SP-100 a reality included addressing material compatibility problems (such as chemical interactions between the fuel, clad, and coolant), developing appropriate structural materials, demonstrating long-term behavior of heat pipes and fuel modules, accomplishing efficient heat transport in very high temperature systems, and designing the reactor system such that restart from a “frozen” state (e.g., frozen sodium within the heat pipes and frozen lithium in the reactor coolant loop) would be feasible and reliable.

1.1.4 NEPSTP/Topaz II

The Nuclear Electric Propulsion Space Test Program (NEPSTP) was established in late 1991 by the Strategic Defense Initiative Organization (SDIO) to investigate the potential flight qualification of a Russian-built Topaz II space nuclear reactor by the United States. The primary goals of the NEPSTP program were to demonstrate and evaluate Topaz II operation and nuclear electric propulsion (NEP) technologies in Earth

orbit, to characterize the self-induced environment resulting from reactor and electric thruster operation, and to conduct additional scientific research in Earth orbit. In total, six Topaz II reactors were purchased from Russia: two systems underwent testing at the Thermionic Systems Evaluation Test (TSET) facility in Albuquerque, New Mexico, two were flight quality units, one was a mechanical test unit, and one was a thermal test unit (Ponomarev-Stepnoi et al. 2000).

The Topaz II space nuclear reactor power system was a liquid metal cooled reactor that operated with an epithermal neutron spectrum and employed thermionic power conversion. The cylindrical reactor core was constructed from 37 single cell thermionic fuel elements (TFEs) containing highly enriched uranium (96% ^{235}U) dioxide (UO_2) fuel that were fit in a monolithic block of zirconium-hydride ($\text{ZrH}_{1.8}$) moderator. A central void in each TFE allowed for venting of fission gases (El-Genk et al. 1993). Reactor cooling was provided by pumped eutectic sodium-79% potassium-21% (NaK) that transported waste heat from the reactor core to the radiator panels for rejection to space.

The NEPSTEP experimental spacecraft design, which coupled a Topaz II nuclear power system to electric thrusters, considered a reference nuclear-safe orbit of 800 km with a 28.5° inclination. The mission design then called for a spacecraft transfer from low Earth orbit to a 36,000 km geostationary orbit using the nuclear electric propulsion system (Ponomarev-Stepnoi et al. 2000). Reactor start-up in space was monitored by neutron detectors positioned near the reactor core (Schmidt 2003b), and reactor control was accomplished by safety and control drums that fit into the beryllium (Be) radial reflector. Each drum contained a section of boron carbide (B_4C) that provided neutron absorption when it was adjacent to the reactor core. The twelve Be/ B_4C drums included three safety drums, which rotated at a fixed rotation speed of $22.5^\circ/\text{sec}$, and nine control drums, capable of rotating at a maximum speed of $1.4^\circ/\text{sec}$. A potential in-space start-up procedure suggested by El-Genk et al. (1993) required the safety drums to be initially

rotated to the “full out” position, such that the B₄C portion of the drum was fully outside the core. The proposed procedure then called for the remaining nine control drums to be slowly rotated to a position in which power was increasing. After achieving the desired power, only small adjustments of the control drum positions would be necessary to maintain a stable critical condition at the desired operating power.

Concerns voiced by SDIO primarily focused on the ability to launch a Topaz II reactor within American safety standards and significant budget constraints. In the as-designed configuration, the Topaz II core could achieve criticality during a water submersion accident, a violation of American safety standards for launch of a nuclear system. Engineers planned to overcome this issue by requiring a portion of the nuclear fuel to be removed from the core during launch, and then remotely inserted only when a sufficiently high orbit had been achieved. In 1993, after only initial testing of the thermionic systems had been completed, pressure induced by a tightening budget resulted in a “reassessment of program priorities” (Polansky et al. 1994). Program restructuring led to the cancellation of NEPSTP at the end of FY 1993 before a flight program had been implemented.

1.2 Soviet Space Nuclear Reactor Programs

From 1961 to 1988, the former Soviet Union supported an extensive research and development program on space nuclear reactor systems. Between 1970 and 1988, the Soviet Union launched 33 space nuclear reactor systems. Thirty-one of these were Russian BUK thermoelectric reactors used to power Radar Ocean Reconnaissance SATellites (RORSATs). The BUK reactors were operated on Cosmos series spacecraft in low Earth orbit at altitudes of approximately 300 km. The remaining two reactors were thermionic Topaz I reactors that were tested aboard the Cosmos-1818 and Cosmos-1867 spacecraft in 1987 (Ponomarev-Stepnoi et al. 2000).

The BUK thermoelectric reactor system utilized a compact, fast spectrum reactor capable of providing 100 kW_t power. The poor conversion efficiency of thermoelectric systems resulted in just 3 kW_e power delivered to the spacecraft. The BUK reactor was fueled with highly enriched (90% ²³⁵U) uranium – molybdenum alloy fuel, and reactor control was accomplished by sliding control rods located in the beryllium reflector. Reactor cooling was provided by a two-circuit liquid metal cooling system that circulated a NaK eutectic alloy through the system. The primary coolant loop provided heat transfer from the reactor core to the thermoelectric generators, while the secondary coolant loop was necessary for waste heat rejection through the radiator.

In only one case did a Russian BUK reactor fail to achieve its intended disposal orbit after completing the planned operations at the nominal 300 km orbit. For unknown reasons, Cosmos-954 failed to separate from the launch vehicle and subsequently reentered Earth atmosphere over the Northwest Territories of Canada on January 24, 1978, leaving an approximate 600 km debris track (Bennett 1989). The reactor system operated per design specifications that called for aerodynamic dispersion of the reactor components in the upper layers of the Earth's atmosphere upon inadvertent reentry. Although several large fragments having relatively high radioactivity levels were recovered, less than 1% of the fuel inventory was recovered in subsequent cleanup efforts (the remainder was dispersed in the atmosphere during reentry). Fuel debris was distributed over approximately 100,000 km², with particle diameters no greater than 1 mm. The recovered radioactive fragments consisted of what was assumed to be a portion of the steel reactor core periphery, beryllium pieces that most likely formed a portion of the reflector, remnants of what may have been cladding material, and reactor control or shutdown rods. The latter rods were the largest of the debris fragments and were nicknamed the “antlers” in subsequent analyses. The amount of radioactive material dispersed in the atmosphere led to no measurable increase in background radiation levels in Canada or in any other sampled regions on Earth.

The Topaz reactors utilized multi-cell thermionic fuel elements (TFEs) in which the nuclear fuel and the thermionic converter were fully integrated in each multi-cell element. Three multi-cell TFE, thermionic converter reactor prototypes were built and tested on the ground from 1970-1973 (Ponomarev-Stepnoi et al. 2000). Tests performed on these prototypes led to the development of the Topaz nuclear power system that would ultimately be flight tested. The Topaz reactors were designed to provide up to 150 kW_t power. While the nominal design was to provide up to 10 kW_e, the two flight-tested units had an electric power output of approximately 5 kW_e (Buden 1994). The Topaz reactor system utilized a cesium vapor supply system that pumped the cesium through the interelectrode gap in the TFEs. The single-circuit heat removal system utilized a pumped NaK loop and integrated a radiator for waste heat rejection. Prior to the flight test, power tests of the Topaz reactor performed on the ground revealed that the thermionic converter efficiency degraded from approximately 5.5% at the beginning of life to only 1.1% after 4500 hours of operation. Two flight tests of the Topaz reactors were performed on the Plasma-A spacecraft in the Cosmos-1818 and -1867 missions. Each of these spacecraft was injected into a circular orbit at an 800 km altitude (orbital lifetime of 350 years). The first of the reactor tests on Cosmos-1818 operated for 142 days; the second reactor test on Cosmos-1867 operated for 342 days. Both tests terminated upon exhaustion of the cesium supply necessary for operation of the thermionic converters. Both systems successfully demonstrated stable operation under autonomous control.

Development of the ENISEY (Topaz II) nuclear power systems, initially designed as the power source for orbiting spacecraft that would provide direct television broadcasting for remote areas of the former Soviet Union, followed the Topaz I program. The ENISEY design again employed thermionic power conversion, but in this case applied a single-cell converter design. The design of the ENISEY reactor was discussed in greater detail in Section 1.1.4 under the guise of the Topaz II system studied in the NEPSTP program. Approximately 30 ENISEY nuclear power systems were fabricated

for testing. A full cycle of ground tests was completed by 1988, including six ground nuclear power tests. Tests confirmed compliance of the reactor to design specifications that called for a 1.5 year operational lifetime, with the capability of lifetime extension up to a total of 3 years. However, due to a loss of funding, development work on the ENISEY spacecraft was terminated in 1988. After significant negotiations with the United States, a program of joint work called the Topaz International Program commenced in 1991, after which the ENISEY reactor was referred to as the Topaz II.

1.3 Testing Approach for Flight Systems

Much can be learned from the Russian experience in ground testing procedures for space nuclear reactors. However, there are significant differences in the Russian and American philosophies of testing hardware for flight qualification and in the requirements for launch approval (Cook et al. 1994). For instance, in Russian testing standards, the actual space vehicle is *not* tested; rather, a *similar* vehicle is subjected to various engineering tests to verify design and operation. In the United States, the *actual* flight vehicle is generally subjected to a number of tests prior to launch. The Russian philosophy is that testing actual flight components stresses the materials prior to launch, making them more likely to fail during flight operation. On the other hand, the American philosophy suggests that although similar models could be tested, this does not eliminate the potential for component failure during flight due to below nominal construction. Both testing methodologies have merit and should be considered in flight qualification analyses. In the case of nuclear systems, full scale nuclear testing is cost prohibitive, and full-up nuclear tests on flight hardware would result in the build-up of radioactivity prior to launch.

Developing an accurate, non-nuclear testing platform in which the test configuration closely mimics the actual flight vehicle and operation is highly desirable.

Adopting some components of the Russian philosophy may be necessary in individual module tests in order to develop space nuclear systems within a tight budget.

1.4 Motivation for Current Space Reactor Research

Fission power can enable the accomplishment of ambitious space missions by providing rapid, affordable access to any point in the solar system. Despite the tremendous potential of space fission systems, the development and application of these systems has proven elusive. The US has demonstrated the use of fission systems in the launch of the SNAP-10A reactor in 1965, but no other US reactor development programs have led to in-flight testing. Although the Soviet Union extensively used space fission reactor systems, these were limited to Earth orbital missions and did not attempt any ambitious deep space missions. Near-term space fission reactor missions must capitalize on the experience of earlier development programs in order to accomplish the design, development and launch of a flight system under a limited budget and within a limited time.

Previous space fission development programs have relied heavily on nuclear testing for system development, causing many programs to delay the accomplishment of major milestones and to experience cost overruns. The recent genesis of space nuclear reactor development has focused on systems that can be developed in an affordable and timely fashion, while adhering to a strict safety culture. These goals lend support to the development of a strong non-nuclear test program that can be applied in component and system testing, characterizing system properties and behaviors and identifying potential failures both quickly and accurately. The primary downside of non-nuclear tests is, of course, their inability to take into account nuclear effects. This condition requires that systems be designed to operate well within radiation damage and fuel burn-up limits such that reactor designers can fully garner the benefits of a strong non-nuclear test program in

which the heat from nuclear fuel is simulated using electrically heated modules. Non-nuclear tests, however, can be coupled with rigorous nuclear simulations, leading to the development of an experimental model that more closely matches the behavior of an operating nuclear reactor. Such techniques will be discussed in this dissertation.

A successful space reactor development program will utilize existing nuclear databases and experiences in system design and testing. In addition, a modern development program should take advantage of the highly reliable computational tools that are now available for modeling nuclear systems. Operation of a fission reactor in space is quite different than a terrestrial system. Models constructed from measured data can be applied in computational analyses to parse out some of these differences, such as the effect of the intense radiation environment in space, providing insight to potential modifications necessary in the system design prior to development of a flight system.

Innovative approaches must be adopted to ensure that the current space fission development program will lead to system utilization in space flight. Several concepts are discussed in this dissertation that utilize model data, employ computational tools, and take advantage of non-nuclear testing capabilities. Each component of this body of work is directed at answering prevalent questions regarding the operation of a fission reactor in space, helping the space reactor development program to further progress toward the ultimate goal of testing a reactor module in flight.

CHAPTER 2

REACTOR THEORY AND ENVIRONMENT DEFINITION

This chapter will outline basic reactor theory necessary to characterize reactor operation in the presence of an external radiation source. In addition, the space radiation environment and its dependencies on time, position, and solar activity will be reviewed to develop a basis for assessing the effect of the space radiation environment on reactor start-up and control.

2.1 Reactor Theory

Most systems under consideration for near-term space fission systems employ compact, fast spectrum reactor cores that utilize highly enriched uranium fuel. The front-runners currently being considered for initial outer planetary missions include a heat pipe cooled reactor (HPR) with in-core, liquid metal heat pipes; a direct-drive gas cooled reactor (DDG) cooled by a noble gas mixture; and a pumped liquid metal cooled reactor (LMR) cooled by circulating NaK or lithium (Li). In addition to using similar fuel and operating neutron spectrum, all three concepts use control mechanisms that are external to the reactor core (e.g., rotating cylindrical control drums or sliding reflectors) and neutron reflectors constructed of beryllium or beryllium oxide necessary to improve the neutron economy (Houts et al. 2003). For initial study of the effects of the space radiation environment on reactor start-up and control, all calculations will apply the heat pipe cooled reactor as a baseline design. Heat pipes are an ideal heat transport mechanism for space applications because they are self-contained, are independent of one

another, and passively transfer heat from the reactor core. In addition, they can be designed with a sufficient amount of their surface area outside the core (e.g., extending above/below the core region), such that decay heat after reactor shutdown can be passively radiated from the system.

2.1.1 Heat Pipe Cooled Reactors

Engineers at Los Alamos National Laboratory (LANL) have designed several heat pipe cooled reactor concepts, ranging in power from 15 kW_t to greater than 800 kW_t, for both surface power systems and nuclear electric propulsion systems. The Safe, Affordable Fission Engine (SAFE) is currently being developed in a collaborative effort by LANL and NASA Marshall Space Flight Center (NASA/MSFC), for which NASA is responsible for fabrication and testing of non-nuclear, electrically heated modules in the Early Flight Fission Test Facility (EFF-TF) at MSFC. Due to the similarities in the operating neutron spectrum and control mechanisms, the effects of the space radiation environment and the subsequent control methodology will be similar for each reactor concept. The methods developed and applied in the present work, while focused on the SAFE heat pipe cooled reactor, will be easily translated to whatever reactor concept is ultimately chosen for the initial application of fission power in a deep space mission.

With funding provided by NASA/MSFC, a molybdenum heat pipe module was fabricated by LANL and tested at the New Mexico Engineering Research Institute (NMERI) in 1996, utilizing equipment and facilities that were purchased for use by the Thermionic Systems Evaluation Test (TSET) program for the Topaz II reactor (see Section 1.1.4). This individual module test, and a later bimodal module that was tested in 1999, demonstrated high temperature operation, fast-start and re-start capability, and operation at full rated power, in addition to demonstrating the soundness of the modular approach to reactor design (Houts et al. 1997).

Because heat pipe reactors are easily scaled to higher power while maintaining similar core layout and design, hardware testing can be accomplished in stages to work out design details before constructing a more complex, higher power core. Following the single module tests, the next step in the testing and assessment of the heat pipe reactor concept was to construct a full 30 kW_t core prototype, where the primary test objective was to demonstrate the robust operation of the simulated nuclear core with cooling provided by in-core heat pipes (VanDuyn et al. 2002; VanDyke et al. 2002). Initial testing demonstrated efficient heat transfer from the simulated fuel elements to a point external to the core, via both heat pipes and direct thermal conduction; it also verified the theoretical analysis of the core performance. Upon completion of initial tests, an off-the-shelf Stirling engine was coupled to the core prototype for end-to-end testing with an ion thruster at the Jet Propulsion Laboratory (JPL) (Hrbud et al. 2002). The objective of this test was to demonstrate an integrated propulsion system, including the reactor core, power conversion system, and a thruster. The end-to-end demonstrator coupled the 30 kW_t simulated reactor core, a 350 W Stirling engine, and a 15 cm ion thruster with slotted carbon-carbon grids. The successful test of the simulated nuclear electric propulsion (NEP) system set a precedent for non-nuclear ground testing.

The SAFE test series addresses issues related to non-nuclear testing and system integration that will lead to the ultimate testing and demonstration of a fully integrated flight unit. Experience gained from the SAFE-30 test series was applied in the design and fabrication of a 100 kW_t stainless steel heat pipe cooled reactor core prototype, which will include a prototypic heat exchanger in FY 2004 testing (VanDyke 2003). Testing of the 61-module SAFE-100 reactor core prototype was recently completed at the EFF-TF (February 2004). Initial check-out testing of the SAFE-100a unit, which is constructed from only the center 19 modules of the SAFE-100 and includes a 19 channel prototypic heat exchanger, commenced in March 2004; SAFE-100a testing will continue through FY 2004 and 2005. A schematic of the SAFE-100a test article is included in Figure 2.1,

where the in-core heat pipes are shown to extend from the core and are coupled to an annular gas flow heat exchanger for energy conversion. Photographs of the actual test hardware are included in Chapters 5 – 7, which discuss tests that were performed on the SAFE-100 core and components of the SAFE-100a heat exchanger. The ultimate goal of this staged testing is to develop a higher power heat pipe cooled reactor core (e.g., a 400 kW_t design) that could be used in an NEP vehicle for an outer planetary mission. All computational work presented in this document will apply to the SAFE-400 reactor design; experimental work will be presented for the prototypic 100 kW_t system.

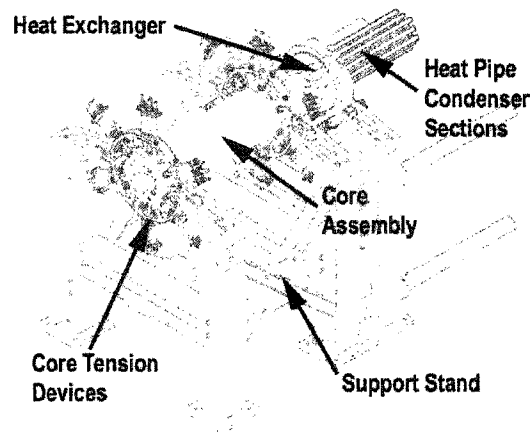


FIGURE 2.1. Schematic of the SAFE-100a Test Article with Heat Exchanger.

2.1.1.1 SAFE-400

The SAFE-400 reactor was designed by LANL for nominal operation at 400 kW_t. Through coupling with a Brayton power conversion system, the SAFE-400 reactor is predicted to provide 100 kW_e. Two 100 kW_e Brayton power systems (assuming 25% conversion efficiency from thermal to electric power) would be included to provide redundancy in the power conversion system to mitigate the risk of a single point failure in the power system. The SAFE-400 core would be constructed with 127 identical modules.

Each module would include a sodium-filled molybdenum heat pipe surrounded by three fuel pins, as shown in Figure 2.2. Each fuel pin would be constructed from a molybdenum tube filled with rhenium-clad, highly enriched (97% ^{235}U) uranium nitride (UN) fuel; the proposed fuel would adhere to the SP-100 fuel specifications. The 127 independent modules are designed to nest together to form a symmetric core configuration. Details of the SAFE-400 design considered in this dissertation can be found in Poston et al. (2002).

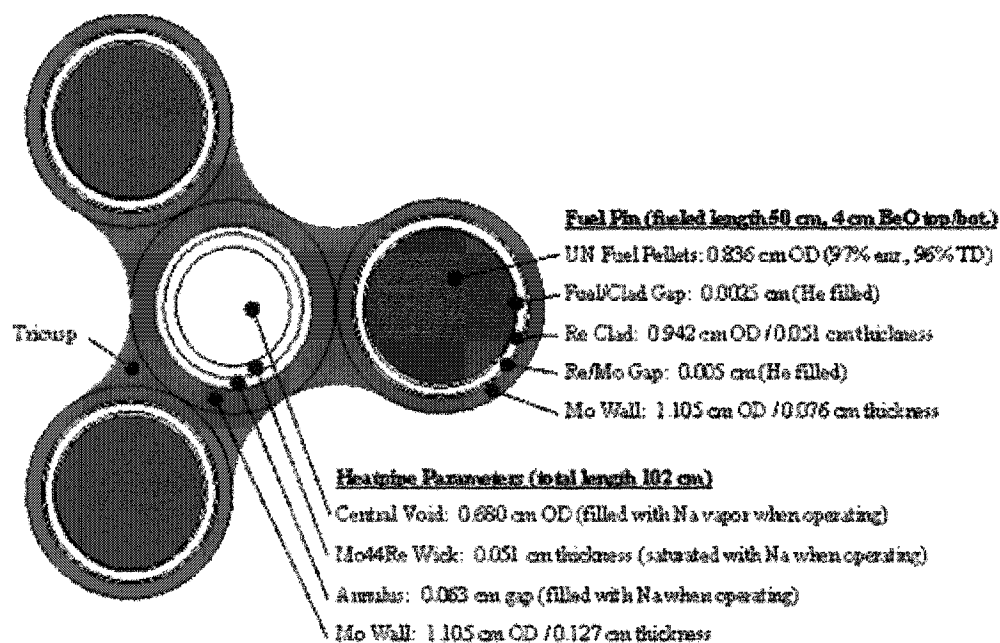


FIGURE 2.2. SAFE-400 Module Design.

The SAFE-400 reactor is a compact, fast spectrum reactor. The hexagonal core has a flat-to-flat size of 26.0 cm, with a 48.0 cm diameter radial reflector. The core height is 58.0 cm (50 cm fueled length), with the heat pipes extending an additional 50.0 cm above the core for coupling to the heat exchanger. Reactor control is accomplished by six, 12 cm diameter cylindrical control drums housed within the beryllium radial

reflector that surrounds the reactor core. Each drum is primarily constructed from beryllium and contains a section of boron carbide (B_4C) within a 120° arc. When rotated to the inner position (adjacent to the core), the B_4C serves as a reactor poison, absorbing neutrons and preventing the reactor from becoming critical. As the control drums are rotated out, such that the B_4C is moved away from the the core, the amount of neutron reflection is increased, increasing the core reactivity and allowing the reactor to become critical. At a drum position of 90° , the effective multiplication calculated by MCNPX 2.5.b (Waters 2002) is 1.00021 ± 0.00024 (Bragg-Sitton and Holloway 2004), corresponding to a reactivity of nearly zero; the MCNPX model will be described in greater detail in Chapter 4. An axial cross section of the SAFE-400 reactor core model is shown in Figure 2.3.

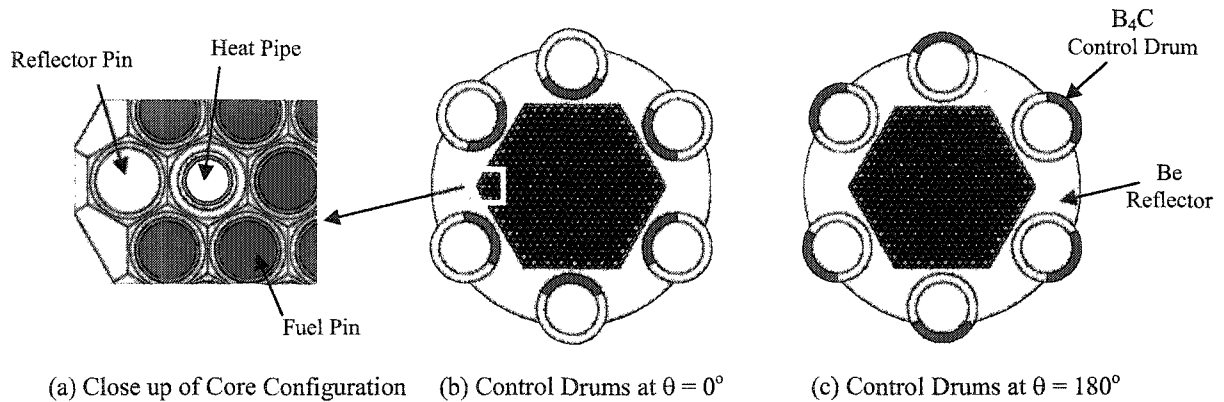


FIGURE 2.3. Axial Cross Section of SAFE-400 Core for Control Drum Positions.

In designing the SAFE-400, it was desirable to keep the system design simple, modular in construction, and easily testable. To the greatest extent practicable, all components utilize existing technology, significantly reducing the cost, duration, and technical risk of the development program. The SAFE-400 design enables inexpensive non-nuclear, electrical testing that can generate a significant database for future work. Testability of components and sub-assemblies reduces the need for full ground nuclear

testing, with the possible exception of zero-power critical experiments necessary to verify the core neutronics. This scope of this dissertation is limited to computational analysis of the reactor operation and non-nuclear experimental testing and will not address critical experiments.

2.1.1.2 Theory of Heat Pipe Operation

The term “heat pipe” was first coined by George Grover of the Los Alamos Scientific Laboratory in 1963 in his observation of heat transfer via the capillary movement of fluids (Grover et al. 1964). In his original observations, Grover suggested possible application of this heat transfer device to space reactors to provide passive heat extraction. A heat pipe is a closed, passive heat transfer mechanism that is generally constructed from a long cylindrical pipe that is sealed at both ends and is partially filled with a working fluid. Heat pipes are self-starting, and they have a self-pumping coolant loop; pumping action is accomplished by a porous wick structure along the inside surface of the heat pipe (Reid 2003). In a frozen heat pipe, all of the working fluid is trapped within the annular gap between the wick and the heat pipe wall, leaving a void in the central region. A heat pipe consists of three distinct regions along the axial dimension: the evaporator, which is actively heated; the adiabatic region, which is neither heated nor cooled; and the condenser, which is actively cooled. Although the evaporator and condenser regions are required, not all heat pipes are operated with an adiabatic region, depending on the specific application. When the evaporator region is heated, the liquid metal begins to boil and is vaporized. In an alkali metal heat pipe, the working fluid is generally evaporated by surface vaporization that occurs at the liquid-vapor interface. The vapor escapes the porous wick structure and diffuses into the central void. Due to the pressure differential along the heat pipe, the vapor diffuses along the central column to the cooler, lower pressure condenser region. The lower temperature in this region causes the vapor to condense back into the wick. In the process, the working fluid gives

up its latent heat of vaporization, which is transferred through the heat pipe wall to the external environment. This energy can be captured by coupling the heat pipe to a heat exchanger. The condensate is again trapped by the wick and is pumped by capillary action in the wick structure back to the evaporator region, where it can again be vaporized. An illustration of the vapor and liquid flow in an operating heat pipe is shown in Figure 2.4. This process establishes a closed loop system in which heat is removed from the heated evaporator region and is transferred to the environment around the condenser; the condenser region is generally coupled to a heat exchanger for energy conversion.

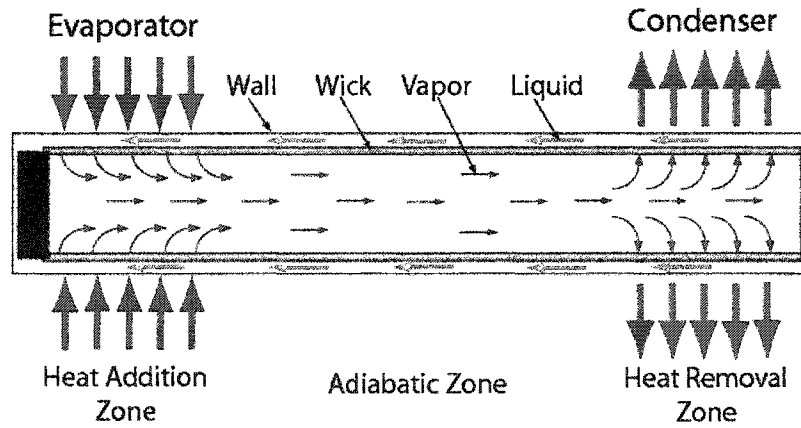


FIGURE 2.4. Illustration of Heat Pipe Operation.

Good heat pipe working fluids should possess high liquid and vapor density (ρ_l, ρ_v); high surface tension (σ), which generates a high capillary driving force in the wick structure; high latent heat of vaporization (h_{fg}), such that a large amount of heat can be transferred by the vapor to the condenser region; and low liquid and vapor viscosity (μ_l, μ_v), which reduces the resistance to liquid flow. The “liquid transport factor” (N_l), which is proportional to the heat pipe axial power density, is expressed as:

$$N_l(T) = \frac{\rho_l \sigma h_{fg}}{\mu_l} . \quad (2.1)$$

Materials adhering to the ascribed properties include the alkali metals, such as cesium, potassium, sodium, and lithium, with lithium demonstrating the highest liquid transport factor. The selection of an appropriate working fluid is dependent on the desired heat pipe operating temperature (Hall and Doster 1990). For high temperature applications, above 700° K, alkali metal heat pipes are an appropriate choice. Sodium and lithium heat pipes, for instance, can provide power throughputs as high as 15 and 30 kW/cm² at operating temperatures of 1150 and 1580° K, respectively (Tournier and El-Genk 2003).

Heat pipes can be designed with various wick structures, including homogeneous screen, arterial flow, concentric annulus, or crescent annulus. The heat pipe wick has several functions: it provides the necessary capillary forces to move the liquid (providing a liquid return path), it primes the liquid in the capillary structure, and it protects the liquid from entrainment in the vapor flow along the central region of the heat pipe. Although it is seldom present in liquid metal heat pipes having wick structures that shield the liquid condensate from the vapor flow, entrainment describes the condition in which the vapor flow, moving in the opposite direction of the liquid flow in the wick, sweeps the liquid out of the wick and into the vapor flow region. If significant entrainment occurs, it can lead to evaporator dry out.

Some desirable properties of a heat pipe wick include small capillary pore size, high thermal conductivity of the wick material, and low impedance liquid return path. Heat pipe operation is based on capillary action. If a small tube is dipped beneath the surface of a liquid, the liquid will rise to a certain level depending on the wetting properties of the liquid. If the liquid does not wet the surface, it will not flow along the capillary tube. Hence, heat pipes require a wetting liquid such that the capillary action will be sufficient to move the working fluid along the liquid return path in the wick from the cool condenser region to the hot evaporator region. For a heat pipe to operate properly, the maximum capillary pressure rise, which is determined by the effective pore radius in the wick, must exceed the total pressure drop in the liquid and vapor regions.

The liquid pressure drop in the heat pipe wick is purely viscous, but the vapor pressure drop in the evaporator and condenser regions is more complicated due to the presence of both viscous and inertial effects. Details of the pressure drop in each region are provided in Reid (2003).

The proposed SAFE-400 heat pipe would be constructed from Nb-1%Zr and would utilize sodium as the working fluid. Sodium is useful as a working fluid over a range of evaporator exit temperatures of 1000° K to 1400° K (Na vapor temperature). The expected properties of the SAFE-400 heat pipe are provided in Table 2.1 (Reid 2003).

Parameter	Value
Wall material / thickness	Nb-1%Zr / 0.64 cm
Length	1.41 m
Evaporator length	0.56 m
Adiabatic length	0.25 m
Condenser length	0.50 m
Outside diameter	1.02 cm
Wick Structure	Crescent annular gap
Wick Material	Drawn mesh, Mo
Working Fluid	Na, 40 g
Design Temperature	1200° K
Design Throughput	3150 W
Startup Time	~ 1 hour
Design margin	> 3 (can transport >3x design power)

TABLE 2.1. SAFE-400 Heat Pipe Properties.

The SAFE-400 heat pipe would employ a “crescent annular gap” wick, in which the annulus created by the wick and the heat pipe wall is slightly eccentric. The wick is expected to be constructed from 400x400 drawn mesh molybdenum, having a wire diameter (d_{wire}) of 0.0254 mm (0.001 in) and a maximum capillary pore size of 0.032 mm (0.00126 in). The wick porosity (e) refers to the open area fraction in the mesh and is approximated using the following expression:

$$e = 1 - \frac{\pi}{4} \cdot 1.05 \cdot mesh \cdot d_{wire}^2, \quad (2.2)$$

where the wire diameter is provided in inches. For the SAFE-400 design, this becomes:

$$e = 1 - \frac{\pi}{4} \cdot 1.05 \cdot 400 \cdot 0.001 = 0.670 \quad (2.2a)$$

Heat pipes are filled to a specified mass of high purity alkali metal by vacuum distillation (Martin and Salvail 2004). During heat pipe fill, a plug at the evaporator end seals off the tube. The condenser end is sealed by “excess” working fluid; this corresponds to approximately 10% more liquid than what is required to saturate the wick, forming a “pool” at the end of the condenser. After the fill is complete, the heat pipe fill stem is crimped and welded to seal off the heat pipe. Before using a heat pipe, it must be heated isothermally along the entire length to ensure that the entire wick has been wetted by the alkali metal working fluid (e.g., Na) before use as a heat transfer mechanism.

Depending on the stage of operation of a heat pipe, various restrictions limit the rate at which it can transfer heat. During heat pipe start-up, heat input in the evaporator region begins to melt the working fluid that is trapped in the wick. The vapor flow moves through several flow regimes during start-up, including molecular flow, transition flow, and continuum or viscous flow. At the melt front that propagates toward the condenser end, all three flow regimes may coexist (Tournier 2003). Once the continuum flow reaches the end of the condenser, a liquid plug forms and the heat pipe becomes fully operational. Little working fluid mass is lost to the vapor flow by sublimation, such that the evaporator wick remains saturated with liquid throughout a proper start-up.

During start-up or low power operations, a heat pipe is viscous limited. During this period, the low vapor pressure is insufficient to overcome the pressure drop along the full length of the pipe and the vapor cannot travel the full length of the condenser, effectively shortening the active length of the heat pipe. At slightly higher power density, but still during heat pipe start-up, the heat pipe can become sonic limited. During this period, the vapor is traveling at high velocity through the evaporator region, but it cannot exceed sonic velocity at the evaporator exit. If the vapor reaches the speed of sound,

choking occurs at the evaporator exit (Tournier 2003). The sonic limit for energy transferred along the heat pipe (Q_{sonic}^{limit}) is expressed in Eq. (2.3), where Q is the actual energy transferred from the evaporator to the condenser region:

$$Q \leq Q_{sonic}^{limit} = A_V \left(\frac{\gamma M}{R_g T_{ev}^{ex}} \right)^{1/2} \cdot h_{fg}(T_{ev}^{ex}) P_{sat}(T_{ev}^{ex}), \quad (2.3)$$

where

- A_V = vapor flow area (m²),
- γ = specific heat ratio = $C_p/C_v = 5/3$ for monatomic gases,
- M = molecular weight of working fluid (g/mol),
- $h_{fg}(T_{ev}^{ex})$ = latent heat of vaporization at evaporator exit temperature, and
- $P_{sat}(T_{ev}^{ex})$ = saturation pressure at evaporator exit temperature.

While the heat pipe is sonic limited, further heat input causes the evaporator region to heat up relative to the condenser region. If a heat pipe is started very slowly, it does not reach the sonic limit. The nominal start-up time varies depending on the heat pipe design.

The minimum of the radiation, sonic, and viscous heat transfer limits provides an estimate of the heat transfer rate from the evaporator to the condenser during heat pipe start-up (Reid 2002). The radiation heat transfer limit (Q_R), given by Eq. (2.4), represents the maximum amount of heat that can be transferred from the evaporator to the condenser, and subsequently to the environment. The radiation heat transfer limit assumes that *all* the heat from the evaporator is fully transferred to the condenser, which is then radiated to the environment:

$$Q_R = \sigma \varepsilon A_{Cond} (T_{Evap}^4 - T_{\infty}^4), \quad (2.4)$$

where

- σ = Stefan – Boltzmann constant (W/m²-K⁴),
- ε = emittance of the heat pipe (condenser) surface,
- A_{Cond} = outside surface area of the condenser (m²),
- T_{Evap} = evaporator temperature (K), and

T_{∞} = temperature of the surrounding environment (K).

The viscous limit (Q_v) is determined by the point at which the total pressure drop along the heat pipe is equal to the saturation pressure at the evaporator outlet temperature. The total pressure drop is the sum of the the vapor pressure drop in the evaporator and condenser and the liquid pressure drop experienced by the working fluid as it travels through the wick from the condenser back to the evaporator. During heat pipe operation, a capillary limit is also present at a power level that produces a mass flow rate of the working fluid sufficient for the liquid and vapor pressure drops to exceed the maximum capillary head potential in the wick. During this stage, the mass of vapor leaving the evaporator region exceeds the mass of liquid returning along the wick. This results in a rising evaporator temperature due to liquid depletion (i.e. the evaporator cooling is reduced due to a smaller amount of liquid returning from the condenser region).

Several features demonstrated by heat pipes are desirable in space systems, and they are often considered for reactor cooling and waste heat rejection in radiator panels. Heat pipes are capable of transferring large amounts of energy over a wide range of temperatures, where the desired operating temperature and power throughput are used to select an appropriate heat pipe design. Passive heat pipe operation removes additional system complexities that are introduced in many heat transfer designs, such as active cooling to a reactor core utilizing high pressure gas flow or a pumped liquid metal loop. Additionally, because each heat pipe is independent, a heat pipe cooled system is a redundant design (e.g., 127 core heat pipes in the SAFE-400 design), increasing failure tolerance by removing single point failure mechanisms. Experimental operation of a sodium filled stainless steel heat pipe, designed for electrically heated testing of the SAFE-100a reactor core prototype, is discussed in Chapter 7.

2.1.2 Reactor Dynamics and Control

Most reactors in commercial nuclear power plants utilize a thermal neutron spectrum. All neutrons emitted in fission are fast; in a thermal reactor, a moderator is required to slow neutrons to thermal energies (at room temperature, ‘thermal’ refers to 0.025 eV mean energy). At these energies, a large fraction of the neutrons are absorbed by the fuel, generating fission. Although fission cross sections decrease significantly with increasing energy, fast fission does occur. Fast reactors are designed to utilize a fast neutron spectrum (~200 keV mean energy) to produce a sustainable chain reaction (Ash 1965). The decreased microscopic fission cross section at the higher neutron energies requires a greater critical mass in a fast reactor than in an equivalent thermal reactor. However, because no moderator is required, fast reactors are significantly smaller than their thermal equivalents and can operate at higher core temperatures than a moderated reactor, making them more desirable for space applications where size and mass are significant factors in the viability of a spacecraft. The geometry of the system results in increased power density, but the poor surface-to-volume ratio also increases the neutron leakage from the core. A neutron reflector, such as graphite or beryllium, must be added around the core to provide a mechanism for some of the neutrons to be reflected back into the fuel material to continue the fission process, improving the neutron economy.

Control of a fast reactor introduces some complications not present in thermal reactors. The reactor neutron population includes both prompt neutrons, which are generated directly in a fission event, and delayed neutrons, which are emitted in the decay of certain fission fragment nuclei subsequent to fission. Prompt critical describes a state in which the reactor is critical on prompt neutrons alone. In this state, only a slight increase in the neutron population is required to generate an exponential rise in the population, causing a prompt critical reactor to be unstable. Delayed neutrons, which have significantly longer characteristic half-lives relative to prompt neutrons, provide a

mechanism for controlling the reactor power and maintaining stable operation. The reactor is generally kept slightly subcritical relative to the prompt neutrons, with the remaining neutron population ($\sim 0.64\%$) required for criticality provided by the delayed neutrons. Below prompt critical, a fast reactor is governed by delayed neutrons, as with a thermal reactor, and the properties of delayed neutrons vary little between the two reactor types. However, prompt neutron characteristics vary significantly. The prompt lifetime is inversely proportional to the product of the neutron speed and the macroscopic absorption cross section (Σ_a). The large difference in the mean neutron speed in each type of reactor results in a prompt neutron lifetime in fast reactors of 10^{-6} to 10^{-9} seconds; the lifetime is on the order of 10^{-3} seconds for thermal reactors (Hetrick 1971a). Although the lifetime is also dependent on the macroscopic absorption cross section, the effect of this parameter is less significant; the decrease in the microscopic absorption cross section (σ_a) for fast reactors is compensated partly by the increase in the density of fuel nuclei (N) in fast reactors, where $\Sigma_a = N\sigma_a$.

In thermal reactors, neutron absorbing materials in the core help to control neutron multiplication. In-core control rods made of an absorbing material and located in the central region of the core can be inserted or removed to varying degrees to maintain the desired neutron population. However, the capture cross section for most absorbers generally decreases with increasing neutron velocity (v), and is thus negligible at fast neutron energies ($\Sigma_{capture} \propto 1/v$). Utilizing in-core control rods alone in a fast reactor would require significant reactivity worth in each rod to be sufficient for control. Instead, fast reactor systems proposed for space applications generally apply ex-core control, which can be used to modify both neutron absorption and leakage from the core. Fast reactor control systems generally employ either the expansion or movement of fuel material or movement of the reflector and neutron absorbing materials to control reactivity (Ash 1965). The latter mechanism is applied in the control systems for the

reactor concept described in this dissertation, which employs ex-core rotating control drums housed within the radial reflector.

For optimal control, tight coupling is required between the reactivity feedback coefficient and reactor power. This coupling is primarily provided by fuel expansion during reactor operation. As the reactor core temperature increases, the materials in the core expand. Close coupling results from the material composition of a fast reactor, and geometric and material changes are more closely tied to reactivity changes than for thermal reactors (Ash 1965). For a compact, fast spectrum reactor, reactivity feedback is dominated by thermal core deformation. Thermal expansion is a negative reactivity feedback mechanism. As the temperature of the reactor core increases, thermal expansion causes overall growth of the core diameter and total core volume, which results in a decrease in the core reactivity due to the increased neutron leakage from the reactor core. In the SAFE reactor, control is achieved by beryllium (Be) control drums that contain a section of boron carbide (B_4C) for neutron absorption. As the amount of B_4C absorber in the reactor core is increased, the reactivity is reduced. The specific relationship between reactivity and core deformation for the SAFE-400 reactor will be discussed in Chapter 5, where it is applied to a virtual reactivity feedback experiment that uses experimental measurements of core deformation for the SAFE-100 reactor core prototype to assess the reactor power as a function of time.

Reactor start-up is accomplished by initiating a chain reaction. Neutrons introduced to the fuel material due to spontaneous fission can lead to a sustained chain reaction, given a critical core configuration, but the relatively small number of spontaneous fission neutrons present in the core and neutron leakage from the core renders this method very slow. The nuclear fuel in the SAFE-400 reactor, which has a 141.7 kg inventory of highly enriched uranium-nitride (UN), produces only 85.5 spontaneous fission neutrons per second from the decay of ^{234}U , ^{235}U , ^{238}U , and ^{239}Pu (Poston 2002). To speed up the process, criticality is often achieved by inserting a

neutron source into the core region. This additional source provides a known neutron fluence, such that the reactor start-up can be modeled prior to implementation. In-core detectors provide real-time feedback to afford reactor operators the assurance of a controlled start-up. Standard start-up sources are placed in a location where neutrons have significant importance; a possible location might be below the core block at the radial center of the core. A common start-up source generally couples an alpha (α) emitter with a nuclide that undergoes an (α ,n) reaction. One such source is plutonium-beryllium (Pu-Be); for alphas emitted in the decay of plutonium, a Pu-Be source produces approximately 10^{-4} neutrons per incident alpha particle (Knoll 1983). Typical reactor start-up sources provide on the order of 10^6 to 10^8 neutrons/second (Hetrick 1971a).

The reactor vessel will be exposed to a large amount of radiation in the space environment. To properly understand and model a reactor start-up in space, it is important to characterize the interaction of this radiation with the reactor core and surrounding materials. During steady state operation, the secondary neutrons produced by interactions of this external radiation source with the reactor vessel are expected to be negligible with respect to the neutron flux in the reactor core. One goal of the present work is to verify this assertion through simulation of the particle interactions and analysis of the reactor operation using the point kinetics model. During transient operations in which the reactor neutron population is relatively low, such as during initial reactor start-up, the external radiation source may have a significant effect on the reactor behavior. The work presented in Chapter 4 quantifies this effect with computational analysis of the core neutronics in the presence of an external source. Through the development and application of an optimized controller for reactor start-up, the potential effects of both constant and fluctuating source conditions are assessed.

2.1.3 System Model: Point Reactor Kinetics

Reactor dynamics can be modeled using the point kinetics equations (PKE), which can be derived from transport and diffusion theory (Hetrick 1971). The PKE representation provides only an approximate model of the reactor; it does not provide a mechanism to describe neutron energy effects or structural details in a heterogeneous reactor. Because fast reactors are relatively small in size and do not contain any moderator to slow the neutrons to a lower energy, the PKE are a good approximation for the dynamics in a fast reactor.

A relatively simple derivation of the PKE begins with the steady-state neutron diffusion equation:

$$\frac{1}{v} \frac{\partial \phi}{\partial t} = D \nabla^2 \phi - \Sigma_a \phi + S, \quad (2.5)$$

where

- D = diffusion coefficient,
- ϕ = neutron flux,
- v = neutron speed,
- Σ_a = macroscopic absorption cross section, and
- S = neutron production term.

The first term on the right-hand side of Eq. (2.5) represents the number of neutrons diffusing into the volume of interest per unit time; the second term represents a loss rate to neutron absorption; the final term represents the total neutron production per unit time. This term can be expanded into its various components, as shown in Eq. (2.6):

$$S = (1 - \beta) k_{\infty} \Sigma_a \phi + \sum_i \lambda_i C_i + S_0, \quad (2.6)$$

where

- $\beta = \sum_i \beta_i$ = total delayed-neutron fraction,
- β_i = delayed-neutron fraction for the i^{th} precursor group,

C_i = delayed-neutron concentration for the i^{th} precursor group,
 S_0 = external (non-fission) neutron source,
 λ_i = decay constant for the i^{th} precursor group, and
 k_∞ = infinite-medium multiplication factor.

In this expression for the external neutron source, the first term represents the prompt-neutron contribution to the source, the second term represents neutrons contributed by radioactive decay of the delayed-neutron precursors, and the final term represents any external, non-fission neutron source that might be present. The delayed neutron populations are given by the balance equation for the production and decay of the delayed neutron precursors:

$$\frac{\partial C_i}{\partial t} = \beta_i k_\infty \Sigma_a \phi - \lambda_i C_i . \quad (2.7)$$

The basic assumptions associated with the PKE are that the shape of the neutron flux does not change with time and that separability of the space and time dependence of the flux is approximately accurate (hence, the partial derivatives in Eqs. 2.5 and 2.7 become complete derivatives in the final expression of the PKE). To derive the point kinetics equations from the steady-state diffusion equation, the following substitutions are made:

$$\phi = nv ; \quad (2.8a)$$

$$\nabla^2 \phi \rightarrow -B_g^2 \phi ; \quad (2.8b)$$

$$B_g^2 = \frac{\nu \Sigma_f - \Sigma_a}{D} ; \quad (2.8c)$$

$$\frac{D}{\Sigma_a} = L^2 ; \quad (2.8d)$$

$$k_{eff} = \frac{k_\infty}{1 + L^2 B_g^2} = \frac{\nu \Sigma_f}{\Sigma_a} ; \quad (2.8e)$$

$$l = \frac{1}{v \Sigma_a (1 + L^2 B_g^2)} ; \quad (2.8f)$$

$$\Lambda = \frac{l}{\beta k_{eff}} ; \quad (2.8g)$$

where

- B_g^2 = geometric buckling,
- k_{eff} = effective neutron multiplication factor,
- L^2 = diffusion length,
- Λ = prompt generation time (Ash 1965),
- l = prompt neutron lifetime (Ash 1965),
- n = neutron population,
- v = neutron velocity,
- ν = average number of neutrons produced in each fission event, and
- Σ_f = macroscopic fission cross section.

Given the above definitions, the value $(1 + L^2 B_g^2)^{-1}$ represents the fraction of neutrons that do not escape from the reactor. Note that k will be used interchangeably with k_{eff} to represent the effective neutron multiplication factor. The substitution for flux in terms of the number of neutrons (Eq. 2.8a) assumes that all neutrons in the reactor have the same energy. Simplifying the expression in Eq. (2.5), we find that the balance equation for the neutron population becomes:

$$\frac{dn}{dt} = \frac{n}{l} [k - 1 - k\beta] + \sum_i \lambda_i C_i + S_0 . \quad (2.9)$$

The reactivity (ρ) defined in terms of the multiplication factor, is essentially a measure of the excess multiplication in the reactor:

$$\rho = \frac{k - 1}{k} . \quad (2.10)$$

Substitution of Eq. (2.10) into Eq. (2.9) yields the standard point kinetics equations in terms of the neutron population and the standard expression for reactivity (Ash 1965; Hetrick 1971a):

$$\frac{dn}{dt} = \frac{\rho - \beta}{l} n + \sum_i \lambda_i C_i + S_0 , \quad (2.11a)$$

$$\frac{dC_i}{dt} = \beta_i k \frac{n}{l} - \lambda_i C_i . \quad (2.11b)$$

Reactivity is often expressed in terms of dollars. The reactivity scale between zero and β is divided into 100 cents, where 100 cents of reactivity is equal to one dollar. Hence, the reactivity expressed in units of dollars is given by Eq. (2.11):

$$K = \frac{\rho}{\beta} . \quad (2.12)$$

The PKE are often written in terms of the prompt generation time (Λ) and reactivity in dollars, as expressed in Eqs. (2.13):

$$\frac{dn}{dt} = \frac{n}{\Lambda} (K - 1) + \sum_i \lambda_i C_i + S_0 , \quad (2.13a)$$

$$\frac{dC_i}{dt} = \frac{n}{\Lambda} - \lambda_i C_i . \quad (2.13b)$$

Studies of reactor dynamics generally recognize six distinct groups of delayed neutrons. For a fast reactor, the appropriate delayed neutron parameters are provided in Table 2.2 (Hetrick 1971b). Approximate solutions to the PKE can be found using a smaller number of groups to represent the delayed neutron population. The studies of reactor control presented in this dissertation will consider only one group of delayed

neutrons. In this case, the one group decay constant (λ) is calculated as a weighted average of the six individual decay constants:

$$\lambda = \left(\frac{1}{\beta} \sum_i \frac{\beta_i}{\lambda_i} \right)^{-1}. \quad (2.14)$$

Delayed Neutron Group	Decay Constant $\lambda_i [\text{sec}]^{-1}$	Relative Yield β_i / β
1	0.0127	0.038
2	0.0317	0.213
3	0.115	0.188
4	0.311	0.407
5	1.40	0.128
6	3.87	0.026
Total Decay Constant, $\lambda = 0.0767 \text{ sec}^{-1}$		
Total Delayed Neutron Fraction, $\beta = 0.00642$		

TABLE 2.2. Delayed Neutron Properties for Fast Fission of ^{235}U (Hetrick 1971b).

In the presence of an external source (S_0), examination of the steady-state solution to the point kinetics equations indicates that a subcritical reactor ($k_{\text{eff}} < 1$) can be made to be self-sustaining. At steady-state, the rate of change of the neutron and precursor concentrations is zero. Solution of Eqs. (2.11) for the corresponding reactivity in the presence of an external source, neglecting any reactivity feedback effects, yields:

$$\rho = -\frac{S_0}{n_{ss}} \cdot \frac{l}{k}, \quad (2.15)$$

where n_{ss} is the neutron population at steady state. Because n_{ss} , l , and k are all positive quantities, analysis of Eqs. (2.10) and (2.15) implies that $k < 1$ when S_0 is non-zero.

Hence, one would expect the final control drum position to correspond to an effective multiplication that is slightly less than one in the presence of an external source. The reactor neutron population (n) can be converted to reactor power (P) using the expression

in Eq. (2.16), where w corresponds to the amount of energy produced in a single fission reaction ($w = 200 \text{ MeV/fission}$):

$$P = n \frac{w}{\Lambda \nu} . \quad (2.16)$$

If reactivity feedback mechanisms are neglected, then the system of differential equations presented in Eqs. (2.11) is linear in n and C . However, the plant description is still highly nonlinear in its dependence on the angular position of the control drums, resulting in a nonlinear control problem. If temperature feedback is included in the expression for reactivity, ρ becomes $\rho(n(t))$, and the equations become nonlinear in n and C . At low power operation, as during start-up, the reactor temperature is relatively low, such that temperature effects do not have a significant effect on the reactivity. Therefore, the analysis of optimal reactor start-up control in Chapter 4 does not include temperature feedback effects on the reactivity. Temperature effects during reactor operation will be explored further in the discussion of experimental applications in Chapter 5. Optimized control to full power operation will require that feedback effects be added in the description of the reactor dynamics.

2.2 Control Theory: Model Based Predictive Control

Reliable reactor control is of imminent importance to reactor safety, both in terrestrial and space systems. For a space system, where the time for communication to Earth is significant, autonomous control is imperative. Based on feedback from reactor diagnostics, a controller must be able to automatically adjust to changes in reactor temperature and power level to maintain nominal operation without user intervention. Of particular interest to the present study is the ability of the reactor control system to provide appropriate control to start the reactor from a cold, zero-power condition in the presence of a time-varying external radiation source within a reasonable amount of time.

During steady-state operation of a reactor at the nominal power level (e.g., 400 kW_t), the neutron population in the reactor is significantly larger than the neutron population generated by external radiation sources. However, during startup, the reactor neutron population is very low and could be dominated by the secondary neutrons produced by interactions of the high energy protons and high Z particles in the external environment with the materials in the reactor module. The latter operational regime is of interest in the present study. The effect of fluctuating external sources on the controllability of the reactor, during both start-up and steady state operation at low power, will be presented in the optimized controller results in Chapter 4.

Model-based predictive control (MBPC) is investigated as a potential control methodology for reactor startup. MBPC is a closed-loop feedback approach that optimizes the control vector over a “moving” or “receding-horizon” (Clarke 1994; Morari 1994). Components that enter into the control algorithm include: the objective or “cost” function (J), which is a user-specified mathematical indicator of the desired performance of the closed loop control; the control vector ($u(t)$), which includes the candidate set of future controls; the control horizon (h), which is the time period over which the objective function is optimized using the control vector $u(t)$; the constraint set, which defines allowable regions for all control and output variables; and the plant or system model, which is used to predict future plant behavior.

A simplistic representation of the MBPC methodology is provided in Figure 2.5. From the plant inputs and the measured plant conditions, the ‘observer’ can arrive at an estimate of the present state of the plant, which can then be fed back into the optimizer. It is the job of the optimizer to compute the present and future manipulations to the controllable plant inputs, utilizing the reference model of the plant to predict the plant behavior over a pre-determined horizon h , while the plant inputs are varied over the same horizon.

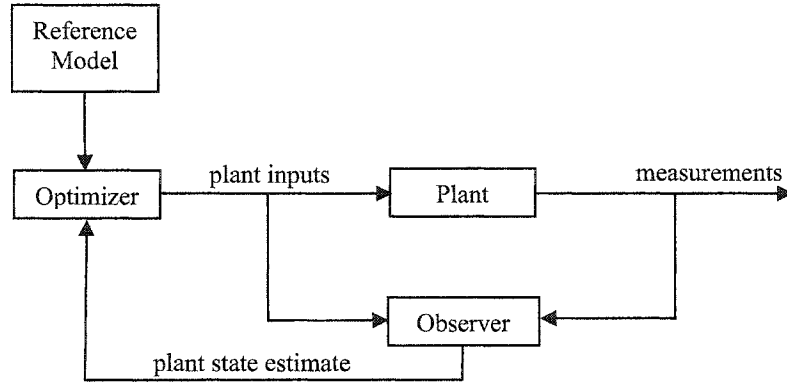


FIGURE 2.5. Representation of MBPC Theory. Modified from (Morari 1994).

MBPC utilizes a moving or receding horizon approach in which only the first element in the control vector $u(t)$ is ever transmitted to the plant. The prediction of the plant response to the series of candidate control steps, $u(t)$, and optimization of the objective function over the control horizon is repeated at each control step. By reference to the plant model, the controller can predict, at each control step, if the current control maneuver will cause future violations of system constraints. If constraints are violated at any point in the vector of candidate controls, regardless of whether the questionable control step would ever be applied to the plant, the proposed control vector is rejected and the optimization is repeated for the current control step.

Most objective functions applied in MBPC are in quadratic form. This structure is convenient to calculations because it avoids sharp transitions that can lead to discontinuities and unstable solutions in minimization routines. A possible structure for an objective function is provided in Eq. (2.17):

$$J = \sum_{j=t_0}^h \alpha_j \cdot (x(t+j) - y(t+j))^2 + \sum_{j=t_0}^h \beta_j \cdot (\Delta u(t+j))^2, \quad (2.17)$$

where

- J = cost of control over current control horizon,
- t_0 = time at start of optimization for current control step,
- t = actual time,

j = time increment within control horizon
 (e.g., incremental time step in the optimization over future time),
 h = control horizon in units of time,
 α_j, β_j = weighting coefficients,
 $x(t+j)$ = estimated system state at time $(t+j)$,
 $y(t+j)$ = desired system state at present time $(t+j)$, and
 $\Delta u(t+j)$ = magnitude of control step at time $(t+j)$.

Feedback from the current plant condition at each sample interval is determined from measurements of the system state. Feedback can be used to “realign” the control at each step, avoiding the accumulated errors that can result from open loop control. The cornerstone of MBPC is the model, and selecting an appropriate model for the system will determine the effectiveness of the controller in plant operation. As will be discussed further in Chapter 3 in the application of MBPC to the controlled reactor start-up, the point kinetics model is coupled with MCNPX analysis to provide an estimate of the reactivity effects of control maneuvers for a specific reactor design.

2.3 Space Radiation Environment

Although no specific mission or launch vehicle has yet been specified for a space nuclear propulsion system, several options have been proposed. A 400-kW_t nuclear electric propulsion system, such as the SAFE-400, could be launched on an expendable launch vehicle, such as the Delta IV Heavy, or could be deployed from the Space Shuttle cargo bay upon reaching a sufficiently high orbit. Launch on an expendable vehicle would allow for deployment to a higher orbit (> 500 km), but expendables entail a significantly higher failure risk than the Shuttle because they lack significant launch history. In fact, the Delta IV Heavy is currently in the development stage and has no launch history, making the first few launches high risk by NASA standards. The Shuttle, on the other hand, carries greater than a 98% success rate, significantly reducing the risk of catastrophic failure at launch. The standard Space Shuttle orbit has an altitude of

approximately 500 km at a 28.5° inclination; the International Space Station (ISS) orbit is also at approximately 500 km, but has a steeper inclination of 51.6° .

The present discussion will consider reactor start-up in several potential orbits, including geosynchronous orbit and a 500 km orbit with varying inclination. It may be possible to boost the NEP system to a higher orbit using an auxiliary system prior to reactor start-up, although this would require a greater launch mass. To fully understand the potential impact on a space fission reactor, the extremes of the space radiation environment that may be encountered by the NEP vehicle should be considered. One potential mission for an unmanned NEP vehicle is a tour of the Jovian system, which possesses one of the most severe radiation environments in our solar system. Hence, reactor operations in the Jovian environment should also be characterized to provide a complete analysis of reactor operation in space.

2.3.1 Near-Earth Space

A significant amount of data has been compiled on the radiation environment in near-Earth space. Trapped particles, galactic cosmic rays, anomalous cosmic rays and solar energetic particles all contribute to this volatile environment. Several models have been generated from experimental data to approximate the populations and energy spectra of each of these components at various times during the 11-year solar cycle (ESA/ESTEC 2002; NASA/GSFC 2001; NASA/LaRC 2002; NRL 1996). Interplanetary trajectories and outer planetary orbits have been characterized far less thoroughly, but some satellite data is available that provides an estimate of the radiation environments in these regions (ESA/ESTEC 2002; NASA/GSFC 2001). The approximate Jovian environment will be discussed in Section 2.3.2 (Fieseler et al. 2002; Garrett and Hoffman 2000; Garrett et al. 2003; Jun et al. 2002).

2.3.1.1 Trapped Particles

A broad spectrum of energetic charged particles, including protons, electrons, and heavy ions, are trapped in the Earth's magnetic field, contributing to the formation of the Earth's radiation belts. Particles spiral about the field lines according to the Lorentz force:

$$\vec{F} = q(\vec{E} + \vec{v} \times \vec{B}), \quad (2.18)$$

where

q = particle charge,
 $\vec{E}$ = electric field,
 $\vec{v}$ = particle velocity, and
 $\vec{B}$ = magnetic field.

As the particles travel along the field lines to regions of increased magnetic field, they experience what is referred to as “mirror motion.” The increased force in the regions of higher magnetic field causes the particles to be reflected back along the field line; only particles having sufficient parallel velocity can escape the magnetic field (Chen 1984). The approximate trajectory of charged particles trapped by the Earth's magnetic field is illustrated in Figure 2.6. Figure used with permission from (Stern and Peredo 2003).

Trapped protons originate primarily from solar particle events. Trapped proton energies can extend from 100 keV to as high as several hundred MeV (ESA/ESTEC 2002). The most energetic trapped protons, above ~10 MeV, are confined to altitudes below 20,000 km, while the lower energy trapped protons can extend to geosynchronous altitude. Figure 2.7 shows the trapped proton flux for energies greater than 10 MeV in near Earth space during solar maximum conditions as a function of the distance from Earth's surface. The semicircle represents the Earth's surface and distance are given in units of Earth radii, where $R_E = 6378.1$ km. The peak trapped proton flux, approximately

10^5 protons/cm²/sec, is observed in the approximate region of 1.5-2.2 R_E at the equatorial plane.

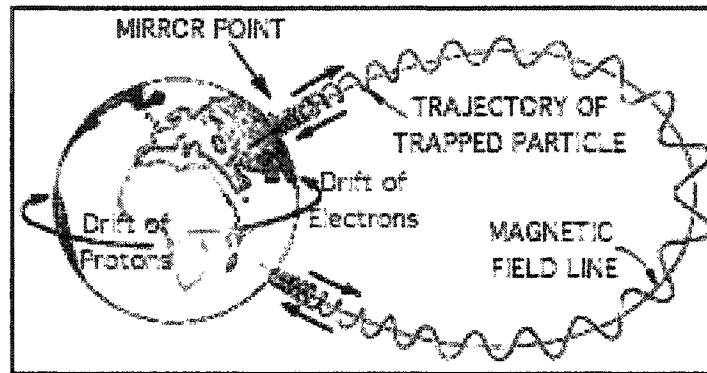


FIGURE 2.6. Approximate Trajectory of Trapped Particles Along Earth's Magnetic Field Lines.

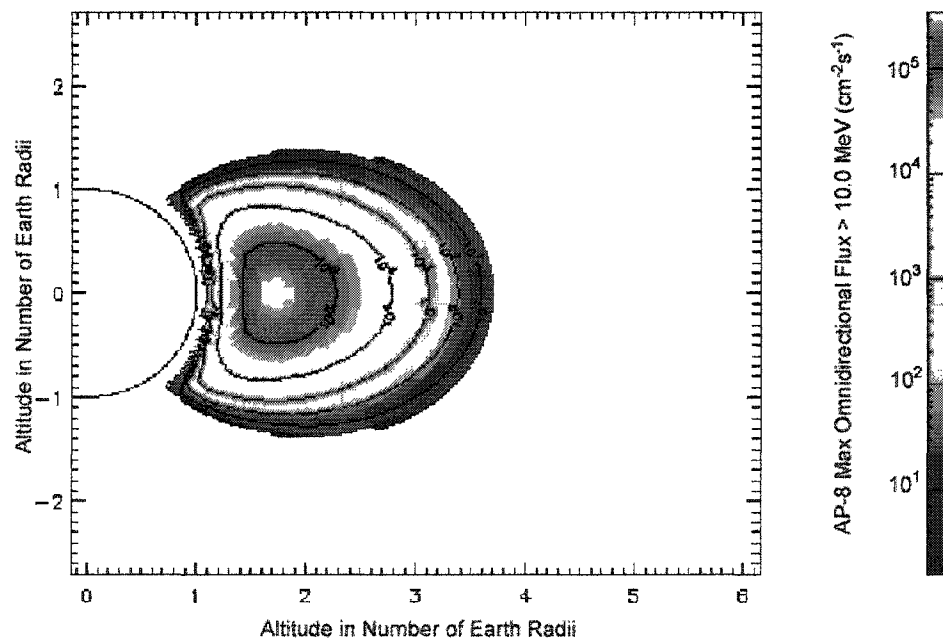


FIGURE 2.7. Greater Than 10 MeV Integral Trapped Proton Flux. Figure included with permission from (ESA/ESTEC 2002).

The trapped proton flux is *omnidirectional*, rather than *isotropic*. Although modeling codes generally average the proton fluence over the entire range of arrival directions (4π steradians), the trapped proton flux is actually highly anisotropic, centered about a plane perpendicular to the local magnetic field line. As the proton energy increases, the anisotropy also increases due to the increased proton gyroradius. Called the Larmor radius, the radius of rotation of a particle traversing a magnetic field B is given by (Chen 1984):

$$r_L = \frac{m v_{\perp}}{|q| B}, \quad (2.19)$$

where

- m = particle mass,
- $v_{\perp}$ = particle velocity perpendicular to the magnetic field,
- q = particle charge, and
- B = magnetic field strength.

Recalling the relation $E = \frac{1}{2} m v^2$, we see that the particle velocity increases as the particle energy increases, resulting in a larger Larmor radius. Charged particles trapped by the earth's magnetic field compose the Van Allen belts, where the magnetic field strength is approximately 500×10^{-9} T (Chen 1984). For a 1 MeV proton, this yields a Larmor radius of approximately 300 km; a 1 eV proton would have a Larmor radius of only 0.3 km. Hence, as a result of the discrepancy in the Larmor radius for each particle energy, the path each high energy proton traverses as it gyrates about its guiding center samples a much larger range of atmospheric densities than lower energy protons. As the altitude increases, however, the anisotropy of particles decreases as a result of the decreasing atmospheric density gradient.

The trapped proton fluence is also affected by what is referred to as the “east-west” effect, with more protons arriving from westerly directions. As illustrated in Figure 2.8, the trajectories of protons arriving from the east gyrate about a point below

the observation point, encountering more residual atmosphere than protons arriving from the west, which gyrate about a point above the observation point where there is less residual atmosphere (CREME96 1996).

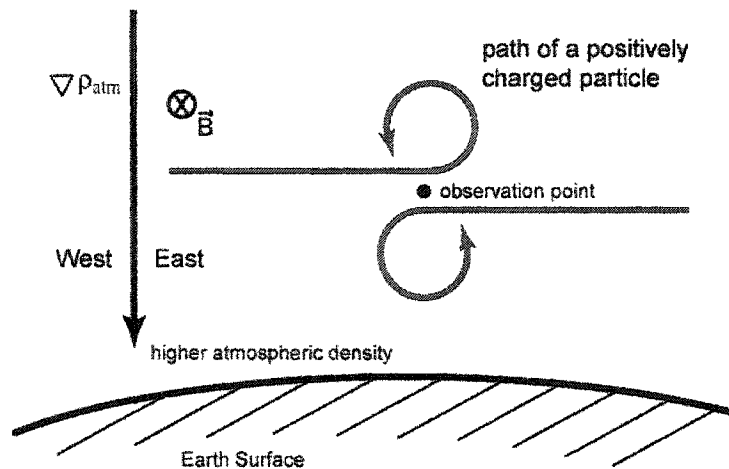


FIGURE 2.8. Illustration of the East-West Effect for Trapped Protons.

The proton flux at all altitudes varies strongly with location; this variation is often at least a factor of two, but can be as much as an order of magnitude. In addition, the trapped proton fluence is strongly affected by the solar cycle, generally exhibiting a higher magnitude during solar minimum than solar maximum. During solar maximum, the increased solar ultraviolet input causes the atmosphere to expand. As a result, more trapped protons are removed from the radiation belts around the Earth, and near-Earth space experiences a reduced proton flux during solar maximum. Hence, calculations for the maximum external radiation source in near-Earth space generally consider trapped proton flux at solar minimum. This is usually considered a conservative estimate for calculations of radiation dose, but may not be conservative in the consideration of reactor start-up using the radiation environment, in which additional neutrons are desirable.

Electrons are also trapped in the Earth's radiation belts. Electrons in the near-Earth environment cannot lead single event effects in electronic systems, but they can

provide a significant contribution to the total ionizing dose (NRL 1996). Therefore, electrons are of greater concern to manned missions than for reactor systems and electronic payloads. Trapped electrons are lower in energy than trapped protons, ranging from a few tens of keV to ~ 10 MeV (ESA/ESTEC 2002). Two flux maxima are observed for trapped electrons in the “inner” and “outer” zones, with the most energetic electrons detected in the outer zone. The inner zone extends to a McIlwain L^i value of approximately 2.4, and the outer zone from approximately L equal 2.8 to 12; the “in-between” region is denoted the “slot.” In Figure 2.9, which shows the integral trapped electron flux for greater than 1 MeV electrons as a function of altitude, the two zones of electron population are readily evident, both indicating a trapped electron flux on the order of 10^6 electrons/cm²/sec. The semi-circle on the left side of the plot represents the Earth's surface and distances are given in Earth radii.

The trapped electron environment is not a significant concern to low altitude missions because only low energy electrons are present in this region. Because they do not contribute to single event upsets in electronic instrumentation or to significant secondary neutron production in the reactor module, the effect of electrons on reactor start-up and operations will not be treated in this dissertation.

Another component of the trapped radiation field consists of trapped helium ions presumed to originate from interactions between trapped protons and atoms in the upper atmosphere. Satellite data provides information on the energy spectra of these He ions. Data collected by the Solar, Anomalous and Magnetospheric Particle Explorer (SAMPEX) satellite indicate that the spectra of these trapped ions extend to

ⁱ The McIlwain L value is a geomagnetic coordinate used to label the magnetic field lines and particle drift shells in Earth's magnetosphere. Given in units of Earth radii (6378.1 km at the equator), L is approximately the distance from the center of Earth's magnetic dipole to the magnetic field line location at the magnetic equator. Geosynchronous orbit is described by $L = 6.6$; $L = 1$ corresponds to Earth's surface.

approximately 20 MeV per nucleon, but the Combined Release and Radiation Effects Satellite (CRRES) recorded ions up to approximately 100 MeV per nucleon (NASA/GSFC 2001). The discrepancy between the SAMPEX and the CRRES data suggests that the ion fluences measured by CRRES could have resulted from a solar particle event.

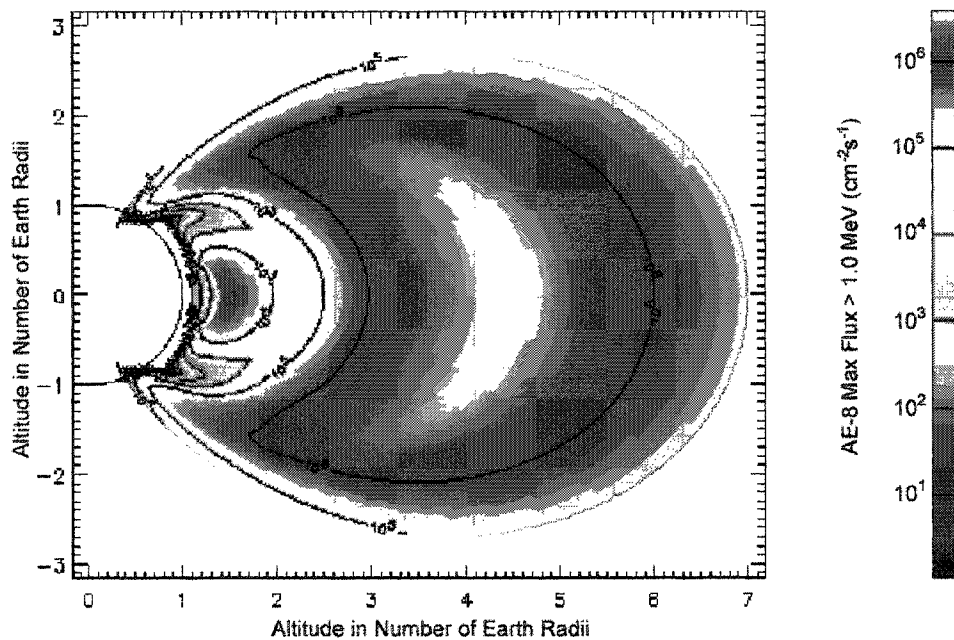


FIGURE 2.9. Greater Than 1 MeV Integral Trapped Electron Flux. Figure included with permission from (ESA/ESTEC 2002).

2.3.1.2 *Galactic Cosmic Rays*

Galactic cosmic rays (GCR) are believed to be extra-galactic in origin. They are accelerated to very high energies as they travel through the Milky Way Galaxy, with most arriving in near-Earth space as fully ionized nuclei. Some, however, arrive with a distribution of charge states, where the charge state is indicative of the origin of that particular nuclei (Tylka et al. 1997).

GCR are composed of nuclei having atomic numbers (Z) from one to 92 (hydrogen to uranium) and energies from hundreds of MeV to as high as 50 GeV/nucleon. At one astronomical unitⁱⁱ (AU), the position of the Earth, cosmic radiation is composed of approximately 83% protons ($Z = 1$), 13% alphas (helium nuclei, $Z = 2$), 1% nuclei of $Z > 2$, and 3% electrons. Outside the heliosphere, the region in space that is affected by the solar wind, the GCR flux is believed to be nearly isotropic. Within the heliosphere, however, space propagation effects result in a slight anisotropy in the GCR arrival angles of approximately 1% (Feynman and Gabriel 1996). Because the anisotropy is very small, calculations concerning GCR effects on spacecraft generally assume that the GCR flux is isotropic.

2.3.1.3 Anomalous Cosmic Rays

Anomalous cosmic rays (ACR) comprise a second cosmic ray source in space. Unlike GCR, ACR generally arrive in near-Earth space as singly ionized species of $Z \geq 2$ nuclei. Scientists believe that ACR originate as neutral interstellar gas that diffuses into the heliosphere. Particles become singly ionized by solar radiation and charge exchange and are then transported to the outer heliosphere by the solar wind. As they travel further into the heliosphere and propagate to the vicinity of the Earth, these ions are accelerated to moderate energies up to approximately 50 MeV/nucleon, significantly lower than the GCR energy spectra (Feynman and Gabriel 1996).

Some ACR become what is referred to as *trapped heavy ions*. Although they originate as singly ionized species, they lose additional electrons as they collide with particles in the upper atmosphere. These particles are not effectively deflected by Earth's magnetic field, allowing them to penetrate deep into the magnetosphere. As the ions lose

ⁱⁱ One astronomical unit is approximately equal to 1.5×10^8 km, the distance between the Earth and the Sun.

additional electrons in collisions, the ion gyroradius collapses and they become trapped in the Earth's magnetic field. The trapped ACR, however, have a very steep energy spectra, such that very few ACR exist at sufficiently high energies to generate significant secondary particle production. As a result, they contribute very little to radiation dose inside spacecraft except in the case of minimally shielded components (NRL 1996).

2.3.1.4 Neutrons

The neutron flux experienced by a space vehicle primarily derives from two components: albedo neutron flux and "local" neutron flux. The albedo neutron flux is generated by collisions of GCR ions with nuclei in Earth's atmosphere. "Local" neutrons, also referred to as "secondary" neutrons, are produced by interactions of cosmic rays and trapped protons with the spacecraft material. Neutron measurements made on the Space Shuttle, the MIR space station, and various satellites have substantiated this theory (Dudkin et al. 1996; Keith et al. 1992; Matsumoto et al. 2001; NRL 1996).

Dudkin et al. (1996) reported that measurements made on the MIR space station in 1991 typically showed a 20-40% higher neutron flux inside the spacecraft than outside due to the contribution of secondary neutrons. Interior measurements were made behind an effective shielding thickness of $\sim 40 \text{ g/cm}^2$ (Dudkin et al. 1996). The measured neutron spectra were similar inside and outside the spacecraft in the intermediate energy range (0.01 MeV – 1 MeV), indicating that neutrons in this energy range could fully penetrate the wall of the spacecraft with minimal interaction in the wall material. Comparison of the internal and external neutron measurements suggested that lower energy neutrons ($< 0.1 \text{ MeV}$) appeared to be decelerated and absorbed in the spacecraft structure.

Measurements of the neutron fluence during solar maximum were made aboard the Space Shuttle on STS-28, -36, and -31 (Keith et al. 1992), and aboard STS-89 (Matsumoto et al. 2001). Both STS-28 and -36 were high inclination, low altitude orbits

(57° inclination, 306 km altitude and 62° inclination, 246 km altitude, respectively). At these high inclinations, where fewer trapped particles exist and GCR ions can more easily penetrate to low altitudes, the measured neutron fluence primarily results from GCR induced interactions. STS-31 was a low inclination, high altitude flight (28.5°, 617 km), predominantly sampling trapped proton radiation. Because the slowing down spectra for neutrons varies as $1/E$, investigators assumed that the neutron spectra measured inside the Shuttle could be approximated by a power law distribution, proportional to $E^{-\gamma}$. The best power law fit for all three flights was $\gamma = 0.765 \pm 0.035$ (Keith et al. 1992), as indicated in Eqs. (2.20).

$$STS - 28: \quad J(E) = 8.63 E^{-0.765} \quad (2.20a)$$

$$STS - 36: \quad J(E) = 5.92 E^{-0.765} \quad (2.20b)$$

$$STS - 31: \quad J(E) = 45.1 E^{-0.765} \quad (2.20c)$$

The expressions for the differential neutron spectra apply over the entire neutron energy range that was sampled (thermal to approximately 10 MeV), where E is in MeV and $J(E)$ corresponds to the energy dependent neutron flux in units of neutrons/cm²/min/MeV.

Also note that the estimated spectra in Eqs. (2.20) correspond to the neutron spectra inside the Shuttle. Due to the altitude and inclination of each of the missions, the neutrons measured for STS-28 and STS-36 were primarily induced by GCR interactions, while the measured neutron flux for STS-31 was primarily induced by trapped proton interactions with the spacecraft. This variation in the external source for STS-31 explains the increased magnitude of the neutron flux that was measured during this mission.

By comparison to calculated neutron models by T.W. Armstrong (NRL 1996), Keith et al. (1992) determined that the neutron fluence measurements on board the Shuttle could be attributed to approximately 65% local production within the spacecraft materials, with these locally produced neutrons contributing approximately 80% of the neutron dose equivalent. Additionally, for the high inclination flights, STS-28 and -36,

the ratio of the neutron dose-equivalent to the charged particle dose-equivalent was approximately 15% and 16%, respectively. The low inclination flight, STS-31, produced a much lower neutron to charged particle dose-equivalent ratio of 6%. Other studies have returned ratios from 14% to 38%. The discrepancy in the ratio of the reported dose-equivalents likely resulted from a failure to correct the neutron fluence data for proton-induced reactions (Keith et al. 1992). Overall, the relatively small ratio of neutron to charged particle dose-equivalent indicates that charged particles are more prevalent in the interior environment of the Shuttle than neutrons, or that the charged particles have a higher quality factor (thereby contributing a greater dose). Given thicker spacecraft materials having greater cross sections for neutron production by interaction with charged particles, as in a reactor system, the local neutron production in the spacecraft materials are expected to increase over that observed in primarily aluminum spacecraft. Hence, secondary neutrons produced by interactions of the external radiation environment in the reactor materials could impact reactor operations. The work presented in this dissertation will quantify this effect and will present a control strategy to mitigate potential effects of the external environment.

2.3.1.5 Solar Cycle Effect on Particle Fields

The solar cycle, which has an approximate eleven year period, can significantly influence the space radiation environment. It is sometimes referred to as a 22-year cycle because the polarity of the solar magnetic field reverses every 11 years, producing what is referred to as “even” and “odd” solar cycles. The period of high solar activity, known as solar maximum, extends approximately seven years. The “quiet” sun period, or solar minimum, extends the remaining four years of the solar cycle.

During solar maximum, the solar wind is more dynamic. The solar wind carries energetic particles from coronal eruptions, and “magnetic clouds” are produced in the solar wind due to embedded magnetic fields (NASA/MSFC 2004). The increased solar

wind during solar maximum is more effective at impeding GCR penetration into the solar system than during solar minimum. Hence, the GCR source is inversely correlated to the level of solar activity. When solar activity is high, the GCR intensity at the Earth is decreased because GCR access to near-Earth space is diminished. The cyclical variation in the GCR fluence is most pronounced at low particle energies, becoming rather insignificant at energies greater than 10 MeV per nucleon. ACR fluence can vary by as much as a factor of 500 from solar minimum to maximum, with the higher fluence being observed during solar minimum (Tylka et al. 1997).

During solar events, the solar proton fluence in Earth orbit increases, but the trapped particle radiation flux decreases in response to a variation in the magnetic fields and atmospheric expansion. As a sum result of these effects, the trapped proton source can decrease by approximately a factor of two during solar maximum. Atmospheric expansion removes more trapped protons from the radiation belts due to the increased energy loss and scattering in the residual atmosphere.

Time Lag Associated with Solar Modulation

Multiple complexities make it difficult to generate models of the variation in the particle fluence as a function of solar activity. A significant time lag exists between an observed increase in solar activity and the subsequent effect on particle fluence. It takes approximately one year for the solar wind to propagate to the edges of the heliosphere, so the effect of the increased solar wind is spread out over this period. The individual ion rigidity (the momentum per unit charge) also effects particle fluence. Ions with very high rigidity are less effectively scattered, and are thus better able to penetrate the heliospheric magnetic field regardless of solar activity levels (Tylka et al. 1997).

The time lag associated with the solar cycle is not constant; further variations reflect whether the solar cycle is odd or even, and whether the cycle is in the ascending or descending phase (Nymmik et al. 1996). The general conclusions presented on solar

cycle effects apply to positively-charged particle fluxes. In most cases, increased lag is observed during odd cycles as compared to even cycle observations. During even cycles, decreased lag is noted as the particle rigidity increases, but this appears to have no effect during odd cycles. To further complicate solar cycle effects, the lag mode is reversed during the initial stages of a cycle (lag time is increased or decreased as compared to the remainder of the cycle), which includes approximately the first 1.3 years after solar minimum. The lag time is also reduced during the ascending phase of a cycle as compared to the descending phase (Nymmik and Suslov 1995). Because there are so many complexities involved in tracking the variations in the particle fluence as a result of the solar cycle, models generally do not take all of these parameters into consideration. Instead, most models provide simple estimates of the radiation environment at solar maximum and solar minimum without including the additional complexities described above. Details of the models applied in the present work are provided in Chapter 3.

Solar Particle Events

For energies less than 100 MeV, the interplanetary proton fluence is dominated by solar protons that derive from solar particle events (SPEs). Particles emitted during solar events can have intermediate energies (~ 10 keV/nucleon) to very high energies (GeV/nucleon), and solar events can last from hours to days. During an SPE, the background radiation level can increase several orders of magnitude above nominal conditions. There are two primary types of solar events, namely “impulsive” events and “gradual” events (Kallenrode 2003). Impulsive solar energetic particles are accelerated in solar flares and are dominated by electron flux, but they may also be identified by a marked enhancement of heavy ions and result in a smaller proton flux than is observed for gradual events. Gradual events are driven by Coronal Mass Ejections (CMEs). CMEs involve very high energy particles and can significantly affect satellite communications and electronics. General classification parameters for SPE’s are the e^-

$/p^+$ ratio and the p^+/He ratio, both of which are higher for gradual events. More specifically, an impulsive event demonstrates soft x-ray duration of less than one hour, with a decay constant of less than 10 minutes, and hard x-ray duration of less than 10 minutes. Conversely, a gradual event demonstrates soft x-ray duration of greater than one hour, with a decay constant greater than 10 minutes, and hard x-ray duration of greater than 10 minutes (Kallenrode 2003).

Iron, carbon, nitrogen, and oxygen make up the majority of heavy ions present in solar events. The heavy ion component in solar events is generally noted by the iron to oxygen (Fe/O) ratio. An impulsive event has an Fe/O ratio of approximately one, but it is only 0.1 for gradual events (Feynman and Gabriel 1996). Solar heavy ions usually are not fully ionized; their charge states are characteristic of the coronal plasma that produced the event.

A recent, major solar flare (CME) occurred on Tuesday, October 28, 2003, with detected increases in the solar particles reaching the Earth on October 29, 2003. Rated as an “X17”, where “X” denotes a major flare and the number corresponds to the intensity of the flare, the October 2003 event ranks as the third largest X-flare on record, behind events occurring in 2001 and 1989 (Britt 2003b). The effect of solar storms on the Earth and orbiting satellites and spacecraft is highly dependent on the direction in which the flare is emitted. The greatest solar storm on record occurred in 1859 and was directly aimed at the Earth; this storm caused telegraph wires to short out and ignited fires in the U.S. and Europe (Britt 2003a). The recent coronal mass ejection (CME) in October 2003 caused a Japanese communications satellite to shut down, but no other major effects were noted. Had this CME been lined up more directly with the Earth’s magnetic field, it could have possibly been one-third the strength of the 1859 event. Figure 2.10 shows a photograph taken during the solar particle event by the Solar and Heliospheric Observatory (SOHO) spacecraft (Britt 2003a).

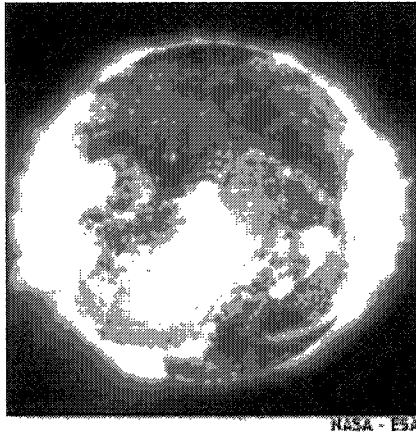


FIGURE 2.10. Solar Particle Event, October 28, 2003.

2.3.2 Jovian System

The Jovian system is one of the potential destinations for a mission using a nuclear electric propulsion system. An NEP vehicle is power rich; the increased power level over a state-of-the-art chemical propulsion system would enable extended orbital studies as opposed to quick fly-by missions that are enabled by traditional chemical propulsion systems. Additionally, NEP systems provide significantly more power to drive scientific instrumentation to obtain, process, store, and transmit mission data to Earth for later analysis. The radiation environment in the Jovian system is significantly more intense than that experienced in near-Earth space. Although reactor startup would occur long before reaching the Jovian system, the reactor would be expected to provide steady state operation in this environment and the capability of the reactor to restart after an unplanned shutdown should be evaluated.

The Energetic Particle Detector (EPD) aboard the Galileo spacecraft collected data on the energetic particle population in the Jovian environment, including electrons, protons, alpha particles, and high Z particles (NASA/GSFC 2001). From 1995 to 2001,

measurements were taken in the Jovian system, including measurements within the Jovian atmosphere and encompassing the orbits of most of Jupiter's moons (from the top of Jupiter's atmosphere to five Jovian radii). Much of this data is still being processed. The Galileo Interim Radiation Electron (GIRE) Model was developed at the Jet Propulsion Laboratory (Garrett et al. 2003) from the EPD data. At present, this model only takes into account trapped electrons. Additional modeling of the trapped proton environment from the EPD data has also been completed, although it had not been compiled into a public-use program similar to GIRE at the time this document was composed. Data on the trapped proton environment was obtained directly from the Jet Propulsion Laboratory (Jun 2003). Because the fully processed Galileo data currently only considers trapped particle flux, Figure 2.11 reveals no significant proton flux at high energies. Note that each curve corresponds to the altitude at which the data apply, in units of Jovian radii ($R_J = 71,400$ km). This upper region of the spectra would be populated by GCR that reach the Jovian system.

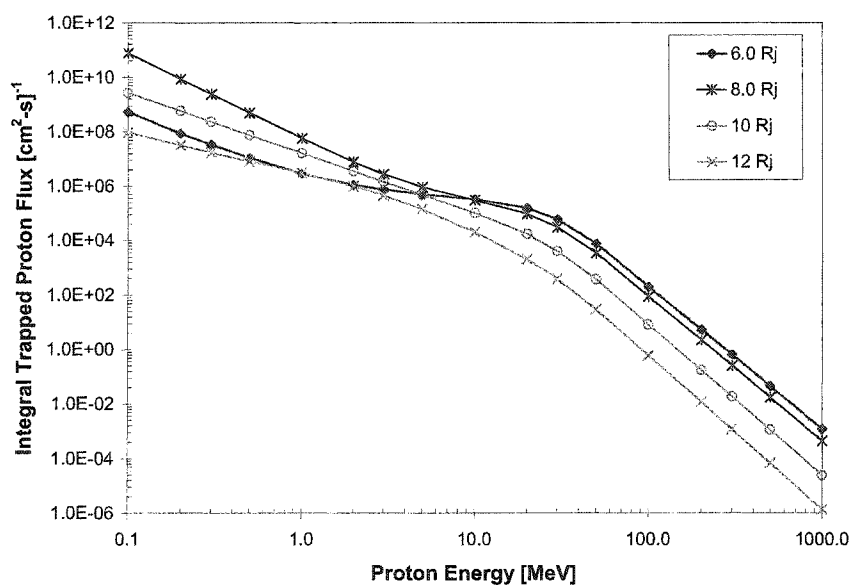


FIGURE 2.11. Jovian Integral Trapped Proton Flux as a Function of Altitude.

The Galileo radiation models are currently being expanded to include cosmic ray contributions to the radiation environment, which will extend the proton spectrum to higher energies. The format of GIRE and other EPD data allows the user to insert a specific mission profile, then calculates a radiation spectrum for each portion of the mission (e.g., Ganymede to Europa transfer, Europa spiral down, 30 days at Europa, etc.). At present, the GCR environment near Jupiter can be approximated as the interplanetary radiation environment, which can further be approximated by the radiation environment in geosynchronous orbit. This component can then be added to the trapped proton environment to calculate an estimate of the proton flux in the Jovian system.

CHAPTER 3

COMPUTATIONAL RESEARCH: MODEL DEVELOPMENT

This chapter provides the theoretical basis for the computational models applied in this dissertation. Experimental applications will be treated separately in Chapters 5, 6, and 7. The present discussion will include an introduction to the modeling techniques used to describe the space radiation environment, the radiation transport calculations applied to determine the externally produced (secondary) neutron population in a reactor core, and the development of a control methodology for reactor start-up.

3.1 Modeling the External Radiation Environment

Several computer codes have been developed to model and predict the radiation field in near-Earth space. In general, these codes are based on some set of experimental data collected by various spacecraft. Extrapolations are made to estimate the environment at future times, and extrapolation from the existing data sets provides estimates of the elemental compositions that were not sampled directly (e.g., measurements made over $Z = 1 - 28$ may be extended to higher Z ions by extrapolation). Considerably less data is available for the interplanetary and outer planetary radiation environments, but some spacecraft data is available to make a rough assessment of these environments (NASA/GSFC 2001).

Available modeling codes for the space radiation environment generally provide integral averages over relatively long periods of time (e.g., a common flux unit for reporting space radiation data is particles/cm²/day). However, the individual particle flux

can vary significantly over relatively short periods of time (e.g., hours), and integral averages may provide inaccurate estimates of the effect of the space radiation over these shorter periods. The start-up procedure for a typical terrestrial reactor can take minutes to hours, depending on reactor type, reactor size, reactor control mechanisms, the available neutron source, and safety precautions established for start-up procedures. The SNAP-10A that was flown in April 1965 achieved criticality after a period of approximately seven hours. Environment data with improved time resolution should allow for improved accuracy in determining the potential effect of the space radiation environment on the reactor start-up procedure. Although it is impossible to predict the exact fluence that the reactor will experience on orbit during start-up, it is possible to select models that are indicative of “best case” and “worst case” scenarios.

3.1.1 Trapped Particle Models: AP8 & AE8

Developed by NASA Goddard Space Flight Center (GSFC), AP8 is an empirical model of the omnidirectional trapped proton flux in Earth’s magnetosphere. The omnidirectional model averages particle flux over the entire range of arrival directions (4π steradians), but does not imply that the flux is equivalent for all arrival directions (NASA/GSFC 2001).

AP8 models protons in the energy range 0.1 to 400 MeV, at McIlwain L values of 1.15 to 6.6. The proton fluence is tabulated as a function of the geomagnetic coordinates L and B/B_0 , where B_0 and B are the magnitudes of the magnetic field at the magnetic equator and at the location of the particle of interest, respectively. Anisotropies in the proton fluence are ignored in the AP8 model. It is generally assumed that the trajectories of typical vehicles at low altitudes cancel the anisotropy effects. Most space vehicles are continuously rotating along their trajectory, such that the proton fluence is “evened-out” about the periphery of the vehicle. At low altitudes, most of the daily dose occurs over

the Eastern South American region. Called the South Atlantic Anomaly, elevated flux in this region results from a dip in the local magnetic field. The integral nature of the data output from AP8, however, tends to reduce the orbital effect of the South Atlantic Anomaly, in addition to averaging out daily variations in the proton flux. The fluence at all altitudes varies strongly with location by more than a factor of two, and can vary by as much as an order of magnitude.

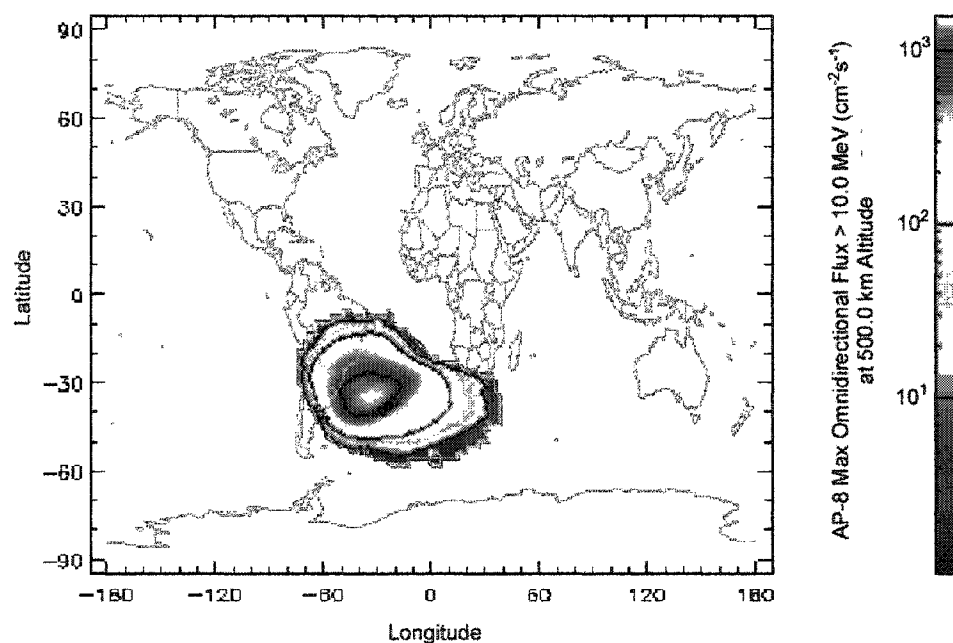


FIGURE 3.1. Trapped Proton Environment at 500 km Altitude During Solar Maximum. Figure included with permission from (ESA/ESTEC 2002).

To a limited extent, the AP8 model incorporates variation due to solar activity. The AP8MAX module calculates proton fluence at solar maximum; AP8MIN calculates proton fluence at solar minimum. However, no interpolation is made for conditions between solar extremes. Because the model data is generated using long-term averages, the representations at each extreme may not be highly accurate. As discussed in Chapter 2, trapped proton fluence is generally higher during solar minimum, so conservative calculations for radiation dose considerations should be performed for solar minimum

conditions. It is unclear whether “conservative” calculations for reactor start-up should consider maximum or minimum flux conditions. Additional neutrons due to increased particle interactions in the reactor materials actually increase the probability of initiating a sustainable chain reaction in a given amount of time, but increased radiation could also induce single event effects (SEE) in electronic equipment, negatively impacting vehicle operation.

Although the AP8 model was developed more than 20 years ago, based on satellite data from the early 1960s to the mid 1970s, it remains the standard in space vehicle analysis for trapped protons in near-Earth space. On-orbit measurements confirm that the omnidirectional, orbit-averaged proton flux predicted by AP8 is within a factor of two of the actual quantities measured on orbit (NRL 1996). Thermoluminescent dosimeter measurements on the Long Duration Exposure Facility (LDEF), which orbited with an inclination of 28.5° and an altitude of 478 km from April 1984 to January 1990, primarily during solar minimum, indicated that the AP8 model was systematically low by a factor of two. Shuttle measurements indicated over- or under-prediction of the proton fluence by AP8 by up to a factor of two. The greatest uncertainties in the AP8 model exist for relatively low altitudes (250-1000 km, $L < 1.5$) and low proton energies ($< \sim 3$ MeV) (NRL 1996).

3.1.2 Ionizing Radiation Environment Models: CREME96

Developed by the Naval Research Laboratory, Cosmic Ray Effects on MicroElectronics (CREME) was first released in 1981 and was updated to its current form in 1996 (NRL 1996). CREME96 can be used to generate numeric models of the ionizing radiation environment in near-Earth or geosynchronous orbit and then, as the name suggests, to assess the potential effects of the radiation on microelectronics aboard the spacecraft. Code developers were primarily concerned with particles that can result

in single event effects (SEE), which can be catastrophic to electronics. In its initial release, CREME modeled the solar cycle as a simple sinusoidal solar modulation with an 11-year period. Studies have shown this model to be over-simplified, contributing to inaccurate predictions of the GCR fluence. CREME96 rectifies this error by incorporating more accurate models of the solar variation derived from observations and measurements. The mean discrepancy in the results obtained from CREME96 and the earlier CREME version is approximately 25%.

Data included in CREME96 are based on measurements made at one astronomical unit (1 AU, Earth orbit), and are generally assumed to be accurate up to 1.5 AU (Mars orbit). The CREME96 models are not applicable to inner heliosphere or deep-space missions. For a nuclear electric propulsion (NEP) mission to the outer planets, several modeling tools would need to be employed to fully model the space radiation environment encountered in the mission, during all stages of reactor operation (start-up, steady state operation, and shut-down).

For trapped protons, CREME96 incorporates the AP8 model. These data are generated in the trapped particle (TRP) module within CREME96, which allows the user to select the AP8MAX or AP8MIN model. Trapped electrons are not included in any of the CREME96 modules. The purpose of this code is to predict the effect on microelectronics; neglecting the effects of electron interactions is justified because they do not contribute to SEE in electronic components.

The ACR model incorporated in CREME96 is primarily based on data collected by the Solar, Anomalous and Magnetospheric Particle Explorer (SAMPEX) satellite, in addition to other satellite data and historical records of ACR measurements. SAMPEX was launched on July 3, 1992 on a mission to study particle energies, charge states, and composition within Earth's magnetosphere, the solar wind, and cosmic rays, maintaining a nearly polar orbit with an 82° inclination at an altitude of 520 km (perigee) by 670 km (apogee). Trapped ACR contributions are not included in CREME96. Because the

trapped ACR energies are generally too low to penetrate typical minimum satellite shielding, neglecting the trapped ACR introduces very little error to the predicted radiation environment and its effects.

The GCR model included in CREME96 is based on a semi-empirical model developed by Nymmik et al. (1992). This model incorporates a semi-empirical correlation between the observed sunspot numbers and the GCR flux level, as a function of the historical record. The correlation relies on the basic assumption that the pattern of solar modulation repeats in a cyclic fashion. This assertion is supported by the historical record, which reveals relatively predictable variation of the GCR intensity with solar activity up to 10 GeV per nucleon.

The space radiation environment includes all naturally occurring nuclides, ranging from an atomic number of one to 92, having energies from less than 1 MeV to as high as 10^{15} MeV (total energy). However, the GCR model developed by Nymmik et al. (1992) only included nuclides having $Z \leq 28$, although higher Z elements can be important to the measured dose (Nymmik et al. 1992). CREME96 extends this model to $Z = 92$ by applying the measured abundances of the ultra-heavy components of GCR relative to iron ($Z = 26$). In some cases, individual elements have not been experimentally resolved; in this case, the model extension is augmented by theoretical estimates.

Geomagnetic transmission describes the ability of a charged particle from interplanetary space to penetrate Earth's magnetic field and to reach a point inside the magnetosphere. The particles capable of reaching the interior of the magnetosphere may be encountered by spacecraft in low Earth orbit. CREME96 provides pre-calculated transmission functions for two common low Earth orbits: the International Space Station orbit, having a 51.6° inclination at an altitude of 450 km, and the nominal Space Shuttle orbit, having a 28.5° inclination at an altitude of 450 km. The pre-calculated transmission functions account for the solid angle obstructed by the Earth at the position of interest, and the predicted particle fluence is averaged over all arrival directions.

CREME96 incorporates two models for the geomagnetic “weather” conditions. The “solar quiet” model assumes that all fields are present at nominal levels, with the output reflecting long-term averages of the particle fluences. The “stormy” model assumes that disturbances exist in the magnetosphere, decreasing the magnetic shielding of the Earth and increasing the ability of cosmic rays to access near-Earth space. Stormy geomagnetic conditions provide an estimate of the maximum radiation environment in near-Earth space.

Solar energetic particles are also modeled in CREME96. CREME96 applies a model developed by the Jet Propulsion Laboratory (JPL) in 1991 (JPL91) to estimate the solar proton fluence in a particular orbit. JPL91, however, does not incorporate a model of the solar heavy ion fluence, nor do other equivalent models exist to estimate the heavy ion contributions from solar activity. Instead, comprehensive modeling codes such as CREME96 generate the solar heavy ion fluence by scaling from the JPL91 proton fluence using energy-dependent average relative abundances. Although this provides some estimate of the contribution due to solar heavy ions at a given time, these models have been demonstrated to be poor indicators of high-energy, heavy ion fluence due to solar activity.

CREME96 incorporates three models of solar energetic particle events (SPE). During an SPE, commonly referred to as a solar “flare,” increased particle fluence is observed in near-Earth space. The solar event models included in CREME96 are based on the SPE that occurred in October 1989. The “worst week” model averages the maximum fluence observed over a 180 hour (7.5 days) period from October 19-27, 1989. The “worst day” model averages fluence over an 18 hour period on October 20, 1989. The “peak flux” model focuses on a 5 minute period on October 20, 1989 during which the highest fluence was recorded.

Some software packages now contain solar particle models based on observations made by the Combined Release and Radiation Effects Satellite (CRRES) satellite during

the March and June 1991 solar events; although the trapped proton and electron models developed from the CRRES data (CRRESPRO and CRRESELE) are not applied in CREME96, the data used to construct the CRRES models can be used to assess the validity of the models that are applied by CREME96. CRRES was launched July 25, 1990 into a geocentric orbit with apogee 33,700 km (near geosynchronous orbit) and perigee 350 km. The satellite was instrumented to measure the heavy ion environment within its orbit. Near the end of March 1991, a major solar particle event occurred that generated a very large heliospheric modulation of the cosmic ray intensity. At its most extreme point, the intensity of cosmic rays at 1 AU having energies greater than 3 GeV were less than 30% of the solar minimum intensity (DuVernois et al. 1996), indicating that the increased flux of solar particles effectively screened out cosmic rays from the inner magnetosphere.

3.1.3 Model Accuracy: Comparison to Satellite Data

Gusshoven et al. (1991) of the U.S. Air Force Phillips Laboratory compared measurements of the proton and electron dose measured by the CRRES satellite with predictions made by the standard radiation models used for satellite and spacecraft design. In particular, data were compared to the NASA-developed AP8 and AE8 predictions of the trapped proton and electron fluences. To generate the models, data was averaged over long periods of time, resulting in large uncertainty in the prediction of particle fields at any specific time. In fact, the stated applicability of the AP8 and AE8 models is only for periods exceeding six months (Gussenhoven et al. 1991).

Overall, better agreement was observed between measured data and model predictions for the dose due to protons than electrons. Inner belt protons ($< 1.8 R_E$) are believed to be stably trapped, exhibiting little variability with solar activity (less than a factor of two over the 11-year solar cycle). On the other hand, outer zone electron

fluence can abruptly vary by several orders of magnitude, making electron fluence much more difficult to predict. Because electrons do not contribute to secondary neutron production, the inaccuracy of the AE8 electron predictions is not important to the present work.

Prior to the solar event recorded by CRRES in March 1991, generally good agreement was observed between the 1-10 MeV measured proton dose and the predictions made by AP8MAX; the maximum discrepancy was a 14% over-prediction of the dose due to higher energy protons by the AP8MAX model. During the solar event, however, a second proton belt formed in the outer region, 1.8 to 2.6 R_E ; prior to the event, a proton belt only existed in the inner region ($<1.8 R_E$). In this case, although relatively good agreement was still observed at less than 1.8 R_E , the model under-predicted the proton dose by as much as 60% due to its failure to predict the second proton belt. The model continued to under-predict the proton dose measured by CRRES by a maximum of 24% after the solar event. Data was reported by CRRES until communication with the satellite was lost on October 12, 1991, presumably due to an onboard battery failure (NASA/GSFC 2001).

3.1.4 Jovian Models

As discussed in Chapter 2 (Section 2.3.2), data on the Jovian radiation environment is incomplete at present. The trapped proton environment has been approximated from measurements taken by instruments onboard the Galileo spacecraft, such as the Energetic Particle Detector (EPD). Processed data from JPL provides an approximation for the trapped proton environment encountered in the Jovian system for a potential mission, including transfer from Ganymede to Europa, spiral down to a stable orbit around Europa, and 30 days spent in orbit about Europa. The approximate trapped environment along these trajectories is shown in Figure 3.2. Note that each data point

represents a sum of the proton fluence over an energy bin, where the point is located at the upper bound of the bin. For instance, the point plotted for 10 MeV includes all protons having energy (E_p) greater than the upper bound of the previous energy bin (5 MeV), such that $5 \text{ MeV} < E_p \leq 10 \text{ MeV}$.

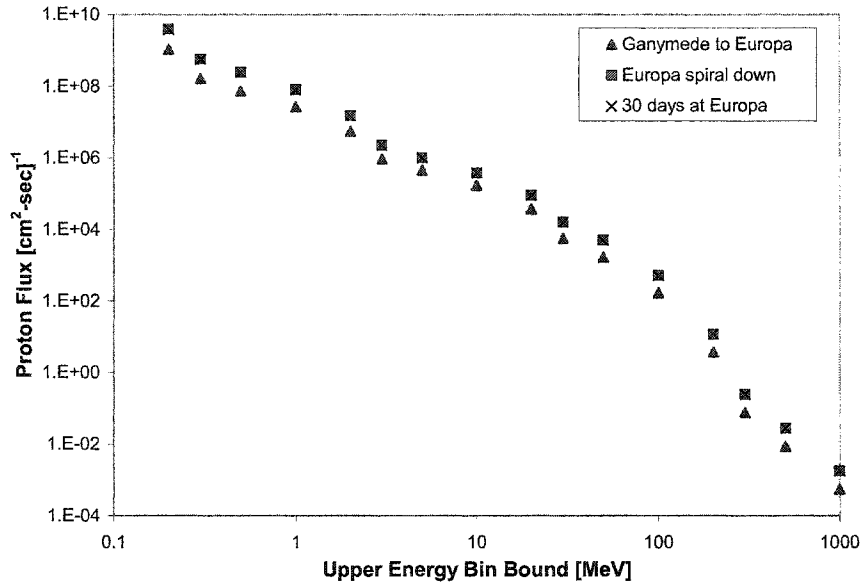


FIGURE 3.2. Trapped Proton Fluence in the Jovian System, as Measured by the Galileo Energetic Particle Detector.

The trapped proton environment at Europa was selected to assess the potential affects on reactor operation. The high energy portion of the radiation spectra would be filled primarily by contributions from GCR and solar particles. These additional contributions from the interplanetary flux can be approximated by the radiation environment at geosynchronous orbit about the Earth. Figure 3.3 shows the trapped proton flux at Europa (compiled from Galileo data) and the approximate total proton flux at solar minimum (as predicted by CREME 96) along with a comparison to the extreme environment at geosynchronous orbit during a solar particle event (peak 5 minute SPE) and the proton flux at the 450 km, 28.5° inclination orbit about the Earth. The model results indicate significantly higher proton flux in the Jovian system during solar

minimum than even predicted for the extreme SPE environment in geosynchronous orbit. Although the primary increase was in the low energy region for trapped protons (recalling that the untrapped component was approximated by the geosynchronous environment during solar minimum), this significant increase can lead to additional secondary particle production in the reactor module that could impact reactor operation. Results calculated using MCNPX to determine the potential effect of this environment on reactor operation are presented in Chapter 4.

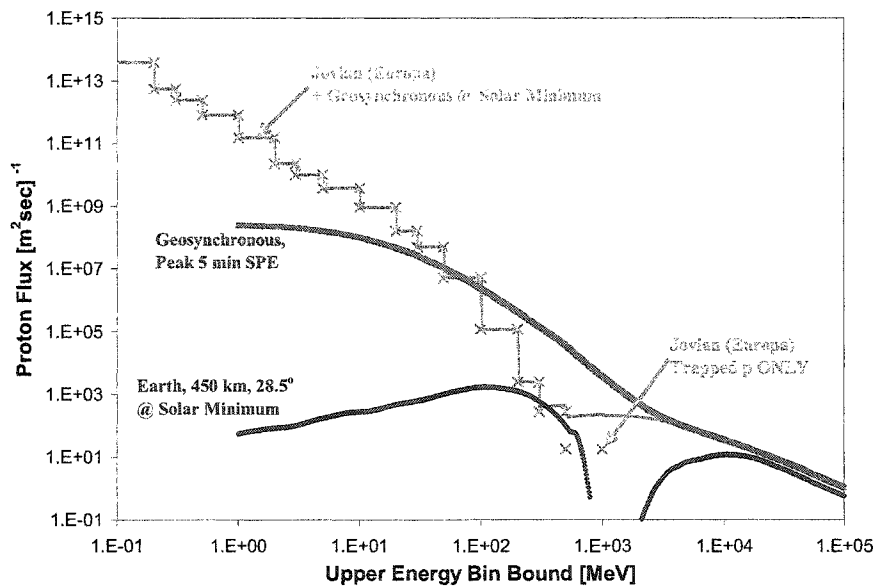


FIGURE 3.3. Approximate Radiation Environment at the Jovian Moon Europa, as Compared to Near-Earth Orbit.

3.2 Radiation Transport Models

Various codes have been developed to study the transport of radiation through materials. Approaches to radiation transport include deterministic methods, as in the CREME96 and HZETRN transport models, and statistical methods, as applied in MCNP and MCNPX.

3.2.1 Deterministic Methods: CREME96 Transport Module

The nuclear transport module included in CREME96 incorporates a numeric solution to the one dimensional continuity equation, accounting for nuclear fragmentation and ionization energy loss using the continuous slowing down approximation (CSDA) (NRL 1996). In the CSDA, one treats energy losses by atomic collisions as a continuous, averaged process, even over relatively small material dimensions (Duderstadt and Hamilton 1976). The transport routines include stopping power and range-energy routines, as well as semi-empirical energy-dependent nuclear fragmentation cross-sections. Although it would be convenient to utilize the transport module included in the same program applied to approximate the radiation environment, these transport calculations were incorporated with a very different goal in mind. The primary purpose of CREME96 is to assess the effect of the ionizing radiation environment on microelectronics, rather than the effect on the dynamics of a nuclear reactor. Hence, there are significant assumptions made within the CREME96 transport module that render it inadequate in the present application.

First, the CREME96 transport module assumes that no neutrons are produced as incident particles interact in the spacecraft shielding. For reactor calculations, secondary neutron production is the primary concern in studying the interaction of the space radiation with the spacecraft. In measurements of the differential neutron energy spectra both inside and outside a satellite, Dudkin et al. demonstrated that the secondary neutrons generated by particle interactions in the spacecraft materials contributed approximately 50% of the total neutron dose, which represented 20-30% of the total dose equivalent measured inside the spacecraft (Dudkin et al. 1990). The remaining neutron dose was provided by albedo neutrons, generated by GCR interaction with the Earth's atmosphere.

Second, the CREME96 transport module does not track target fragments. Again, these fragments could result in significant particle production or may decay to more stable forms, potentially resulting in neutron production.

Third, the only material provided as a transport medium in CREME96 is aluminum. Although aluminum is the primary material used in spacecraft shielding, various materials are used within and surrounding a nuclear reactor, and particle interactions with these materials could result in more significant reactions than particles slowing down in aluminum.

Finally, silicon is the only target material provided in CREME96. A common element in microelectronics, silicon is not a component of interest in an NEP system. Although electronic systems (i.e., radiation detectors, reactor monitoring equipment, control modules, etc.) are included in the design of an NEP system, and damage to any of these components can ultimately affect the reactor operation, production of secondary radiation fields in other target materials are of greater interest in studying the effect of space radiation on reactor start-up and operation. Due to the arguments presented here, the CREME96 transport module was not applied in any calculations associated with the effect of the space radiation environment on reactor start-up and control.

3.2.2 Deterministic Methods: HZETRN

HZETRN, an acronym that refers to the transport of high energy, high Z ions (HZE), is a deterministic code based on an approximate solution to the Boltzmann equation. Developed at the NASA Langley Research Center (LaRC), HZETRN integrates the galactic heavy ion transport code GCRTRN and the nucleon transport code BRYNTRN into a single package (Wilson et al. 1995; Wilson et al. 1996). Public access to a limited version of HZETRN is available through the Space Ionizing Radiation Environment and Shielding Tools (SIREST) website, maintained by the NASA Langley

Research Center (NASA/LaRC 2002). SIREST provides environment models for near-Earth space, specifically for past Space Shuttle missions and for the International Space Station; for the Martian environment; and for the Jovian trapped particle environment as determined by the Divine-Garret model constructed from data collected by the Pioneer and Voyager spacecraft (Divine and Garret 1983).

The cosmic ray model employed in HZETRN was developed by Badhwar and O'Neill, which incorporates a mathematical description of the differential energy spectra of GCR as a function of solar activity (Badhwar and O'Neill 1992; Badhwar and O'Neill 1996). This model utilizes the diffusion-convection theory of solar modulation and develops a fit to observations of GCR fluence made from 1954 to 1989 to predict the GCR intensity to a root mean square error of $\pm 10\%$. The most recent rendition of the model also accounts for the polarity of the interplanetary magnetic field (Badhwar and O'Neill 1996).

Interactions in HZETRN are modeled using the straight-ahead approximation. This approximation assumes that secondary ions are produced only in the direction of motion of the primary ions (Wilson et al. 1991). Space Shuttle and International Space Station (ISS) models are currently included in SIREST, and a catalog of past missions is provided to study dose equivalents given specific orbital trajectories (includes orbital inclination, apogee, and perigee) at specific times. At present, no other complex geometries can be input to HZETRN via the SIREST interface.

As particles traverse a material, HZETRN applies the abrasion/ablation model to determine what secondary particles are produced and their corresponding trajectories, as well as the primary particle trajectory, following a collision. In the abrasion/ablation fragmentation model, a relativistic projectile nuclei collides with a stationary target nuclei. In the 'abrasion' portion of the interaction, the overlapping portions of each nuclear volume (projectile and target) are sheared away by the collision. The remaining projectile, referred to as the pre-fragment or primary residue, continues along its

trajectory with essentially the same velocity as it had prior to the collision. The pre-fragment is a highly excited nuclei; hence, it will decay by emission of a gamma ray or other nuclear particle, with the decay dominated by heavy particle emission. The latter process in the interaction is referred to as ‘ablation’, and the resultant isotope is referred to as the secondary product.

The transport calculations included in HZETRN incorporate LET dependent quality factors and includes dose contributions from neutrons, protons, alphas, HZE, target nuclear fragments, primary particles, and secondary particles. As in all cases when simplifying assumptions are required, some shortcomings are introduced. For instance, although the nucleon-nucleus cross sections included in HZETRN are fully energy dependent, the nucleus-nucleus cross sections are not. Another simplifying assumption is required in the tracking of secondary particles. For HZE projectiles, the secondary particles are assumed to have the same velocity as the primary particle. Although this is not purely accurate, it is a conservative assumption for neutrons produced in HZE fragmentation for dose considerations. HZETRN was developed with the primary goal of predicting the dose in the interior of a spacecraft due to the external radiation environment, in which “conservative” estimates yield a maximum potential dose. Conversely, a “conservative” approach to predicting the neutron production in reactor materials may be an underestimate of the actual production. In reactor start-up, too few neutrons can have a negative impact on the time required to generate a sustainable chain reaction. All totaled, the computational simplifications made in HZETRN account for an approximate 15-30% underestimate in dose predictions (Wilson et al. 1991).

HZETRN has been shown to provide relatively accurate dose equivalent estimation inside the Space Shuttle and ISS, as much of the shielding material is aluminum or materials that do not generate significant secondary particle fluence (e.g., water) (Wilson et al. 1995). However, it is uncertain how accurate results may be for more complex space vehicles incorporating a variety of materials that may undergo

complex interactions with the incoming particles. A recent upgrade to HZETRN included the development of a neutron transport algorithm that incorporated both elastic and nonelastic particle interaction processes. The Monte Carlo codes LAHET and MCNPX were used as benchmarks for the accuracy of the newly implemented neutron transport algorithm, which uses a bidirectional multigroup solution to the straight-ahead Boltzmann equation, given an aluminum shield and a water target of varying thicknesses. Comparison to Monte Carlo results revealed that HZETRN underpredicted the neutron fluence in the energy range 5-200 MeV. Inclusion of an estimate of the isotropic neutrons produced at each energy improved the predictions, but more accurate absorption cross sections remain to be added to the code (Clowdsley et al. 2000).

The capabilities of HZETRN with regard to the transport of high Z particles are intriguing. The available statistical transport codes, such as MCNPX, cannot track high Z particles due to the lack of detailed cross section data for these interactions. The current publicly available version of HZETRN, however, does not allow for user-defined geometries to be incorporated into the analysis, rendering it inadequate for application to reactor start-up calculations. While available Monte Carlo codes do not provide high Z transport, they do remedy the failure of SIREST to allow user-defined geometries and provide a mechanism to perform criticality calculations in addition to radiation transport.

3.2.3 Statistical Codes: MCNP / MCNPX

Monte Carlo N-Particle (MCNP) is a “general-purpose, continuous-energy, generalized-geometry, time-dependent, coupled neutron/photon/electron Monte Carlo transport code” (LANL 1997). MCNP can be employed to transport particles through complex, user-defined three dimensional geometries composed of various materials to determine particle fluence or dose equivalents at specified locations in the problem geometry, or to compute criticality parameters (e.g., k_{eff} eigenvalues) for a fission reactor.

While deterministic methods generally solve the transport equation for the average particle behavior, Monte Carlo is a statistical code that simulates individual particle transport and interactions, recording aspects of their average behavior. Using transport data, the probability distributions governing each event are statistically sampled to describe the total phenomenon, where statistical sampling is performed based on the selection of random numbers. Events for an individual particle might include scatter, absorption, leakage, capture, or fission; additional particle production may also result from interaction events. The average behavior of the simulated particles is then used to infer the behavior of particles in the physical system. Increasing the number of simulated particles, or particle “histories,” decreases the error in predicting the true system response. MCNP is quite versatile, offering the user freedom in the geometry description and material selection, a large range of source definitions, multiple tally options, geometry and output tally plotters, variance reduction schemes, and an extensive resource of cross section data.

Originally developed for computational studies of nuclear reactors and for shielding applications, MCNP and MCNPX both incorporate a “kcode” module for computing criticality parameters. Specifically, the kcode module calculates the effective multiplication factor (k_{eff}) in a fissile system, where the multiplication factor is defined as the ratio of neutrons produced in successive generations. One generation in the simulation is defined as the life of a fission neutron from birth by fission to death by escape, parasitic capture, or absorption that leads to fission. A criticality calculation often includes a fission source, which is defined on the “ksrc” card in an MCNP or MCNPX input deck. This option places fission source points at specified locations, where at least one source point must be located within fissile material. In the first k_{eff} cycle in a kcode run, a specified number of particles are emitted isotropically with a fission energy distribution from the specified source points (Harmon et al. 1994). Subsequent cycle calculations are dependent on the neutrons produced by fission in the

defined geometry. The ability to compute criticality parameters sets MCNP and MCNPX apart from the other transport codes that were considered in the present application.

Developed outside of the MCNP development path, MCNPX extends the capability of MCNP to all particles and energies. MCNPX has the capability of tracking up to 34 particle types, including protons, deuterons, tritons, and alpha particles (Waters 2002); MCNP is limited in its application to photons, neutrons, and electrons. Development of MCNPX began in 1994 for application to high energy physics and accelerator applications. MCNPX version 2.4.0 and all later versions represent a merger with MCNP4c and several high energy physics codes. In general, cross section libraries are accessed to simulate the interaction of incoming particles with each material that they encounter. Where no libraries are available, the interaction physics are based on standard interaction models. MCNPX defines low, intermediate, and high energy particles from a particle physics rather than a reactor physics perspective. Tabular data are generally available for low energy phenomena (up to 150 MeV). In the intermediate energy range, 150 MeV to a few GeV, intranuclear cascade/pre-equilibrium/evaporation models are most commonly applied. For very high energies, models involving quantum effects are applied. The interaction models applied in MCNPX have proven to be accurate at the high energy extremes, but their accuracy for low energy incident particles must be established.

3.2.3.1 Interaction Physics

Intermediate energy interactions are modeled in MCNPX based on three physics packages: Bertini, Isabel, and the Cascade-Exciton Model (CEM). Bertini and Isabel were developed out of the LAHET code system (Bertini 1963; Bertini 1969), while the CEM package was specifically adapted for use in MCNPX (Mashnik and Sierk 2001; Waters 2002). Each of these three physics packages is an intranuclear cascade (INC) model. In an INC model description, particles incident on a nucleus are assumed to

interact with individual nucleons. These individual interactions are assessed via particle-particle cross sections. Intranuclear interactions are followed by pre-equilibrium, evaporation and/or fission, and finally de-excitation. High energy particles and light ions that are emitted from this process can then interact with other nuclei in the system.

While each of the models is capable of running outside its recommended limits, the potential errors associated with using a given model should be understood. At lower energies than recommended (e.g., < 20 MeV), no detailed nuclear structure information is included in the models. At very high energies (e.g., > 20 GeV), the Bertini and CEM models tend to underpredict quantities. The recommended ranges for each of the three physics models are: Bertini, 20-150 MeV to 3.5 GeV; Isabel, 20-150 MeV to 1 GeV; and CEM, 100 MeV to 5 GeV. The range in the lower energy bound is due to the presence of tabular data for some incident particles and target materials up to 150 MeV, in which case the physics models would not be required.

Tabular cross section data is included in MCNPX as a supplement to the high-energy physics capabilities, such that the same calculations that can be performed in MCNP can also be performed using MCNPX. Standard nuclear data libraries applied in MCNP4B and in MCNPX model nuclear structure effects in detail, while INC physics does not include any nuclear structure effects. As a result, a discontinuity in the predicted results is often observed when transitioning from the upper range of the cross section libraries to INC physics. The LA-150 proton and neutron cross section libraries extend the available tabular data to 150 MeV for some target nuclei. Proton cross section data, however, is not available for many reactions of interest in a space reactor system. For instance, the LA-150 libraries do not include any interaction data for protons striking beryllium, boron, molybdenum, or uranium. Hence, model physics must be relied upon for proton interactions in these materials.

High energy interactions in MCNPX are modeled using an early version of the FLUKA high energy physics code. The specific model applied in MCNPX is the dual

parton model event generators for hadron-hadron and hadron-nucleus collisions as implemented in EVENTQ, a component of the FLUKA-87 hadron cascade code (Aarnio et al. 1986; Aarnio et al. 1987).

MCNPX remains limited in its ability to track all particles at all energies that may be of interest. Given its original design intent to model nuclear reactors, MCNPX (and MCNP) can track neutrons over the entire energy range of interest, from thermal energies to TeV, where tabular data extends to hundreds of MeV for some target nuclei and models are applied for higher energy incident neutrons. Protons can be tracked from very high energies to a minimum energy of 1 MeV, where tabular data is available for only *some* target nuclei and only up to 150 MeV; model data must be relied upon outside this range. Application of MCNPX physics models to low energy incident particles must be verified by appropriate benchmarking to experimental data for source / target combinations in which tabular data is not available. For alpha particles, no tabular data is available within MCNPX, and models must be relied upon for all analyses involving incident alpha particles. To date, $Z > 2$ ions cannot be treated by MCNPX.

3.2.3.2 Validation

MCNPX was selected to assess the effect of the space radiation environment on reactor operation due to its versatility in geometry definitions, particle transport capabilities and applicability to reactor criticality studies. Originally designed for nuclear reactor applications, the physics associated with neutron transport in MCNP and MCNPX has been tested and proven over many years. However, proton and alpha transport are relatively new additions to MCNPX. Therefore, it was necessary to benchmark the MCNPX results to published, experimentally determined thick target yield (TTY) data to validate the MCNPX predicted secondary neutron production. Prediction of the TTY should consider a thick target, where a “thick target” is one that is many times thicker than the range of the incident particle in the transport medium. The TTY can be

measured experimentally, or it can be calculated from measured interaction cross section and stopping power data. The neutron yield is defined as the number of neutrons produced per incident particle. Figure 3.4 gives a simplistic pictorial representation of a source-target geometry that might be used in a study of thick target yield.

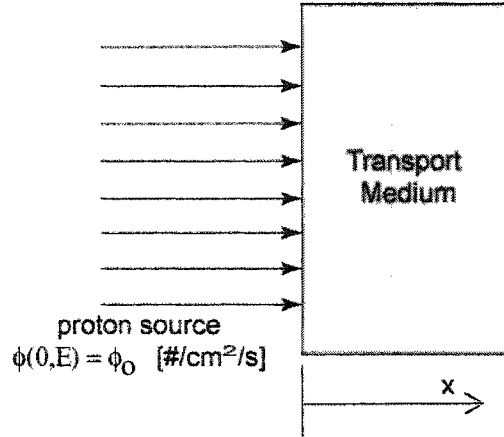


FIGURE 3.4. Representation of Thick Target Yield Geometry.

From the basic definition of TTY, we can write the expression:

$$Y(E) = \frac{\iint \phi(x, E') \Sigma(E') dx dE'}{\phi(0, E)}, \quad (3.1)$$

where

- $\phi(x, E')$ = incident particle flux at position x ,
- $\phi(0, E)$ = incident particle flux at the surface of the transport medium, and
- $\Sigma(E')$ = interaction cross section at energy E' .

This basic expression for the thick target yield assumes that the incident particles compose a parallel beam that strikes the target material. Recognizing that every x position corresponds to a specific E' and, conversely, that every E' corresponds to a specific x position, we can write:

$$\phi(x, E') = \phi(x), \quad (3.2)$$

and

$$\Sigma(E') = \Sigma(E(x)) . \quad (3.3)$$

Separating the incident particle flux into the position and energy dependent components, the flux can be written as:

$$\phi(x, E') = \phi(x) \delta(E' - E(x)) , \quad (3.4)$$

where δ is the Dirac delta function. Substituting, we obtain:

$$Y(E) = \frac{1}{\phi_o} \int_0^\infty \phi(x) \Sigma(E(x)) dx , \quad (3.5)$$

where ϕ_o has been substituted for $\phi(0, E)$ to represent the incident flux at the surface of the material. From the continuous slowing down approximation, the stopping power is defined as:

$$S(E) = -\frac{dE}{dx} . \quad (3.6)$$

Solving for dx , we find:

$$dx = -\frac{dE}{S(E)} . \quad (3.7)$$

The limits of integration of Eq. (3.5) can be converted to energy by recognizing that at $x = 0$, $E = E$ and at $x = \infty$, the particle energy has reduced to zero. Making the appropriate substitutions, we obtain:

$$Y(E) = \frac{1}{\phi_o} \int_0^E \phi(x(E)) \frac{\Sigma(E(x))}{S(E)} dE . \quad (3.8)$$

Because the interaction cross section is small, very few interactions occur and we can assume that $\phi(x(E))$ is approximately constant. Hence, $\phi(x(E)) \sim \phi_o$. With this final substitution, we obtain the common expression for the thick target yield (Anderson and Hertz 1971):

$$Y(E) = \int_0^E \frac{\Sigma(E')}{S(E')} dE' . \quad (3.9)$$

A series of benchmarking calculations were undertaken to validate MCNPX results for proton and alpha particle interactions in beryllium (Be), which is the primary material used to construct the radial reflector that surrounds the SAFE-400 reactor core. Although some portion of the incident radiation will enter the reactor along the axis, directly striking core material, the largest area exposed to the incident radiation is the radial reflector. Therefore, benchmarking calculations were only performed for interactions in beryllium. The threshold energy and Q value (energy emitted in the reaction) for some neutron producing reactions due to incident protons and alpha particles are presented in Table (3.1). Results of the validation studies are presented in Chapter 4.

Reaction	Energy Threshold [MeV]	Q Value [MeV]
${}^9\text{Be}(p, n){}^9\text{B}$	2.06	-1.85
${}^9\text{Be}(p, n)(p, 2\alpha)$	1.75	-1.57
${}^9\text{Be}(\alpha, n)3\alpha$	2.27	-1.57
${}^9\text{Be}(\alpha, n){}^{12}\text{C}$	0	5.7

TABLE 3.1. Reaction Thresholds for Incident Protons and Alpha Particles on Beryllium that Result in Neutron Production.

3.3 Control Methodology

A reactor control system should provide stable reactor response during start-up and steady state operation in any potential environment that could influence the reactor operation. Before establishing a control methodology for a given plant (i.e. a nuclear reactor), several questions need to be answered. First, what is the goal of the control system (or, alternately, what is focus of the optimization)? Second, what parameters are available for control? Some common terms and variables in model based predictive control (MBPC) were presented in Section 2.2.1. The applied control methodology will

seek to minimize an objective or “cost” function (J) of the form presented in Eq. (2.17) at each control step; this optimization will be performed over the future control horizon (of length h) by varying the control vector $u(t)$ at each control step.

Point kinetics, discussed in Section 2.1.3, was employed as a model of the reactor dynamics for the predictive controller. In the initial development of a control methodology, the model used by the predictive control algorithm is also used to provide the “real” system feedback. This assumes that the model perfectly describes the system being controlled. In the application of a controller to a real system, feedback to the controller would be provided by measurement instrumentation, such as neutron detectors, to assess whether the reactor is at the predicted conditions.

The development of an appropriate objective or cost function is very important to the success of a control algorithm, as is the selection of an appropriate system model, and care needs to be taken in selecting the parameters included in the cost function. The process by which a cost function was selected for the present application is discussed in the following sections. Results at various stages in the assessment of an appropriate cost function are provided in Chapter 4.

For a reactor system, the goal of the control methodology is to attain and maintain a pre-determined “goal” power level within a reasonable amount of time without violating any of the pre-established system constraints. The present application only considers start-up of a reactor to a low power level to initialize the system. During this period, temperature feedback effects are insignificant due to the relatively low temperature in the reactor core. In addition, during the initialization period, the heat pipe working fluid (i.e. sodium) is only beginning to thaw and gas flow through the heat exchangers has not been initiated. Therefore, no heat is being extracted from the reactor core. Although the control methodology will likely be the same for all reactor maneuvers (e.g., zero power to low power start-up; low power to high power; power transients; shutdown), higher power, higher temperature operations will require modification of the

governing equations for the reactor model to include temperature feedback effects and to incorporate heat removal, and associated modifications may be required for the cost function and control parameters.

3.3.1 Reactivity Control

The heat pipe cooled reactor considered in the present analysis utilizes rotating drums to control the reactivity in the core, as discussed in Section 2.1.1.1. The angular position of the control drums governs the amount of neutron reflection, which adjusts the neutron multiplication and, hence, the reactivity. As the portion of the control drum containing the B₄C neutron absorbing material is rotated away from the reactor core, because increased neutron multiplication is possible, the reactivity increases. When the drums are in a position that makes the reactor critical, the reactivity is zero. A subcritical reactor (too much neutron absorption) has a negative reactivity, while a supercritical reactor (too much neutron multiplication) has a positive reactivity.

The reactivity as a function of control drum position (θ) can be computed using a series of kcode calculations in MCNPX, with the control drums incremented to a new angular position in each subsequent run. The reactivity is calculated from the multiplication factor (k) that is returned by MCNPX:

$$\rho = \frac{k - 1}{k} , \quad (3.10)$$

and an empirical fit to the calculated results at each angular drum position will then yield an expression $\rho(\theta)$. This expression can be substituted into the point kinetics equations (Eqs. 2.11) to provide an appropriate model of the system being controlled. The error in each calculated value of $\rho(\theta)$ is determined from the statistical error in the multiplication factor (σ_k) that is reported by MCNPX. The appropriate expression for calculating the error in the reactivity (σ_ρ), given in Eq. (3.11), is derived using error propagation (Bevington and Robinson 1992):

$$\sigma_{\rho}^2 = \rho^2 \sigma_k^2 \left(\frac{1}{(k-1)^2} + \frac{1}{k^2} \right). \quad (3.11)$$

For nominal reactor operation, one can assume that all control drums move symmetrically. The only off-nominal condition assessed in this dissertation is the effect of a control drum stuck in the full-in or full-out position, which could have significant effect on the core reactivity. The results of the reactivity calculations as a function of drum position are presented in Chapter 4.

3.3.2 Expression of a Cost Function

In MBPC, the present and future control actions are found by solution of an optimization problem. In defining a cost function, primary consideration was given to the difference between the present neutron population ($n(t)$) and the desired neutron population (n_{goal}), which corresponds to a desired fission power (P_{goal}) (see Eq. 2.16). The selection of other parameters in the cost function is dependent on the selected control variable. The initial cost function considered in the present analysis is given by Eq. (3.12):

$$J = \text{penalty} \cdot \left[\int_{t_o}^{t_o+h} (n(t) - n_{goal})^2 dt + \int_{t_o}^{t_o+h} (a[u(t) \cdot u(t)] + b[\Delta u(t) \cdot \Delta u(t)]) dt \right], \quad (3.12)$$

where

- J = cost of the present control step,
- t_o = initial time for the present control step,
- h = length of the control horizon [sec],
- dt = control time step [sec],
- $u(t)$ = control vector at time t , and
- $\Delta u(t) = u(t + dt) - u(t)$.

One may note that each of the terms in the cost function is squared. The quadratic form is applied to avoid sharp transitions in the cost function, which could lead to instabilities in finding a function minimum. The initial multiplier, identified simply as *penalty*, represents penalties that are applied to the entire cost function if specified

constraints are violated. The source of these penalties is discussed in the following sections with respect to each potential control variable.

The primary goal of the control algorithm was to increase the power in the modeled reactor from its initial zero-power condition to a pre-selected goal power level, which is then maintained at steady-state. The controller developed in this dissertation does not consider additional power transients from the low initial power level to the nominal high power level that would characterize an operating nuclear reactor. As will be seen in the results for the secondary neutron production due to the space radiation environment (Ch. 4), the external environment only impacts reactor operation in the very low power regime. Because the goal of the present work was to assess the effect of the radiation environment, and to propose options for reactor control in the presence of a time varying external radiation source, investigation and analysis was extended only to the operational regime in which the external source was significant.

The first term in the bracketed expression in Eq. (3.12) corresponds to the cost associated with the deviation of the current neutron population (which corresponds to the current fission power) from the goal neutron population (which corresponds to P_{goal}) over the full control horizon. The cost function was designed such that the power term dominates early in the reactor start-up, when the reactor power is far from the desired power level, but approaches zero once the desired power level has been achieved.

The second and third terms in the bracketed expression relate to the cost placed on the control variable. The appropriate weighting coefficients a , b are selected for each control problem. The first of these terms, proportional to $u(t)$, corresponds to the cost of the control itself. Because the control variable is expressed as a vector over the control horizon, the square of $u(t)$ is computed as a dot product of $u(t)$ with itself over the full horizon length. The last term, proportional to $\Delta u(t)$, relates to changes that are made in the control vector over each control time step; this term is equivalent to the first derivative of the control vector. Again, the square of this term is computed as a dot

product over the full horizon length. The appropriate weighting factor for each of the control-related terms is dependent on the parameter selected for system control.

3.3.3 Selection of a Control Variable

In a computational analysis, the engineer or designer has various options in selecting a control variable, whether or not it is physically possible to control the selected parameter directly in the actual system or plant. The control variable for a nuclear reactor should be selected based on its ability to control reactivity. As discussed above, reactivity is governed by the position of the rotating control drums in the modeled SAFE reactor. In this case, the designer may choose to control the system based on the angular position of the control drums ($\theta(t)$); the rate of change of the control drum position, ($\omega(t)$), where:

$$\omega(t) = \frac{d\theta(t)}{dt}; \quad (3.13)$$

or the current applied to the control drum drive mechanisms ($I_{drive}(t)$), where:

$$\frac{d\omega(t)}{dt} = \alpha I_{drive}(t) + drag; \quad (3.14)$$

and α is determined by the characteristics of the specific control drum drive mechanism that is selected. Control based on the angular position of the control drums is not physically possible, as this implies that changes to the control drum position can be made instantaneously. However, investigation of control based on $\theta(t)$ can help to identify possible strategies for more physically appropriate control variables. Using $\omega(t)$ as the control variable better approximates the physical system because it implies that there is a finite rotational velocity available to the control drums. The angular velocity of the control drum motion is actually determined by the current that is applied to the control drum drive mechanisms, as indicated in Eq. (3.14). However, since no drive motors have

yet been specified for a SAFE type reactor, control based on $\omega(t)$ is the best physical representation of a potential reactor control system that is presently achievable.

Regardless of the selected control variable (θ , ω , or I_{drive}), a system constraint must be applied to limit the range of control drum motion. The angular position of the control drums can only range from 0° (full in position) to 180° (full out position), defined relative to the position of the neutron absorbing material. This constraint can be accomplished by penalizing any proposed solution to the optimization problem that would result in a drum position outside of the allowable range, implementing the penalty as a multiplier on the entire cost function. The angular penalty will be referred to as “*ftheta*”, which replaces the “*penalty*” multiplier in the cost function. The value of *ftheta* is given by:

$$ftheta = \begin{cases} 1 & 0^\circ \leq \theta \leq 180^\circ \\ 1000 & \text{else} \end{cases}, \quad (3.15)$$

where the factor of 1000 was arbitrarily selected to result in a large cost for a drum position selected outside of the allowable range, discouraging an angle selection outside the specified range.

3.3.3.1 Drum Position, $\theta(t)$, Control

In the course of this dissertation, an initial attempt at optimized control considered controlling the reactor start-up by using the actual drum position $\theta(t)$. Although this scenario is not physically possible (drum position cannot instantaneously change), it provided a first step to optimized control that minimized the number of variables being considered.

In developing a cost function for $\theta(t)$ control, it was necessary to apply a cost to the rate of change of the drum position to better simulate what is physically possible in the reactor configuration (e.g., large changes to the drum position cannot be accomplished instantaneously, so these are more costly). This cost was applied by

including the first derivative term in the cost function specified in Eq. (3.12), weighted by the parameter b . This term is expected to be large during the early part of the control period when large control drum maneuvers are necessary to increase reactor power and the cost function is dominated by the neutron population (fission power) term. Under nominal operation, this term would then be expected to approach zero as the goal power level is achieved because the drum position is expected to be approximately constant once a steady state power has been achieved at the desired goal power level.

In the initial implementations of the control algorithm, a cost was also applied to the control vector itself, weighted by the parameter a . However, it is not clear if this term is necessary in the cost function when the control vector is represented by the drum position. The final drum position will be non-zero in an operating reactor. For example, the control drum position for criticality in the SAFE-400 reactor design is approximately 90° (this result will be presented in Chapter 4). Hence, as the reactor approaches steady state at the goal power level, the cost to control, weighted by a , is not expected to approach zero. Therefore, for control based on the drum position, it was expected that the weighting coefficient a should be set to a relatively small value or zero, but that the cost to $\Delta\theta(t)$, the final term in the cost function, should have a non-zero weighting ($b > 0$). Initial results obtained by operating the reactor under $\theta(t)$ control are presented in Section 4.4.2.1.

3.3.3.2 Angular Velocity, $\omega(t)$, Control

The second generation of the control algorithms utilized the angular velocity of the control drums ($\omega(t)$) as the control variable. Early application of the $\omega(t)$ control did not place any (minimum or maximum) constraints on the angular velocity. However, the control drum drive mechanisms that are selected will place a constraint on the maximum turning rate of the control drums, as will safety considerations associated with rapid start-up of a reactor. Again, because the drive mechanisms remain to be defined, simulations

that implemented a constraint on the angular velocity of the control drums considered a few different maximum turning rates. Data presented in Chapter 4 consider constraints set at 0.01 rad/sec (0.57°/sec) and 0.0175 rad/sec (1.0°/sec) to demonstrate the capability of the control methodology to provide a smooth, controlled start-up without violating the specified ω_{max} constraint. From Section 1.1.4, the reader will recall that the maximum angular velocity of the Topaz II control drums was 1.4°/sec (El-Genk et al. 1993). The constraint to the maximum drum velocity is applied as an additional penalty on the cost function, referred to as f_{wmx} . As in the f_{theta} penalty, f_{wmx} is set to a large value if any predicted control step in the horizon exceeded the specified ω_{max} (regardless of whether this control step will actually be applied to the plant):

$$f_{wmx} = \begin{cases} 1 & \omega \leq \omega_{max} \\ 1000 & \omega > \omega_{max} \end{cases} \quad (3.16)$$

As in the definition of f_{theta} , the factor of 1000 was arbitrarily selected to result in a large cost to $\omega > \omega_{max}$ to discourage the controller from making this selection. The penalty in the initial cost function (Eq. 3.12) is now replaced by $f_{theta} * f_{wmx}$.

Unlike control on the drum position, control based on $\omega(t)$ requires that a significant cost be applied to the control variable itself. Since $\omega(t)$ is the rate of change of the control drum position, it is equivalent to the first derivative term in the $\theta(t)$ control scenario ($\Delta\theta(t)$). As the optimal control drum position is reached at the steady state goal power level, $\omega(t)$ should approach zero, indicating that no changes to the control drum position are necessary to maintain a constant power level. During the early part of the control, the cost function is dominated by the neutron population (power) term, and a large cost to the control vector is insignificant. However, as $n(t) \rightarrow n_{goal}$, the cost placed on the control should become more important to the optimization of the cost function. Hence, the weighting coefficient a was expected to be non-zero in this implementation.

On the other hand, it is suggested that the first derivative of the control parameter is no longer appropriate in the cost function. The term multiplied by the parameter b discourages large changes in the control variable from one step to the next. When controlling the system based on the rate of change of the drum position ($\dot{\omega}(t)$), one control step might apply a (relatively) large angular velocity while the subsequent step might apply an angular velocity of zero, indicating that the current drum position is appropriate for the present time. This maneuver would be desirable, but would result in a large derivative term. To avoid discouraging these desirable control maneuvers, the weighting coefficient b was set to zero for $\dot{\omega}(t)$ control.

3.3.4 Optimization

MATLAB 6.1 was selected to develop the MBPC methodology and to determine the optimal reactor control during start-up. Minimization of the cost function was accomplished using the *fminsearch* algorithm within MATLAB's Optimization Toolbox (MathWorks 2001). *Fminsearch* utilizes a Nelder-Mead simplex algorithm to find the minimum of the indicated function to a prescribed tolerance. The Nelder-Mead algorithm is a direct search method appropriate for multi-dimensional, unconstrained, derivative-free nonlinear optimization (Lagarias et al. 1998). In this application, the Nelder-Mead algorithm was applied to find the minimum of the specified cost function. The minimization was performed over the full control horizon at each control step, where the current neutron population, $n(t)$, was determined by solution of the PKE given in Eqs. (2.11).

To find a solution to the PKE within the MATLAB framework, a stiff ordinary differential equation (ODE) solver, *ode15s*, was applied. *Ode15s* is a variable order solver based on numerical differentiation formulas. Gear's method, also known as backward differentiation formulas, was applied. A multi-step solver, *ode15s* generally

requires the solutions at several preceding time points to compute the current solution. In cases in which *ode15s* appeared ineffective, another stiff solver, *ode23s*, was applied. A one-step solver, *ode23s* is more efficient than *ode15s*, but it results in cruder tolerances.

Computational results included for the control methodology in Chapter 4 demonstrate the process by which an optimal cost function was determined. Results at each stage of the analysis are presented, indicating the benefits and problems associated with each proposed control variable and set of control parameters. The final cost function is applied in constant and fluctuating source conditions.

CHAPTER 4

COMPUTATIONAL RESULTS

A series of computational analyses were undertaken to characterize the effect of the space radiation environment on the start-up and control of a heat pipe cooled reactor core. Calculations were performed to characterize the kinetics of the proposed reactor design, in both nominal and off-nominal conditions; to assess the secondary particle production in several space radiation environments, impinging on the core both isotropically and from a preferential direction; and to develop and demonstrate a reactor control system capable of providing robust control during reactor start-up, from zero power to a nominal low power condition, in the presence of a constant and a fluctuating background source.

4.1 SAFE-400 Reactor Kinetics

The kinetics of the SAFE-400 heat pipe reactor design was studied using criticality calculations performed in MCNPX. These calculations were performed to assess the reactivity worth of the six rotating control drums that provide reactor control, developing an empirical relationship between control drum position and total neutron multiplication (or total reactivity), and to assess the effect of a failed control drum drive mechanism on the radial neutron flux. During nominal operation, all six control drums are expected to be rotated symmetrically to the same angular positions. In this configuration, the reactor is designed to provide symmetric radial neutron flux and power. Given a single failed control drum drive, the flux was expected to become

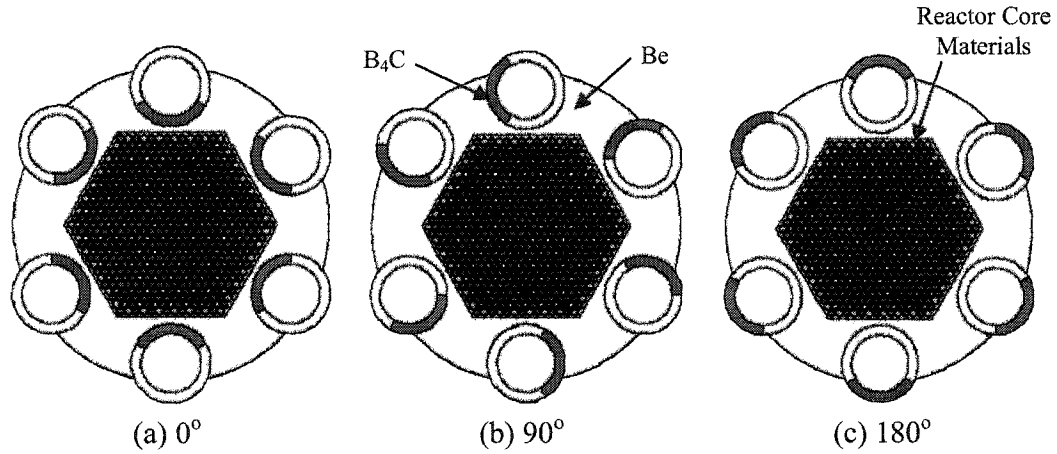
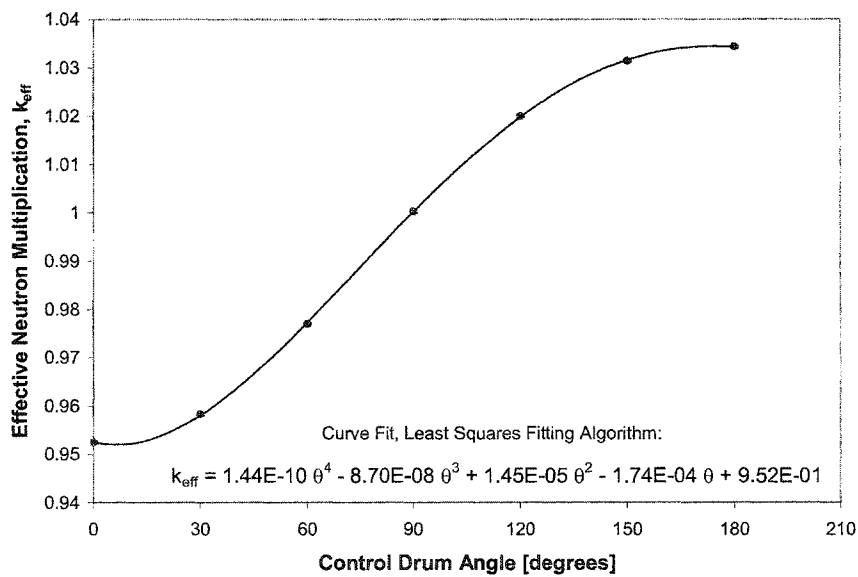
asymmetric. The magnitude of this asymmetry and the effect on the core reactivity was assessed by calculation.

4.1.1 Nominal Operation: Reactivity and Control Drum Motion

A reactor core is designed with a sufficient amount of excess reactivity at the beginning-of-life (BOL) to compensate for temperature defects, the negative reactivity worth of each control drum, the negative fuel and core temperature coefficients, and burn-up of the nuclear fuel at the end-of-life (EOL). Over the lifetime of a reactor core, the total reactivity is expected to decrease due to the loss (burn-up) of ^{235}U atoms. For the SAFE-400 core design, the average fuel burn-up after 10 years of operation is 1.2%, with a peak fuel burn-up of 1.9% in the central fuel pins (Poston et al. 2002). For a cold, clean SAFE-400 reactor core with all control drums rotated fully out, calculations indicate that the effective multiplication factor is 1.0342 ± 0.0002 . At the end-of-life, calculations by Poston et al. (2002) indicate that the corresponding k_{eff} is expected to be reduced to 1.0173 ± 0.0002 .

The effective neutron multiplication factor was calculated as a function of the angular control drum position using MCNPX. Design details were obtained from core designers at Los Alamos National Laboratory, and the MCNPX input deck was constructed using macro-body surface and cell definitions, which significantly simplify the geometry definition (e.g., an 8-surfaced hexagonal prism can be defined using a single surface card rather than 8 individual cards that explicitly define each surface). The core neutron multiplication was assessed for control drum angles from 0° to 180° , adjusted in 30° increments. Figure 4.1 (a-c) illustrates the drum positions at 0° , 90° , and 180° . These calculations were performed using a “kcode” calculation in MCNPX, with a fission source distributed throughout the core fuel elements (see discussion in Section 3.2.3). No external radiation sources were included in this series of calculations. The

resulting effective neutron multiplication factor was plotted as a function of drum position in Fig. 4.1(d).



(d) Effective Multiplication Factor

FIGURE 4.1. Effective Neutron Multiplication in the SAFE-400 Reactor Core as a Function of Control Drum Position.

In all calculations used to generate Fig. 4.1, the statistical error in the reported k_{eff} was approximately 0.0002. Error bars were plotted for each point in Fig. 4.1 (d), but their relatively small size renders them difficult to discriminate from the individual data

points. A least squares fit was performed using Microsoft Excel to fit the data to a fourth order polynomial, resulting in the expression given in Eq. (4.1):

$$k_{\text{eff}} = 1.44\text{E-}10 \theta^4 - 8.70\text{E-}08 \theta^3 + 1.45\text{E-}05 \theta^2 - 1.74\text{E-}04 \theta + 0.952 \quad (4.1)$$

As reported by Poston et al. (2002), MCNPX calculations verified that the k_{eff} for a cold, clean SAFE-400 reactor at BOL was 1.0342 ± 0.0002 with all control drums rotated to 180° . With all drums rotated in, the k_{eff} was 0.9524 ± 0.0002 (Bragg-Sitton and Holloway 2003). The effect of fuel burn-up on the core reactivity was not assessed in this dissertation.

One may note that between 0° and 30° , the curvature of $k_{\text{eff}}(\theta)$ curve suggests a very small, artificial decrease in the multiplication factor, experiencing a local minimum at approximately 6° . This slight decrease was not expected to effect computational results, as the primary region of operation was expected to be at a drum position of approximately 90° . However, this curvature may have caused small, unnecessary difficulty for the start-up controller to move the drums away from their initial position at 0° in the case in which only very small control steps are allowed.

Analysis of the MCNPX results indicates that a critical configuration exists when the reactor control drums are rotated to approximately 90° . In addition to the neutron multiplication factor, the neutron flux profile during nominal operation was also assessed by computing a neutron flux tally in each of the fuel elements in the reactor core. As in the previous set of calculations, a fission source distributed throughout the core fuel elements provided the initial neutrons required to initiate fission. In the inset for Figure 4.2, row “S6” corresponds to the center row of fuel pins and row “S1” is near the core edge. The calculated neutron flux is presented in a bar plot in Fig. 4.2, where each bar corresponds to an individual fuel element. The x-axis label “position across core” corresponds to the pin number, as numbered from the left-most pin at the center profile of the modeled reactor core.

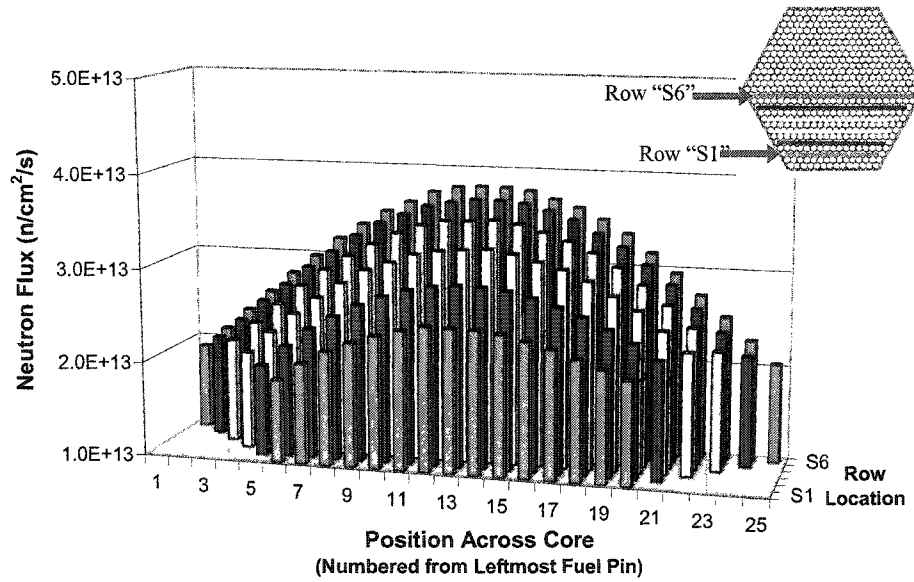


FIGURE 4.2. Neutron Flux in the SAFE-400 Reactor Core, $k_{\text{eff}} = 0.9770 \pm 0.0002$.

The flux profile provided in Fig. 4.2 corresponds to a control drum position of 60° , such that k_{eff} was less than one. This flux was calculated using a standard fission source in an MCNPX kcode calculation for nominal reactor operation (i.e. 400 kW_t). For the given drum configuration, the corresponding effective multiplication factor is calculated to be 0.9770 ± 0.0002 (Bragg-Sitton and Holloway 2003). This configuration allows fission neutrons to be produced and tallied in the simulation, but the subcritical configuration ensures that infinite multiplication cannot result. If the multiplication factor is even *slightly* greater than one, the neutron inventory in the reactor core approaches infinity and the MCNPX calculation fails to complete. Solution of the neutron diffusion equation indicates that a finite cylindrical reactor will have a radial flux distribution that approximates a Bessel function; the axial flux distribution has a cosine dependence (Lamarsh 1965). The dependence of the flux is approximated by Eq. (4.2) for a bare, finite cylindrical reactor, where R is the radius of the core and H is the core height:

$$\phi(r, z) \propto J_0\left(\frac{2.405 r}{R}\right) \cos\left(\frac{\pi z}{H}\right). \quad (4.2)$$

This expected flux shape is qualitatively observed in Fig. 4.2.

4.1.2 Off-Nominal Operation: Failed Control Drum Drive

Under nominal operating conditions, all six control drums would be expected to be positioned symmetrically to provide a symmetric neutron flux and power profile across the reactor core. However, possible failure scenarios were considered to estimate the potential effect on reactor operation. Specifically, the effect of a single failed control drum drive mechanism on the multiplication factor and the radial neutron flux was assessed by MCNPX calculations. Two off-nominal conditions were considered: one control drum stuck at 0°, with the B₄C adjacent to the reactor core, and the opposite condition in which one control drum was stuck at 180°, with the B₄C rotated fully outside the reactor core. Results of the MCNPX calculated k_{eff} , as a function of the position of the remaining five operational control drums, are provided in Figure 4.3. The statistical error in the calculated k_{eff} was less than 0.00025 for each case; due to their relatively small magnitude, error bars were not included in the plotted results.

Comparison of the off-nominal results to the results for symmetric control drum rotation indicates that neither case would prevent reactor operation, nor would the safe operation of the reactor be forfeited. In the case in which one control drum was stuck at 180°, the shut-down k_{eff} was increased from 0.9524 ± 0.0002 to 0.9651 ± 0.0002 , still sufficiently sub-critical for safe, reliable reactor shutdown. The opposite condition in which one control drum was stuck at 0°, with the remaining drums rotated to 180°, reduced the k_{eff} to 1.0207 ± 0.0002 , still sufficiently supercritical to provide reliable reactor operation throughout the life of the reactor.

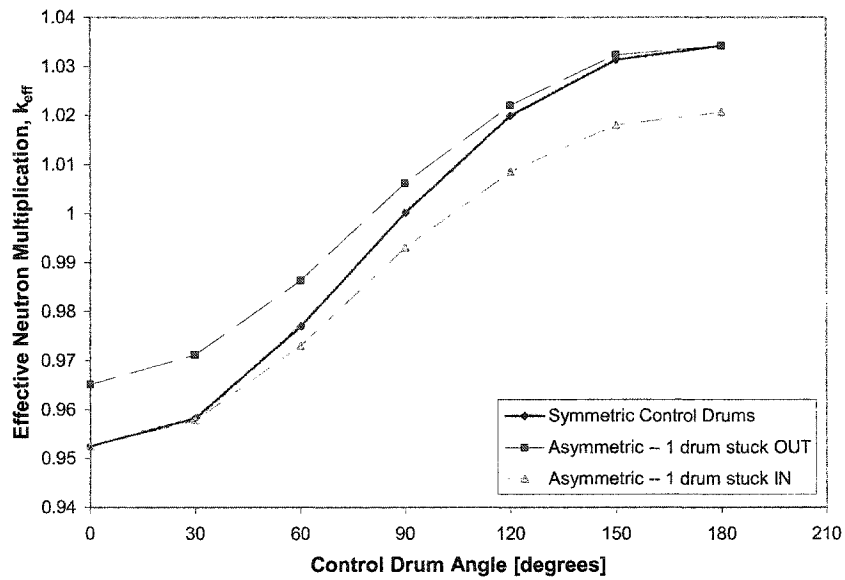
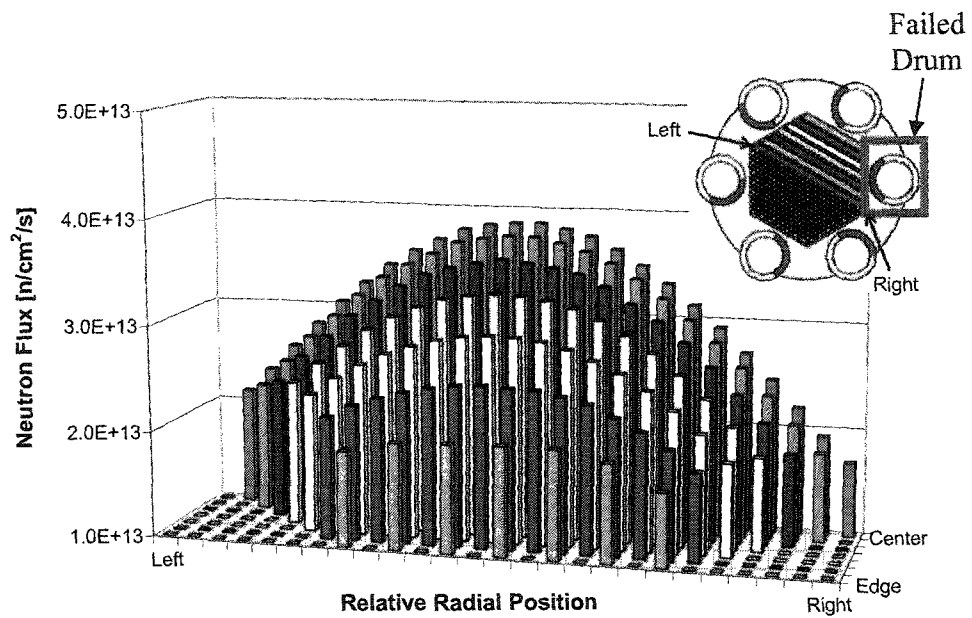
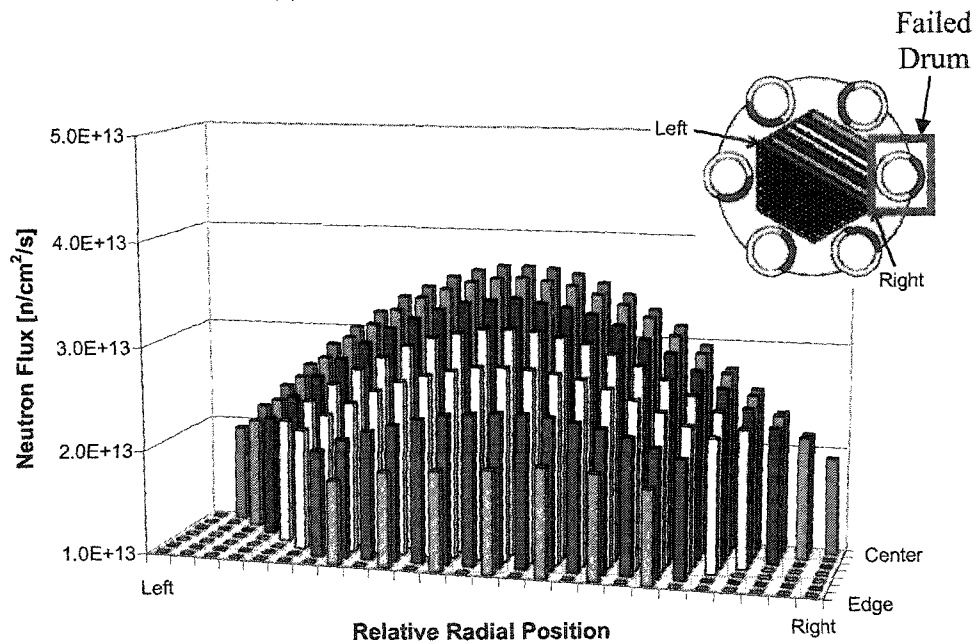


FIGURE 4.3. Effect of a Failed Control Drum Drive on k_{eff} .

The corresponding neutron flux profile across the modeled reactor core was also of interest given off-nominal conditions. As discussed above, failure of a single control drum drive mechanism was expected to affect the symmetry of the neutron flux across the core diameter. The resulting neutron flux for both off-nominal conditions is shown in Figure 4.4 (a) and (b). To better demonstrate the impact of the failed control drum drive on the symmetry of the neutron flux, the neutron flux plots in the asymmetric conditions considered different rows of fuel pins than the symmetric case. The asymmetric neutron flux was plotted along cross sections that were rotated 60° from the symmetric drum cross sections, such that the failed control drum drive directly impacted the flux. The edge neutron flux shows data at every other radial position; this results from the fact that every other pin along the face of the core was a BeO axial reflector pin. The flux profile along the very edge of the core was not shown in the symmetric control drum results (Fig. 4.2).



(a) One Control Drum Stuck at 0° .



(b) One Control Drum Stuck at 180° .

FIGURE 4.4. Neutron Flux in the SAFE-400 Reactor Core with One Failed Control Drum Drive; Remaining Control Drums Were Positioned at 60° .

The effect of the failed control drum drive mechanism is more readily evident in the case of a drum stuck at 0° than at 180° . Given symmetric control drum positions, the ratio of the maximum to the minimum flux along the central row of fuel pins was 1.8. With one control drum stuck at 0° , this ratio increased to 2.3. The asymmetric drum position caused the ratio of the flux in the left-most fuel pin to that in the right-most fuel pin to be 1.24. Because a different radial cross section was considered for the symmetric control drum runs, the ratio of the left and right fuel pin flux was not comparable to the asymmetric case. For one control drum stuck at 180° , the ratio of the maximum to minimum neutron flux across the center of the core was 1.9, only slightly elevated from the nominal condition. The left-most and right-most fuel pins reported equivalent neutron flux (ratio of 1.0) in this configuration.

When the control drums are operated in off-nominal conditions, the neutron leakage from the reactor and the neutron absorption by reactor poisons (B_4C) is affected. The failed control drum drive was more evident for the case in which additional neutron poison was inserted into the core, resulting in a larger amount of neutron absorption than expected for the given position of the remaining control drums. This resulted in a significant dip in the neutron population near the failed drum, skewing the radial flux profile. In the second case in which the failed drum was stuck at 180° , additional neutron reflection was provided, such that a slightly higher neutron population near the failed drum would be expected. Had the remaining fully operational drums been positioned in a critical configuration (e.g., critical assuming all drums operational and at the same position prior to drum failure), failure of a drum causing it to rotate to 180° would cause the reactor to become prompt supercritical. However, there is no mechanism by which a failed drum would suddenly rotate to the outward position, and fail-safe mechanisms would be in place to ensure that no failure mode could result in this drum motion. Conversely, were a control drum to suddenly return to the 0° (full-in) position while operating at steady-state (in a critical configuration), loss of this drum would introduce a

significant amount of negative reactivity and cause the reactor to become subcritical. A reliable, robust control system should be equipped to respond to failure of a control drum, preventing a supercritical state from arising or reactor shutdown due to a large insertion of negative reactivity.

4.2 Benchmarking MCNPX: Proton and Alpha Particle Transport

A series of benchmarking calculations was undertaken to validate MCNPX results for proton and alpha particle interactions in beryllium (Be). The SAFE-400 is designed with a beryllium radial reflector that surrounds a majority of the reactor core. As a result, the impinging external radiation is expected to primarily encounter beryllium when it strikes the reactor vessel. Some fraction of radiation may also enter the exposed end of the reactor, where it has a shorter path to the fissionable materials in the core. The calculations included in this dissertation do not consider radiation interactions with any components of the energy conversion system, radiation shield, or payload that would be coupled to the reactor module.

For benchmarking purposes, MCNPX calculations for incident protons and alpha particles were compared to experimental thick target yield (TTY) data included in the published literature for 2.3 – 5.4 MeV protons and for 4 – 30 MeV alpha particles striking a beryllium target. Incident proton calculation included an additional comparison to the calculated TTY based on the continuous slowing down approximation using Eq. (3.9).

A source / target geometry was constructed in MCNPX to compute thick target yields for a parallel beam of protons or alpha particles striking a thick slab of beryllium. This geometry was initially illustrated in Fig. 3.3 in association with the TTY derivation as a function of stopping power and interaction cross section. For incident protons, the

modeled beryllium slab was 0.1 cm thick, and the source and target were defined such that all particles emitted from the source encountered the target material. The 0.1 cm thickness of Be was sufficient for a “thick target,” as the range of 0-10 MeV protons in Be is less than 0.1 cm; the range of 0-10 MeV alpha particles in Be is less than 0.01 cm. The range of protons, alpha particles, and lithium ions is plotted in Figure 4.5 as a function of particle energy in MeV (Ziegler and Biersack 2003). The neutron production per incident proton in the slab was computed using a neutron current tally (MCNPX type 1 tally) to determine the number of neutrons leaking out of the slab per source proton or alpha particle. Assessment of MCNPX reliability was accomplished by comparing the MCNPX results with the published TTY data or the CSDA calculation of the TTY using Eq. (3.9).

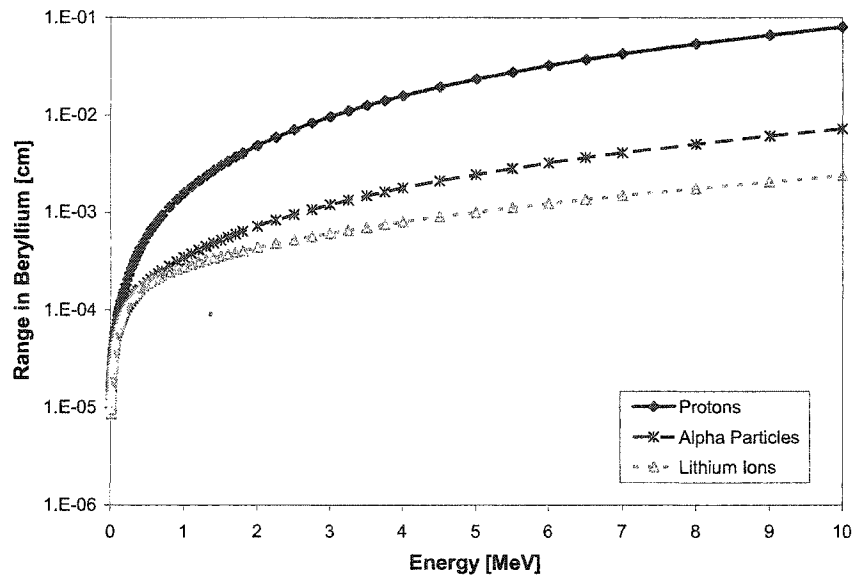


FIGURE 4.5. Ion Range in Beryllium.

4.2.1 Incident Protons

Three MCNPX runs were completed for incident protons, considering three different initial proton energies: 3.7 MeV, 4.0 MeV, and 5.4 MeV. MCNPX defines a default “cutoff” energy for each particle type, below which particle interactions are no longer tracked. When particle energy reaches the cutoff value, the remaining energy is deposited locally by MCNPX. For protons, this cutoff energy is 1.0 MeV.

Several researchers have studied neutron production due to incident protons on a thick beryllium slab. Because thick target yield experiments are often conducted using proton beams (particle accelerators), the resulting yield is generally reported as the number of neutrons produced per charge deposited in Coulombs (C). For instance, Howard et al. (2001) reported results for protons incident on a Be target in terms of neutrons produced per nC beam current, where the proton accelerator was operated at a maximum beam current of 4 mA. One proton has a charge of 1.6×10^{-19} C. Using this conversion factor, published results were converted to neutron yield per incident proton. Stopping power data from Northcliffe and Schilling (1970) and cross section data from Kim et al. (1966) were applied to compute the TTY for 2.3 – 5.4 MeV protons using Eq. (3.9); these data are reproduced in Fig. 4.6. The default cutoff energy of 1.0 MeV for proton transport in MCNPX was applied for 3.7 and 4.0 MeV incident proton calculations. However, for the 5.4 MeV source, the cutoff energy was set to 2.3 MeV due to the range of data available for the CSDA calculated yield.

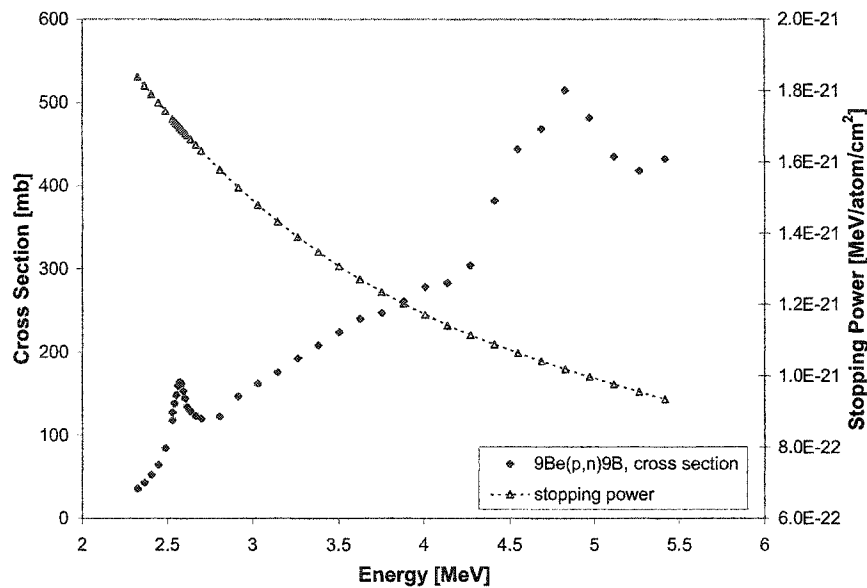


FIGURE 4.6. Proton Stopping Power and Neutron Production Cross Section Data for 2.3 – 5.4 MeV Protons Incident on Beryllium.

Thick target yield results for incident protons are summarized in Table 4.1. No error was reported for the calculated TTY because no error was reported by Kim, et al. (1966) or Northcliffe and Schilling (1970) in the published stopping power or interaction cross section data. In general, the MCNPX results predicted a somewhat lower neutron yield than expected. The MCNPX calculated yield was 26 – 31% less than the measured TTY for 3.7 and 4.0 MeV protons striking beryllium, and was 53% less than the yield predicted using the CSDA. Because the MCNPX-predicted neutron production was within a factor of two of the results determined from experimental measurement, MCNPX was considered sufficiently reliable for the present application. Significant uncertainty exists in the prediction of the space radiation environment at any given time, such that a factor of two under-prediction of the secondary neutron flux did not introduce significant error to the calculations. The discrepancy between the MCNPX calculated TTY and the experimentally determined TTY may arise from inaccuracies in the physics models applied in MCNPX. Because tabular data was unavailable for proton interactions

in beryllium, physics models were applied for all proton energies. Although MCNPX also applied a cutoff energy of 1 MeV, below which protons were not tracked, this was not a likely contributor to the discrepancy. The primary neutron-producing interactions have threshold energies greater than 1 MeV (Table 3.1), and it is improbable that these low energy protons would have sufficient energy to escape the Be reflector to cause neutron generation in the core materials.

Proton Energy [MeV]	Thick Target Yield			
	Experimental [n/proton]	Calculated, Eq. 3.9 [n/proton]	MCNPX [n/proton]	Error: MCNPX vs. Exp/Calc
3.7	$1.54 \pm 0.077 \times 10^{-4}$	Data not available for calculation	$1.136 \pm 0.069 \times 10^{-4}$	26 %
4.0	$2.37 \pm 0.110 \times 10^{-4}$		$1.644 \pm 0.084 \times 10^{-4}$	31 %
5.4	----	8.1×10^{-4}	$3.838 \pm 0.125 \times 10^{-4}$	53 %

TABLE 4.1. Experimental and Calculated TTY for Protons Incident on Beryllium (Howard et al. 2001; Kim et al. 1966; Northcliffe and Schilling 1970).

4.2.2 Incident Alpha Particles

An MCNPX model similar to that discussed for incident protons was also developed to determine the accuracy of alpha-particle transport in MCNPX. In this study, a 3-12 MeV alpha source was considered. An attempt was made to compare MCNPX results to experimentally determined TTY data for 3-9 MeV alpha particles (Bair and Gomezdelcampo 1979); however, it was noted that all alpha particles in the simulation were lost due to energy cutoff. Further inspection revealed that the minimum energy cutoff for alpha particles in MCNPX is 10 MeV; alpha particles below this energy cannot be transported. An MCNPX calculation for a 12 MeV alpha source was therefore completed to compare to experimental data collected by Lone et al. (1981), but only a few neutrons were generated in the MCNPX simulation by alpha particle interactions. Most alpha particles were still lost to energy cutoff. Figure 4.7 shows the approximate

alpha particle spectrum for a 450 km, 51.6° inclination orbit (nominal ISS orbit) and a geosynchronous orbit during the 1989 SPE. CREME96 predicts that for the latter orbit, 93% of the alpha spectrum is below 10 MeV, with another 6.5% lying between 10 and 100 MeV.

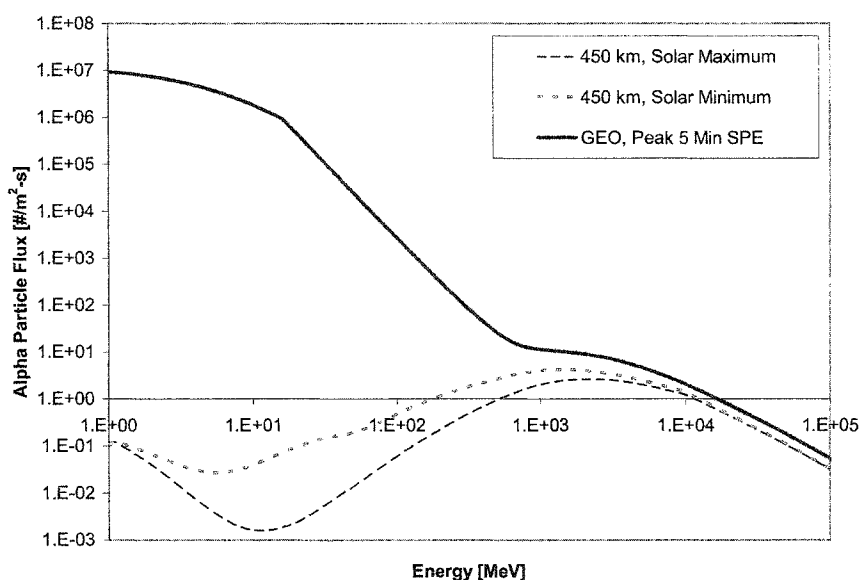


FIGURE 4.7. Alpha Particle Flux in Near Earth Space.

Additional MCNPX simulations were performed to assess neutron production due to alpha bombardment on reactor core materials, although strict benchmarking was not performed for these target nuclei. In this case, the full reactor core geometry was constructed in MCNPX, and this structure was bombarded isotropically by alpha particles (4π geometry), with the approximate alpha energy spectrum for the geosynchronous orbit (Fig. 4.7). While a majority of the incident particles only interact with the Be reflector, the isotropic geometry allows some particles to directly interact with reactor core materials. Errors occurred in the MCNPX simulation that prevented estimation of the secondary neutron production from alpha particles. Investigation indicated that MCNPX returned a zero weight when high energy alpha particles strike molybdenum, generating

an error and failing to complete the run. This error was reported to the MCNPX development team, but it could not be resolved given the available physics models.

At relatively low alpha energies, the results presented in this dissertation indicate that MCNPX cannot reliably estimate the alpha-induced neutron production in a reactor module. While experimental data indicate that neutron production does occur due to low energy alpha particles interacting with beryllium, MCNPX did not predict this alpha induced neutron yield. MCNPX also had difficulty in predicting the neutron production due to high energy alpha particles incident on the reactor core materials. This problem should be addressed in future upgrades to the physics models applied in MCNPX to better estimate the secondary particle production due to alpha particle bombardment. In this dissertation, secondary neutron production due to incident alpha particles was assessed using published data and empirical relations to ascertain the relative importance of alpha particles in reactor start-up and control.

4.3 Secondary Neutron Production: SAFE-400 Reactor

The secondary neutron production in the SAFE-400 reactor vessel was assessed for the approximate radiation environment in near-Earth space. Two specific orbits were considered: the International Space Station (ISS) orbit at an altitude of 450 km and an orbital inclination of 51.6° (at solar minimum) and a geosynchronous Earth orbit during a solar event. The latter was applied to estimate an extreme radiation environment in near-Earth space, considering stormy geomagnetic conditions and solar particle fluence during the peak five minutes of the October 1989 SPE. Both environments were developed from CREME96 models. The proton and alpha particle flux for each of these orbits is provided in Figures 4.8 and 4.9. The following analysis includes the secondary particle production by incident protons (as predicted by MCNPX) and incident alpha particles (as predicted from empirical equations).

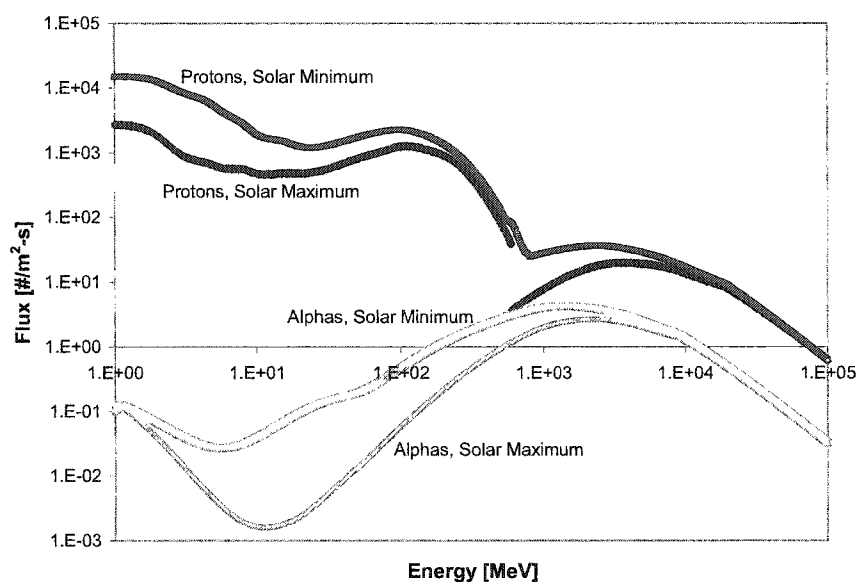


FIGURE 4.8. Proton and Alpha Particle Flux in Near-Earth Space, International Space Station Orbit (450 km, 51.6° inclination).

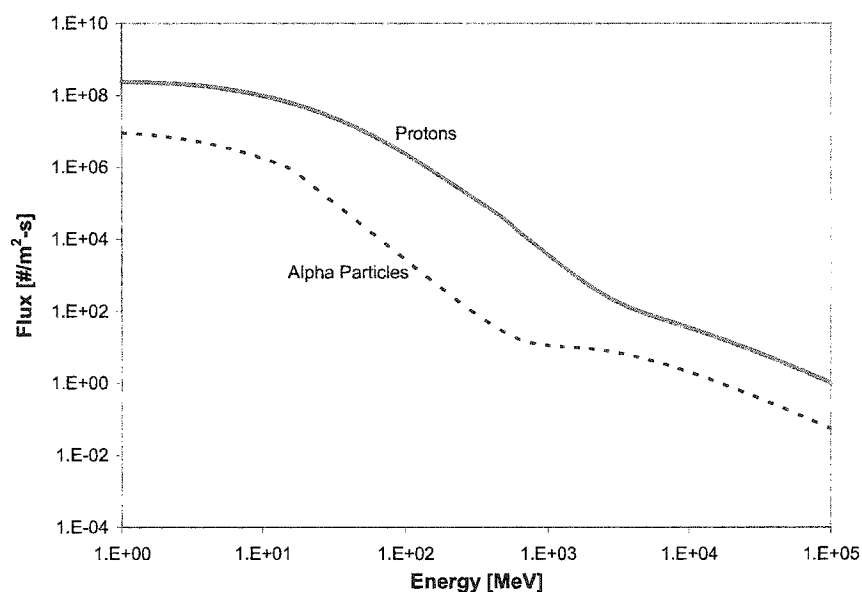
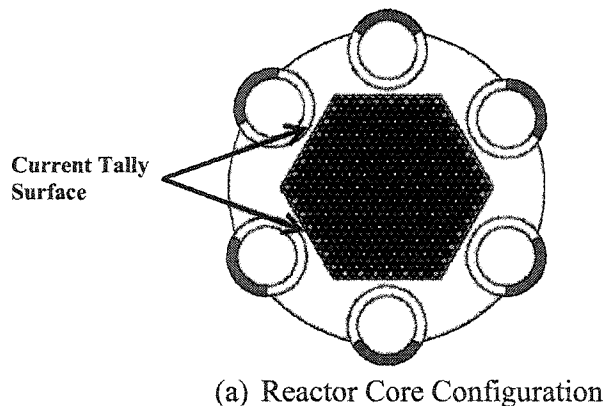
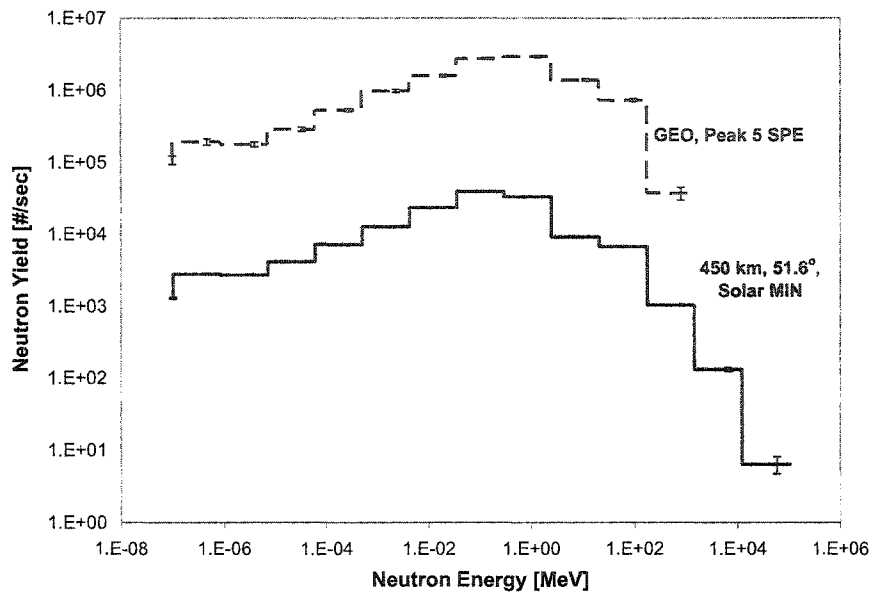


FIGURE 4.9. Proton and Alpha Particle Flux in Geosynchronous Earth Orbit During the Peak 5 Minute Flux of the October 1989 SPE.

4.3.1 Incident Protons

The neutron yield due to incident protons was computed at the hexagonal core surface of the reactor vessel using a surface current tally in MCNPX. The total surface current and the corresponding incoming and outgoing currents were considered to estimate the secondary neutron source at the core surface. The results presented in Figure 4.10 correspond to the total neutron current entering the reactor core. The reactor vessel was bombarded isotropically with all six control drums rotated to 180° , such that the B_4C was facing outward. This configuration would produce a supercritical reactor (1.0342 ± 0.0002 at BOL). Therefore, the option to produce fission neutrons was turned off in MCNPX (using the “nonu” card). This prevented infinite neutron multiplication from occurring due to fission and provided a reliable estimate of the neutron production due to proton interactions in reactor vessel materials without including additional fission neutrons. The total neutron current at the hexagonal surface of the core, obtained by integration under each curve in Fig. 4.10(b), was $(3.24 \pm 0.05) \times 10^5$ n/sec in the ISS orbit and $(2.13 \pm 0.07) \times 10^7$ n/sec in the geosynchronous orbit. The *incoming* neutron current at this surface was $(1.44 \pm 0.02) \times 10^5$ n/sec and $(1.16 \pm 0.03) \times 10^7$ n/sec for the ISS and geosynchronous orbits, respectively.





(b) Incoming Neutron Current

FIGURE 4.10. Incoming Neutron Current Due to Isotropic Proton Bombardment of the SAFE-400 Reactor Core.

4.3.2 Incident Alpha Particles

As discussed in section 4.2.2, benchmarking studies performed to compare MCNPX calculations to experimentally determined thick target yields indicated that MCNPX was unreliable for alpha particle transport in the media of interest. An estimate of the secondary neutron production due to incident alphas was determined from published experimental thick target yield data (Anderson and Hertz 1971; Lone et al. 1981). Anderson and Hertz (1971) collected neutron production data for (α, n) reactions in a beryllium slab bombarded by low energy alpha particles (5 - 10 MeV) that were emitted in the decay of ^{210}Po , ^{241}Am , ^{238}Pu , ^{239}Pu , ^{242}Cm , ^{266}Ra , and ^{227}Ac . In order to use the published data in the present application, an empirical fit to a power law equation was determined using Microsoft Excel to obtain a relationship between the thick target yield and alpha particle energy:

$$\text{TTY} = 0.1917 E_{\alpha}^{3.5329}, \quad (4.3)$$

where the incident alpha particle energy (E_{α}) is given in MeV and the resulting TTY is in units of [neutrons/ 10^6 alpha particles]. Earlier data reported by Runnalls and Boucher (1956) for alpha particles emitted in the decay of ^{239}Pu , ^{241}Am , and ^{242}Cm ($E_{\alpha} = 5.14$, 5.47, and 6.10 MeV, respectively) were fit to the power law expression in Eq. (4.4) (Runnalls and Boucher 1956):

$$\text{TTY} = 0.144 E_{\alpha}^{3.65}. \quad (4.4)$$

As shown in Figure 4.11, the curve fits that were generated from each set of data fell along a similar trend, deviating no more than 15% from one another over the reported 3-10 MeV energy range.

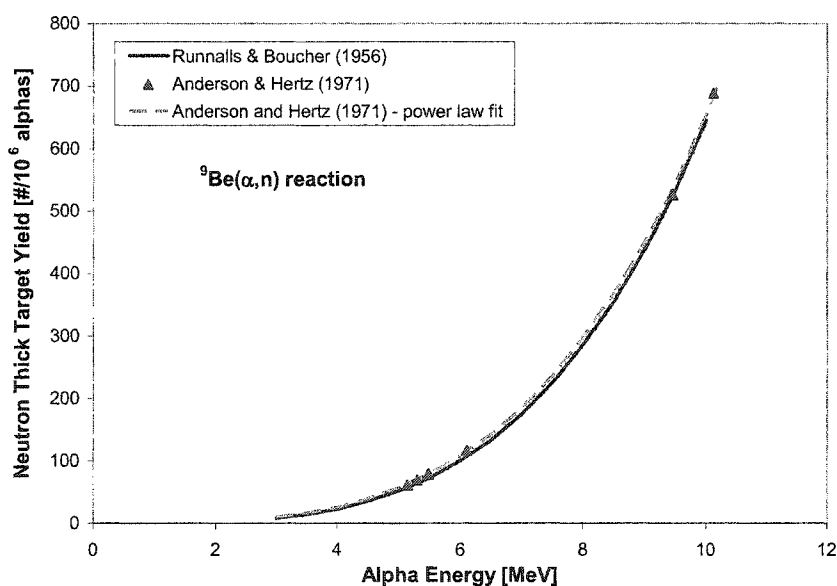


FIGURE 4.11. Experimentally Determined Thick Target Yield for Alpha Particles on Beryllium.

Data for higher energy alpha particles, 12-30 MeV, were reported by Lone et al. (1981). In this study, both neutron yield and the average energy of the outgoing neutron

($E_{n, avg}$) were reported as a function of the incident alpha energy. For use in the present application, the reported TTY data were again fit to a power law using Microsoft Excel (Eq. 4.5). The average neutron energy was fit to a third order polynomial expression using the least-squares fitting algorithm in Microsoft Excel (Eq. 4.6).

$$TTY = 0.3308 E_{\alpha}^{3.177} \quad (4.5)$$

$$E_{n, avg} = -1.4 \times 10^{-4} E_{\alpha}^3 + 1.2 \times 10^{-2} E_{\alpha}^2 + 2.2 \times 10^{-1} E_{\alpha} + 4.4 \quad (4.6)$$

The average neutron energy and incident alpha particle energy are expressed in MeV, and the TTY is again given in units of [neutrons/ 10^6 alpha particles]. A power law expression for the TTY was selected for comparison to the previous empirical expressions determined by Runnalls and Boucher (1956) and from data presented in Anderson and Hertz (1971). The experimentally determined thick target yield for 12-30 MeV alphas, and the corresponding neutron energy data, are shown in Figure 4.12.

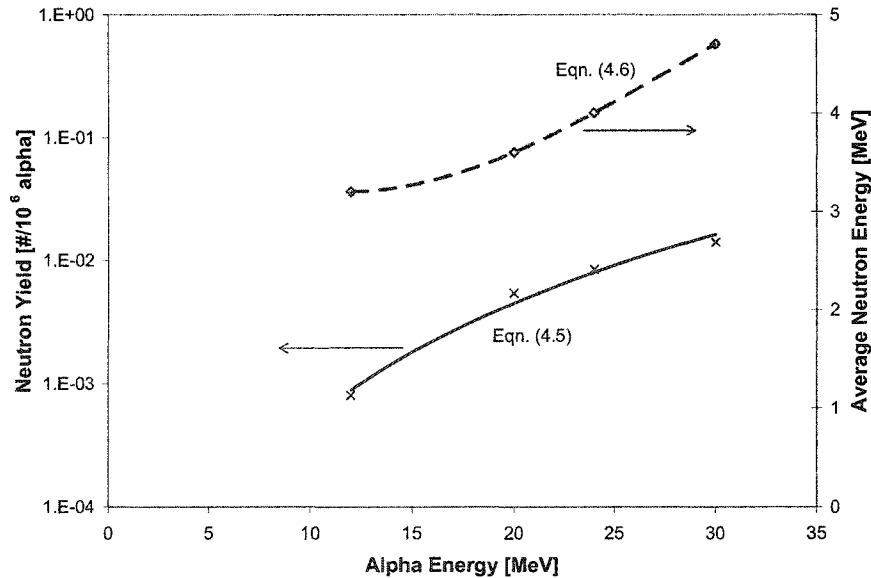


FIGURE 4.12. Experimentally Determined Thick Target Yield and Average Neutron Energy for Alpha Particles on Beryllium (Lone et al. 1981).

Although experimental data were only reported up to 30 MeV incident alpha particles in the literature, alpha particles in the space environment extend up to significantly higher energies (~100 GeV). Therefore, the empirical fit determined from the Lone et al. (1981) data for 12-30 MeV alpha particles was extrapolated to higher incident particle energies. This extrapolation was required to compute an estimate of the secondary neutron production in the SAFE-400 reactor core in which incident particles extended to higher energies. The resulting calculated neutron yield for the alpha particle environment in geosynchronous orbit is shown in Figure 4.13 for 0-30 MeV alphas. This estimated yield was compared to the neutron yield that resulted from incident protons to assess the relative importance of each component of the space radiation environment. The approximate alpha particle flux in geosynchronous orbit illustrated in Fig. (4.9) was applied in the analysis. Integrating over incident alpha energies up to 30 MeV, the range of applicability of the empirical equation (Eq. 4.5), the total neutron production was approximately 2.9×10^5 n/sec. Extrapolating to 100 MeV incident alpha energy increased the estimated neutron production due to incident alphas to 2.5×10^6 n/sec. Note, however, that this neutron yield corresponds to the total number of neutrons that would be produced in the beryllium by the incident alpha particles, but does not represent the fraction of those neutrons that would actually be transported through the beryllium to the core region. For incident protons in the same orbit, the total incoming neutron current at the reactor core surface was $1.16 \pm 0.03 \times 10^7$ n/sec. Because the proton induced incoming neutron current was an order of magnitude greater than the total alpha induced current, secondary neutrons produced by alpha interactions in the beryllium reflector were neglected in the current analysis.

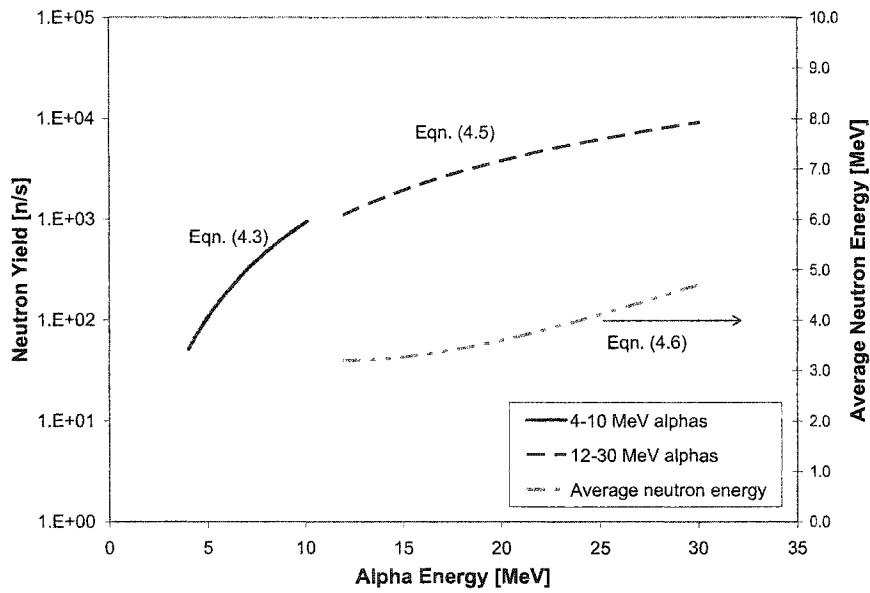


FIGURE 4.13. Calculated Neutron Yield Due to Alpha Source in Geosynchronous Orbit.

4.3.3 Non-Isotropic Radiation Source

For the calculations presented in sections 4.3.1 and 4.3.2, the reactor module was isotropically bombarded by the external radiation source. However, as discussed in section 2.3, the radiation environment demonstrates a measurable directional preference. Models of the space radiation environment typically assume that a satellite or spacecraft will rotate along its trajectory, such that over the extended mission time, all surfaces of the vehicle would be exposed to approximately the same total radiation fluence. In the present discussion on reactor start-up control, the effect of the radiation environment is of interest over much shorter time periods (e.g., seconds to minutes). In this regime, the directional component of the radiation source may be important to the system because each surface of the reactor core may not be exposed to the same radiation source during the start-up maneuver.

Using the worst-case proton source in geosynchronous orbit, three source-target configurations were considered. Each geometry utilized the same proton source spectrum

and source strength, but the variations in the configuration for each case required that a different normalization be applied. In each case, the calculated results were multiplied by a “normalization factor.” Because MCNPX reports tallies per source particle, each result must be multiplied by the fraction of source particles that actually strike the reactor module per second.

Model data (e.g., CREME96) generally provides an estimate of the angular particle flux in terms of particles/m²-sec-sr-MeV. Removing the angular dependence requires analysis of the source-target geometry. The particle flux, $\phi(E)$ can be determined from the angular dependent particle flux by integration over the direction of flight of the source particles (Eq. 4.7). The particle current, $J(E)$, is determined by including the direction of travel of each particle (Eq. 4.8).

$$\phi(E) = \int \Psi(E) d\hat{\Omega} \quad (4.7)$$

$$\bar{J}(E) = \int \Psi(E) \hat{\Omega} d\hat{\Omega} \quad (4.8)$$

Because model data is also dependent on the area through which the particles travel, integration over the target area is also required:

$$\bar{J}(E) = \int \int \Psi(E) \hat{n} \cdot \hat{\Omega} d\hat{\Omega} dA. \quad (4.9)$$

The energy dependence of the particle current and flux is removed by integration over all energies:

$$\Psi_{CR} = \int_{E_1}^{E_2} \Psi(E) dE, \quad (4.10)$$

where Ψ_{CR} is the total angularly dependent particle flux in units of particles/m²-sec-sr. In practice, this integration was performed numerically by multiplying $\Psi(E)$ (predicted by CREME96) at each energy E by the discrete energy bin width (difference between current and previous energy). This resulted in an estimated particle flux, Ψ_{CR} , which was held constant for all source-target geometries.

First, the reactor module was bombarded isotropically. An illustration of this configuration is provided in Figure 4.14. The isotropic source geometry was constructed in MCNPX by surrounding the core with an 80 cm radius spherical shell, where the center of the reactor core was located at the center of the sphere. The axial height of the reactor core was 58 cm and the radius of the radial reflector was 24 cm, such that the 80 cm radius source sphere fully encompassed the core. The spherical surface was used to define a proton source directly radially inward, such that all source protons were directed toward the reactor core.

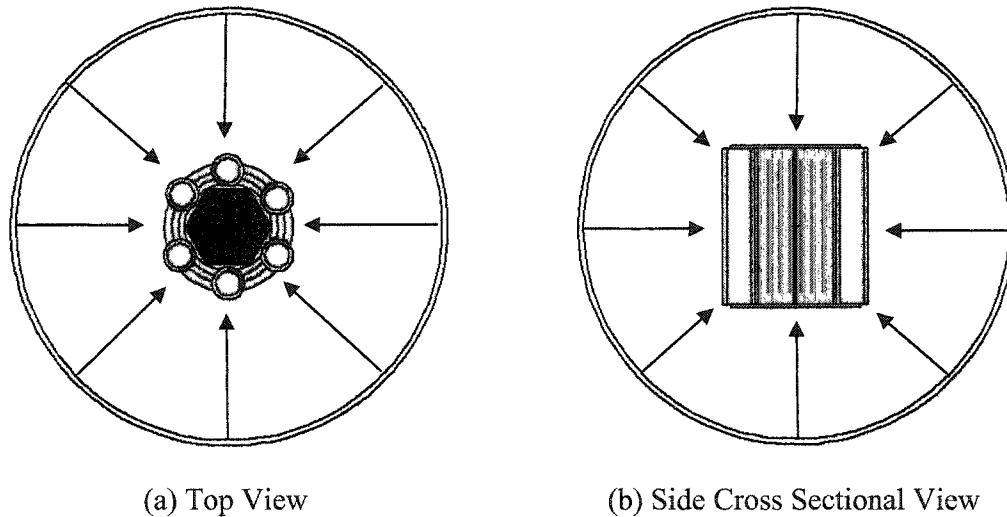


FIGURE 4.14. Isotropic Proton Bombardment of the SAFE-400 Reactor.

For an isotropic source, emitted from a spherical surface and directed inward, the total source current is derived in Eqs. (4.11). Because source particles can strike all surfaces of the reactor module, the approximate target area is given by $A = 2\pi R(R+H)$, the total surface area of a cylinder.

$$\begin{aligned}
\bar{J}_{Total} &= \int \int_{\bar{\Omega} \cdot \hat{n} < 0} |\bar{\Omega} \cdot \hat{n}| \Psi_{CR} dA d\Omega \\
&= \Psi_{CR} A \int_0^{2\pi} \int_0^1 \mu d\mu d\gamma \\
&= \Psi_{CR} A 2\pi (1/2) \\
&= \Psi_{CR} (2\pi R (R + H)) \pi \\
&= \Psi_{CR} 2\pi^2 R (R + H)
\end{aligned} \tag{4.11}$$

Second, a planar surface source was defined to assess the effect of directional sources. Source protons were defined to be monodirectional (parallel beam), emitted normal to the source plane. In this configuration, source particles entered from the top of the core and were directed along the axis of the reactor module. This source-target geometry is illustrated in Figure 4.15. The source was defined to fully cover the reactor module, such that all source particles entered the core or radial reflector (e.g., a source-target view factor of one). This directional source configuration will be referred to as the “axial proton source.”

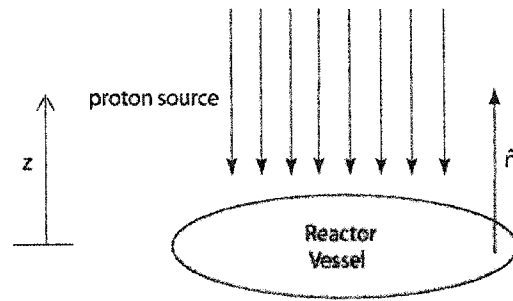


FIGURE 4.15. Illustration of the Axial Proton Source Configuration.

To determine the appropriate normalization for the axial proton source, directed along the z-axis, the total source current is derived in Eqs. (4.12). In this source-target geometry, the source only strikes the top surface of the reactor vessel, such that $A = \pi R^2$.

$$\begin{aligned}
\bar{J}_{Total} &= \int \int_{\bar{\Omega} \cdot \hat{n} < 0} \delta(\hat{\Omega} - \hat{z}) \left| \bar{\Omega} \cdot \hat{n} \right| \Psi_{CR} dA d\Omega \\
&= \Psi_{CR} A \int_0^{2\pi} \int_{-1}^0 \delta(\mu + 1) \mu d\mu d\gamma \\
&= \Psi_{CR} A 2\pi \int_{-1}^0 |\mu| \delta(\mu + 1) d\mu \\
&= \Psi_{CR} A 2\pi 1 \\
&= \Psi_{CR} 2\pi^2 R^2
\end{aligned} \tag{4.12}$$

Third, the axial surface source was modified to provide a monodirectional source in which source particles entered from the side of the reactor, perpendicular to the core axis. This source-target geometry is illustrated in Figure 4.16. As in the axially directed source, the source plane was defined such that it extended over the full height and width of the reactor module. Therefore, all source particles emitted from the source plane entered the core and reflector. This configuration will be referred to as the “planar proton source.”

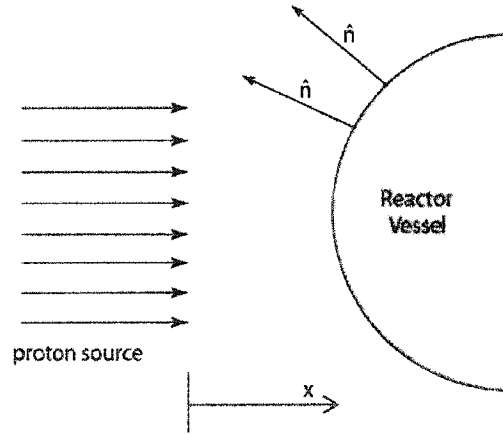


FIGURE 4.16. Illustration of the Planar Proton Source Configuration.

To determine the appropriate normalization for the source directed along the x-axis, the total source current is derived in Eqs. (4.13). Note that because the direction of $\hat{n}$ varies as one moves around the cylinder, $\mu = \cos\theta$ has been substituted in order to

complete the integration. In this case, the target area seen by the incoming proton source is a half cylinder.

$$\begin{aligned}
 \bar{J}_{Total} &= \int_{\vec{\Omega} \cdot \vec{n} < 0} \int \delta(\hat{\Omega} - \hat{x}) \left| \vec{\Omega} \cdot \vec{n} \right| \Psi_{CR} dA d\Omega \\
 &= \Psi_{CR} H \int_0^{2\pi} \int_{-\pi/2}^{\pi/2} R \cos \theta d\theta d\gamma \\
 &= \Psi_{CR} H 2\pi \int_{-\pi/2}^{\pi/2} R \cos \theta d\theta \\
 &= \Psi_{CR} H R 2\pi 2 \\
 &= \Psi_{CR} 4\pi R H
 \end{aligned} \tag{4.13}$$

Neutron flux results for the three configurations described above are illustrated within the corresponding plot of the neutron flux in the reactor core, provided in Figures (4.18-4.20). For each of the three configurations, the reactor control drums were rotated to 60°, providing $k_{eff} = 0.9770 \pm 0.0002$. Although the neutron population in the radial reflector is not shown in the plots, this was also included in the MCNPX simulation. Figure 4.17 illustrates the location of each cross section that was plotted for each of the source geometries. For all configurations, the centermost cross section (plotted in light blue and identified as “Center”) demonstrated the highest neutron flux and the outermost cross section (plotted in orange and identified as “Edge”) demonstrated the lowest neutron flux.

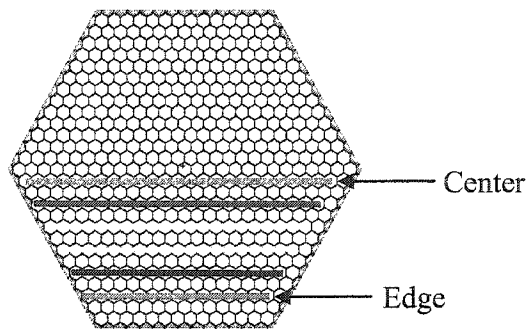


FIGURE 4.17. Identification of Cross Section Locations for Figures 4.18-20.

On initial observation, all three source-target geometries result in a similar neutron flux shape, all approximating the expected shape of the fundamental mode seen in an operating nuclear reactor. On closer inspection, the axially directed proton source was noted to produce a more centrally peaked neutron flux than the other configurations. This result is likely due to the interaction mechanisms generating the neutrons in the reactor core. The axially directed protons interact with core materials (e.g., UN fuel) to generate neutrons, without first having to travel through the reflector. Although some neutrons are also generated in the surrounding beryllium reflector, these neutrons tend to have velocity in the axial direction (along the same vector as the source protons) and, therefore, have a very low probability of reaching the central core materials where they can initiate further neutron producing reactions.

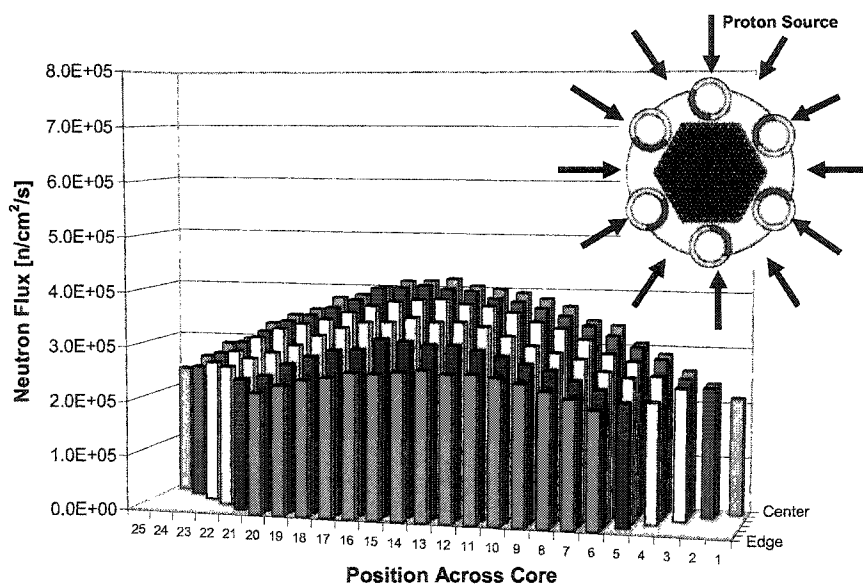


FIGURE 4.18. Neutron Flux in the SAFE-400 Reactor Core: Isotropic Proton Source.

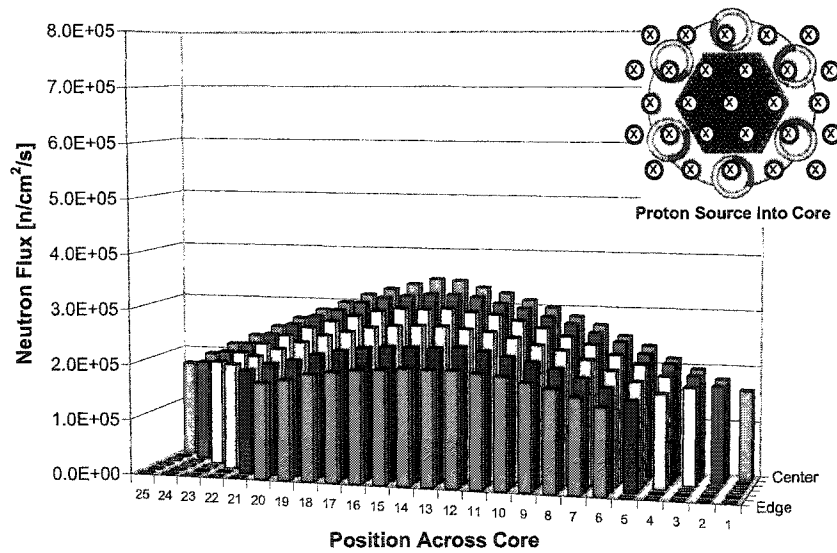


FIGURE 4.19. Neutron Flux in the SAFE-400 Reactor Core: Axial Proton Source.

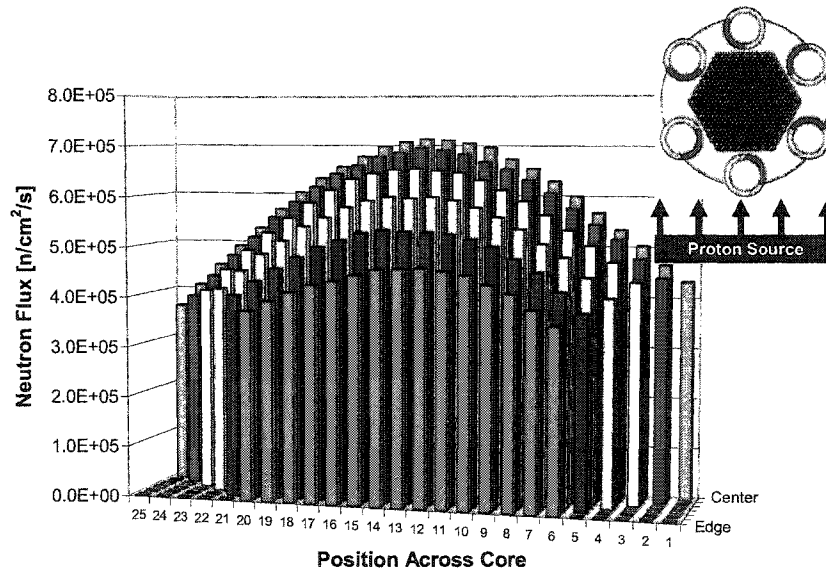


FIGURE 4.20. Neutron Flux in the SAFE-400 Reactor Core: Planar Proton Source.

The most distinct observation from these results is the increased magnitude of the neutron flux in the SAFE-400 core for the planar proton source (Fig. 4.20). In this case, the total neutron flux is approximately twice the magnitude calculated for the other configurations. This result illustrates the importance of (p, n), (p, 2n) and other neutron

producing reactions that occur in beryllium. All neutrons entering the reactor in this configuration must travel through the reflector before reaching the core. Although some high energy protons may reach the core materials without interaction, the vast majority will interact with the beryllium, producing secondary particles that may then reach the reactor core and the nuclear fuel, where they have a chance to influence the fission process.

4.3.4 Jovian Environment

As discussed in Section 2.3.2, the Jovian radiation environment is extremely intense relative to near Earth space. The data shown in Section 3.1.4 (Fig. 3.3) correspond to the trapped proton environment in an orbit around Europa, in addition to a rough estimate of the GCR contributions using the solar minimum data in geosynchronous orbit. An MCNPX calculation was performed for this environment using an isotropic source configuration. The resulting neutron current at the core surface is shown in Figure 4.21. Integrating over all neutron energies, the incoming neutron current at the hexagonal core surface in the approximate Jovian proton environment was $(0.26 \pm 0.02) \times 10^7$ n/sec. Previous results reported for geosynchronous orbit during the peak five minutes of the October 1989 solar particle event indicated an incoming secondary neutron current that was approximately five times greater, $(1.16 \pm 0.03) \times 10^7$ n/sec, at the same hexagonal core surface. Recall, however, that the peak 5 minute SPE was an extreme worst-case scenario for near Earth space, whereas the Jovian radiation environment was estimated at nominal solar minimum conditions. The approximate secondary neutron current in the Jovian environment was nearly 20 times greater than that predicted for the nominal solar minimum proton environment in the ISS orbit (ISS current = $(1.44 \pm 0.02) \times 10^5$ n/sec).

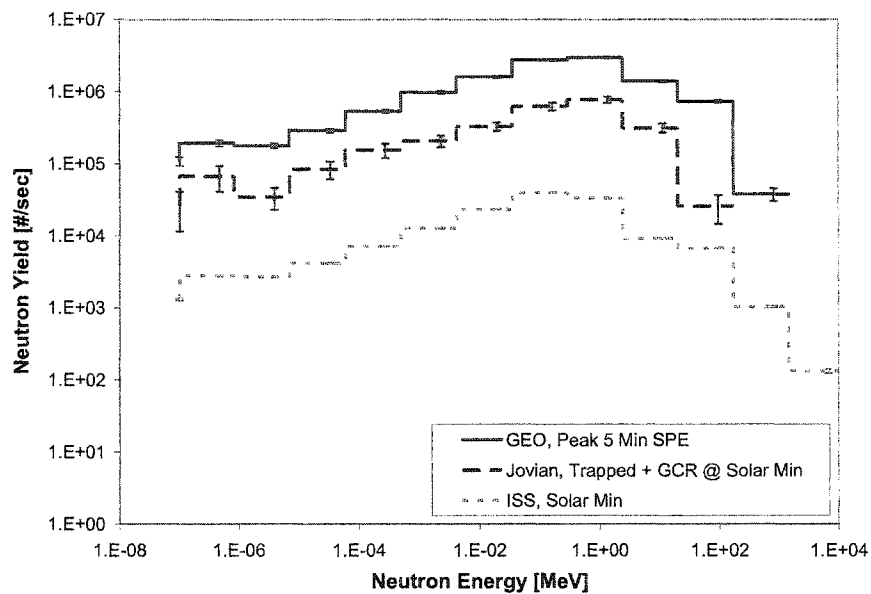


FIGURE 4.21. Secondary Neutron Flux at the Surface of the SAFE-400 Reactor Core.

The intensity of the Jovian radiation environment under nominal conditions (during solar minimum or solar maximum) suggests that extreme care should be taken in designing any system for operation in this environment. For a mission to the Jovian system powered by a reactor in a nuclear electric propulsion vehicle, the reactor would be fully operational at its nominal design power level prior to reaching Jupiter. Although nominal operation would not require shutdown and restart in the Jovian system, reactor designers should have an understanding of the potential interaction of the extreme radiation environment near Jupiter should low power transients become necessary.

4.4 Reactor Control Methodologies

Application of the model based predictive control methodology to the start-up of a fast spectrum reactor to low power is demonstrated. All calculations assume an initially cold, zero-power condition and are optimized to achieve a goal power level of 1.0 kW_t. No maximum or minimum time was established as a constraint on achieving this goal. Initial calculations assume a constant incoming neutron source of 10⁶ neutrons/second,

comparable to an average secondary neutron source produced by incident protons in near-Earth space (e.g., an average of the neutron production in the solar minimum ISS orbit and the geosynchronous orbit during the peak 5 minute SPE).

Development of an optimized control methodology was accomplished in a staged manner, beginning with control algorithms based on the angular position of the drums, and then moving to control algorithms based on the angular velocity of the control drums. Initial analyses applied a relatively long prompt neutron lifetime of 10^{-3} sec in the point kinetics model to minimize computation time. Once a successful control algorithm was determined for angular velocity based control, indicated by a successful, stable start-up to the desired power level, a more accurate prompt lifetime of 10^{-7} sec for a fast spectrum reactor was adopted in all calculations. The final control algorithm was also applied in the presence of a temporally varying background source to assess the ability of the controller to respond to changes in the external neutron environment at various stages in the reactor start-up (e.g., during the initial power rise and during steady-state operation). Results are provided below for each stage in the development of the control algorithm.

4.4.1 Control Drum Motion and Reactivity

The reactivity as a function of control drum position θ (in degrees) was computed using several kcode calculations in MCNPX, with the control drums incremented 30° in each subsequent calculation. These results are shown in Figure 4.22, where the error bars were calculated from the statistical error in the MCNPX calculation for k_{eff} using Eq. (3.11). In all cases, the error in the calculated reactivity was less than 0.04% and error bars may be difficult to discern.

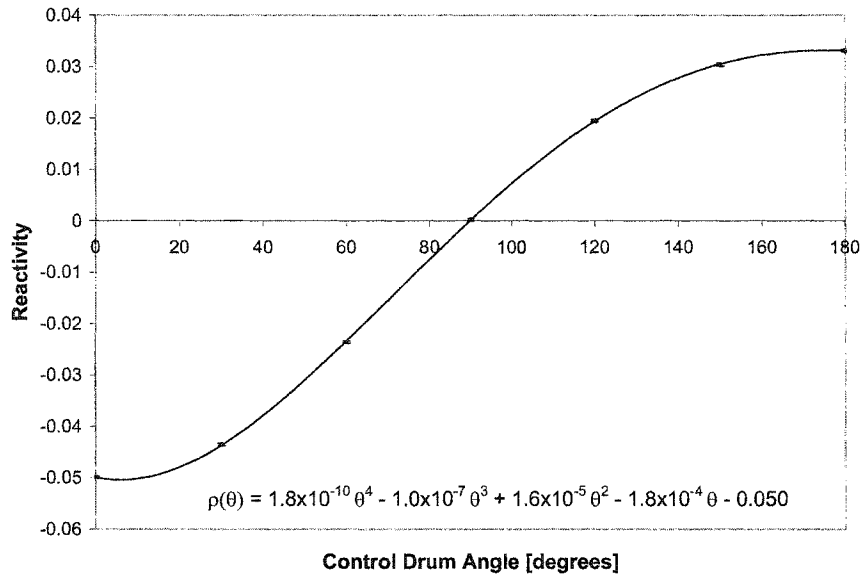


FIGURE 4.22. Reactivity as a Function of Control Drum Position, SAFE-400.

A fourth order polynomial was fit to the kcode results using a least-squares fit in Microsoft Excel. When a fit is calculated in Excel, an “R-squared” value is also reported to indicate the “goodness of fit” to the actual data; R-squared can range from zero to one, with one corresponding to a good fit. The polynomial fit presented in Figure 4.22 is provided in Eq. (4.14), where $R^2 = 1$, indicating that all data points were precisely intersected:

$$\rho(\theta) = 1.81 \cdot 10^{-4} \theta^4 - 1.00 \cdot 10^{-7} \theta^3 + 1.57 \cdot 10^{-5} \theta^2 + 1.79 \cdot 10^{-4} \theta - 4.99 \cdot 10^{-2}. \quad (4.14)$$

The above empirical equation was used in the reactor model implemented by the control algorithm to assess the effect of changing control drum position on the reactivity.

4.4.2 Reactor Start-Up: Constant External Source

In the original construction of the cost function (Eq. 3.12), one may note that each of the terms was expressed in different units, such that the additional dependencies are

rolled into the weighting coefficients a and b , which therefore cannot be unitless parameters:

$$J = \text{penalty} \cdot \left[\int_{t_0}^{t_0+h} (n(t) - n_{goal})^2 dt + a[u(t) \cdot u(t)] + b[\Delta u(t) \cdot \Delta u(t)] \right]. \quad (3.12)$$

To account for the difference in the magnitude of each of the cost terms, the coefficients (a , b) must be relatively large, and appropriate values were not immediately evident.

In the MatLab control program, the cost function was written in terms of the reactor neutron population, $n(t)$ and n_{goal} , as opposed to the actual fission power. A goal power level, P_{goal} , was selected at the start of the program, and the corresponding n_{goal} was calculated using Eq. (2.11). A fission power of 1.0 kW_t was selected for all start-up control calculations. To simplify the calculations in the initial control runs, the prompt neutron lifetime was set to 10^{-3} sec rather than the 10^{-7} sec typical of a fast spectrum reactor; the correct prompt lifetime was applied in later control runs after details of the control methodology were worked out.

At 1.0 kW_t and a prompt neutron lifetime of 10^{-3} sec, the corresponding neutron population would be 6.9×10^{10} . Hence, an initial guess for the weighting parameters a and b in the cost function (Eq. 3.12) might be on the order of $(10^9)^2$, since a small difference between the current neutron population and the goal neutron population would be on the order of $(10^9)^2$. For example, if the neutron population at some time t was 6.8×10^{10} , the initial term in the cost function would be on the order of $(6.9 \times 10^{10} - 6.8 \times 10^{10})^2 = 10^{18}$. As a result, the a and b parameters had to be set to very large values to be of any significance to the cost function. This approach was applied in the initial theta control option, where a and b were alternately set to either zero or 10^{18} .

4.4.2.1 Drum Position, $\theta(t)$, Control

Calculations were performed for various control parameters using the MatLab 6.1 control program constructed to control reactor start-up by the physical drum position.

Multiple combinations of parameters were assessed to determine the optimal cost function for start-up control. In the initial trials, four conditions were considered for the weighting of the cost to control:

Trial	<i>a</i>	<i>b</i>
1	0	0
2	0	10^{18}
3	10^{18}	10^{18}
4	10^{18}	0

TABLE 4.2. Weighting Coefficients for $\theta(t)$ Control.

In each of these trials, the prompt neutron lifetime was set to 10^{-3} sec and control parameters were set to a control horizon of 5 seconds and a control time step of one second; the control loop was only repeated for 50 seconds in each case and long term effects of the control were not assessed. In plots provided for each trial, the plot on the left shows the position of the control drums (all six drums are assumed to move simultaneously), where the solid curve represents the initial guess provided to the controller for the first control horizon and the dashed curve represents the optimized control drum positions. After the first step in the control was determined by the optimization routine, the first element in $u(t)$ was transmitted to the plant, and the initial guess for the next control step is determined from the set of candidate controls from the previous step. For instance, the initial guess provided to the controller in each of the $\theta(t)$ control trials ($u(t) = \theta(t)$) was:

$$\theta_{guess}(t) = [108 \ 105 \ 100 \ 98 \ 96],$$

where each element in the control vector represents the control drum position in degrees. In the initial step of the control for Trial 1 ($a = 0$, $b = 0$), the optimized control vector returned by the optimization routine was:

$$\theta(t) = [113.9821 \ 103.4324 \ 99.4160 \ 97.1349 \ 96.7509].$$

The first element in this vector was transmitted to the system, setting the control drums to 113.9821° ; the remaining elements were used to construct the initial guess for the control vector in the next control step. Hence,

$$\theta_{guess}(t) = [103.4324 \ 99.4160 \ 97.1349 \ 96.7509 \ 96.7509],$$

where the final element in $\theta(t)$ was repeated in $\theta_{guess}(t)$ to maintain the proper vector length. This process was repeated throughout the control loop to provide an initial estimate for the control vector to the optimization routine.

Results of Trial 1 are provided in Figure 4.23, where the cost function was fully dependent on the reactor power level and no cost was attributed to the control ($a = 0$, $b = 0$). It is immediately evident that if the drums could be instantaneously positioned, with no limitations set on the control drum drive mechanisms, the desired power level can be obtained in a very short time. On initial inspection, the resulting control for Trial 1 appears to be very good over the time period that was analyzed, with the control drums settling at approximately 90° for steady-state operation at 1 kW. No long term controller stability was assessed in the $\theta(t)$ control trials.

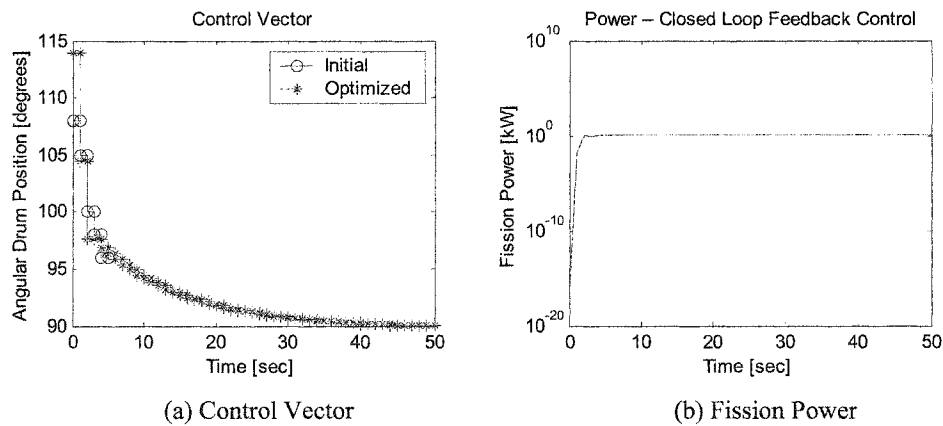


FIGURE 4.23. Control Dependent on Drum Position: $a = 0$, $b = 0$.

Focusing on the initial control region in Figure 4.24, where power has just attained the goal power level of 1 kW, a slight overshoot of the goal power level is evident when no cost is applied to the control itself ($a = 0$, $b = 0$).

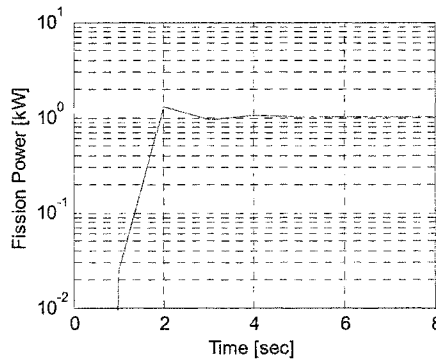


FIGURE 4.24. Control Dependent on Drum Position: $a = 0$, $b = 0$, Zoom Showing Slight Overshoot in Power.

In Trial 2, a cost was placed on the change in the control ($\Delta\theta(t)$), penalizing large changes in the drum position from one control time step to the next. Figure 4.25(a) indicates that the initial control maneuver rotated the drums to a much larger angle than in Trial 1, when the cost was dominated by the neutron population term, but in subsequent steps the controller moved the control drums close to their final position, such that only small adjustments to the drum position were required in later steps when the fission power was approximately equal to P_{goal} . Additionally, the controller allowed essentially no overshoot of the goal power level (Fig. 4.26) and the desired power level was achieved in half the time observed for Trial 1 (1 sec vs. 2 sec in Trial 1).

Trial 3 considered the effect of adding the middle term to the cost function ($a \neq 0$), where a cost was applied to the control itself ($\theta(t)$). These results are provided in Figure 4.27. The response of the drum position to the control loop was very similar to that observed in Trial 2, with only a very slight overshoot of the goal power level observed in Fig. 4.27(b).

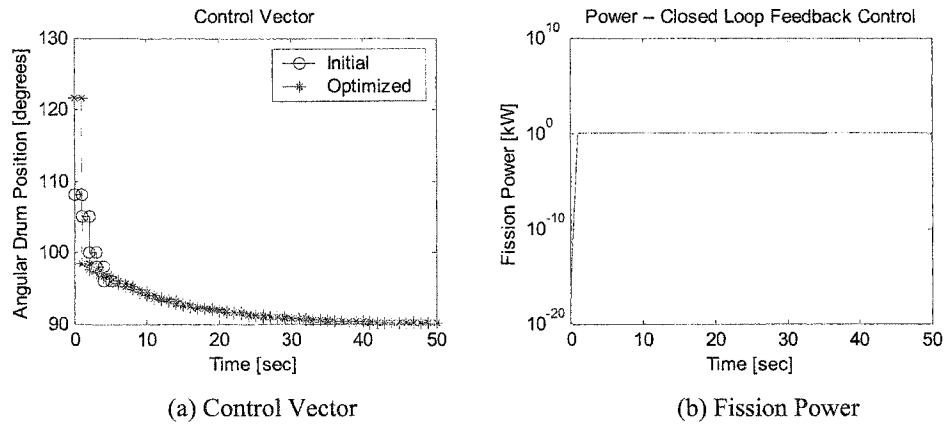


FIGURE 4.25. Control Dependent on Drum Position: $a = 0$, $b = 10^{18}$.

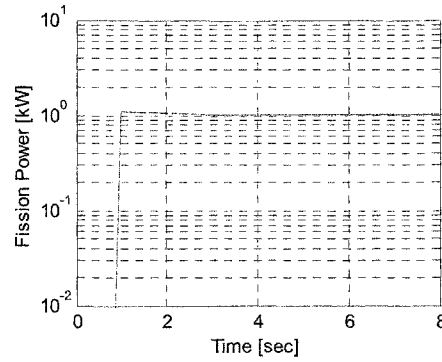


FIGURE 4.26. Control Dependent on Drum Position: $a = 0$, $b = 10^{18}$, Demonstrating Minimal Overshoot in Power and Achievement of Goal in One Second.

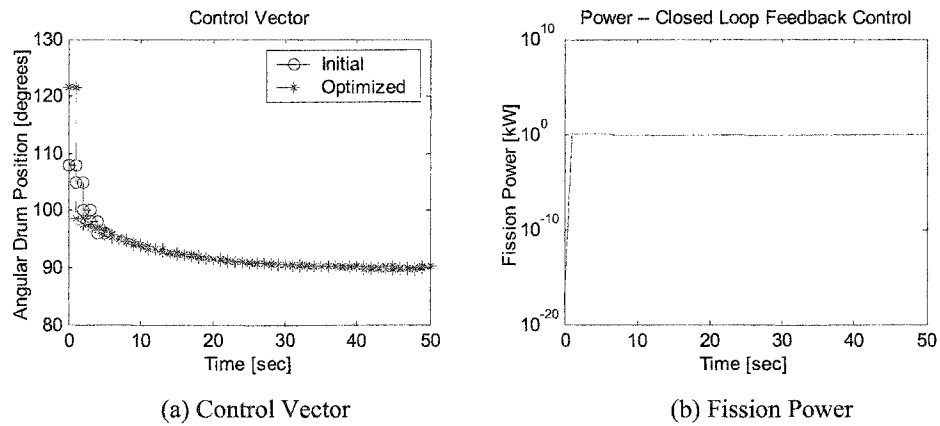


FIGURE 4.27. Control Dependent on Drum Position: $a = 10^{18}$, $b = 10^{18}$.

In the final trial of the $\theta(t)$ controller, the cost applied to changes in the control drum positions ($\Delta\theta(t)$) was set to zero, such that the cost function was dependent on only the fission power level and the control drum position. It is immediately evident in Figure 4.28 that this form of the cost function was ineffective. In this trial, once the cost to the power dropped to essentially zero ($P(t) \sim P_{goal}$), the cost function became dominated by the cost associated with the control drum position. To reduce this cost, the controller moved the drums to zero degrees, and the reactor fission power began to slowly drop once again. If this controller were considered over a longer period, it is expected that as the fission power continued to drop, the cost function would again be dominated by the first term, and the controller would again move the control drums to a larger angle to compensate, resulting in a periodic pulsed operation.

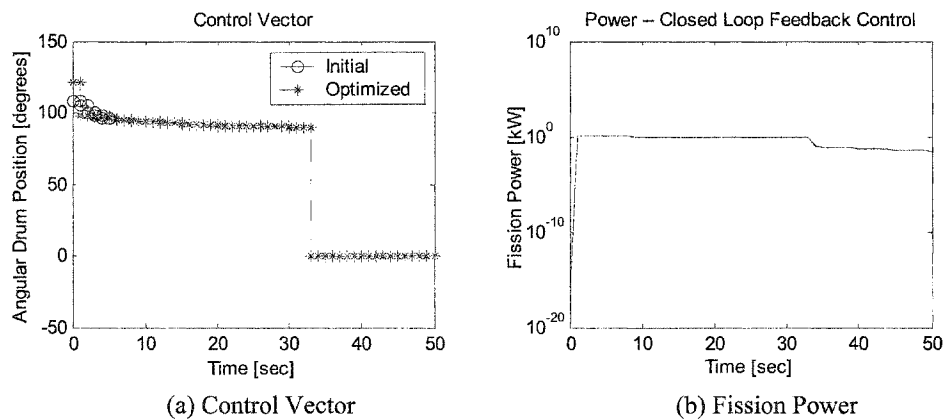


FIGURE 4.28. Control Dependent on Drum Position: $a = 10^{18}$, $b = 0$.

From the trials presented in Figures 4.23-28, it is evident that the best formulation of the cost function included penalties associated with the current state of the reactor neutron population (or fission power) relative to the goal condition, and with the magnitude of the change in the control vector at each control step. A cost placed on the physical control drum position suggested potential problems would be encountered in

long term control. After this was observed in Trial 4 ($a = 10^{18}$, $b = 0$), Trial 3 ($a = 10^{18}$, $b = 10^{18}$) was repeated over a longer time to determine if this problem would again arise during steady-state control. The results presented in Figure 4.29 indicate that at long times, the cost to the control variable again dominated the cost function at long times, causing the drum position to drop to zero to reduce the cost, which subsequently resulted in decreased fission power. These results verify the original assertion that for the $\theta(t)$ controller, the weighting parameter a should be set to zero.

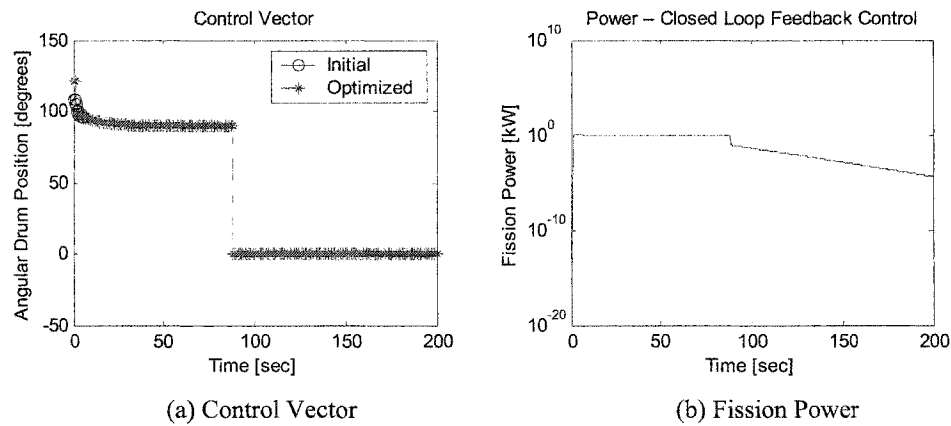


FIGURE 4.29. Control Dependent on Drum Position: $a = 10^{18}$, $b = 10^{18}$, Application of Control Over Extended Time.

4.4.2.2 Angular Velocity, $\omega(t)$, Control

The MatLab 6.1 control program was modified to control reactor start-up based on the angular velocity of the control drums. Initial analyses maintained all three terms in the cost function, with no maximum drum turning rate defined, and with the goal of the controller being to attain the desired power level of 1.0 kW_t in a minimum time.

Determination of a more appropriate cost function for $\omega(t)$ control is also discussed.

As in the $\theta(t)$ controller, all six drums were assumed to move simultaneously. An initial guess for the control vector was supplied to the control algorithm at the start of

the control loop, the control vector was optimized using the objective (cost) function, and the first element of the optimized vector was transmitted to the system. The initial guess for the next control step was again determined from the set of candidate controls from the previous step.

Initial application of $\omega(t)$ control illustrated the benefits of using a closed loop control methodology versus an open loop controller. In open loop control, the entire optimal control vector is determined before any control signal is transmitted to the plant. In this case, the final optimal control vector determined from the closed loop calculations was applied in a single step to the reactor, and the resulting reactor power was reported, without opportunity of feedback to the controller at the end of each control step. As in the $\theta(t)$ controller discussed above, these initial trials applied a prompt neutron lifetime of 10^{-3} sec to simplify calculations. Control parameters were set to a control horizon of 5 seconds and a control time step of one second, and the control loop was repeated for 100 seconds. For this trial, the weighting parameters were somewhat arbitrarily selected at $a=b=10^{13}$, such that the cost to control was only significant when the reactor power had approached the desired level. Results of this trial are shown in Figure 4.30. Note that the control vector is shown over a shorter time scale (25 sec) than the remaining plots to allow the detail at early times to be observed.

The trial shown in Fig. 4.30 again applied a goal power level of 1 kW_t. The initial guess for the control vector, in degrees/sec, was [160 0 0 0 0]; only the optimized control vector is shown in the results presented in Fig. 4.30(a). Using the angular velocity of the control drums to define the control vector, start-up to 1 kW_t was accomplished in under 5 seconds with minimal power overshoot. The resulting angular position of the control drums is shown in Fig. 4.30(c), where the final position of the control drums was 89.84 degrees.

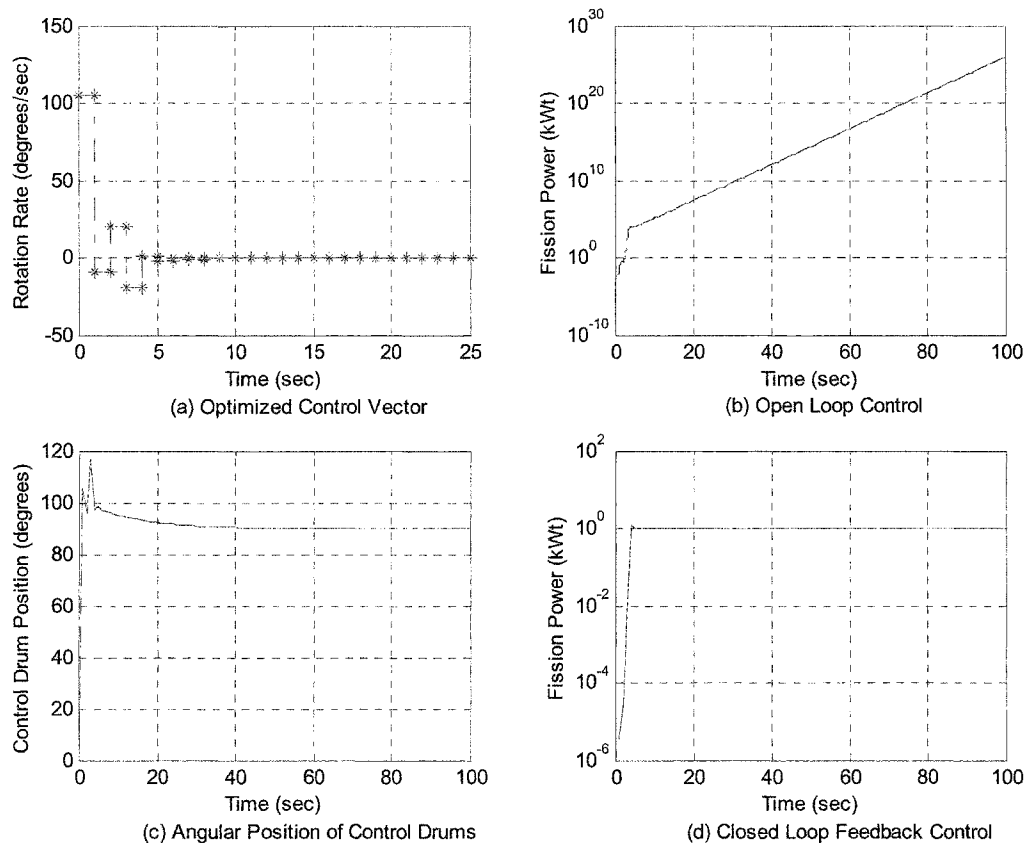


FIGURE 4.30. Sample Reactor Startup Control Program to 1 kW_t for $\omega(t)$ Controller.

Two separate power trajectories are shown in Fig. 4.30(b) and 4.30(d). The trajectory in Fig. 4.30(d) corresponds to the reactor power when the control was applied in a closed loop. The optimal control step was re-calculated at each sampling interval, and the control vector was modified to the appropriate value based on the present conditions in the plant. The trajectory in Fig. 4.30(b) corresponds to an open loop system. To assess the application of open loop control, the optimal control vector determined in the closed loop program was applied in a single step to the reactor system for the total control time. Without the feedback provided in closed-loop control, however, the plant had no mechanism to make adjustments. The small numerical differences that result from solving the PKE on a short (~ 1 sec) versus a long (~ 100 sec)

time scale in each implementation caused the “after-the-fact” open loop calculation to drift away from the desired power level because there was no active controller to push it back to the appropriate value. The open-loop system is unstable, so errors accumulate and the system drifts away from the desired power. This result clearly demonstrates the superior performance of the system when under feedback control.

4.4.2.3 Cost Function Performance for Angular Velocity Control

Analysis was performed to determine optimal weighting for each component in the cost function. In the process of determining the appropriate control parameters for $\omega(t)$ control, it became evident that it may be beneficial to adjust the cost function such that the weighting parameters were unitless. This was accomplished by assessing the proper unit conversions that would result in each term in the cost function having the same units. Recalling the original formulation of the cost function from Section 3.3.2,

$$J = \text{penalty} \cdot \left[\int_{t_0}^{t_0+h} (n(t) - n_{goal})^2 dt + \int_{t_0}^{t_0+h} (a[u(t) \cdot u(t)] + b[\Delta u(t) \cdot \Delta u(t)]) dt \right], \quad (3.12)$$

we see that the units of the first term in the cost function are (neutrons)², which is then integrated over the control horizon. The units of the second term, weighted by a , are (radians/time)². Hence, a proper conversion factor would have units of (neutrons*time/radians)². Although it was initially unclear which time or angular scale would be appropriate, the factor

$$a(n_{goal})^2 \left(\frac{\Delta t_c}{\pi} \right)^2 \quad (4.15)$$

was introduced to the cost function to replace the original weighting coefficient a , where Δt_c is the length of each control step in seconds. Because the derivative term (weighted by b) was not appropriate in $\omega(t)$ control (discussed in Chapter 3), this term was dropped from the cost function. The weighting factor was introduced in the second integral term to render the integrand unitless. Because n_{goal} was not a function of time, it was moved

outside of the integral. The resulting cost function for $\omega(t)$ control is now given by Eq. (4.16):

$$J = f_{\theta} \cdot f_{\omega} \cdot \left[\int_{t_o}^{t_o+h} (n(t) - n_{goal})^2 dt + a (n_{goal})^2 \int_{t_o}^{t_o+h} \left(\omega(t) \frac{\Delta t_C}{\pi} \right)^2 dt \right]. \quad (4.16)$$

This final expression allowed simpler selection of the weighting parameter a , which was now expected to be on the order of one or smaller.

After implementing the modified cost function for the controller, all applications of the control algorithm applied the prompt neutron lifetime of 10^{-7} seconds that is more appropriate for a fast spectrum reactor. Control runs performed to determine appropriate control parameters included a maximum turning rate for the control drums, such that the simulated reactor start-up better modeled the start-up of an actual reactor system. All subsequent runs applied the f_{ω} penalty as defined in Eq. (3.16). The maximum angular velocity of the drums was set to a relatively small value of 0.0175 radians/sec (1° /sec) or 0.01 radians/sec (0.6° /sec) in all applications that applied this penalty. During a reactor start-up in space, the control drums will only be allowed a minimal rotation rate to prevent inadvertent supercriticality. As the reader will recall from Section 1.1.4, the maximum control drum rotation rate for the Topaz II reactor was 1.4° /sec. Because the reactor control system has not yet been determined for a potential mission, the corresponding physical limitation to the maximum drum rotation rate is unknown. The maximum values selected represent a reasonable range that could be applied to reactor start-up.

Proper selection of the modified, unitless weighting parameter a was investigated. Three values were considered: $a = 0$, $a = 1$, and $a = 0.01$. Selection of $a = 0$, such that the cost function was optimized over only the neutron population as a function of time, resulted in instabilities in the control loop. Figure 4.31 demonstrates controller instability after approximately 90 seconds of operation, resulting in infinite fission power. Analysis

of the candidate control vectors at each control step indicated that late in each prediction horizon, the predicted value for ω was increasing. Recall, however, that the predicted controls are not immediately applied; only the first element in the optimized vector is applied to the system at a given time, with the later portion of the vector providing an initial guess for the next control time step. Because no cost was applied to the control vector, no limitation was set on the amount of control drum motion at any time during the horizon, with the exception of the f_{wmx} penalty. A large ω value late in the prediction horizon reduced the cost to the reactor power term (large ω resulted in increased $P(t)$ late in the horizon) with no added cost due to the large control maneuvers. Although these values were not transmitted to the plant initially, they generated errors that eventually caused the system to become unstable as the large maneuvers became necessary to reach and maintain the desired power level earlier in the prediction horizon.

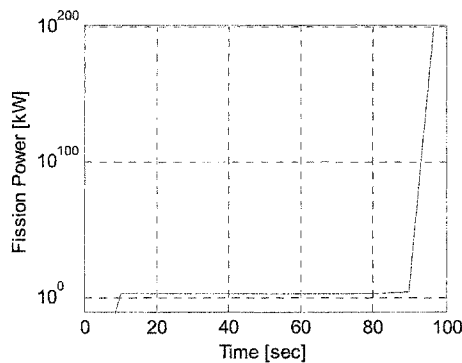


FIGURE 4.31. Control Dependent on Angular Velocity of Control Drums; Instability Demonstrated for $a = 0$.

Selection of $a = 1$ also presented difficulties in system control. Early in the control, when the fission power was far from the goal power level, the cost to the control was on the order of the power cost term. Hence, large initial control moves that are required to initiate the reactor start-up were discouraged. This result is shown in Figure 4.32 in which the power failed to increase significantly above zero, maintaining steady

state operation at approximately $10^{-6.54}$ kW_t. Reducing the weighting to $a = 0.01$ solved this problem and provided stable control given the proper selection of control parameters. Selection of the control parameters with the appropriate weighting of the cost terms is discussed in the following section.

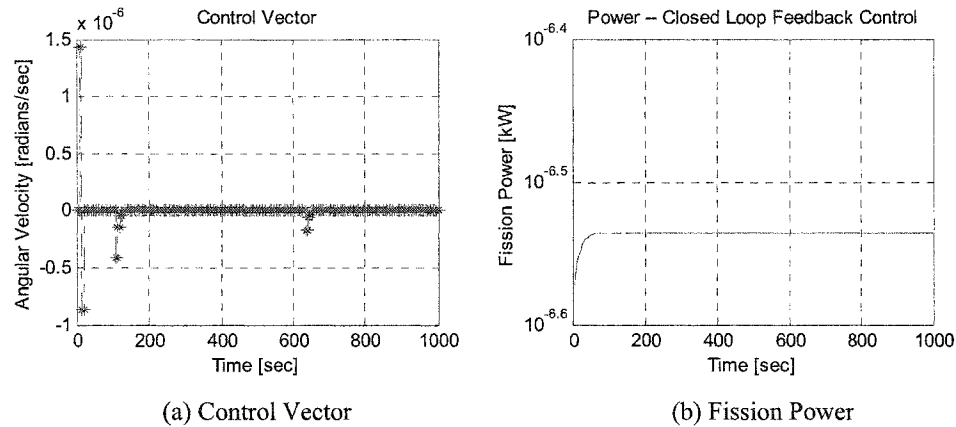


FIGURE 4.32. Control Dependent on Angular Velocity of Control Drums; Failure to Achieve Goal Power for $a = 1$.

4.4.2.4 Stability Analysis

Selection of the components to include in the cost function for an optimized control scheme, and the weighting of these individual components, can have a significant impact on the ability of the algorithm to converge to a good solution at each control step. In addition, selection of the parameters in the control algorithm itself (e.g., the length of the control horizon and the time between control steps) can affect the stability of the controller. Controller stability was investigated for the $\omega(t)$ controller to determine acceptable control parameters throughout the control period, from zero power to steady-state operation at a low power level.

Analysis of any of the power curves presented in the preceding sections reveals two distinct regions for control: (1) the initial power rise, and (2) steady state operation at

the goal power level (P_{goal}). These operational regimes are identified in Figure 4.33. Initial studies revealed generally good controller response at early times in the control, but resulted in unstable control during the steady state region. This type of response was observed in Fig. 4.31, where reactor operation was stable until late in the steady state region. As a result of their considerably different attributes, a different set of control parameters was appropriate for each region. The control algorithm was split to match the distinct control regions: when the reactor power level approximately reached the goal power, the control parameters were switched to the parameters for steady state operation.

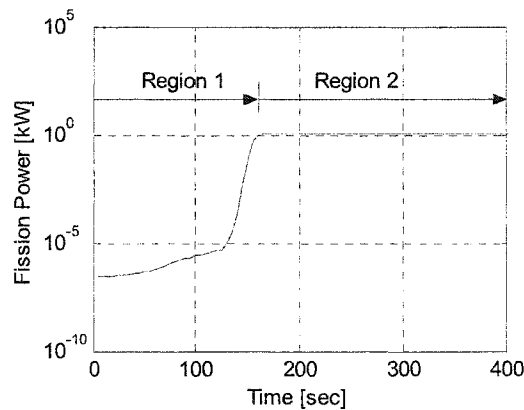


FIGURE 4.33. Illustration of Distinct Regions of Operation for Reactor Start-Up Control.

Region 1: Initial Power Rise

Selection of control parameters in the power rise region was relatively straight forward. It was quickly determined that the horizon must be set such that it was long enough to “see” past the power rise region to the point at which $P(t) \sim P_{goal}$. For a shorter horizon, the controller demonstrated significant difficulty in rising from the zero power level. This resulted in large control maneuvers late in the horizon in an attempt to compensate for the problem, causing the reactor power to become infinite. For example,

given a horizon of 40 seconds and control time step of 5 seconds, the neutron population was only approximately 4.0 neutrons after 120 seconds of control, at which point the controller began to experience integration failures, indicating that a large maneuver was being attempted that resulted in $P(t) \rightarrow \infty$, and the program was prematurely terminated.

For a slow startup at $\omega_{max} = 0.0175$ rad/sec ($\sim 1^\circ$ /sec), a control time step of 5 seconds was sufficiently short to provide smooth start-up in minimal computation time. A shorter time step was also successful, but added unnecessary calculations to the optimization. A control time step longer than 5 seconds resulted in instabilities similar to the short horizon instability discussed above, in which the controller experienced difficulty in increasing reactor power, eventually leading to a large maneuver and $P(t) \rightarrow \infty$. At long control times ($\Delta t_C > 5$ sec), the rapid power rise in Region 1 could not be resolved by the controller, causing the reactor power to initially stay close to zero, followed by large maneuvers in a single step that were attempted to reduce the cost associated with the discrepancy in the desired and goal power levels.

Initial runs of the control algorithm at varying horizon lengths indicated that a horizon of 120 seconds was sufficient for the given constraint on the control drum angular velocity ($\omega_{max} = 1^\circ$ /sec). In this case, the initial guess for the control vector was selected such that the first 15 elements were set to ω_{max} and the remaining elements were set to zero. A robust controller should provide a reasonable control solution regardless of the initial guess to the control vector. Direct application of the initial (guessed) control vector in this particular case would result in a drum position of only 75° after 75 seconds of operation. The optimized control vector resulted in a more appropriate drum position, with the control drums settling at approximately 90° . To verify independence of the control relative to the initial guess, a second control run was performed with a modified initial guess in which only the first 5 elements were set to ω_{max} , with the remaining elements again set to zero. Each case resulted in the same final control vector and control drum position, indicating that the controller was independent of the initial guess provided

by the user. Fig. 4.34 shows the results for application of these parameters in the initial control region. Stability during steady state operation was achieved by application of the stability analysis results for Region 2, which will be discussed in the next section (control parameters: $dt = 5$ sec, $h = 10$ sec). One notable feature in Fig. 4.34(b) is that once the control parameters were optimized, the open loop application of the control no longer demonstrated the instabilities that were previously observed in Fig. 4.30.

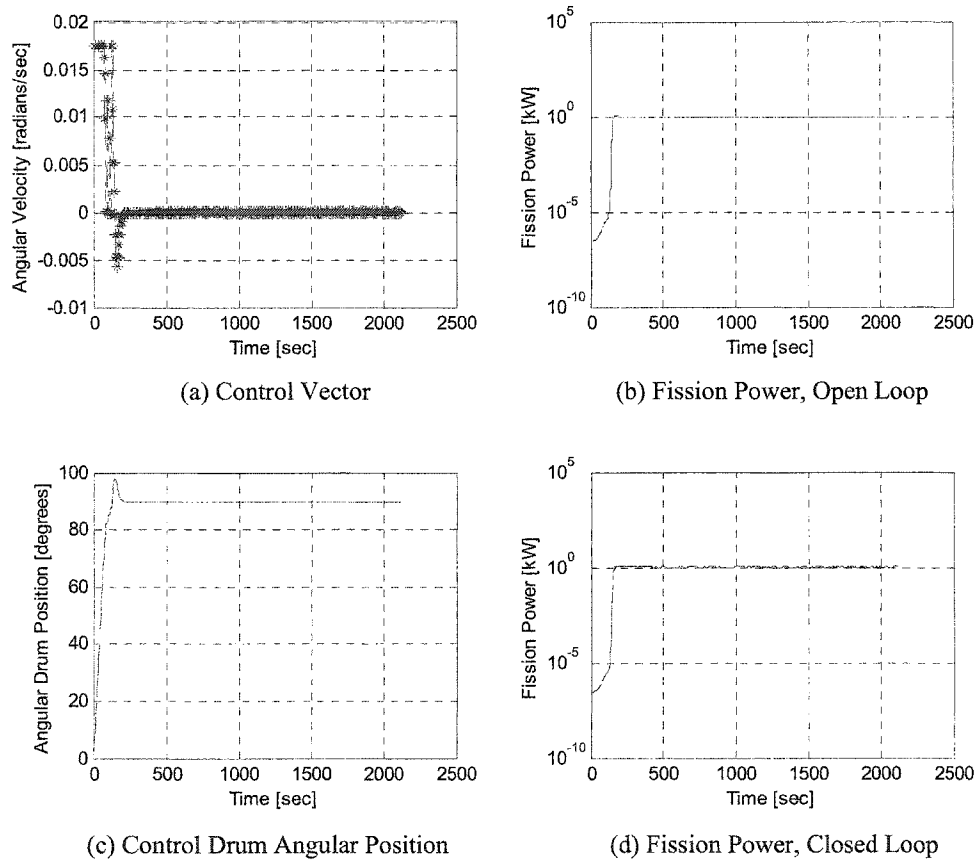


FIGURE 4.34. Demonstration of Stable Controller Operation Given Two Control Regions: Power Rise (0 - 120 sec) and Steady State Operation (> 121 sec).

Region 2: Steady State Operation

An analysis of controller stability was performed during steady state reactor operation at 1 kW_t. The control drums were positioned very close to the final optimized position found in previous runs of the control algorithm ($\theta = 90^\circ$), and the initial conditions for the neutron and precursor populations were set to appropriate values for ~ 1 kW_t of fission power. In this case, the prompt neutron lifetime was adjusted to the more correct value of 10^{-7} seconds a fast spectrum reactor. Hence, the initial conditions provided to the controller for steady state stability analysis were: $\theta_o = 90^\circ$, $n_o = 6.8 \times 10^6$, and $C_o = 1.9 \times 10^{12}$, with the initial guess for the control vector $\omega(t)$ being the zero vector (only small controller moves were anticipated during steady state operation).

During steady state operation at low power, the control drums were expected to remain at a fixed position, such that $\omega(t)$ over long times could be approximated by a vector of zeroes. Hence, a relatively long control time step was not expected to affect system stability. It was expected, on the other hand, that a long time step would mimic open loop control. In this case, if the system were left to run in open loop for an extended period of time, the power would drift from the nominal value set for P_{goal} . As discussed above, the power rise region required a long control horizon and a relatively short control time step. Control runs that maintained the same parameters for both regions of operation resulted in instability of the controller during steady state operation, such that $P(t) \rightarrow \infty$ at long times, as observed in Fig. 4.31. This result likely resulted from an overly long prediction horizon in steady state operation, in which small errors tended to drift over time, rather than due to the short control time step.

Various combinations of the control parameters h and dt were attempted under these conditions. The combination was reported as “stable” if the controller provided steady state operation over an extended period of observation and control (ranging from 200 – 1500 seconds). If the controller resulted in oscillation of the fission power during any individual optimization of the cost function (noted by observing intermediate results

during the control run), eventually returning infinite fission power and failure of the control run, the combination was reported as “unstable”. The results of this stability analysis are summarized in Figure 4.35.

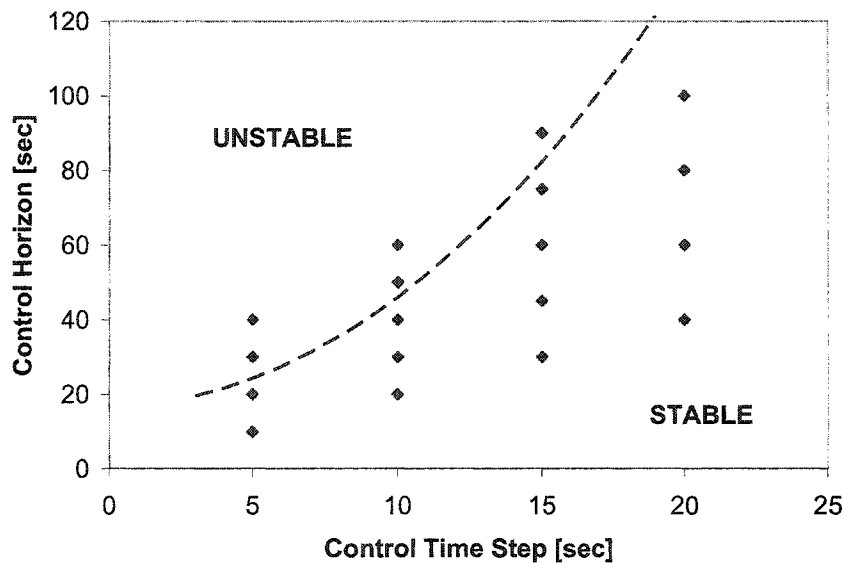


FIGURE 4.35. Stability Analysis of the Control Algorithm During Steady State Operation at 1 kW Fission Power.

For each control time step that was selected, the controller became unstable for a long horizon lengths (relative to Δt_C). This condition does not hold true for the longest, 20 second control step, however, which remained stable for all horizons selected. The parabolic stability line plotted in Fig. 4.35 corresponds to $h = 0.29 \Delta t_C^2 + 17$. Note that contrary to what was expected, the stability relation did not extrapolate to $\Delta t_C = 0$ and $h = 0$. An additional run to assess the stability of a control time step of 1 sec and a control horizon of 10 sec indicated that this stability relationship did hold true, providing stable control over an extended period of time.

The results shown in Fig. 4.35 were somewhat contrary to the expected result. It was expected that a long time step would leave the system “unattended” for an extended period of time, essentially behaving as an open loop controller, allowing errors to build.

Instead, as the control time step was increased, the system became less sensitive to the length of the control horizon and stable operation was maintained. This result reinforces the observation made from Fig. 4.34, in which the open loop power was no longer unstable if the control parameters were selected wisely. If the control parameters were selected on appropriate time scales, the open loop application did not result in the same run-away power response that was observed previously in Fig. 4.30. This difference could also be a result of the change in the prompt neutron lifetime in each application, which was adjusted from 10^{-3} sec (Fig. 4.30) to 10^{-7} sec (Fig. 4.34). In the latter case, the reactor system was able to respond more rapidly to control maneuvers than in the previous case. Because of this significant difference, it is unclear whether the open loop control results can be directly compared.

The cost function developed in Eq. (4.16) was constructed to remove the dimensionality from the weighting coefficient a . However, given the results of the stability analysis, the modifications made to the control vector cost were reassessed. The factor that was applied in Eq. (4.16) to non-dimensionalize a inadvertently placed a $\Delta t_C^2/h$ dependence on the cost to control. Given a relatively long control horizon and short control time step, the overall cost to the control was decreased and control maneuvers essentially became free. Because the cost associated with control maneuvers was insignificant relative to the cost to the current state of the reactor power, the optimization function eventually selected a poor a control vector that led to $P(t) \rightarrow \infty$ at long times, as previously observed in Fig. 4.31 when a was set to zero. As the factor dt^2/h was increased, the controller demonstrated the same trend ($P(t) \rightarrow \infty$) as observed for $a = 0$. Due to the quadratic dependence of the control cost on Δt_C , the genesis of the essentially parabolic stability line in Fig. 4.35 now becomes evident. Although the $\omega(t)$ controller that was developed using the cost function expressed in Eq. (4.16) provided stable control given appropriate selection of the control parameters, it may not be applicable in more generalized control scenarios.

4.4.3 Reactor Start-Up: Variable External Source

The results presented for all control algorithms discussed thus far considered a constant external neutron source of 10^6 neutrons/second entering the reactor core. However, as demonstrated in the discussion of the space radiation environment, the external radiation source can be highly variable, a function of the solar activity and spacecraft location. Therefore, the robustness of the $\omega(t)$ controller was assessed in the presence of a time varying external neutron source. During the initial power rise, a variation in the external source may be significant to reactor operation as a result of the relatively small neutron population in the reactor core during the initial rise to power. On the other hand, source perturbations during steady state operation, even at low power levels, were not expected to affect the system operation.

The selected time varying source introduced arbitrary increases and decreases in the neutron source during each region of operation. The results presented in Figure 4.36 introduce a 100x perturbation at $t = 75$ s, 10x perturbation at $t = 500$ s, and 10^{-2} x perturbation at $t = 1000$ s. The resulting input neutron source is shown in Fig. 4.36(b). Although source perturbations would demonstrate smooth transitions, the applied source was mathematically described as a step function. Although not physically accurate, this description will result in a conservative estimate of controller robustness because it is much more difficult to respond to a step change in the source than to respond to a smooth transition. Note that the control program was designed such that the controller is only aware of the source magnitude at the present time (t_i). As in a real system, the future value of the external source is unknown, so the controller assumes that the external source is constant over future time. For optimization of the control vector at some time t_i , the controller applies $Q(t_i)$ over the entire control horizon t_i+h .

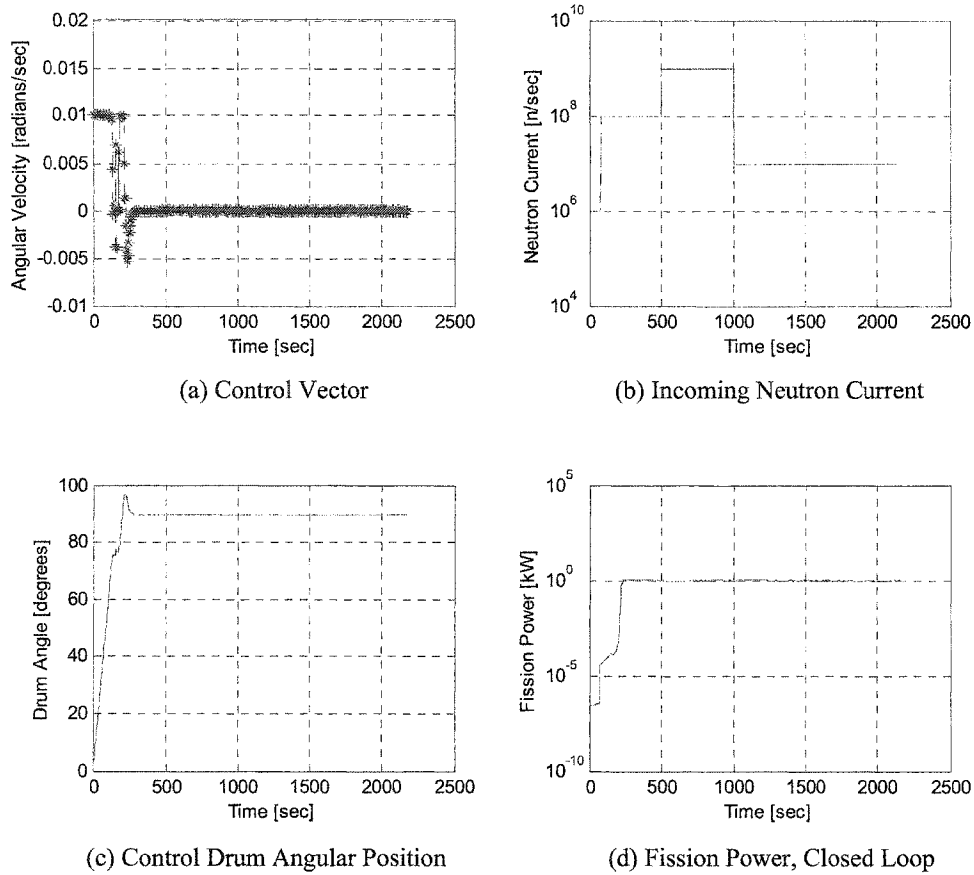


FIGURE 4.36. Reactor Start-Up Control in the Presence of Source Perturbations.

For variable source calculations, the maximum drum rotation rate was set to 0.01 radians/second (0.6° /sec). Given this reduced constraint on ω_{max} , an initial control run with a constant background source (10^6 n/sec) indicated that the initial control horizon for Region 1 should be set to approximately 180 sec; a control time step of 5 sec was again selected for the power rise region, per the analysis discussed in Section 4.4.2.4. The results of the steady state stability analysis were applied in the selection of control parameters for Region 2. During steady state operation, the control time step was set to 5 sec and the control horizon to 10 sec.

The lower right-hand plot of Fig. 4.36(d) shows the power response to the applied source perturbations. The increase in power in response to the increased source at 75 sec

after start-up was initiated is readily evident. At this stage, the reactor neutron population was sufficiently low that the increased background source provided a “boost” to the start-up, although the goal power level was not achieved any faster than for the constant background source. After achieving steady state power at 1 kW_t, additional source perturbations at 500 and 1000 sec had no effect on the reactor power because the neutron production rate due to fission in the reactor core was significantly greater than the external neutron source. At 1 kW_t, the predicted neutron production rate in the SAFE-400 reactor core is on the order of 10¹⁴ n/sec, while the maximum external neutron source in the simulation was only 10⁹ n/sec. The results presented here demonstrate that the proposed controller, optimized for use with the $\omega(t)$ control vector, is sufficiently robust to maintain reactor control in the presence of source fluctuations.

4.4.4 Future Controller Enhancements

As discussed in Section 4.4.2.4, reassessment of the cost function applied for $\omega(t)$ control after completion of the stability analysis indicated that the cost function developed in Eq. (4.16) introduced additional controller dependence on the control parameters Δt_C and h . An additional cost function was attempted, in which the time and angular dependence of the cost to the control were removed:

$$J = ftheta \cdot fwmx \cdot \left[\int_{t_o}^{t_o+h} (n(t) - n_{goal})^2 dt + a(n_{goal})^2 \int_{t_o}^{t_o+h} \omega(t)^2 dt \right]. \quad (4.17)$$

A steady state analysis was repeated using the cost function expressed in Eq. (4.8) with a control time step of 5 sec and a control horizon of 10 sec. Several cases were attempted in which a ranged from zero to 1.0E6. The only case found to be unstable was that in which $a = 0$. Although the controller was expected to become unstable for relatively small, positive weighting coefficients in addition to $a = 0$, the minimum positive a value attempted, 1.0E-4, maintained stable controller response.

Setting a to a constant value of unity, several combinations of the control time step and the control horizon were attempted to assess the controller stability over a wide range of applications. Tested combinations focused on h , Δt_C pairs that resulted in instabilities in the previous configuration. These included, where each value is provided in seconds: $\Delta t_C = 5$, $h = 10 - 100$; $\Delta t_C = 10$, $h = 50 - 200$; $\Delta t_C = 15$, $h = 75 - 150$; $\Delta t_C = 20$, $h = 100 - 200$. Results indicated that for this configuration of the cost function, each of the attempted Δt_C and h combinations provided stable controller response during steady state operation.

By removing the Δt_C and h dependence from the cost function, it may be possible to apply the same control parameters during both the power rise and steady state operational regimes. In this case, control parameters would have to be selected based on the requirements for Region 1 (e.g., long horizon and short control time) to account for the rapid changes in the reactor power during this period. However, although a long h , short Δt_C was demonstrated to provide stable control during steady state operation using Eq. (4.17), it is not immediately evident that this would be desirable. Due to the rapid changes in Region 1, many calculations are required to achieve a desirable reactor response (e.g., short Δt_C time steps over a long horizon). During steady state operation, only small changes in the reactor state occur, and frequent changes are unnecessary. Hence, maintaining the same parameters throughout both regions of operation may unnecessarily increase the computational time during each optimization.

Future refinement of the objective function for reactor start-up control using $\omega(t)$ as the control variable might consider including an additional term proportional to the second derivative of the control vector. While relatively large values of the first derivative (ω') should be acceptable to the controller, a large second derivative (ω'') would imply high frequency oscillations in the controller. This concept is illustrated in Figure 4.37.

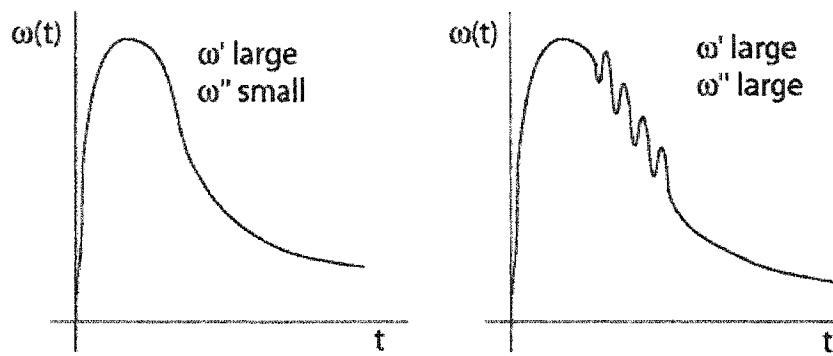


FIGURE 4.37. Rate of Change of the Control Drum Angular Velocity.

While both scenarios may result in the same $\omega(t)$ at long times, the control vector in Fig. 4.37(b) would not be desirable from the standpoint of the drive motors due to the high frequency oscillations that would effectively shorten the life of the motors. Inclusion of a second derivative term (proportional to ω'') to the cost function could improve the robustness of the controller, minimizing the work done by the control drum drives and increasing the relative lifetime of the drive motors.

CHAPTER 5

VIRTUAL REACTIVITY FEEDBACK EXPERIMENTS

Development of a nuclear reactor for space applications requires a significant amount of testing prior to deployment of a flight unit. High confidence in fission system performance can be obtained through a non-nuclear test series, with the heat from nuclear fission simulated using electric resistance heaters. Through hardware based design and testing, the Early Flight Fission Test Facility (EFF-TF) at the NASA Marshall Space Flight Center is equipped to investigate fission power and propulsion components, subsystems, and integrated system design and performance (Van Dyke et al. 2003). Through demonstration of system concepts in relevant environments, previous non-nuclear tests have proven to be a highly effective method to identify and resolve integration issues, from both cost and performance standpoints (Hrbud et al. 2002; VanDyke 2003).

A set of experiments conducted on the SAFE heat pipe cooled reactor design will be discussed. The suite of experiments relate to the SAFE-100 reactor core prototype, which is equipped with 61 modules and designed to provide 100 kW_t. Each module contains three fuel tubes and one heat pipe tube, as shown in Figure 2.2 for the SAFE-400. A total of 183 graphite resistance heaters are used to simulate the heat of nuclear fission. The SAFE-100 is a scaled-down version of the SAFE-400, constructed using stainless steel in place of refractory metal modules (VanDyke et al. 2002). The SAFE-100 prototype was not equipped with operational heat pipes; instead, hollow stainless steel tubes were included at the center of each core module to represent the heat pipes

that would be employed in the actual system. There was no interstitial material (e.g., braze) that provided coupling of the core modules in the SAFE-100 prototype. Instead, the bank of tubes used to construct the SAFE-100 prototype was held together by three steel clamps along the axial length of the core, as shown in Figure 5.1. The next installment of heat pipe reactor testing will be the SAFE-100a, which will only include the center 19 modules of the SAFE-100, but will be equipped with sodium heat pipes and a prototypic heat exchanger. The experimental work discussed in this chapter includes measurement of core deformation that results from material thermal expansion with increasing reactor temperature and the implementation of a virtual reactivity feedback control loop.

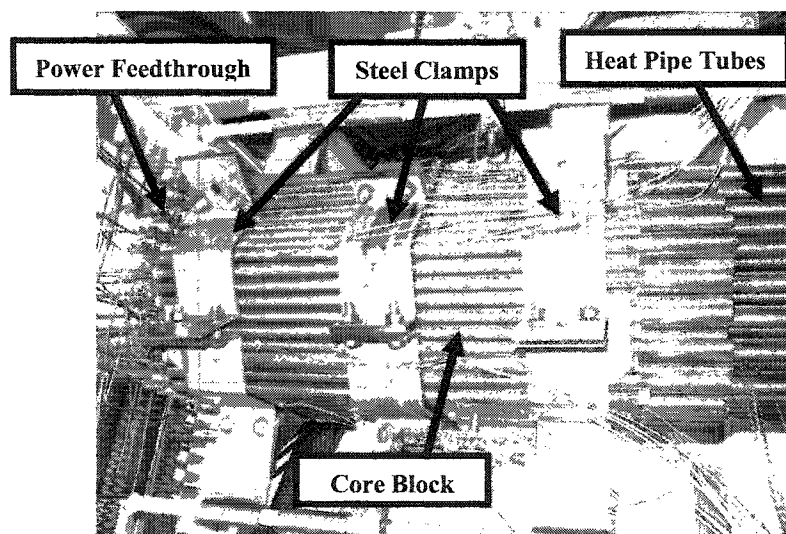


FIGURE 5.1. SAFE-100 Prototype.

For compact, fast spectrum reactors, reactivity feedback is dominated by core deformation. As the core temperature increases, the reactor materials expand, resulting in a larger geometry and reduced material densities. The effects of the geometric and material density changes in the reactor core can be simulated experimentally by coupling

electrically heated reactor tests with neutronic analyses that establish the reactivity worth of various core deformations. Deformation from the original geometry can be measured, the reactivity decrement can be assessed from Monte Carlo calculations, and the corresponding core power as a function of time can then be computed via kinetics calculations. Rapid iteration of this control loop provides a mechanism for the power in the electrically heated system to be matched to the calculated neutronic response of the complementary nuclear system. The SAFE-100 core prototype allows simulation of slow to moderate transients (e.g. reactor start-up) at low power, while realistically taking into account the negative reactivity feedback introduced by geometric expansion of the core. The validation of selected deflection measurement techniques, and the application of these techniques to complete a virtual reactivity feedback control loop, is discussed.

Deflection tests were performed on a 316 stainless steel (SS316) core prototype that was manufactured according to the SAFE-100 core fabrication drawings. As discussed previously, the heat from fission was simulated using graphite resistance heaters, which will be referred to as thermal simulators. Because the prototype was not equipped for heat removal, the experiments that were performed simulate the operation of a fission system in the absence of cooling, a condition that would be present during the start-up period for a heat-pipe cooled reactor prior to the complete thaw of the working fluid in the heat pipes or initialization of the heat exchanger.

5.1 Validation of Core Deflection Measurements

Two deflection measurement techniques were tested for use with the SAFE-100 core: a linear gap displacement transducer (LGDT) and a charged couple device (CCD) imaging system. The LGDT transducers use a long-life infra-red light emitting diode (IR LED) to project light onto a target surface through a concentric ring, fiber optic probe; the distance to the target is determined from the amount of light that is reflected from its

surface. Assessment of the core width required two LGDTs placed opposite one another on either side of the core. Although a proven technique, LGDTs generally have a limited operating temperature range and lack the flexibility to measure multiple target points during the same testing session. The second measurement technique applied high resolution photography to measure core deflection using a CCD array, eliminating temperature and measurement range restrictions associated with the LGDTs. Imagery from a 6.3 mega-pixel high resolution camera was used to assess core deflection as a function of temperature by scanning multiple cross sections along the axial dimension of the core. The radial core size was determined from the number of pixels across each profile. Comparison of a time series of core snapshots referenced to room temperature snapshots provided the core deflection over time. Where possible, CCD imaging results were validated by comparison to mechanical caliper measurements at low to medium temperature conditions (room temperature to $\sim 450^{\circ}\text{C}$).

Prior to implementation of a control loop utilizing deflection measurements, it was necessary to validate the proposed measurement techniques. The initial set of validation tests compared two LGDTs, placed on opposite sides of the core, and the CCD imaging system, focused at the same axial location as the LGDTs. Radial core deflection was measured at a specified axial location while attempting to maintain an isothermal condition radially across the core. Testing was performed at three temperature levels: low, medium, and high (e.g. 250° , 500° , 750°C).

5.1.1 Set-up of Test Hardware

All testing was performed inside a nine foot diameter vacuum chamber, with support hardware located adjacent to the chamber. The SAFE-100 reactor core prototype was located on a fixed stand inside the chamber. The layout of the SAFE-100 and the CCD imaging system with respect to the vacuum chamber is shown in Figure 5.2.

Although not shown in this figure, the LGDTs were positioned inside the vacuum chamber at the same axial location sampled by the CCD imager.

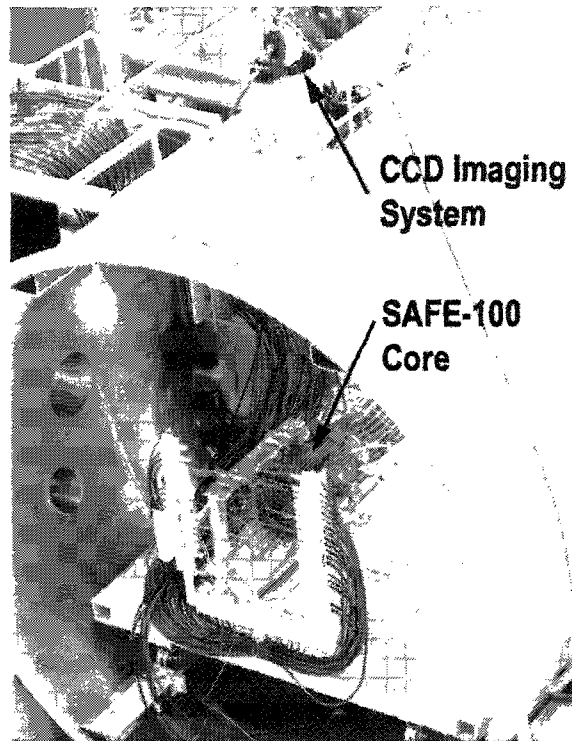


FIGURE 5.2. SAFE-100 Test Hardware Setup.

SAFE-100 Prototype

The SAFE-100 core was equipped with 183 graphite heaters to simulate nuclear fuel elements. Earlier testing of the prototype during FY 2002 and 2003 was performed to characterize the thermal properties of the SAFE-100. This testing generated significant wear on the hardware, and some electrical and mechanical modifications were required prior to implementation of the deflection tests. The original graphite heater elements were fitted with two alumina (Al_2O_3) insulator rings, located at each end, to prevent the heater from shorting to the fuel tube. The ‘fuel tube’ refers to the stainless steel tube that the heater slides into, simulating the clad that would surround a nuclear

fuel element. After multiple tests of the SAFE-100 for different applications prior to the virtual reactivity feedback experiment, the copper connectors and heater leads began to experience a physical load due to the weight of the power hook-up. When any pressure was applied to the leads, it caused the middle of the heater to bow and touch the fuel tube, shorting the entire control zone. Figure 5.3 shows the reconfigured SAFE-100 power hook-up to illustrate its scale and complexity. Prior to deflection testing, all of the heaters were removed and refurbished by adding a third insulating ring at the center of each heater. The original and refurbished heater designs are shown in Figure 5.4.

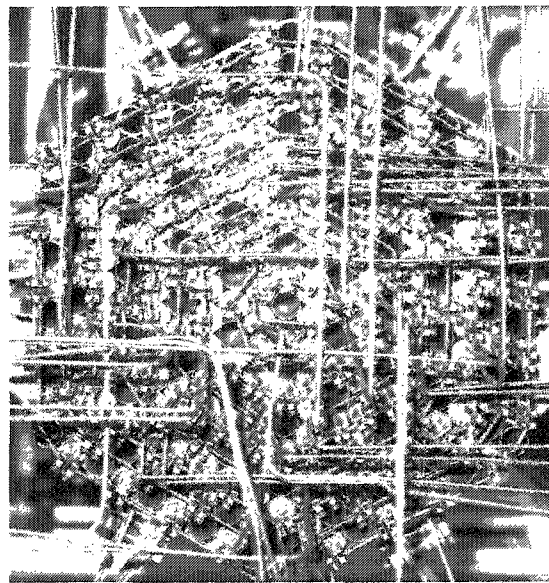


FIGURE 5.3. Power Hook-up for the Reconfigured SAFE-100 Tests.

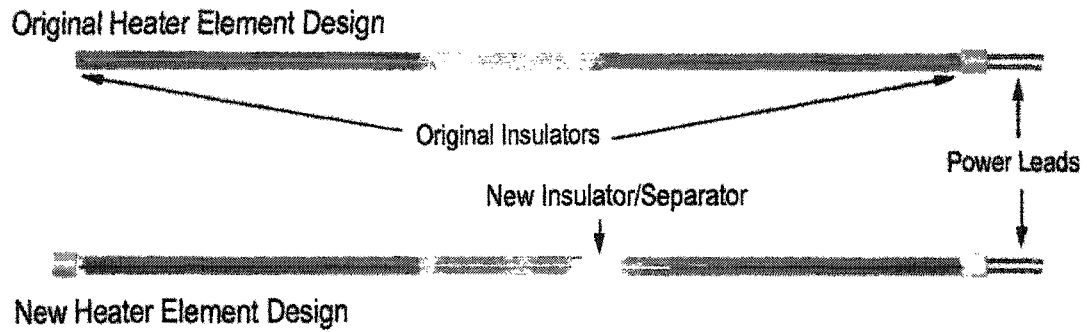


FIGURE 5.4. Original and Refurbished Heater Elements for the SAFE-100.

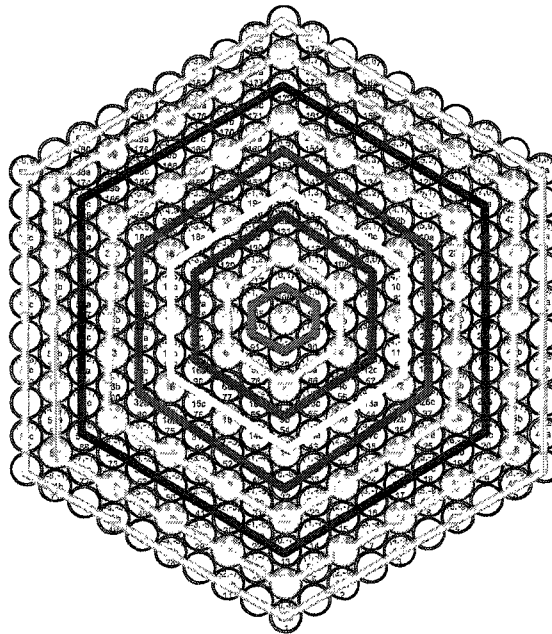


FIGURE 5.5. Reconfigured SAFE-100 Control Zones.

The original SAFE-100 configuration was operated using 32 control zones. In the reconfigured core, the 183 heaters were connected to create nine concentric heater rings. Each of these rings formed an individual control zone. Application of different power levels to each control zone provided the capability to match the predicted radial power profile for the SAFE reactor. Each control zone is shown as a separate ring in Figure 5.5. In this configuration, the reactor power is symmetric along the “radial rings” (i.e. each

fuel pin in a concentric ring receives the same input power). For validation tests, power was uniformly applied to the core in attempt to create an isothermal condition across the core. The approximate radial power profile for an operating nuclear reactor was not modeled in validation testing, but was applied in subsequent reactivity feedback tests.

The virtual reactivity feedback experiment required real-time monitoring of the variations in core deflection, determination of the corresponding reactivity worth of measured deflections from neutronic simulations performed off-line, and calculation of a new solution to the point kinetics equations to appropriately adjust the predicted reactor power. To accomplish this feedback loop, the response time of the power control system had to be very rapid (on the order of seconds). Previous experiments on the SAFE-100 prototype utilized an automated power system that required up to 30 seconds to update the individual power supply settings after a new value was entered. This update delay resulted from reliance on a serial connection to provide system communication. To accomplish the rapid response time required to implement the virtual reactivity feedback loop, the control system for the power supplies was modified to provide communication via an Ethernet network, resulting in a response time on the order of 100 ms. To accomplish rapid control, the earlier serial control system was replaced with a National Instruments LabView interface and compact field point units.

To monitor temperature, the SAFE-100 prototype was equipped with 32 internal thermocouples distributed evenly throughout the core, positioned in the hollow SS tubes representing the core heat pipes. One half of the modules were equipped with thermocouples. The thermocouples were pushed into the blank modules from the end opposite the power feeds to the heaters, such that the probe ends were located near the axial center of the 55 cm long core. Because internal thermocouples were added after the core prototype was constructed, there was no access to the central region to precisely locate the probes. Therefore, the probes were not bonded or attached to the interior surface of the modules, leaving the thermocouples to rest at the center location. The

precise location of the probe tips was not verified, and some probes may have been in contact with the module tube wall. An additional series of thermocouples was spot-welded to the perimeter of the core to provide surface temperature data. Placement of internal and perimeter thermocouples is shown in Figure 5.6.

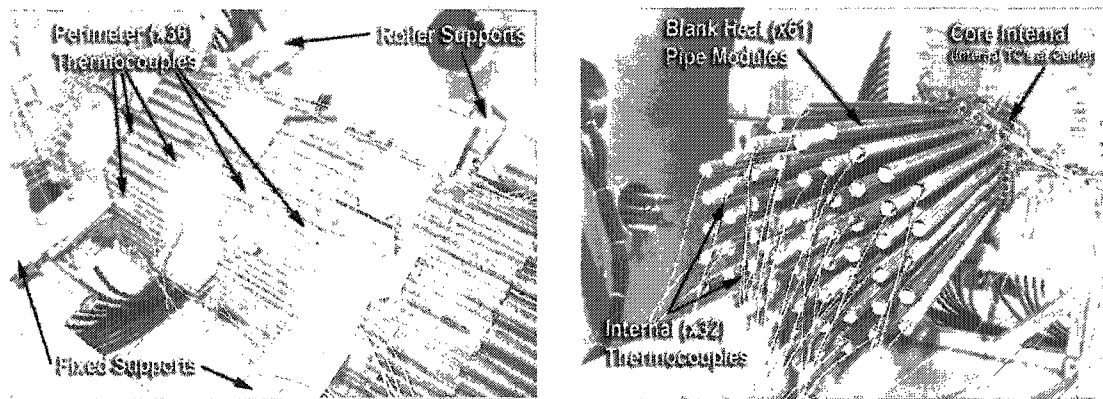


FIGURE 5.6. Thermocouple Placement on SAFE-100 Core.

5.1.2 LGDT Measurement Configuration

Linear gap displacement transducers utilize a IR LED to project light onto a target surface through a concentric ring, fiber optic probe; the distance to the target surface is determined by the amount of light reflected from its surface. The voltage of the reflected signal corresponds to a separation distance between the probe tip and the target surface, determined from the appropriate LGDT calibration curve. To determine core width, a pair of LGDTs located directly opposite one another was required. An illustration of the LGDT deflection measurement setup is shown in Figure 5.7. Photographs of the actual test setup are provided in Figure 5.8.

Deflection Measurement LDGT Experimental Setup

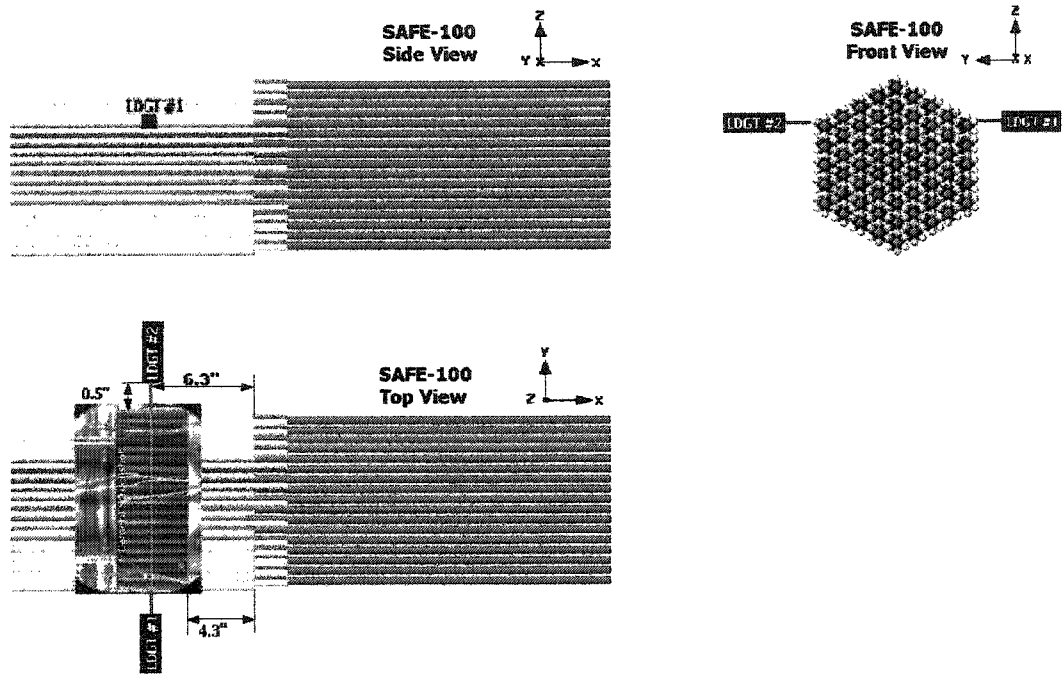


FIGURE 5.7. Setup of the LGDT System.

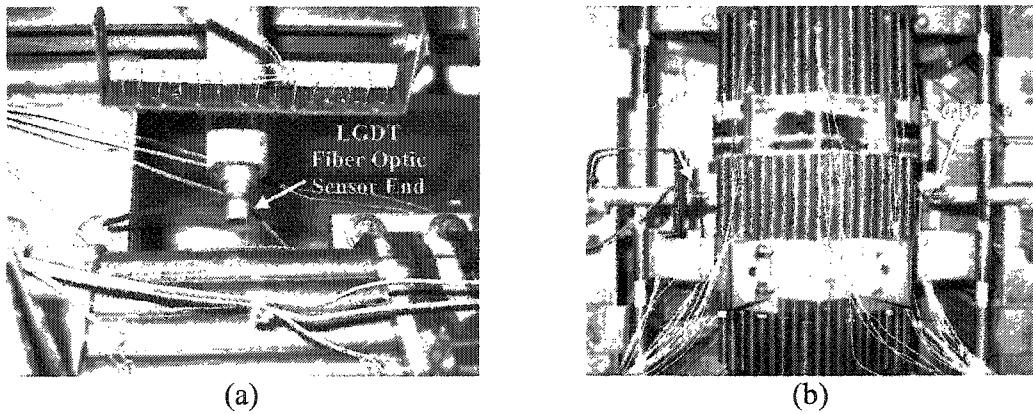


FIGURE 5.8. Photographs of the LGDT Setup Inside the Vacuum Chamber.

The LGDTs applied in deflection measurements were Type 4 LDGTs (LGDT – 4) manufactured by BEI Precision Systems & Space Division. Manufacturer specifications are provided in Table 5.1. Although the intended core temperatures were well above the reported probe operating range, the sensor head was placed a small distance away from the core (not to exceed 14.75 cm) and no significant heating of the fiber optic sensor end was anticipated for the low temperature range (250 – 450° C) of the deflection tests. It was expected that the LGDT measurements would be reliable for this temperature range.

Parameter	LGDT – 4
Target Distance	6.75 – 14.75 mm
Sensitivity	1.25 V/mm
Resolution	8×10^{-4} mm
Probe Temperature Range	-55 – 105° C

TABLE 5.1. LGDT Manufacturer Specifications.

5.1.3 CCD Measurement Configuration

The CCD imaging system was configured to measure the radial core width at several axial locations. The test setup is shown in Figure 5.9. The SAFE-100 prototype was located inside the vacuum chamber on an immobile test stand. The camera was located outside the vacuum chamber, positioned directly above the reactor core prototype, and was capable of imaging the core through a fused silica viewing window.

Initial measurement validation tests considered the radial core width at two axial locations in the field of view. At each location, the core width was determined along a slice having a single pixel thickness. For the selected camera field of view settings, each pixel had a width of 0.09 mm (in the radial direction). Without spatial or temporal averaging, the measurement resolution was one pixel. Later tests incorporating reactivity feedback control considered three axial locations in the field of view, shown as D1, D2, and D3 in Figures 5.9 and 5.10. To further refine/improve the CCD imaging system

robustness, an averaging “box” (containing several edge pixels) rather than a sampling line (containing only a single edge pixel), was also incorporated at each slice location. All applications of the CCD measurement technique in the virtual reactivity feedback loop tests considered a slice thickness of 30 pixels. The photo provided in Figure 5.10 was taken during test, with the measurement locations superimposed on the photo.

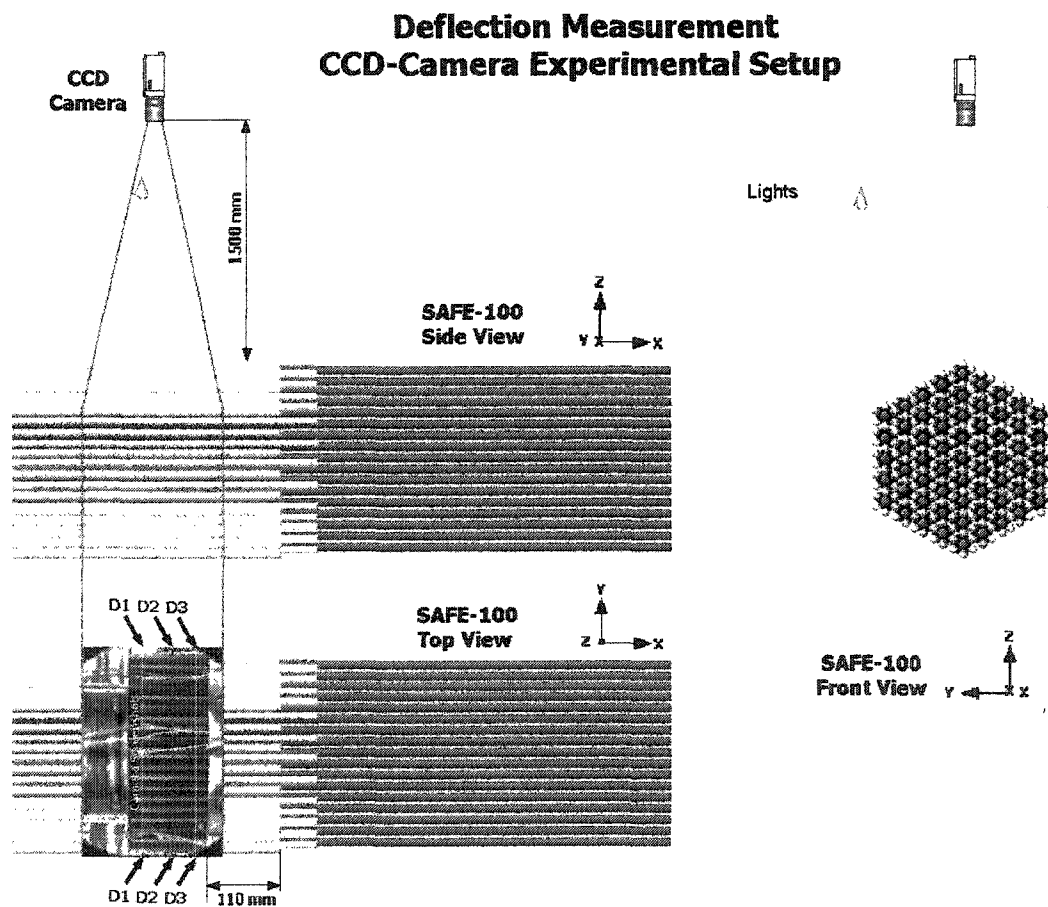


FIGURE 5.9. Setup of the Camera / Reactor Core Prototype.

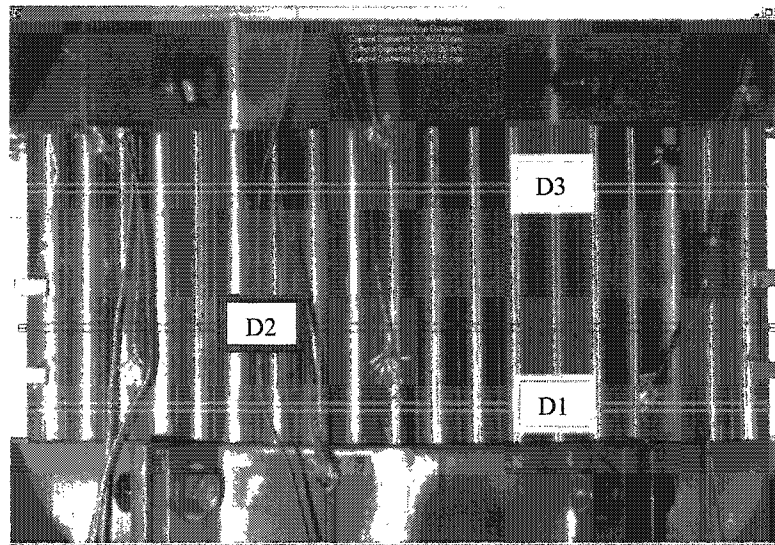


FIGURE 5.10. Screen Shot Taken During Test Showing Three Axial Locations for Core Width Measurement.

5.1.4 Validation Results

A series of tests was performed to evaluate the CCD and LGDT techniques for measuring core geometric changes. During validation tests, it was found that without regular adjustments to the voltage and current settings for each control zone, it was difficult to maintain a steady state isothermal condition at any of the pre-determined temperature set points due to the lack of heat removal from the SAFE-100 reactor core prototype.

Validation Test 1

The objectives of the first deflection test were to determine the appropriate voltage and current settings necessary to obtain a steady state core temperature at each of the three temperature set points (250, 500, and 750° C), to compare LGDT and CCD measurement data, and to determine the applicability of the LGDTs to the temperature range of interest. To improve heat transfer in the core and to reach uniform temperatures more rapidly, the chamber was flooded with approximately 50 Torr of gaseous helium.

The results of the initial deflection measurement test are shown in Figure 5.11, where P0 and P1 represent the locations of the two LGDTs. The CCD result corresponds to the centermost cross section, identified as D2 in Fig. 5.10.

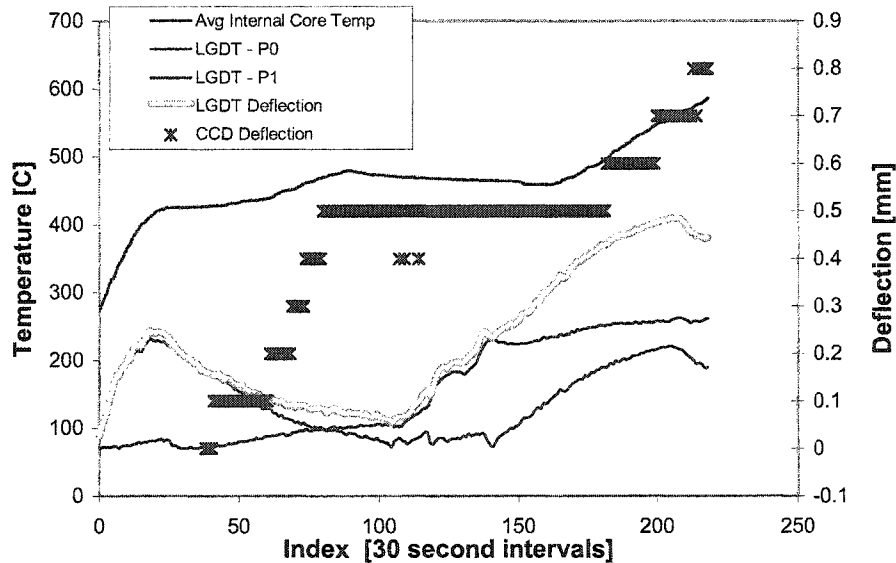


FIGURE 5.11. Results of the Initial Deflection Measurement Validation Test.

The CCD imager behaved as expected, returning increased core size as the core temperature increased. In this implementation, the reported core size was rounded to the nearest pixel, or 0.09 mm. This resulted in the stair-step appearance of the core size and is the source of the abrupt drop in the deflection at approximately index 110, equivalent to a single pixel width. The three curves provided for the LGDTs correspond to each individual unit (placed at positions P0 and P1 directly across from one another) and the corresponding total core deflection as determined from the two individual measurements. The behavior of the LGDTs was somewhat erratic, first showing the expected increase up to an average core temperature of approximately 240°C, but then dropping to a minimum during the approximate steady state operation at 475°C. This was again followed by a rapid climb in the measure core size, but an abrupt drop was again noted at the high

temperature end of the test ($\sim 550^{\circ}\text{C}$). Post-test inspection revealed that the LGDT probe ends were covered with epoxy. Epoxy had been used in previous SAFE-100 tests to secure optical strain gauges to the outer surface of the core clamping brackets. At high temperature, the epoxy baked off the central core clamping bracket and plated the surface of the LGDTs. The epoxy contamination can be seen in the close-up photo of the LGDT probe ends in Figure 5.12.

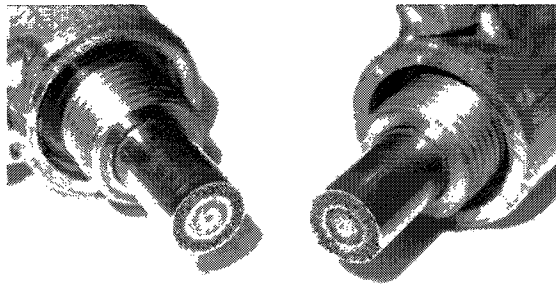


FIGURE 5.12. LGDT Probe Ends Showing Epoxy Contamination Post-Test.

LGDT Bench Tests

After cleaning and polishing the fiber optic tips of the LGDTs to remove all epoxy contamination, a series of bench tests was performed to further assess the performance of the diagnostic under SAFE-100 thermal conditions. Tests determined the sensitivity of the LGDTs to surface temperature, surface reflectivity (dull or polished), and surface shape (flat or rounded). The setup constructed for the bench tests is shown in Figure 5.13. The sensor end of the LGDT was clamped, and the target was moved using a sliding table equipped with a dial meter to provide precise positioning. The target used in the tests was alternately a flat aluminum plate or a rounded stainless steel tube ($5/8''$ diameter), tested both at room temperature and heated from $25 - 500^{\circ}\text{C}$. The SS tubes

had various surface properties, including polished, rough (roughened with sand paper), and heavily oxidized.

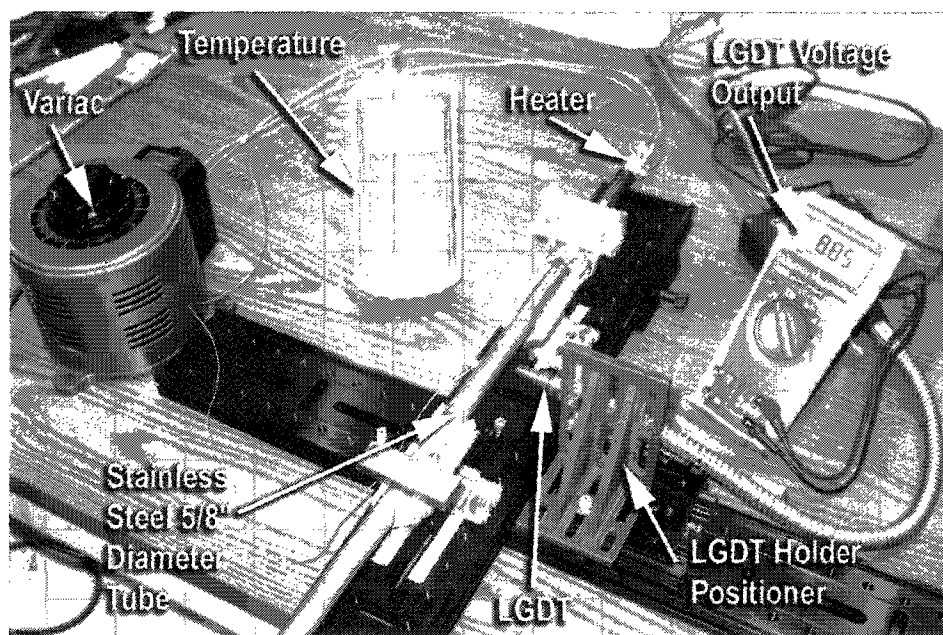


FIGURE 5.13. LGDT Calibration Test Setup.

The LGDT calibration tests indicated that the behavior and repeatability of the LGDT data at room temperature was subject to ambient lighting, surface shape (flat or rounded), and surface condition (SS tube with and without surface oxidation). Although suitable for room temperature operation, the accuracy of the LGDT measurement was subject to the calibration curve selected. Figure 5.14 shows the factory calibration curve and the results of room temperature tests using various targets. Deviation from the factory calibration was partially attributed to previous damage caused by the epoxy coating in the initial use of the LGDTs. However, it was immediately evident that highly oxidized surfaces significantly affected the ability of the LGDT to accurately predict target distance. The remaining targets demonstrated similar response at small separation distances, but diverged from one another as distance increased. These results indicate

that a calibration specific to the geometry, material, and surface characteristics for the target of interest should be performed to ensure the accuracy of LGDT results.

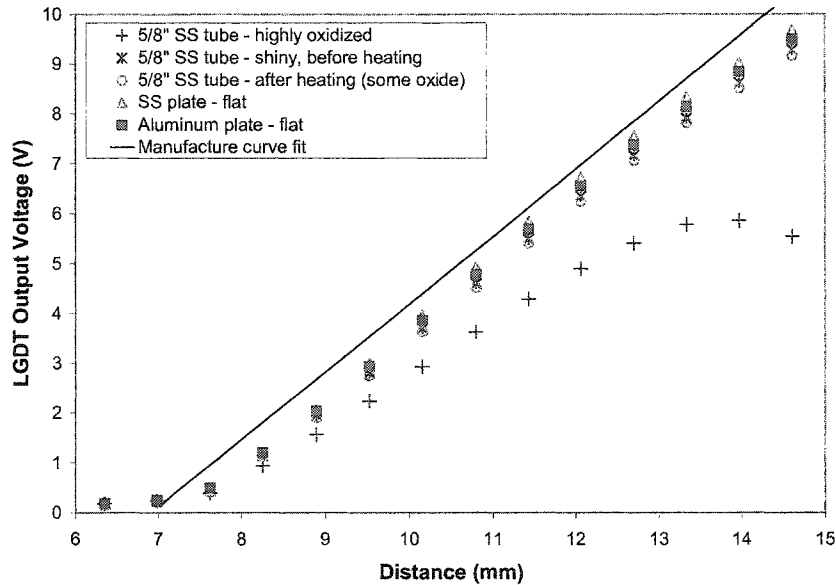


FIGURE 5.14. LGDT Test Results at Room Temperature.

LGDT output demonstrated significant temperature dependence during heated tests. With the LGDT locked at a fixed position relative to the target, variations in the LGDT voltage were recorded as the sample was heated from room temperature to 500° C. The expected variation in the target distance due to material expansion was calculated based on the CTE for stainless steel. The calculated separation distance and the test results for a heated aluminum plate and a heated stainless steel tube are shown in Figure 5.15. Data revealed that LGDT output voltage behaved erratically for the rounded SS target; the output voltage initially increased up to a target temperature of 200° C, experienced a shallow decline to a minimum at 350° C, and then followed a sharp increase. A thermocouple placed on the LGDT probe shell indicated a probe temperature of 80-90° C, just within the reported operating range of the probe. Note, however, that the probe thermocouple was temporarily placed on the probe casing by hand and was not

securely fixed to the surface. The actual temperature of the probe may have been somewhat higher than the measurement indicated. The large discrepancy in the expected and measured results for the stainless steel tube was likely the result of a shift in the specific point at which the probe was targeted due to the expansion of a rounded target.

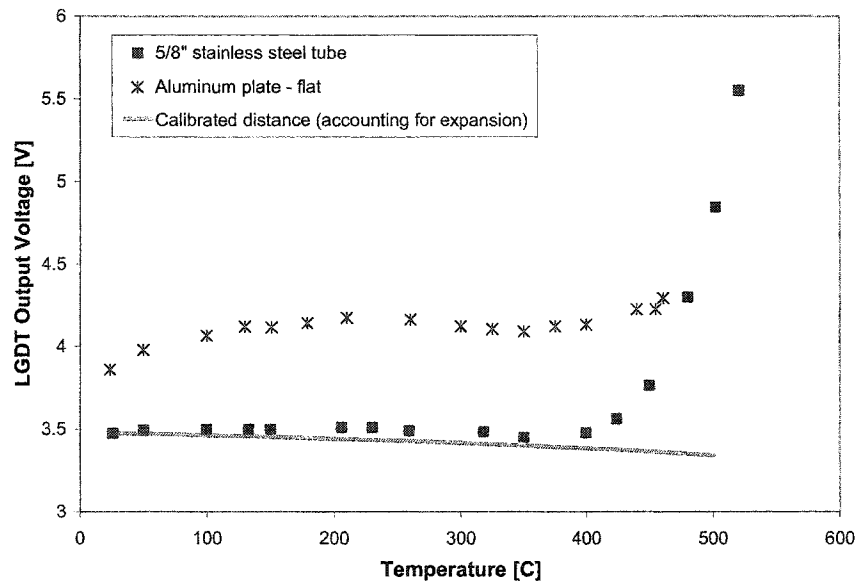


FIGURE 5.15. LGDT Test Results for a Heated Target.

In short, bench tests indicated that LGDTs were not a reliable measurement device for the SAFE-100 tests, where the target was a heated stainless steel tube that exhibited surface oxidation after heating. Data indicated that, for reliable determination of the separation distance between the probe and the target, the target must have a diffuse, reflective surface. In addition, heated tests indicated that one should be wary of using LGDTs in high temperature applications, especially for rounded surfaces. Because of their failure at high temperatures, LGDTs were eliminated from subsequent deflection measurements on the SAFE-100 core prototype.

Validation Test 2

The objective of the second measurement validation test was to validate the CCD imaging system measurements by comparison to a mechanical caliper measurement of the SAFE-100 core width. The core was operated at low temperature (250 – 450° C) in air to allow access to the core during testing. For this and subsequent tests, the core support structure was slightly altered to allow radial expansion in one dimension, minimizing the “jerky” motion that was observed in initial core expansion tests. One side of the core was fixed to the support frame while the other side was allowed to move freely. On the “free” side, the core supports rested on rollers to allow for free radial expansion. This modification is shown in Figure 5.16. On the fixed support side, only one bracket was bolted to the frame while the other merely rested on it, such that axial expansion was not inhibited.

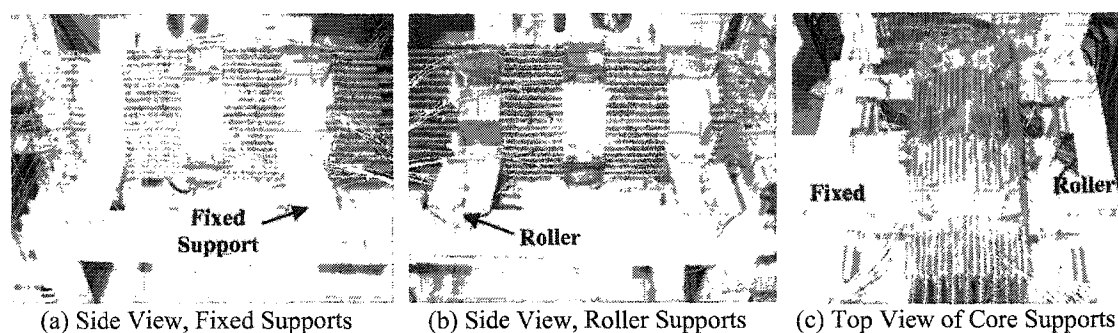


FIGURE 5.16. Modified Core Support Structure to Allow Free Radial Expansion.

While at room temperature, the core width was measured along two CCD sampling lines (1-pixel thick slice) using a 1000 mm mechanical caliper, which provided a measurement accuracy of ± 0.07 mm. To accurately locate the physical measurement, the caliper was positioned with the assistance of real-time image guidance using the CCD camera. The initial measurement of core width provided a zero-point for the CCD imager, locking the initial core dimensions in the data acquisition code and providing the

data necessary to compute the measurement resolution. The camera field-of-view settings indicated a core width of 2894 pixels; the initial caliper measurement was 264.757 mm, yielding approximately 0.09 mm/pixel. The core was slowly heated to an average core internal temperature of 250° C. During the temperature rise, the CCD data showed continuous growth of the core as expected. After the core temperature had quasi-stabilized, a second caliper measurement was made, again using image guidance to ensure proper axial location of the measurement. These measurements were repeated at a quasi-steady state temperature of 450° C. All caliper measurements were performed quickly to prevent any heating of the caliper ends. Results of these tests are provided in Table 5.2. The real time results for one of the CCD sampling lines are shown in Figure 5.17. The second sampling line demonstrated similar agreement with the caliper measurements, as seen in the tabular data. The CCD imaging system tracked the core growth extremely well, demonstrating agreement with caliper measurements to within a single pixel.

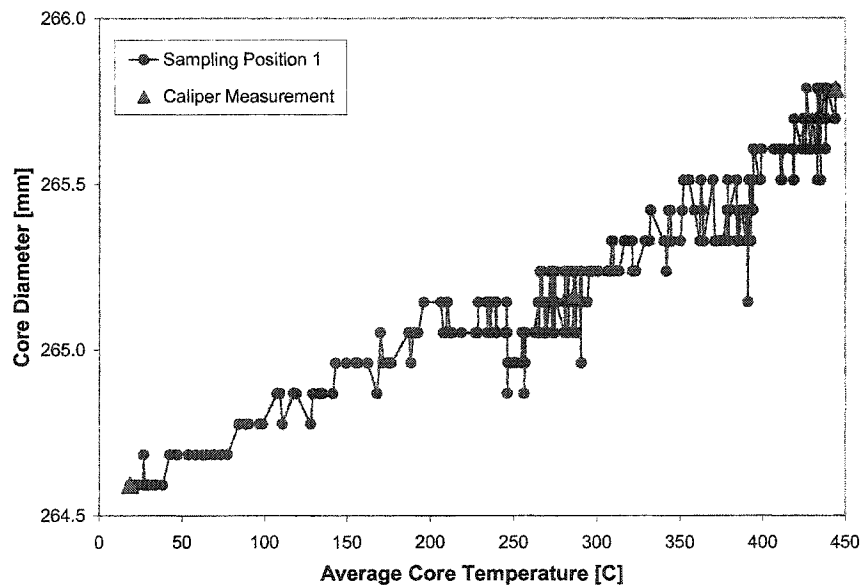


FIGURE 5.17. CCD Data and Caliper Measurements for Test 2 (Sampling Position 1).

Sampling Position	Temp (°C)	Caliper (mm)	CCD System (mm)	Deflection Calipers (mm)	Deflection CCD System (mm)	Error (%) of Caliper
1	25	264.757	Anchor Point	---	---	---
	280	265.354	265.4	0.597	0.643	-7.7
	450	265.963	265.95	1.206	1.193	1.1
2	25	264.592	Anchor Point	---	---	---
	280	265.163	265.16	0.571	0.568	0.5
	450	265.786	265.69	1.194	1.098	8.0

TABLE 5.2. CCD Imaging System and Caliper Measurements (Test #2).

Validation Test 3

The third validation test utilized the same hardware setup adopted in the previous test, with the exception of the core clamping structure. For this and all subsequent tests, the center core clamp was slightly loosened (but maintained at a “light snug” position) to allow core expansion with less constraint at the axial center of the core. The objectives of this test were to verify the Test 2 results for the low temperature atmospheric pressure conditions and to evaluate the CCD imager at higher temperatures under vacuum conditions. The CCD data acquisition code was modified to allow up to three axial positions to be monitored, using an increased slice thickness at each position. Data were sampled over an averaging “box” that monitored several core edge pixels (30 pixels per slice) at each axial location to reduce the potential for signal loss or error. The results of the low temperature, atmospheric tests are shown in Figure 5.18 for the centermost cross section (labeled D2 in Fig. 5.10). Again, the CCD imaging system demonstrated good performance and tracking of the core dimensional changes. The CCD data appears smoother than in the previous test as a result of the spatial averaging implemented in each core profile.

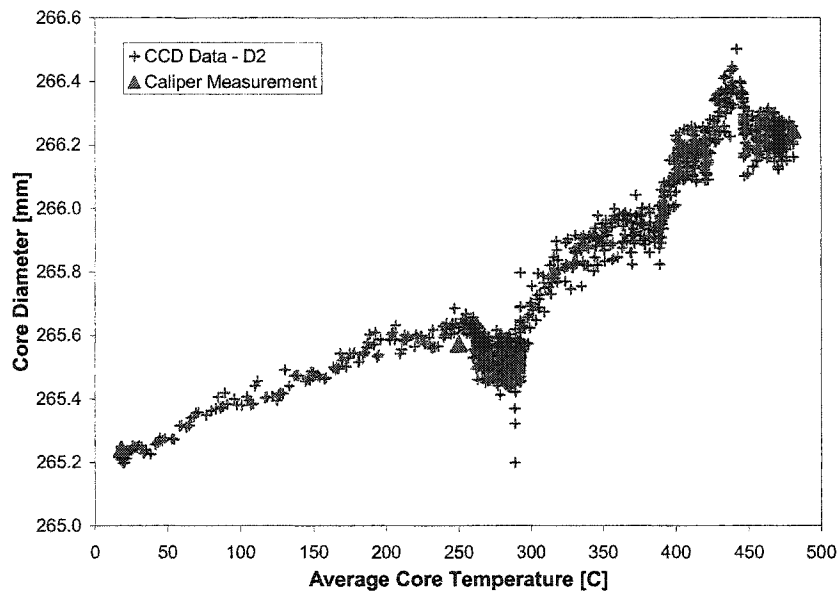


FIGURE 5.18. CCD Data and Caliper Measurements for Test 3 (Diameter D2).

After completing caliper measurements at the 475° C set point, the vacuum chamber was closed and evacuated to 10^{-3} Torr for evaluation of measurement techniques at higher core temperature. Vacuum conditions were required for high temperature operation of the graphite heater elements to prevent oxidation and damage to the heaters. Figure 5.19 shows the measured core growth, determined from CCD imagery, and average internal core temperature over the complete duration of the test, from atmospheric to vacuum conditions. The indicated index number corresponds to 10 second time intervals in the data acquisition. During cool-down, a single thermocouple wire broke, causing the reading to become artificially high and corrupting the average core temperature reported beyond index 3000.

Two observations can immediately be made from these results. First, measurement noise was significantly reduced when the environmental conditions were changed from standard atmosphere to vacuum. This signal noise was attributed to heat waves rising from the core, resulting in a “quivering” effect in the digital image. Establishing a vacuum environment eliminated this condition. Second, a sudden increase

in the core diameter was apparent during the evacuation process. This result may be attributed to any of several conditions: deformation of the sight glass as a result of the pressure differential under vacuum conditions, creating a lens effect; slight compression of the vacuum chamber shell under vacuum, shortening the distance between the camera and the core; a change in the index of refraction between air and vacuum conditions; or, finally, there may have been a real increase in the core diameter. When the chamber was evacuated, convective and conductive heat transfer to the air present under standard atmospheric conditions was removed, causing the core temperature and, subsequently, the core size to increase. The discrepancy between vacuum and atmospheric measurements required that the procedure of anchoring the CCD imager data to pre-test caliper measurements be performed after the chamber was evacuated, providing the correct mm/pixel correlation for the given camera field-of-view settings and environmental conditions.

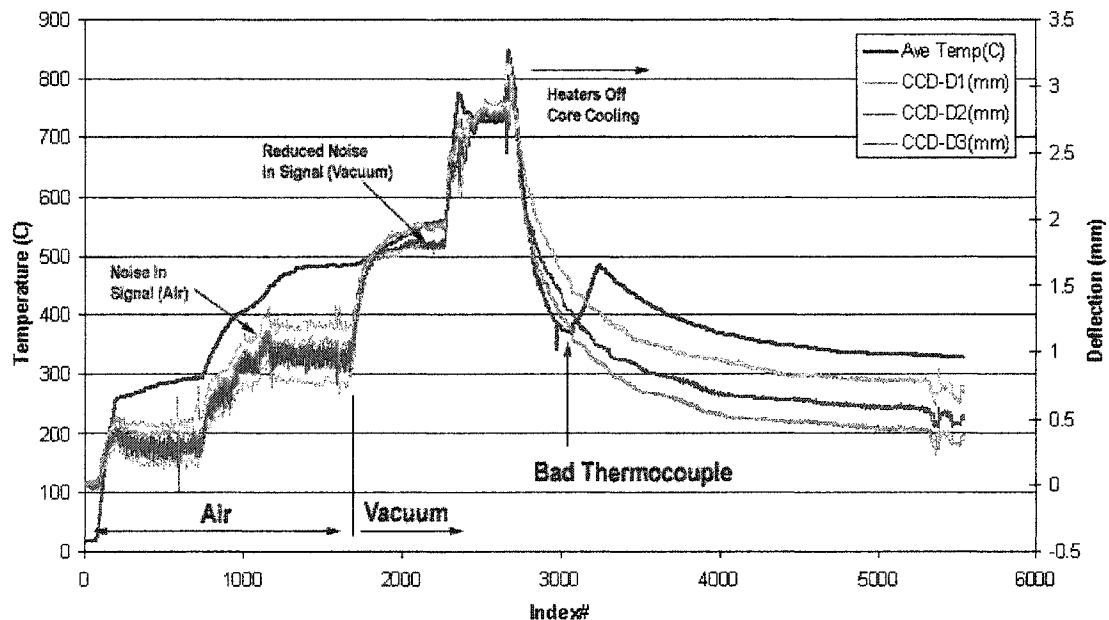


FIGURE 5.19. Measured Deflection and Average Core Temperature for Test 3.

Validation Test 4

A final checkout test was performed for the CCD imaging system to establish initial conditions similar to those for a typical core test. Calibration to caliper measurements was established at vacuum conditions ($\sim 10^{-3}$ Torr). To accomplish this, caliper measurements were taken at room temperature in air, but the mm/pixel calculation was performed using an initial CCD image taken after achieving vacuum conditions. This calibration technique assumes that no physical change in the core size occurred due to the change in the chamber pressure. The core was then heated to three temperature set points (400, 600, and 650°C), applying power to the nine control zones to create an approximately isothermal condition. The correct radial power profile for an operating heat pipe reactor was not applied in the final validation test.

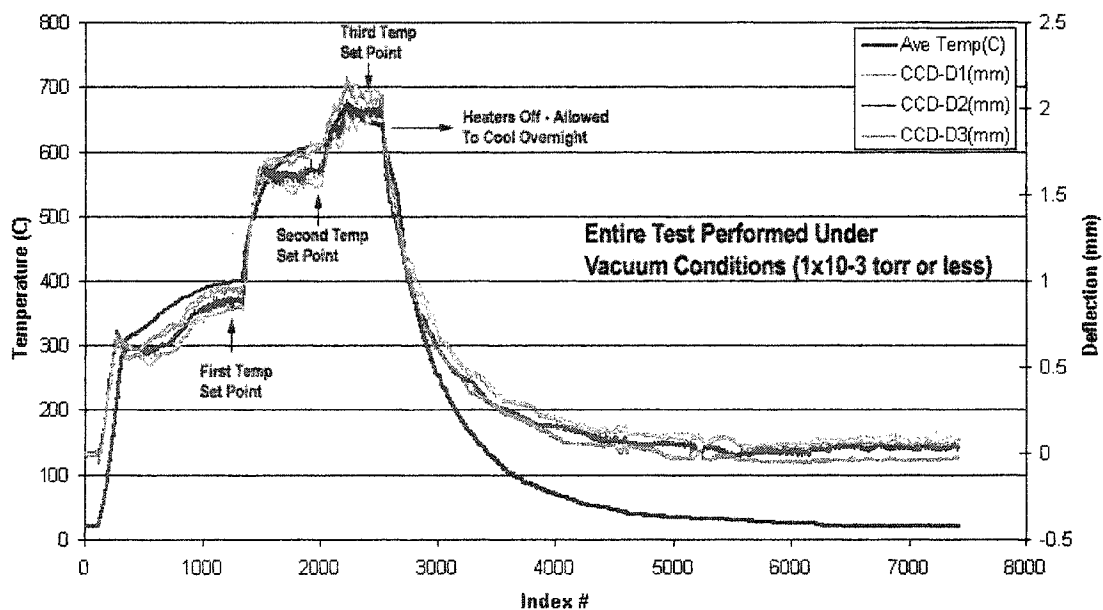


FIGURE 5.20. Measured Core Deflection and Temperature at Vacuum Conditions.

The results shown in Figure 5.20 demonstrate good tracking of the CCD measurements to the core average temperature over a sampling period of nearly 24 hours, ramping through the temperature set points and followed by a cool down to room temperature. The indicated index number corresponds to 10 second time intervals in the data acquisition. Caliper measurements indicated that the core returned to its approximate initial conditions, with a small amount of permanent deformation evident for sampling lines D1 and D2.

5.2 Deflection / Reactivity Feedback Loop

After completion of the CCD validation tests, simulated nuclear feedback was added to the SAFE-100 test (Bragg-Sitton and Forsbacka 2004). This application required a calculation of the reactivity worth of various core deflections to develop either an empirical relationship or a look-up table (LUT) for reactivity as a function of deflection. Analytical solutions were determined for the point kinetics equations in a sourceless ($S = 0$) environment to reduce the calculation time required by the LabView control program during the experiment.

5.2.1 Neutronic Analysis of Core Deflection

For a compact, fast-spectrum reactor, reactivity feedback is dominated by core deformation. As the reactor core temperature increases, the materials in the core begin to deform. Although the deformation is relatively small, it can have a significant impact on the core reactivity due to the high power density in fast reactors. Because the predominant material in the SAFE-100 reactor core prototype was stainless steel, it was assumed that core deflection would be dominated by the thermal expansion of this material. Figure 5.21 shows the coefficient of thermal expansion (CTE) for SS-316 as a

function of temperature (ASME 2001; ORNL; US-DOD 1998). A least squares fit to the tabular CTE data yields:

$$CTE(T) = -1.76 \cdot 10^{-11} T^4 + 5.71 \cdot 10^{-8} T^3 - 7.01 \cdot 10^{-5} T^2 + 4.27 \cdot 10^{-2} T + 6.99, \quad (5.1)$$

where T is the material temperature in degrees Kelvin and CTE is in 10^{-6} mm/mm/K.

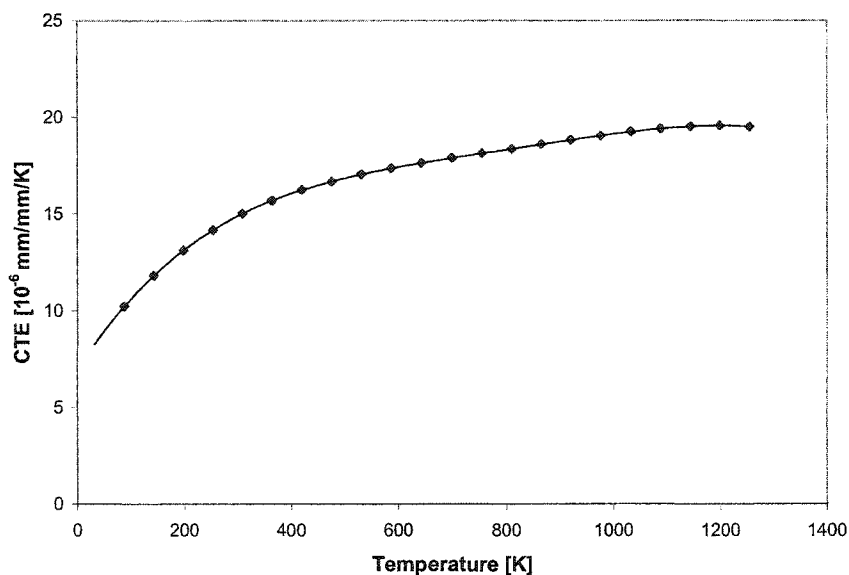


FIGURE 5.21. Average Thermal Expansion Coefficient for Stainless Steel 316.

To simulate core deformation in MCNPX, the entire core was expanded both radially and axially based on the thermal expansion characteristics of SS316. No axial bowing effects were modeled. As the core materials expand, the material density decreases and the gaps in the core (e.g., between fuel and clad) are narrowed. Only geometric and density changes were made in the MCNPX model as the core temperature was increased. No Doppler broadening of the material cross sections was considered at elevated core temperatures. MCNP calculations performed by J. Durkee at Los Alamos National Laboratory on the larger SAFE-500 design (500 kW_t) demonstrated that Doppler broadened cross sections did not significantly impact the core reactivity. Calculations indicated that the predicted core multiplication factor increased less than

0.05% (from $\sim 1.0375 - 1.038$) when Doppler broadened cross sections were included in the simulation (Poston et al. 2004).

Rather than constructing a new input deck to precisely model the SAFE-100, all neutronic calculations were performed using the SAFE-400 model constructed in MCNPX. Conceptually, the heat pipe reactor was designed to be scalable, maintaining similar geometric configuration and component sizes (VanDyke et al. 2002). For example, the SAFE-100 and SAFE-400 designs both maintained a core flat-to-flat size of 26 cm (distance between opposing sides of the hexagonal core) and a fueled height of 50 cm, yielding expected similarity in neutron leakage properties. Therefore, it was assumed that the effect of elevated temperature on core reactivity would have a similar trend in the SAFE-100 and SAFE-400 designs. To more accurately describe the prototype, all the refractory metal (molybdenum) structure in the MCNPX model was replaced by SS316 in the simulation. The experimental application presented here demonstrates the ability to couple non-nuclear testing with nuclear simulations to better replicate the response of a operating reactor to geometric and material changes. Future applications of the methodology will attempt to more precisely model the test article in the nuclear simulations.

Criticality calculations were performed using MCNPX for the stainless steel SAFE-400 core with the control drums rotated to various positions. All calculations assumed that the six control drums moved simultaneously and symmetrically. The reactivity as a function of core deformation for control drums positioned at 120° and 90° from the full-in position is provided in Figure 5.22. The total core deformation is given by $CTE(T) \cdot L_o$, where L_o is the original core diameter before core heating. The center curve (control drums at 106°) was constructed by linear interpolation from the 120° and 90° results to illustrate the effect of increasing temperature on an initially critical reactor core (reactivity = 0 at 300° K). Error bars correspond to one standard deviation and were calculated from the statistical error in the MCNPX calculation of k_{eff} (see Eq. 3.11).

Calculations assumed that the entire core was at a uniform temperature, generating uniform core deformation radially and axially. The corresponding core temperatures are indicated on the vertical profile lines superimposed on the reactivity plot.

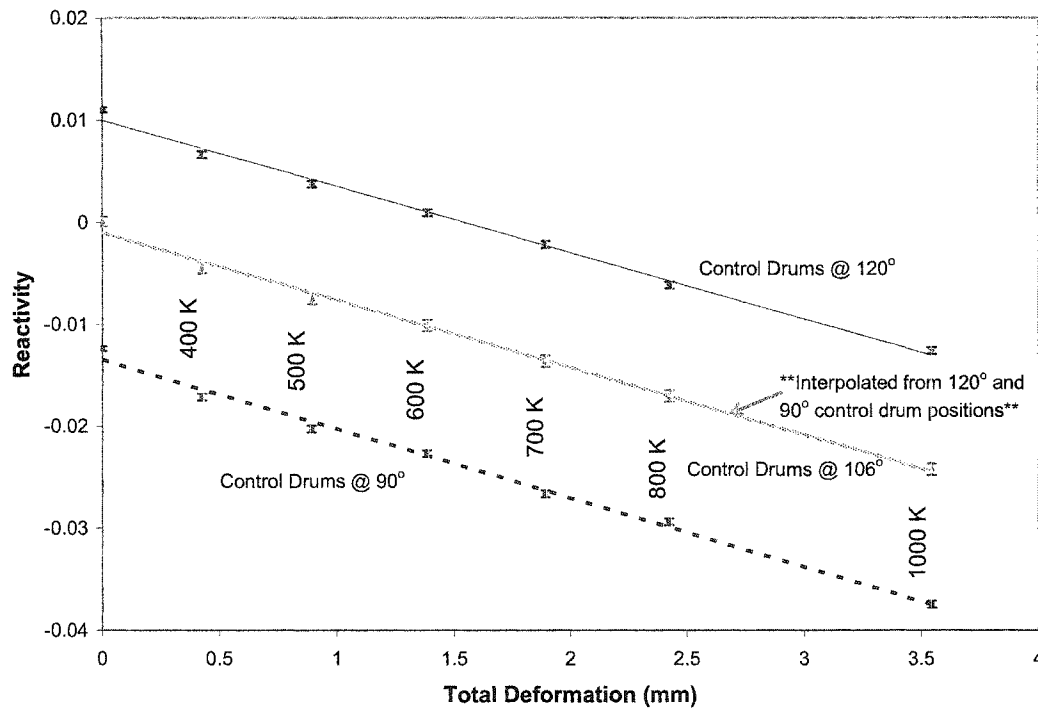


FIGURE 5.22. SAFE-400 Reactivity (SS316 Core) as a Function of Core Deformation.

A sensitivity analysis was performed at 400 K, 800 K, and 1000 K to determine the potential error associated with expansion of the entire core based on the properties of stainless steel. The most sensitive component in the reactor core is the nuclear fuel; variations in the fuel geometry can significantly impact the core neutronics. The expansion of the UN fuel was considered for three scenarios at 400 K: no UN expansion, expansion based on the CTE for UN, and expansion based on the CTE for SS316. The latter two cases were also considered at the higher temperatures of 800 K and 1000 K. The CTE for UN at 900 K (627° C) is $8.5 \times 10^{-6} / \text{K}$ (Suzuki and Arai 1998); this value was applied for UN expansion at all three temperatures considered. Results of the sensitivity

analysis provided in Table 5.3 reveal no significant differences in the calculated reactivity at the tested temperatures, indicating that no significant error was introduced by the assumption that core deformation was dominated by the properties of stainless steel.

Condition	Temperature [K]		
	400	800	1000
no UN expansion	1.00774 ± 0.00033	-----	-----
UN expansion with CTE for UN	1.00675 ± 0.00033	0.99546 ± 0.00032	0.98827 ± 0.00032
UN expansion with CTE for SS	1.00674 ± 0.00032	0.99437 ± 0.00034	0.98814 ± 0.00034
Percent Difference	0.001 – 0.1 %	0.1 %	0.01 %

TABLE 5.3. Calculated Effective Multiplication Factor at the Indicated Conditions.

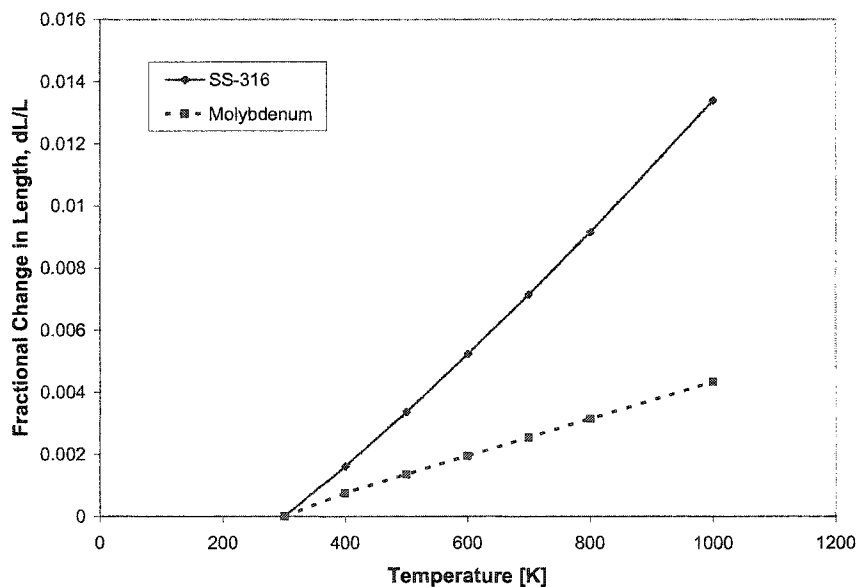


FIGURE 5.23. Thermal Expansion of Stainless Steel 316 and Molybdenum Metal as a Function of Temperature.

The results provided in Figure 5.22 indicate that core deformation significantly effects reactivity for a stainless steel reactor core. A refractory metal core matrix, on the other hand, would experience significantly reduced deflection at the same temperatures, as shown in Figure 5.23. As a result, the anticipated negative reactivity feedback due to

thermal expansion is significantly reduced for a refractory metal core relative to a stainless steel core over the same temperature range, as shown in Figure 5.24.

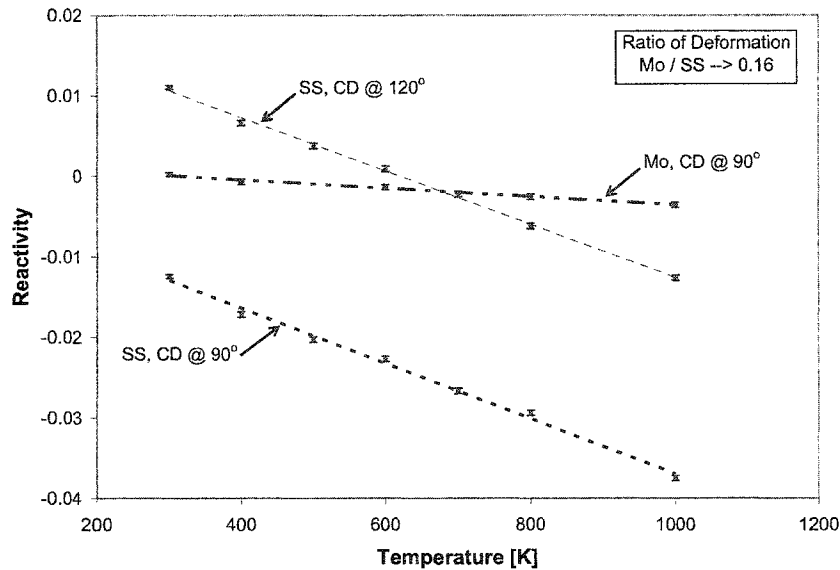


FIGURE 5.24. Variation in SAFE-400 Reactivity as a Function of Core Temperature and Composition.

For the virtual reactivity feedback loop, the nine independent control zones were used to simulate the appropriate radial power profile of an operating reactor. Each control zone in the SAFE-100 core was equipped with its own power supply, each capable of providing 15 kW of power. Neutronic calculations were performed using MCNPX for the SAFE-400 design; these results were then used to determine the appropriate radial power profile for the SAFE-100 tests. The SAFE-400 design includes 12 radial rings that contain fuel elements, as compared to the nine rings in the SAFE-100 design, although the radial core size would be the same. The profile for the SAFE-400 was redistributed over the nine available control zones, yielding the normalized power profile shown in Figure 5.25. The axial power profile was matched by using shaped graphite heaters specifically designed to match the axial power distribution for a SAFE-type reactor.

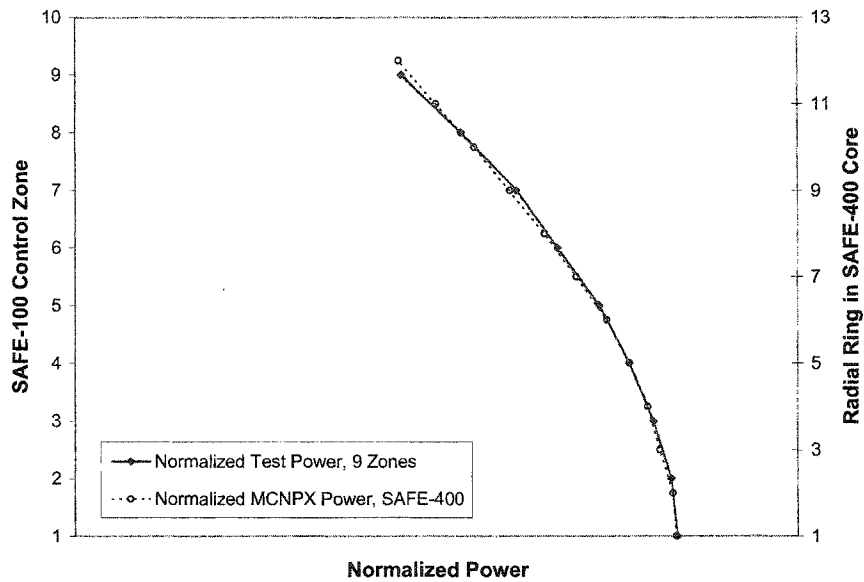
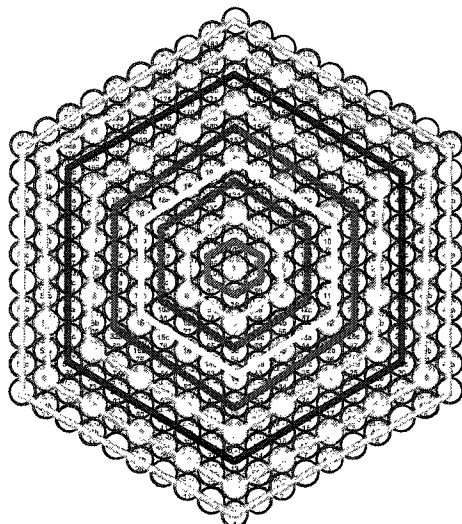


FIGURE 5.25. Normalized Power Profile for the Simulated SAFE-400 Reactor and Distribution of Power for the SAFE-100 Reactor Prototype.

The nine control zones applied in the deflection tests are shown in Figure 5.26. Table 5.4 provides the zone identifiers and the corresponding fraction of the total power applied to each control zone during the virtual reactivity feedback tests. The innermost ring corresponds to control zone 1; the outer ring is control zone 9.



Control Zones

Zone	Color	Power Supply	Fraction of Power
1	Navy	F1	0.134
2	Aqua	G1	0.133
3	Brown	G2	0.129
4	Yellow	G3	0.123
5	Purple	G4	0.117
6	Orange	H1	0.107
7	Black	H2	0.098
8	Green	H3	0.086
9	Pink	H4	0.072

FIGURE 5.26. SAFE-100 Control Zone Map. **TABLE 5.4.** Fractional Power Levels Applied to Each Control Zone.

5.2.2 Point Kinetics Equations: Calculation of Eigenvalues & Eigenvectors

Given the total “virtual” reactivity in the simulated reactor at a given time, the power delivered to the SAFE-100 core was determined by solution of the point kinetics equations (Eqs. 2.11) for the predicted reactor power, $P(t)$. The total reactivity is given by the sum of the drum reactivity (ρ_{drum}) at the specified control drum position and the reactivity worth of the core deflection (ρ_{dL}),

$$\rho_{total} = \rho_{drum} + \rho_{dL} . \quad (5.2)$$

The drum reactivity for the stainless steel SAFE-400 reactor core was determined by MCNPX calculation at 60°, 90°, and 120° from the full-in control drum position. A simple empirical relationship $\rho(\theta)$ was fit to the calculated data points using the least-squares fitting algorithm provided by Microsoft Excel (Figure 5.27). Because only a small range of angular motion was anticipated for the control loop, a simple second order polynomial was selected for the relationship, yielding:

$$\rho_{drum} = \rho(\theta) = -3.85 \cdot 10^{-6} \theta^2 + 1.59 \cdot 10^{-3} \theta - 0.124 , \quad (5.3)$$

where the angle θ is given in degrees. The results presented in Figure 5.22 indicated that the negative reactivity inserted by core deformation maintains a linear relationship (with approximately the same slope) regardless of the initial control drum configuration. Therefore, an expression for ρ_{dL} was determined by a linear fit for an initially critical reactor ($\theta \sim 106.427^\circ$):

$$\rho_{dL} = \rho(dL) = -1.864 \cdot \frac{dL}{L_o} , \quad (5.4)$$

where L_o is the initial core diameter prior to heating (~ 300 K) and dL is the measured deflection from the start of the experiment.

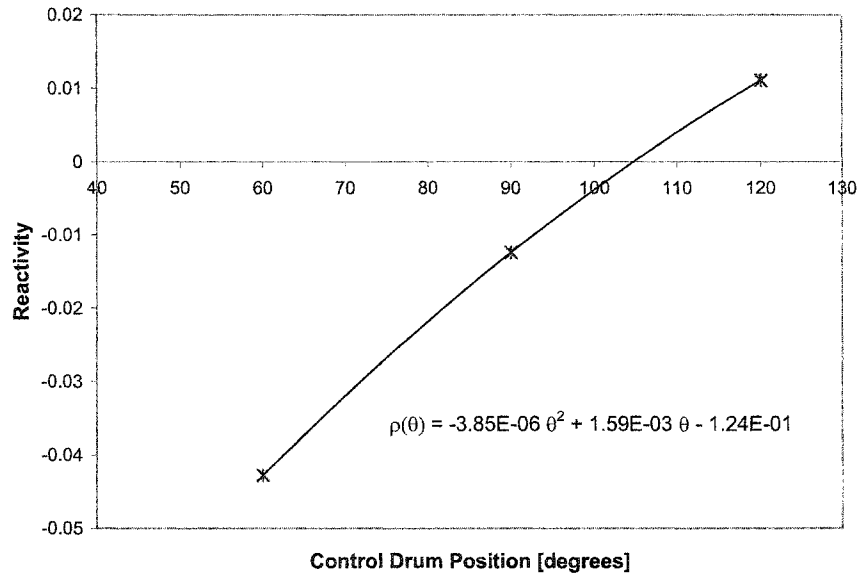


FIGURE 5.27. Empirical Relation for Reactivity as a Function of Control Drum Position; Applied in Virtual Reactivity Experiment.

To minimize the computation time required by the LabView data acquisition program, the eigenvalues and eigenvectors of the system of ordinary differential equations that compose the point kinetics equations (Eq. 2.11) were pre-calculated and programmed in algebraic form in LabView. The PKE applied in the feedback loop were solved in the absence of an external source ($S = 0$) and for one group of delayed neutrons. Written in terms of power, the system of differential equations is given by:

$$\frac{dP}{dt} = \frac{(\rho - \beta)}{\Lambda} P + \lambda C, \quad (5.5a)$$

$$\frac{dC}{dt} = \frac{\beta}{\Lambda} P - \lambda C. \quad (5.5b)$$

The general solution for this linear, homogeneous system of equations can be constructed from its eigenvalues and eigenvectors. The eigenvalues, r_+ and r_- , are easily found from the homogeneous equations and are given by:

$$r_{\pm} = \frac{1}{2} \left[- \left(\lambda + \frac{\beta - \rho}{\Lambda} \right) \pm \sqrt{\left(\lambda + \frac{\beta - \rho}{\Lambda} \right)^2 + 4 \left(\frac{\lambda \rho}{\Lambda} \right)} \right], \quad (5.6)$$

which have the corresponding eigenvectors, ξ_+ and ξ_- :

$$\xi_{\pm} = \begin{pmatrix} 1 \\ r_{\pm} - \frac{\rho - \beta}{\Lambda} \\ \frac{\Lambda}{\lambda} \end{pmatrix} = \begin{pmatrix} 1 \\ e_{\pm} \end{pmatrix}. \quad (5.7)$$

The general solution of Eqs. (5.5) can then be constructed:

$$\begin{bmatrix} P(t) \\ C(t) \end{bmatrix} = a_+ e^{r_+ t} \xi_+ + a_- e^{r_- t} \xi_- . \quad (5.8)$$

Given the initial conditions for the system, the specific solution to the system of differential equations can be found. At time t_0 , the power and precursor concentrations are given by $P(t_0) = P_o$ and $C(t_0) = C_o$. Substituting these values at time t_0 , the coefficients a_+ and a_- are found from Eq. (5.9):

$$\begin{pmatrix} a_+ \\ a_- \end{pmatrix} = \frac{1}{e_+ - e_-} \begin{pmatrix} -e_- & 1 \\ e_+ & -1 \end{pmatrix} \begin{pmatrix} P_o \\ C_o \end{pmatrix}. \quad (5.9)$$

Equations (5.6-5.9) were programmed into the data acquisition and control program that was written in LabView, such that $P(t)$ and $C(t)$ were solved in real time given variations in the core reactivity, ρ_{total} . The solution for $P(t)$ was used to determine the rate of change of input power to the nine control zones at each iteration of the control loop. Although the precursors could not be simulated in the electrically heated core, the solution for $C(t)$ was used to find new initial conditions at each point the control loop was iterated. Implementation of the control loop will be discussed in more detail when the full test procedure is presented (Section 5.2.4).

5.2.3 Determination of CCD Imaging System Resolution

The resolution of the CCD measurements with spatial and temporal averaging was assessed. With the core at room temperature, the core width was measured along three CCD sampling lines using a caliper. As discussed in the validation test program

these measurements were used to calibrate the CCD measurement in the CCD data acquisition program, which was coupled to the LabView control program. As in previous tests, the camera field of view (FOV) settings provided a CCD pixel size of approximately 0.09 mm.

For a measurement taken along a single pixel width at a single time point, the CCD resolution corresponds to one pixel, or 0.09 mm. However, spatial and temporal averaging can be used to improve the measurement resolution. Incorporating a spatial averaging box containing multiple edge pixels (versus a single edge pixel in initial tests), as shown in Figure 5.28, removes bias that could exist due to the selection of the precise cross section location. In addition, a spatial average can account for small deviations in the straightness of the core or a small amount of twist in the core positioning. Twist could result in a slightly diagonal cross section in lieu of a cross section that is perfectly perpendicular to both core edges. For each profile selected in reactivity feedback tests, a 30 pixel (2.7 mm) slice thickness was applied.

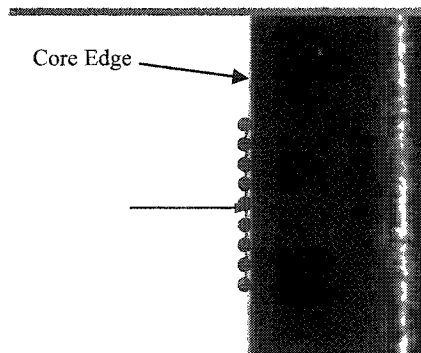


FIGURE 5.28. Incorporation of Spatial Averaging in CCD Deflection Measurements.

To determine the resolution of the CCD imaging system with spatial averaging implemented (30 pixel slice), data were acquired for approximately one hour with the core at room temperature and at vacuum conditions ($\sim 10^{-4}$ Torr). A noise analysis was

performed on the resultant data, taken at one second intervals. Figure 5.29 shows the distribution of the pixel width measurements over this time for the centermost profile, identified as “D2” in Figure 5.10. The mean pixel width of the room temperature core was 2894.147 pixels with a corresponding standard deviation of 0.0736 pixels. This corresponds to a measurement resolution of $0.0736 \text{ pixels} * 0.09 \text{ mm/ pixel}$, or 0.0068 mm.

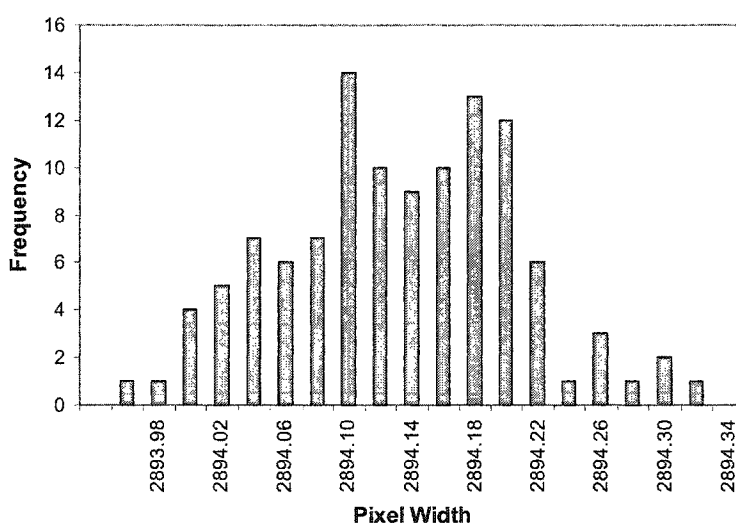


FIGURE 5.29. Distribution of One Second CCD Data.

To further improve the measurement resolution, a temporal average of the CCD data was implemented. In the averaged data, the reported value at a given time point corresponds to the average of the values for the previous five seconds. A five point moving average is constructed, with data still being reported every second. The distribution of temporally averaged data is shown in Figure 5.30. The mean pixel width of the core was 2894.149 pixels, essentially identical to the mean width in the one second data ($\Delta = 0.002$ pixels), with a standard deviation of 0.0379 pixels. This corresponds to a measurement resolution of $0.0379 \text{ pixels} * 0.09 \text{ mm/ pixel}$, or 0.0035 mm.

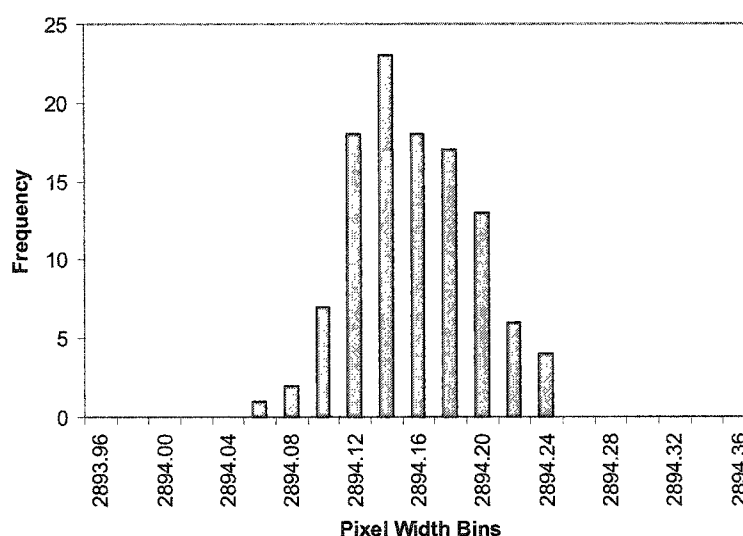


FIGURE 5.30. Distribution of CCD Data Applying a Five Second Moving Average.

Due to the improved measurement resolution provided by spatial averaging, as compared to a single pixel slice thickness, all implementations of the virtual reactivity feedback loop applied spatial averaging of the CCD data over a 30 pixel axial thickness. Initial tests of the reactivity feedback loop did not include temporal averaging, but later tests incorporated a 5-second moving average to further improve the measurement resolution.

5.2.4 Virtual Reactivity Test Procedure

The test procedure applied in the virtual reactivity feedback loop is illustrated in the flow chart shown in Figure 5.31. Using mechanical calipers and real-time image guidance, initial core measurements were taken at each of the three cross sections identified in Figure 5.10 to calibrate the system. Growth was tracked along all three selected cross sections, but the LabView control program only utilized growth at the centermost cross section, D2, to modify the calculated reactivity and to implement control loop cycles.

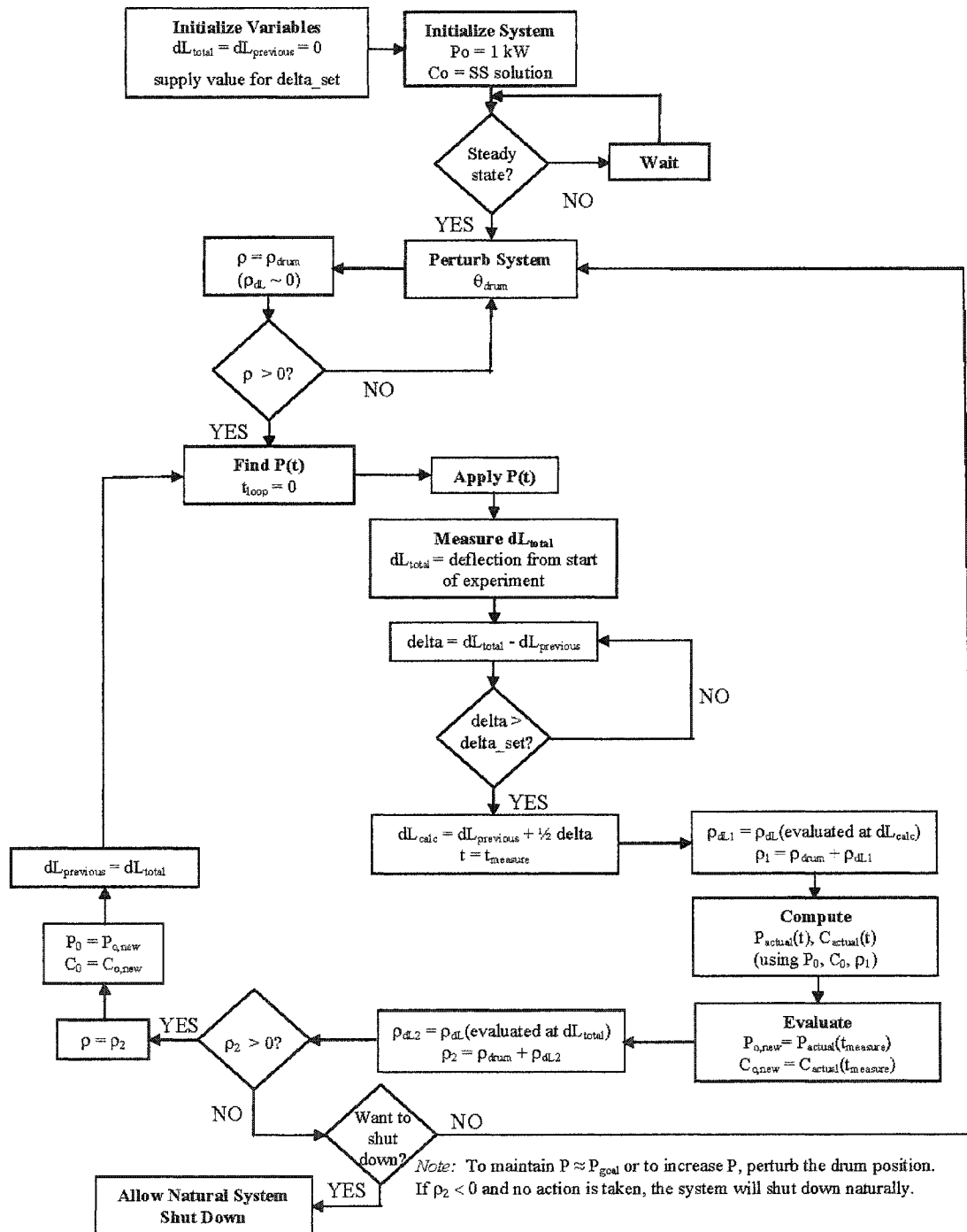


FIGURE 5.31. Virtual Reactivity Feedback Test Flow Chart.

After initial calibration measurements were completed, the vacuum chamber was evacuated to approximately 10^{-4} Torr. The initial caliper measurements were then input into the CCD data acquisition program to calibrate the CCD measurement and to lock the initial core size to the reference image. The core was initialized to a low power condition (1 kW_t to the full core), applying the power distribution in Table 5.4. The initial precursor concentration (C_o) was found from the steady state solution of the PKE at P_o . At steady state, the time derivative goes to zero, yielding:

$$C_o = -\frac{\rho - \beta}{\lambda \Lambda} P_o . \quad (5.10)$$

With the power initialized to 1 kW_t, the average core temperature, calculated as a simple average of thermocouples across the core radius, was allowed to increase to approximately 150 – 200° C prior to implementation of the reactivity feedback loop. Due to the power peaking in the central region of the core and the lack of any mechanism for heat removal, the inner region of the core was at significantly higher temperature than the outer regions. As a result, a relatively high average core temperature was required to increase temperatures at the core periphery before measurable deflection was detected by the CCD data acquisition system.

When initializing the core at low power, it was assumed that the control drums in the corresponding nuclear system were positioned such that the reactor was critical. Hence, the “virtual” control drums in the electrically heated core were also set to a position such that $\rho_{drum} = 0$ at P_o . For the stainless steel version of the SAFE-400 core, criticality at 300 K (room temperature) corresponded to a drum position of 106.427° C. After reaching the desired initial power and temperature, the theoretical control drum positions were perturbed to insert positive reactivity and to increase core power. The corresponding $P(t)$ was found by solution of the PKE for the given reactivity insertion using Eqs. (5.6-5.9), and the reactor power was allowed to increase along this curve until a deflection greater than a pre-set value “delta_set” was reported by the CCD data

acquisition software. The measured “delta” value was determined as the deflection measured since the previous calculation of $P(t)$, such that $\text{delta} = dL_{\text{total}} - dL_{\text{previous}}$. The “delta_set” value should be set with respect to the measurement resolution of the CCD deflection measurement. For instance, with spatial (30 pixel) and temporal (5 second) averaging implemented, the CCD resolution was 0.0035 mm. In this case an appropriate delta_set would be 0.01 mm, approximately three times the measurement resolution. This value was set just prior to implementation of the control loop, but initial tests of the control loop also allowed adjustments to delta_set during test to permit the user to get a better feel for the system response prior to implementation of fully automated control.

After reaching the desired core deflection, the solutions to the PKE were recalculated to better simulate the response of a nuclear system to the measured change in core geometry. At this point in the test procedure flow chart (Fig. 5.31), one will note that the deflection used to calculate the new initial conditions for the PKE is given by $dL_{\text{calc}} = dL_{\text{previous}} + \frac{1}{2} \text{delta}$. Because an actual operating nuclear reactor would not have precisely followed the previous $P(t)$ that was applied to the power controllers, but instead would have been continuously updated based on the real-time geometric changes in the core, calculation of dL_{calc} using half of the measured deflection (delta) at time t_{measure} allows some adjustment for what the current values of power and precursor concentration would be in the corresponding reactor. This adjustment to the applied electrical power is illustrated in Figure 5.32.

After making the necessary adjustments for the measured deflection, the values $P(t_{\text{measure}})$ and $C(t_{\text{measure}})$ are then used as the new initial conditions ($P_{o,\text{new}}$ and $C_{o,\text{new}}$) for solution of the PKE. To solve for the new $P(t)$ and $C(t)$, the total measured deflection from time zero (dL_{total}) is applied. Prior to calculation of the new $P(t)$, the new total reactivity, identified as ρ_2 in the flow chart, is assessed. If the current reactivity is still positive, the loop automatically recalculates the power $P(t)$ and continues operating as in the previous loop iteration. If, however, the measured deflection at t_{measure} has introduced

sufficient negative reactivity to cause the total reactivity to become less than or equal to zero, additional positive reactivity must be introduced by perturbation of the control drums to continue to increase power or to maintain the current power level. If the operator chooses to allow the simulated reactor shut down, simply leaving the “virtual” control drums in their current position will cause the controller to decrease power as a result of the negative reactivity feedback and “natural” shutdown will occur.

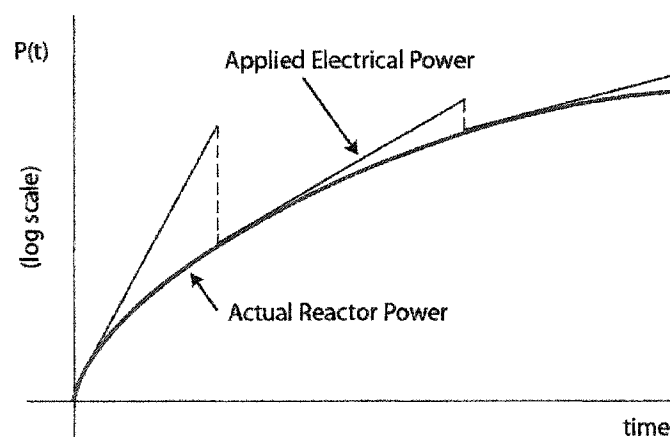


FIGURE 5.32. Correction to the Applied Electrical Power at Each Measurable Core Deflection; Applied to Better Simulate an Operating Nuclear Reactor.

5.2.5 Experimental Results

The virtual reactivity feedback test was operated several times in “manual” mode to work out any programming issues in the LabView control code written specifically for this application. In the initial tests, the reactor power transient calculated by LabView for a given virtual control drum position and measured deformation were verified off-line by direct solution of the reactor point kinetics equations. When operated manually, the controller measured core deflection automatically, but then required user intervention to implement the control loop when the desired deflection, δ_{set} , was reached. In addition, manual adjustments to the virtual control drum positions were required

throughout the test. Future versions of the controller for the reactivity feedback loop will automatically recalculate $P(t)$ and $C(t)$ after reaching the desired deflection, automatically adjusting the theoretical control drum position to achieve and maintain the desired goal power level. The program for the fully automated control loop was completed, but during its initial implementation, the power supplies to two different control zones shorted to one another, resulting in a current surge that burned out transistors in both power supplies. As a result, the experimental program was temporarily shut down to repair the damaged power supplies and to reassess the parameters applied in the power control system to prevent damage to the power supplies in future operation.

A series of three manual tests was performed prior to the power supply failure. In all plots of the experimental results for power, temperature, deflection, and virtual control drum position, the delay time between the implementation of each control loop was not accounted for in the recorded data. The data acquisition program simply paused when the measured deflection reached the desired amount, waiting for user intervention to begin the next control loop iteration and to increment the time index. As a result, at each iteration of the control loop, a few seconds of time were “lost” and the time stamp on each of the data points was not completely accurate. Fully automated implementation of the control loop will eliminate this error in the recorded data for future applications.

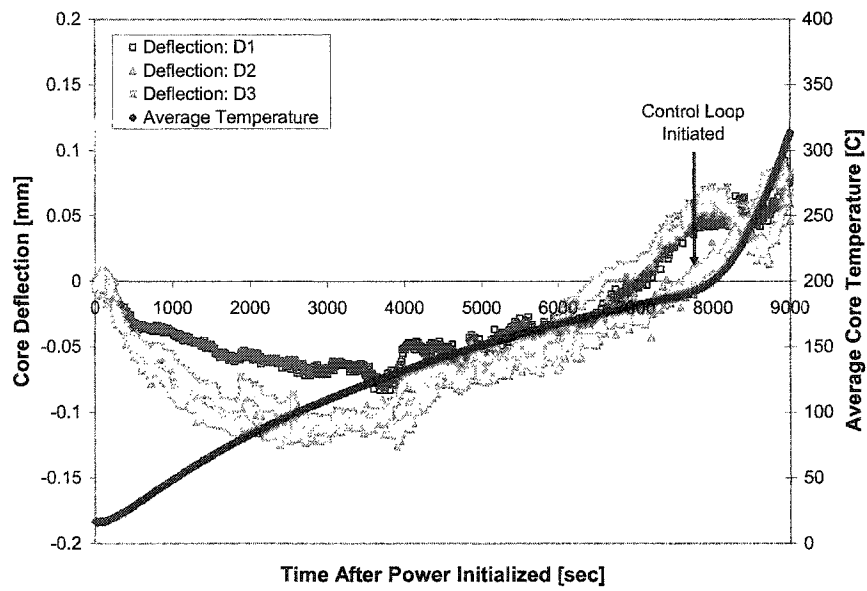
All tests of the virtual reactivity feedback loop were conducted inside the vacuum chamber. Initial tests were performed at vacuum conditions (4×10^{-4} Torr). For the final test, the vacuum chamber was evacuated to the 10^{-4} Torr range and was then back-filled with approximately 50 Torr of argon gas. Because the SAFE-100 reactor core prototype was simply a bank of stainless steel tubes held together by steel clamps around the core periphery, the primary heat transfer was by radiation at vacuum conditions. Filling the chamber with a small amount inert gas improved the thermal core coupling by providing a medium for heat conduction between the individual stainless steel tubes. The optimal fill pressure was not determined. The latter environment better simulates the coupling in

an actual reactor core in which modules would be brazed together or inserted into a monolithic block.

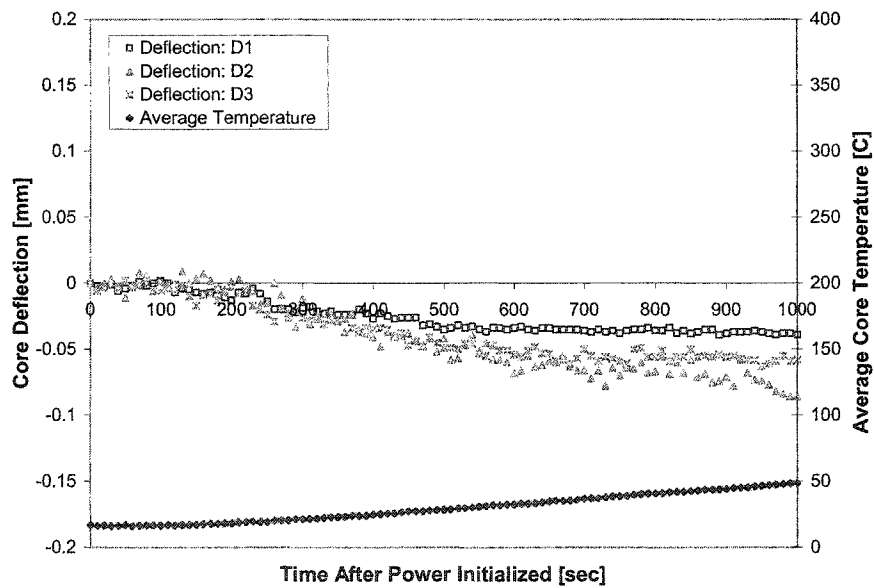
Initialization of Core Power

In all tests, the reactor core was initialized by applying approximately 1 kW_t to the full core. This was accomplished by setting the current for each control zone to a maximum of 6.5 A, allowing the National Instruments power controller to determine the appropriate current in each zone to obtain the desired radial power profile. Upon application of power to the core, the measured core size along all three sampled cross sections initially decreased, reporting a negative deflection after approximately 100 – 200 seconds. This behavior occurred in all deflection tests. As the average core temperature began to increase, the core size reached a minimum before beginning to increase once again.

For example, in the final test conducted with argon gas in the chamber, the core reached its original size at an average temperature of approximately 180° C. The tests performed in vacuum required a slightly higher average core temperature (> 200° C) prior to recovering the initial core size. A sample of this behavior is shown in Figure 5.33. The data presented correspond to the initial 9000 seconds of operation with the chamber filled to approximately 50 Torr of argon gas. A decline in the measured core size was readily evident, with a subsequent rise over the remaining period. In this test, the control loop was initiated at approximately 7900 seconds, when the center core cross section, D2, indicated that the core had returned to its original size.



(a)



(b)

FIGURE 5.33. Measured Core Deflection During Initial Core Heat Up.

Possible sources of this unexpected decline in core size could be electrical noise, effects of elevated environmental temperature, or “shifting” of the core modules as a result initial stresses placed on the core. If electrical noise was responsible for the

behavior, the decline in core size would be expected to immediately follow initialization of the power. Examination of the first 1000 seconds after turning on the power (Fig. 5.33 (b)) indicates that the decrease in core size was not immediate, but began to occur after approximately 200 seconds, eliminating the possibility that electrical interference generated an artificial decrease in the core size reported by the CCD imager. Elevated environmental temperature could also affect the camera operation. However, the decrement in core size occurred before any significant heating of the core module was measured (average core temperature $\sim 50^{\circ}\text{C}$), suggesting that temperature effects were not a significant component of the unexpected behavior. Pre-stressing of the core by the core clamping structure was the most likely source of the initial decrement in the measured core size. The three steel clamps around the core (see Fig. 5.1) were tightened snugly to the core, applying initial stress to the core modules. As the core heated up, the temperature of the center modules increased much more quickly than the temperature of the modules on the periphery, causing the interior modules to deform earlier than the remainder of the core. The temperatures for a thermocouple located near the center of the core and one at the periphery are shown in Figure 5.34. As early deformations occurred on the interior, the core elements likely shifted somewhat due to the assistance of the clamping stresses applied at the periphery. At the point when the interior expansion stresses exceeded the initial stresses imposed by the clamps and the temperature of the clamps themselves began to rise, the core diameter could physically increase once again, explaining the behavior observed by the CCD imaging system.

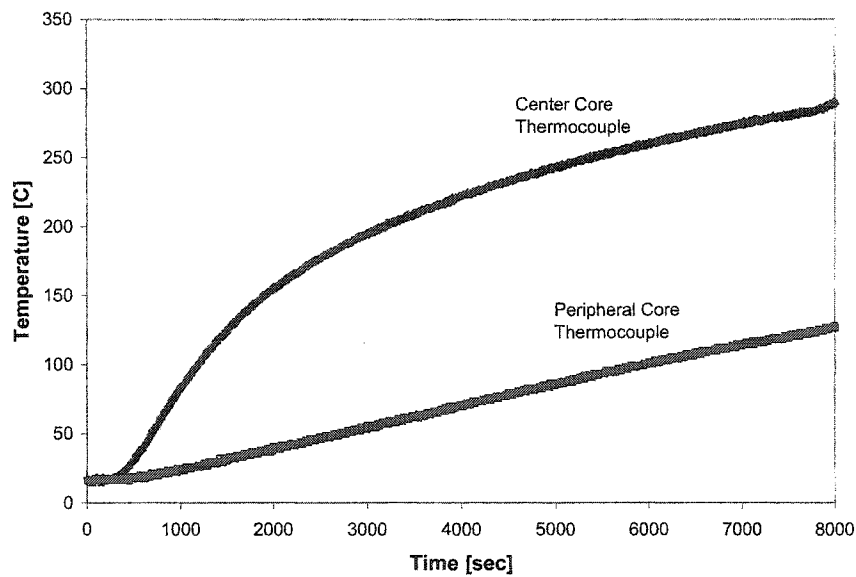


FIGURE 5.34. Measured Core Temperature During Initial Core Heat Up.

Control Loop Implementation

In the first application of the reactivity feedback control loop, the test objective was to demonstrate increasing core power for a given perturbation to the virtual control drums (positive reactivity insertion) and the subsequent stabilization of the core power level due to negative reactivity inserted by core deformation. Testing was performed at approximately 10^{-4} Torr. After reaching an average temperature of approximately 153°C , the control loop was initiated. Due to the reduction in the core size observed upon application of power, the deflection was zeroed prior to implementation of the control loop. While not completely accurate in its model of a nuclear system, making this change allowed the control loop to be tested without concern regarding the effect of negative deflections on the code logic.

The core was perturbed from steady state operation at 1 kW_t by moving the virtual control drums from their original position of 106.427° to 107.5° , a 1.073° perturbation. The correct $P(t)$ for this perturbation was calculated using Eqs. (5.6-5.9), and the power supplies provided the prescribed calculated power to the control zones in

the SAFE-100 core. At a measured core deflection of 0.05 mm, the control loop was implemented to recalculate the solution to $P(t)$ and $C(t)$. The calculated and applied powers are shown in Figure 5.35. The decrease in power evident at approximately 270 seconds is a result of calculating the new initial conditions, $P_{o,new}$ and $C_{o,new}$, using only half of the measured deflection from time 0 to 270 seconds. As the rate of change of the power decreased, readjustment of the initial conditions for the next loop iteration was less evident. Application of the virtual reactivity feedback loop allowed the core to reach a power of approximately 20 kW_t and maintained this power for approximately 150 seconds before the negative reactivity feedback provided by the core deformation exceeded the drum reactivity and the reactor power began to decrease. The full shut down is not shown due to a failure in the data acquisition code. This error was corrected before subsequent tests were performed.

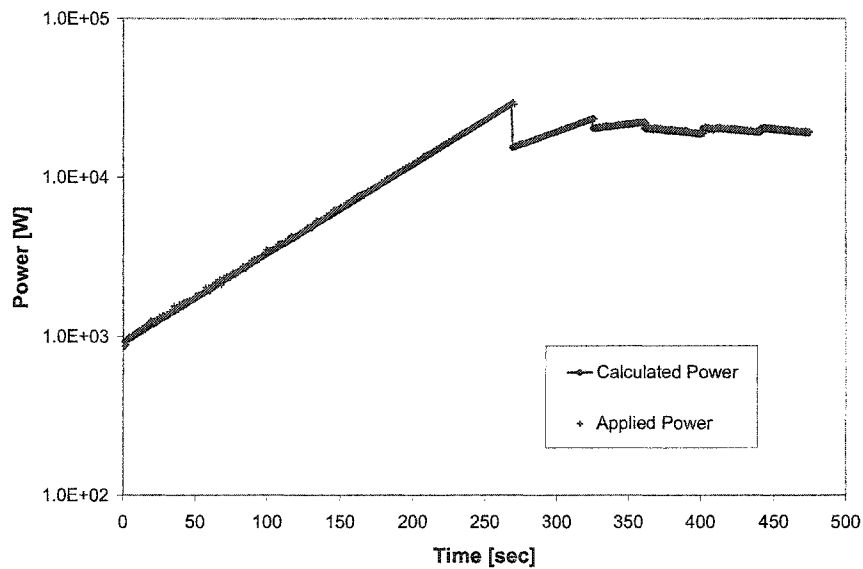


FIGURE 5.35. SAFE-100 Power for Virtual Reactivity Feedback Control (Test 1).

The second application of the feedback loop was again performed at vacuum conditions ($\sim 4 \times 10^{-4}$ Torr) and the core was initialized to 1.0 kW_t. The average core

temperature was allowed to increase to approximately 200° C prior to implementation of the control loop. At this point, the measured core size was still negative, but no artificial adjustments were made to the deflection measurement to avoid negative values. The approximate measured deflections from time zero at the indicated average core temperatures prior to control loop implementation are provided in Table 5.5, indicating negative deflection from the start of heating. The variable “delta_set,” the point at which the PKE are re-solved for the current conditions, was set to 0.0 mm to allow the core to return to its original size prior to implementing the control loop. The measurement delta_set does not consider the absolute value of the deflection, only the increase in core size from the previous control iteration. Prior to implementing the control loop, delta_set was referenced to the calibrated core size, such that a value of 0.0 corresponded to the core having returned to its original size after the initial reduction in the core width prior to implementing the next step.

$T_{avg} \rightarrow$ Core Profile $\downarrow$	130 °C	172 °C	200 °C
$\Delta D1$ (mm)	-0.09	-0.11	-0.02
$\Delta D2$ (mm)	-0.10	-0.06	-0.06
$\Delta D3$ (mm)	-0.06	-0.08	-0.05

TABLE 5.5. Measured Core Deflection for During Initialization at 1 kW_t.

A very slow transient was selected for this test, with a very slight drum perturbation from 106.427° to 106.5°, which would provide a power rise from 1 kW to 10 kW over 400 seconds if $P(t)$ were not adjusted based on measured deflections. However, the variable “delta_set” was poorly selected at 0.0 mm. Although this set point allowed the core to return to its original size prior to the next control loop iteration, because only half of the measured deflection was used in recalculating the initial conditions for the next iteration of the control loop, the deflection dL_{calc} was negative (e.g., $dL_{prev} = 0$, so $dL_{calc} = dL_{prev} + \frac{1}{2} \text{delta} = 0 + \frac{1}{2} (-0.06) = -0.03$ mm). As a result, the total core reactivity

caused the power to suddenly drop from approximately 8 kW to 1.3 kW, the calculated initial condition for the next loop iteration. Recall from Fig. 5.32 that the large adjustments made to the applied electric power level were corrections intended to bring the simulated system closer to a true reactor in which the reactivity worth in the core would constantly be adjusted by core deformation. In the result shown in Figure 5.36, which considers the first 400 seconds after implementation of the control loop, the jump in the applied power level was rather large due to the negative deflection value. Because all subsequent loop iterations incorporated a positive value for dL_{calc} , this error was not repeated through the remainder of the test.

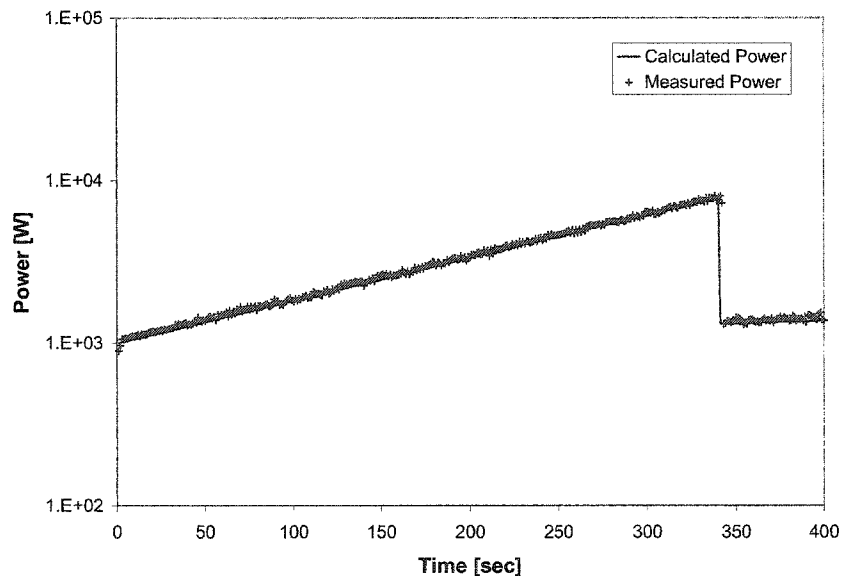


FIGURE 5.36. SAFE-100 Power: Miscalculation of Initial Conditions (Test 2).

For the remainder of the test, δ_{set} was adjusted to a more appropriate value of 0.01 mm, approximately three times the calculated measurement resolution (spatial and temporal averaging were implemented). The virtual control drum position was perturbed to 107° to provide a gradual power increase, with a goal of achieving a steady state power at approximately 10 kW_t. As shown in Figure 5.37, the control drum position

was perturbed many times up to a maximum position of 111.1° in order to achieve the desired power level during rapid core deformation. Because the power rise was performed more gradually during this test, the average core temperature reached higher values than in the previous test for the same core power, and the core deformed to a greater extent over the same power range. As a result, a larger amount of positive reactivity had to be inserted by rotating the virtual control drums to increase the power to the desired level of 10 kW_t . However, it was very difficult to maintain steady state operation at this power level because there was no mechanism for heat removal. The temperature continued to increase even while power was decreased, causing continued core deformation and negative reactivity insertion. As a result, without regular adjustments to the control drum position at each implementation of the control loop, the reactor would tend to shut down. During this test it became evident that automated adjustments to the control drum position and automated iteration of the control loop were required to properly control the reactor power at a constant value.

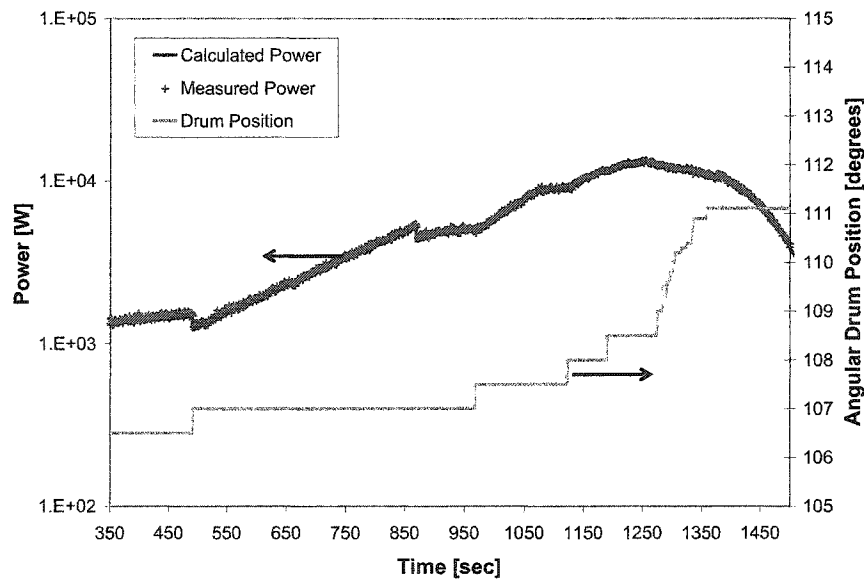


FIGURE 5.37. SAFE-100 Prototype: Full Test to 20 kW_t With Shut Down (Test 2), Thermal Power and Virtual Control Drum Position.

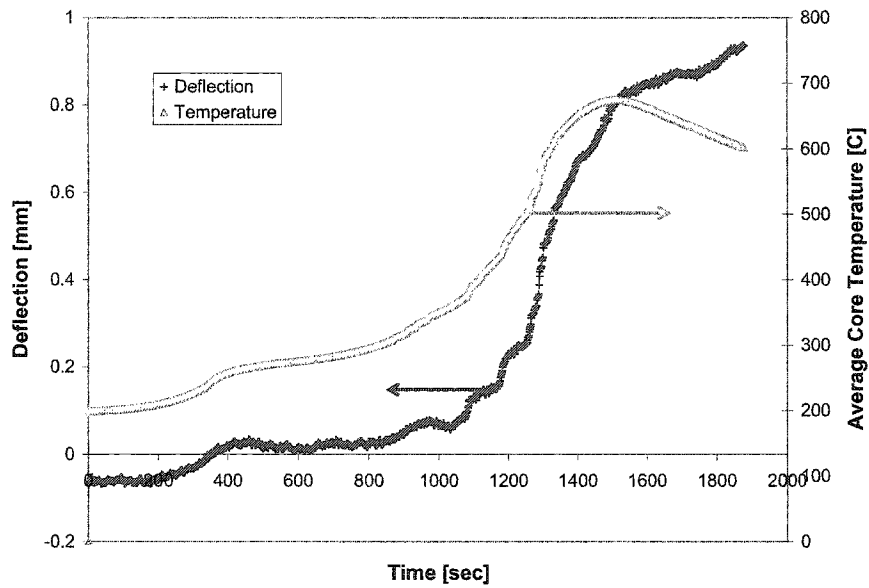


FIGURE 5.38. SAFE-100 Prototype: Full Test to 20 kW_t With Shut Down (Test 2), Average Core Temperature and Radial Deflection.

The third and final test of the manually iterated virtual reactivity feedback loop was performed with argon fill (50 Torr) in the vacuum chamber to improve core coupling. The objective of this test was to increase core thermal power from an initial power level of 1 kW_t to approximately 10 kW_t, to hold the goal power level at steady state, and to allow natural shutdown of the simulated reactor based on negative reactivity inserted by core deformation. Because there was no mechanism for heat removal in the SAFE-100 prototype, the temperature continued to increase when the core was held at a constant (or decreasing) power level. Therefore, the goal power level of 10 kW_t could only be held for a limited time before the temperatures at the center of the reactor core approached levels that could cause damage to system components.

The calculated and applied thermal power and the corresponding virtual control drum positions are shown for the full duration of the test in Figure 5.39. During the initialization of the core at low power, the argon fill provided improved core coupling, resulting in a flatter temperature profile across the core radius. Therefore, the peripheral

core temperature increased more rapidly than in previous tests and measurable deflection was noted at lower average core temperature.

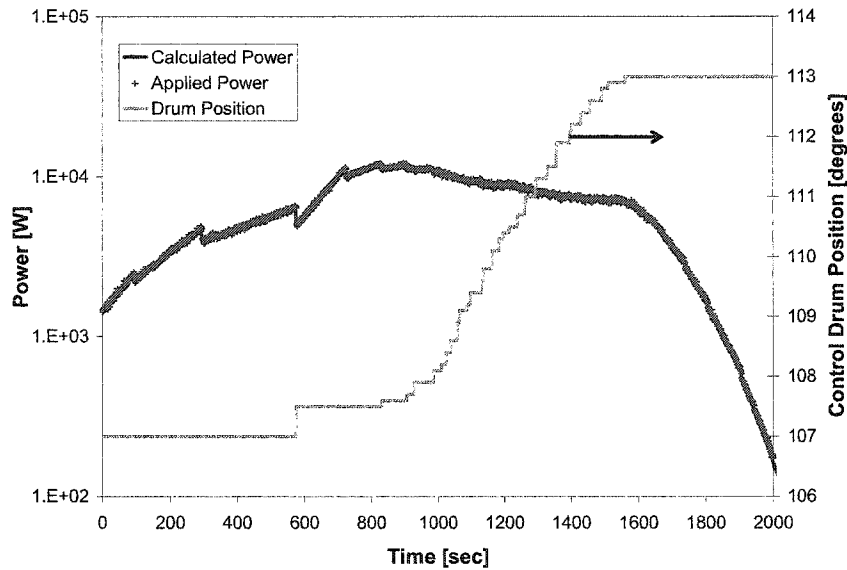


FIGURE 5.39. SAFE-100 Prototype: Full Test to 10 kW_t With Shut Down (Test 3), Thermal Power and Virtual Control Drum Position.

In this final application with argon fill, the core was observed to contract slightly during the initial application of power, but as the average core temperature increased the core deflection returned to zero more rapidly due to the improved core coupling. The deflection measurement at each of the three monitored core profiles returned to zero at an average core temperature of 185° C, at which point the control loop was initiated with a drum perturbation of 0.563° (106.427° to 107°). The power to the prototype was initialized at 1.0 kW_t, but due to operator error, the core power increased slightly to 1.4 kW_t prior to implementing the control loop. This increased core power was only applied for a few seconds before implementing the control loop and did not have an effect on the average core temperature. However, the initial condition $P_o = 1.4 \text{ kW}_t$ and the corresponding precursor concentration were used as the initial conditions to solve the PKE for the first control step. After each measured deflection of 0.02 mm ($\Delta_{\text{set}} =$

0.02 mm), the solutions to the PKE were recalculated and the core power, $P(t)$, was adjusted accordingly. As discussed in the previous test, early in the operation, each recalculation of $P(t)$ and $C(t)$ (each control loop iteration) resulted in a slight bump in the calculated and applied power to readjust the simulated reactor to conditions that would be experienced in an operating nuclear system. A smaller value of δ_{set} may be used to minimize the affect of readjusting the PKE, but limitations in the measurement resolution of the current CCD imaging system prevent reduction of δ_{set} much below 0.01 – 0.02 mm.

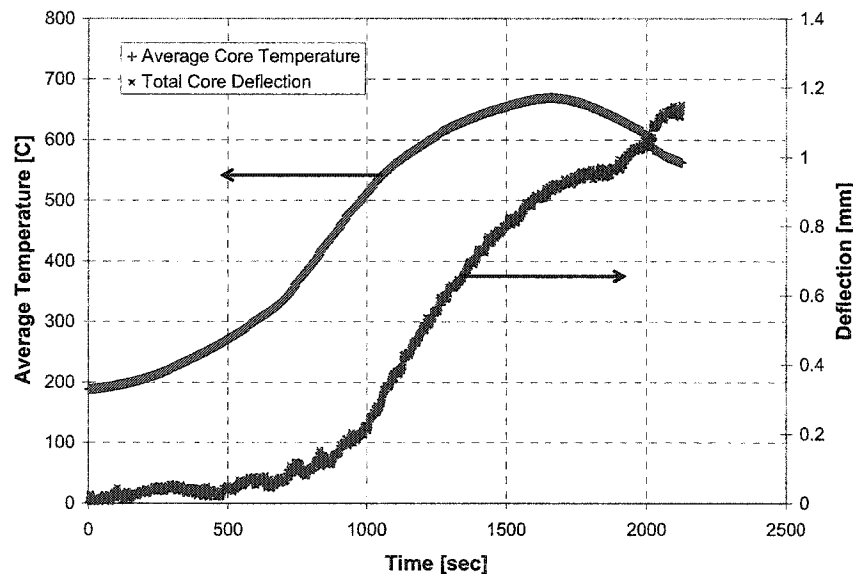


FIGURE 5.40. SAFE-100 Prototype: Full Test to 10 kW_t With Shut Down (Test 3), Average Core Temperature and Radial Deflection.

As seen in Figure 5.40, the measured core deflection lagged the average core temperature. This delayed response in the deflection resulted from the radial temperature profile. The increase in the average temperature was primarily driven by the temperature near the center of the core. Although coupling was improved in this test by heat conduction through the argon gas in the chamber, the peripheral temperatures were still

significantly lower than the central core temperature, resulting in a delay in the measured core deflection with respect to the average temperature.

Due to the lack of heat extraction from the core and the rapid rate of core growth at elevated temperature, it was difficult to maintain steady state operation at the desired 10 kW_t using manual iteration of the control loop. Virtual control drum positions were manually adjusted at each control loop iteration in attempt to maintain the desired power level, but the rapid increase in the core temperature ultimately required that power be allowed to decrease to avoid damage to the core due to elevated temperatures. Fully automated iteration of the control loop, with automatic adjustments to the virtual control drum positions to reach and maintain the desired power level, will alleviate some of the difficulties experienced in the tests performed to date. To decrease core power, the theoretical control drum position was held constant at its final value of 113°. Without further positive reactivity insertion by virtual control drum motion, the simulated negative reactivity introduced by core deflection provided a natural mechanism that shut down the power in the reactor core prototype. Without heat removal, the core temperature continued to increase during the power-down phase, resulting in further core deformation that increased the rate at which core power decreased to zero.

5.3 Discussion & Conclusions

The virtual reactivity feedback test program demonstrated the ability to couple non-nuclear testing of an electrically heated reactor system with nuclear simulations to more accurately simulate the behavior of an operating nuclear reactor. The validation test series demonstrated that the deflection of a heat pipe reactor core could be reliably measured using high resolution photography. Application of this technique to provide real system feedback to assess the virtual neutronic response was demonstrated by a virtual reactivity feedback loop. Although applied with manual iteration of the control

loop, the technique was successfully demonstrated. A fully automated version of the LabView control program was completed, but failure to properly insulate the copper wires for the power feedthrough resulted in an unexpected electrical short and damage to two power supplies. The experimental program was temporarily halted to install Nextel sleeving over all power input wires to prevent future short circuits, to repair the damaged power supplies, and to optimize parameters in the power controllers to prevent erroneous signals from being transmitted to the power supplies in the future.

5.4 Future Enhancements to Virtual Reactivity Feedback Control

While the test program was temporarily halted, the SAFE-100 reactor core prototype was removed from the vacuum chamber and replaced by the SAFE-100a test article. The SAFE-100a includes only the central 19 modules from the SAFE-100 core geometry, but each module incorporates a fully operational sodium filled heat pipe and the core module will be coupled to a prototypic heat exchanger. The SAFE-100a core, heat exchanger, and test stand are shown in Figure 5.41. This new core configuration will allow testing of the core and heat exchanger design, providing a means to simulate reactor start-up, heat pipe thaw and heat exchanger operation. The heat exchanger design will be discussed in more detail in Chapter 6, which will present experimental results for a fully-instrumented channel designed to mimic a single flow channel of the heat exchanger. The SAFE-100a test article will provide conditions that better replicate an operating nuclear reactor and energy conversion system, allowing heat extraction and operation at steady state power and at isothermal conditions. The test article is currently being configured for check-out testing, and the virtual reactivity feedback control loop will be implemented in mid to late FY 2004.

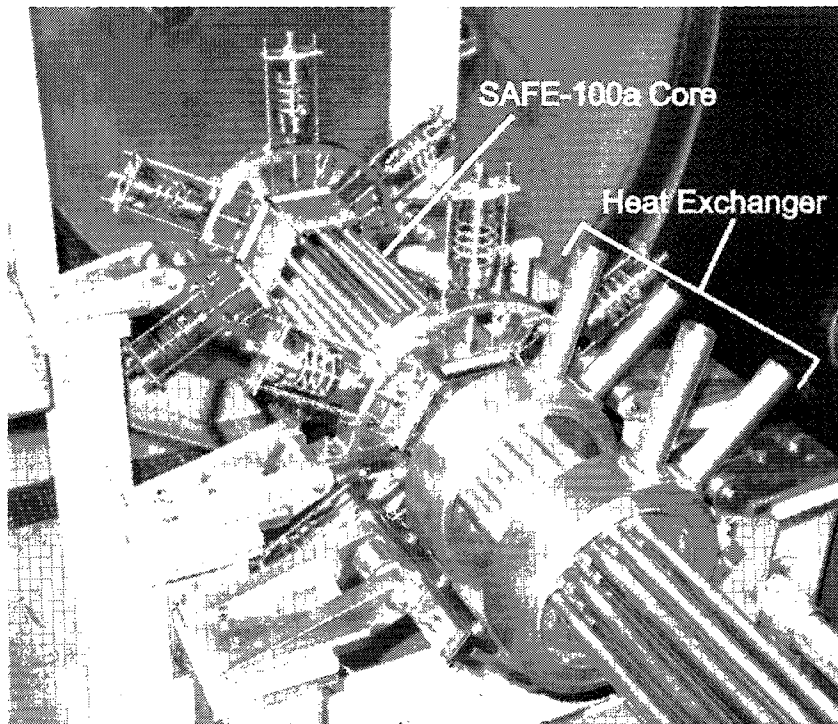


FIGURE 5.41. SAFE-100a Core and Heat Exchanger Assembly.

The virtual reactivity feedback control that was demonstrated on the SAFE-100 core will be applied to the SAFE-100a system in its fully automated form. Upon demonstrating successful system operation as a function of the virtual control drum position, $\theta(t)$, the control program will be modified to control reactor start-up and power transients based on the angular velocity of the control drums, $\omega(t)$. As discussed in Chapter 4, control based on the control drum angular velocity better approximates the control that would be applied to an operating nuclear reactor. Finally, the model based predictive control program that was presented in Chapter 4 for optimized reactor start-up from zero power will be modified to include reactivity feedback effects, such that it can be implemented with the experimental SAFE-100a hardware.

Future enhancements to the virtual reactivity feedback loop will also include consideration of alternate measurement techniques to better characterize the core deformation. One proposed technique would apply three CCD imaging systems, such

that the core could be viewed from three perspectives, providing the data necessary to construct a three dimensional model of the core deflection. Additional measurement techniques, such as an x-ray system, may also be investigated.

CHAPTER 6

SINGLE CHANNEL TESTING FOR ANNULAR FLOW CHARACTERIZATION

Experiments have been designed to characterize the coolant gas flow in two reactor concepts that are currently being investigated for application to nuclear electric propulsion vehicles: the direct-drive gas-cooled reactor (DDG) and the heat pipe cooled reactor (HPR). Both concepts were briefly introduced in Chapter 2, and applicable details will be highlighted in this section. While both concepts are fast spectrum reactors that utilize highly enriched UN fuel, they differ in the mechanism used to remove heat from the reactor core. In the DDG design, the coolant passes through annular channels in the core along the outside of the fuel tubes. Because the coolant gas can directly drive the Brayton turbo machinery, no intermediate heat exchanger is required (Godfroy et al. 2003). In the HPR design, the heat pipes are approximately twice the length of the fueled core. Heat exchangers (HX) fit over the condenser section of the heat pipes that extend from the core, and coolant gas flows through annular flow paths along the outside of the heat pipes to accomplish the necessary heat extraction. Both concepts utilize a closed Brayton cycle for the power conversion system, whose operating efficiency is sensitive to the system pressure drop. The similarity of the annular flow paths in the DDG core and in the HPR-HX allow a similar testing methodology to be applied for both concepts (Bragg-Sitton et al. 2004).

A two-step experimental approach has been adopted: (1) Testing of a highly-instrumented single channel; (2) Testing of a prototypic core (DDG) and core plus heat-exchanger assembly (HPR-HX). The single channel experiments were designed to

accomplish simple, low cost testing of the flow performance. The simple configuration allowed increased access for instrumentation of the flow annulus relative to what is possible in the multi-channel DDG core or SAFE 100a heat exchanger. Tests of a highly-instrumented single channel allow full characterization of the flow dynamics in a nominal flow channel (e.g, axial pressure drop and film temperature rise) prior to construction of the full prototypic system. The single channel test development and implementation is discussed in this dissertation. The follow-on partial-array tests will serve to provide pressure drop and flow distribution data for the full heat exchanger, will provide operational data (e.g. startup and shutdown characteristics), and will demonstrate the robustness of the system when operated at prototypic conditions (Kapernick and Guffee 2003). Tests on the prototypic 37 channel DDG core are currently being conducted, and partial-array tests on the 19 module SAFE-100a heat exchanger will be implemented in mid to late FY 2004.

6.1 Direct-Drive Gas Cooled Reactor

The DDG reactor was designed to operate at high pressure (~1.38 MPa (200 psi)) and to provide core cooling by flowing a 70/30 (atom percent) mixture of helium-xenon (He/Xe) gas. For the initial single-channel tests, nitrogen gas was used as the working fluid for simplicity of operation, availability, and reduced cost. For the non-nuclear multi-channel tests, a 20/80 helium-argon (He/Ar) gas mixture is proposed. This mixture provides reasonable similarity to the He/Xe gas mixture for Reynolds numbers, dynamic heads and Mach numbers. To simulate the heat that would be produced by nuclear fission in the DDG reactor core, axially shaped graphite heaters (shown previously in Fig. 5.4) were used to approximate the power profile that would be present in an operating nuclear reactor.

6.1.1 Test Article Description

The full test stand for the single channel DDG flow test is shown in Figure 6.1. Nitrogen enters the test article at the lower end of the system, flows through a turbine flow meter, and into the test article. Two K-type thermocouples are located at both the entrance and the exit of the flow channel to assure that accurate temperature changes are measured. Although the thermocouples were well secured at the inlet and exit of the flow channel, duplication of the temperature measurement allowed assessment of the accuracy of each reading (e.g. it was readily evident if a particular thermocouple was outside the main flow stream or was in contact with the metal housing). Using the inlet and outlet thermocouples, the axial temperature rise along the module could be determined. This value was held approximately constant for the full test series. Similarly, atmospheric pressure and channel inlet pressure were measured using Type 872 Flow-Through Micro-Baratron pressure transducers with a range of 0 – 100 psi (measurement accuracy of $\pm 1\%$ of reading). Flow measurements were acquired using a turbine flow meter (Flow Technology model FT-12AENA-GEAS2) coupled with a frequency-to-DC converter (Sponsler Co., Inc. model SP741-V10-120-WM) having measurement uncertainty of $\pm 0.11\%$. Although not shown in Fig. 6.1, the flow channel was insulated with a 2" thick layer of Insulfrax insulation to prevent heat loss to the environment during test.

For the DDG system, the gas flow annulus was formed by the central fuel tube, which houses the resistance heater, and an outer stainless steel tube, cut to the same inner diameter as the holes drilled through the monolithic core block. While the heater simulates the nuclear fuel, the "fuel tube" represents the clad that would surround the fuel. Two flow channel designs were tested to determine the effect of changes in the flow annulus geometry on the axial pressure drop: 1) "Spiral Wrap": The fuel tube was wrapped with a 0.0625" (1.6 mm) diameter wire, spiraled with a three inch pitch along the full length of the tube and welded at either end, as shown in Figure 6.2. The wire

wrap maintained the concentric location of the fuel tube inside the outer tube and provided a spiral flow path that increased the gas residence time in the annulus. 2) “Finned Tube”: Three 0.0625” (1.6 mm) diameter, 1.75” (44.5 mm) length wires were welded to the outer surface of the fuel tube, as shown in Figure 6.3. Three sets of wires were located along the length of the tube, with the wires in each set placed at 120 degree intervals around the circumference. The wire “fins” maintained the concentric positioning of the fuel tube without introducing the additional pressure drop predicted for the spiral wrapped design.

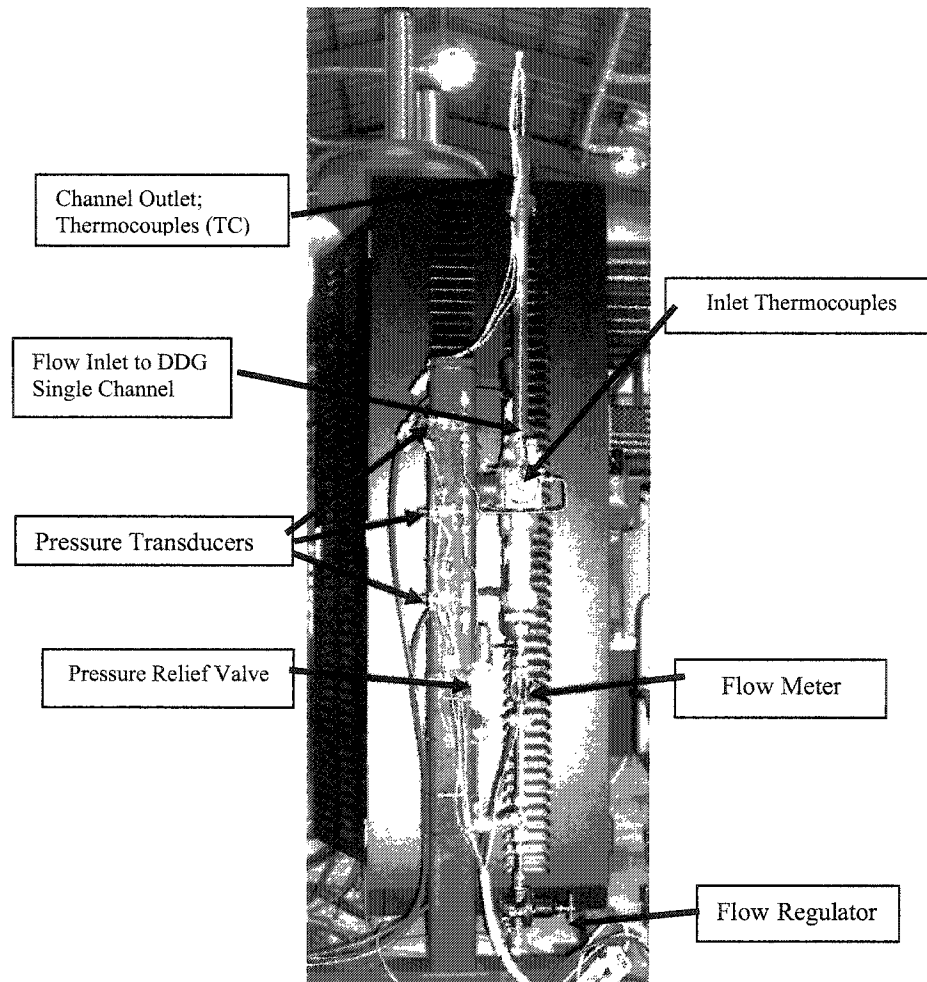


FIGURE 6.1. DDG Single Channel Test Apparatus.

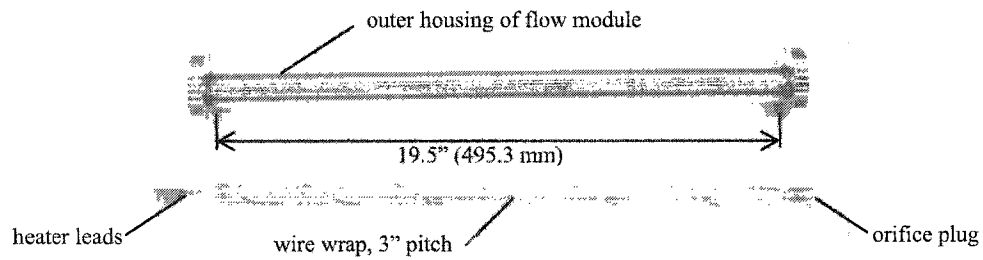


FIGURE 6.2. Breakout of Flow Path; Lower Configuration Fits Snugly Inside the Upper Tube Housing.

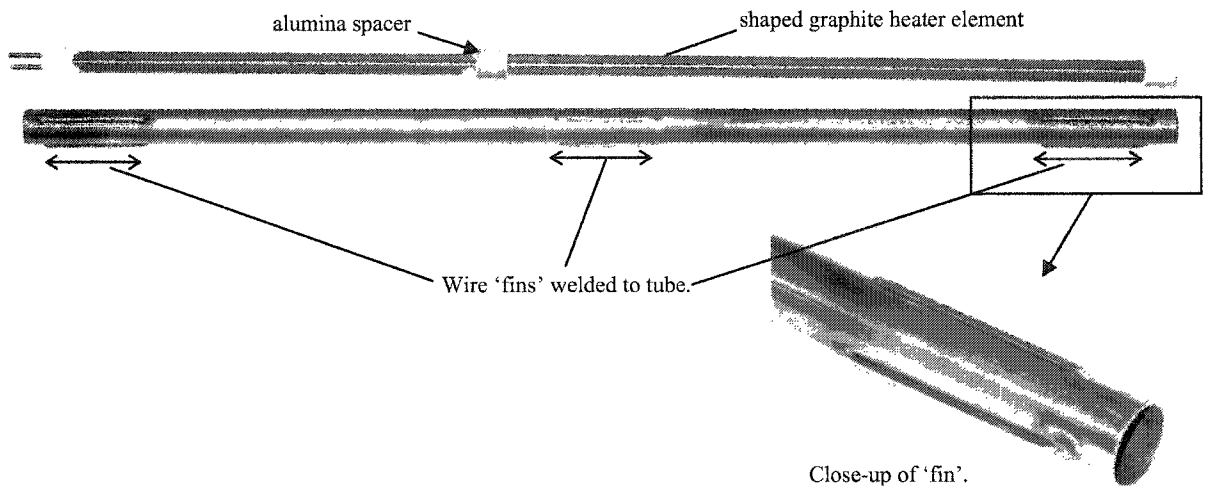


FIGURE 6.3. "Finned" Design for Maintaining Concentric Placement of the Heater Housing.

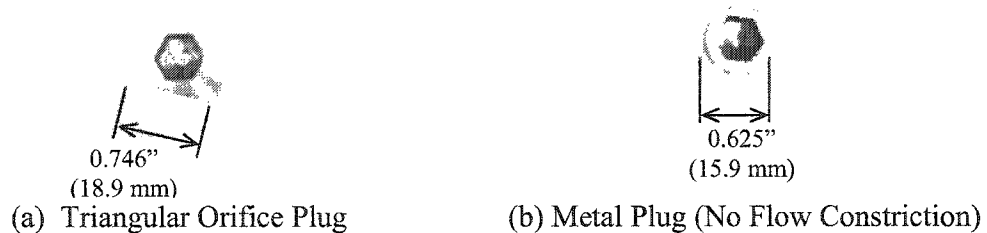


FIGURE 6.4. Orifice and Plug Designed to Hold Heater in Place at the Downstream End of the Flow Annulus.

The test series considered two types of plugs at the downstream end of the fuel tube. The heater was held in place inside the fuel tube by use of either by a triangular orifice or a metal plug, shown in Figure 6.4. The orifice and plug also provided a gas seal to prevent unwanted gas flow through the inside of the fuel tube along the heater surface. Further, the triangular orifice (Fig 6.4(a)) provided flow resistance in the channel. The proposed DDG core design intends to use different orifice designs to vary the annulus flow radially across the core in attempt to provide uniform coolant temperature rise for all coolant channels. For both channel designs, tests were conducted with the metal plug as well as the flow orifice in order to better quantify the annulus flow performance exclusive of the orifice.

6.1.2 Experiment Design & Test Matrix Development

To simplify and expedite the single channel experiments, initial testing was not conducted at the actual temperature or pressure that will be seen in the full core demonstrator. Instead, nitrogen gas entered the channel at approximately room temperature and exited the channel at ambient pressure. Power was applied to the resistance heater located inside the fuel tube, with a goal of matching the expected 180° C axial temperature rise expected for the reactor core. The prototypic outlet temperature for the 37-pin DDG is approximately 850 K (577° C).

The test matrix was selected to span a range of Reynolds numbers from laminar ($Re \sim 1500$) to turbulent ($Re \sim 15000$) flow. The Reynolds number (Re) can be calculated from Eq. (6.1) using the hydraulic diameter of the flow channel (D_h), the radially averaged flow velocity (u_{avg}) and the kinematic viscosity(ν):

$$Re = \frac{u_{avg} D_h}{\nu} . \quad (6.1)$$

The theoretical pressure drop, ΔP_s , was calculated using a standard expression, neglecting buoyancy (Kays and London 1964):

$$\Delta P_s = \frac{\dot{m}^2}{2g_c \rho_{in} A^2} \left((K_c + 1 - \sigma^2) + 2\left(\frac{\rho_{in}}{\rho_{out}} - 1\right) + \frac{4fL}{D_h} \frac{2\rho_{in}}{\rho_{in} + \rho_{out}} + (K_e - 1 + \sigma^2) \right), \quad (6.2)$$

where:

- A = flow area (m²),
- f = friction factor,
- g_c = proportionality factor in Newton's 2nd Law,
- K_c = entrance loss coefficient,
- K_e = exit loss coefficient,
- L = length of flow channel (m),
- $\dot{m}$ = mass flow rate (g/s),
- ρ = density (g/cm³),
- σ = ratio of free-flow to frontal area (A_c/A_f) of one side of heat exchanger.

The DDG flow channel was approximated as a smooth tube. For turbulent flow, the smooth tube correlation was applied to estimate the friction factor (Kays and London 1964) in the pressure drop calculation:

$$f = 0.079 \text{Re}^{-0.25}. \quad (6.3)$$

The expressions in Eq. (6.1-6.3) were applied to compute a test matrix for the DDG single channel tests over the desired range of Reynolds numbers.

A sample test matrix, corresponding to the spiral wrap annulus design with a triangular orifice, is provided in Table 6.1. Note that the ΔT shown in the table reflects the average radial temperature difference between the coolant and the wall temperature on the inner edge of the flow channel. The DDG flow channel was not instrumented to measure the wall and flow temperatures along the channel length, such that verification of the predicted ΔT_{radial} was not made. The axial coolant temperature rise was held approximately constant throughout the test while the heater power and coolant flow rate were varied to achieve the desired Reynolds number for each test case. Inlet temperature was assumed constant at room temperature, 295 K (22 °C), requiring an outlet temperature of approximately 475 K (202 °C) to achieve the desired temperature rise.

Case	Reynolds Number	Power (kW)	Mass Flow Rate (g/s)	ΔP_{axial} (kPa)	ΔT_{radial} (K)	Mach No. Exit	Mach No. Orifice
1	1500	0.147	0.788	1.8	117.5	0.038	0.072
2	2000	0.196	1.05	3.0	152.6	0.050	0.097
3	2500	0.246	1.31	4.4	150.0	0.063	0.121
4	3000	0.295	1.58	6.2	140.2	0.074	0.145
5	3500	0.344	1.84	8.2	136.1	0.086	0.169
6	4000	0.393	2.10	10.4	134.0	0.097	0.193
7	4500	0.442	2.36	12.8	133.2	0.108	0.217
8	5000	0.491	2.63	15.4	132.7	0.118	0.242
9	6000	0.589	3.15	21.0	133.0	0.138	0.290
10	7000	0.688	3.68	27.1	135.2	0.155	0.338
11	8000	0.786	4.20	33.7	138.8	0.171	0.386
12	9000	0.884	4.73	40.6	142.1	0.186	0.435
13	10000	0.982	5.25	47.7	145.2	0.200	0.483
14	11000	1.081	5.78	55.0	148.0	0.212	0.531
15	12000	1.179	6.30	62.5	150.6	0.223	0.580
16	13000	1.277	6.83	70.1	153.0	0.233	0.628
17	14000	1.375	7.35	77.9	155.3	0.242	0.676
18	15000	1.473	7.88	85.7	157.4	0.251	0.725

TABLE 6.1. Sample Test Matrix for Single Channel DDG Testing.

Initial calculations indicated that the triangular orifice was a significant contributor to the pressure drop in the channel due to the additional flow restriction at the channel exit. The predicted pressure drop in Figure 6.5 does not include the effect of a specific annulus design (e.g. no wire wrap on the fuel tube). Due to the orifice effect, tests conducted with the metal plug at the downstream end of the fuel tube provide a more accurate estimate of the pressure drop along the annulus itself, e.g. for the spiral wrap or finned tube design. Results for all four potential configurations are provided.

6.1.3 Results & Discussion

Initial testing indicated that prior to data acquisition, the resistance heater used in the test configuration had to be “burned-in” to eliminate binding materials that were used in heater fabrication. During this time, higher temperatures and slightly lower pressure drops were observed than during subsequent heater operation. Heater operation at 1 kW for approximately one hour proved to be sufficient. Each data acquisition was for a

minimum of two minutes, with data taken every ten seconds; hence, multiple data points were taken at each flow condition.

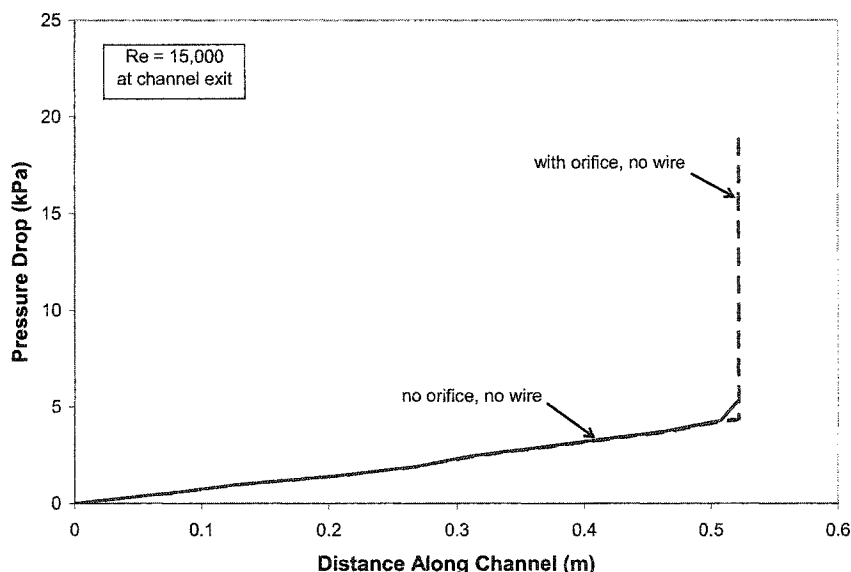


FIGURE 6.5. Effect of Orifice on Channel Pressure Drop.

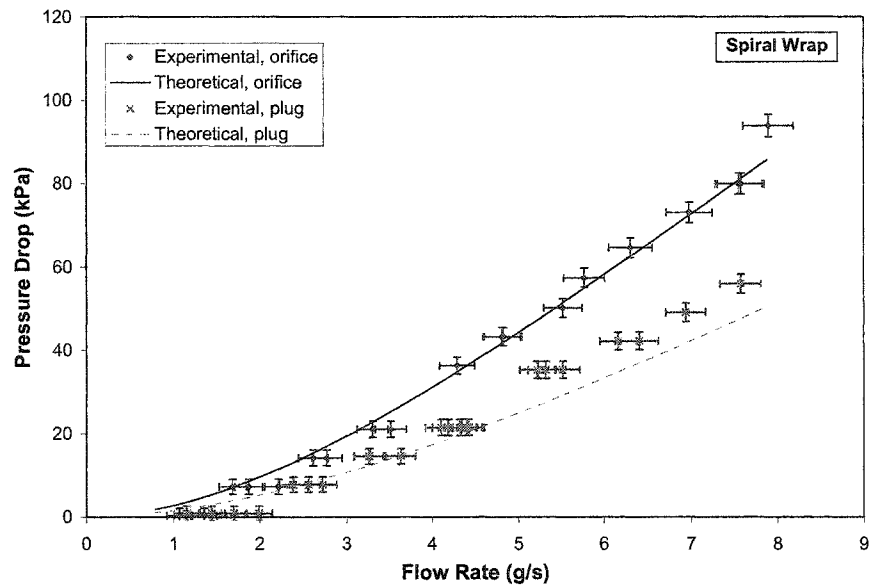
In each test, the goal was to maintain a constant outlet temperature of 202° C assuming that the inlet temperature would remain constant throughout the test. Because it was difficult to maintain this precise temperature difference, the temperature rise along the flow channel varied slightly through the course of each test. In addition, it was noted that during the course of testing the inlet nitrogen temperature dropped from approximately 22° C to a low of approximately 16° C at the end of testing. The nitrogen gas is supplied from a ground line, such that the gas entering the system during the initial part of testing was in the ground lines themselves rather than directly from a large storage tank. At later times, after the nitrogen in the lines was depleted, the nitrogen entering the test article was from the main storage tank. Due to expansion of the gas as it moves from the main tank to the transit lines, the gas temperature decreases. Future tests should attempt to maintain constant temperature rise along the flow channel rather than focusing

on a specific outlet temperature. In each test, the heater was set to the power indicated by theoretical calculations for the specific configuration, and the nitrogen flow rate was then selected to achieve the desired outlet temperature. Test data were acquired from low power, low flow conditions to high power, high flow conditions.

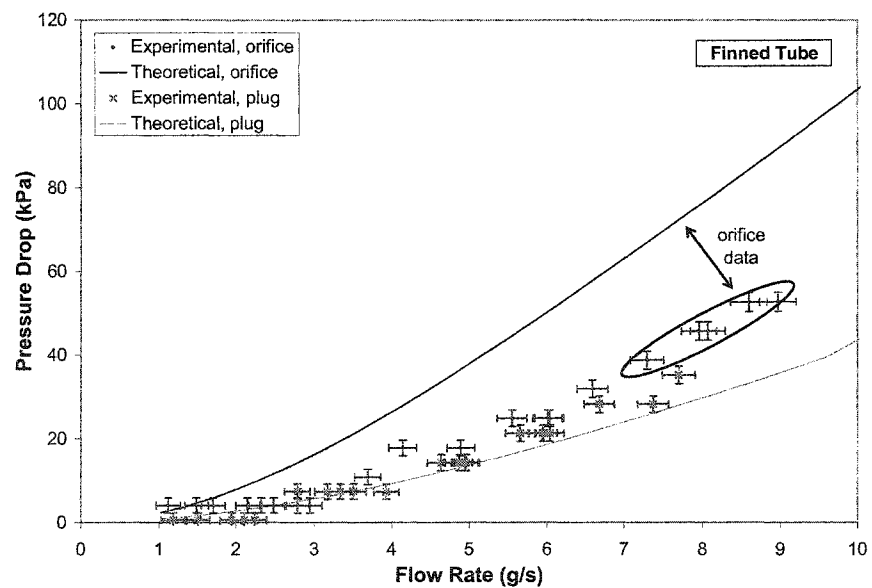
Figure 6.6 shows the measured and calculated pressure drops as a function of nitrogen flow rate. The error bars on the pressure measurement are based on measurement accuracies of 1% of the instrument reading. The error in the flow rate was a compilation of the 0.11% uncertainty in the frequency to DC converter and the error in the pressure measurement that was used to convert the measured actual cubic feet per minute (ACFM) to mass flow rate. One may note that the pressure drop appears to be constant for several different flow rates in each test. This phenomenon occurred in regions where the slope of the theoretical pressure drop curve was small (e.g. where small increases in the flow rate do not correspond to a large change in the pressure drop). The LabView data acquisition software applied a default setting in which the measurements were updated only when the measured value varied by more than 1%. This specification prevented the pressure data from being jumpy during data acquisition, but resulted in identical pressure drops being recorded for more than one power/flow rate combination. Future tests will eliminate this parameter from the data acquisition program so that small changes in the pressure drop will be measurable.

In addition, accuracy was limited by the available hardware at the time the tests were conducted. As discussed in the experimental design, the differential pressure data were acquired using two separate measurements of the absolute pressure, reporting the pressure drop as the difference in these two readings. Measuring a pressure drop on the order of a few psi on a 0-100 psi gauge with an accuracy of 1% of the reading is inadequate. More accurate measurements of the pressure drop are possible using a differential pressure gauge referenced to the pressure at the inlet of the flow channel.

This hardware was on order when the DDG tests were conducted and was not implemented in this test series.



(a) Spiral Wrapped Flow Annulus.



(b) "Finned" Flow Annulus.

FIGURE 6.6. Comparison of the Measured and Calculated Pressure Drop for the DDG Flow Annulus.

Generally good agreement was observed between the measured and calculated pressure drop with the exception of the data acquired for the finned tube fitted with the triangular orifice. As expected, the majority of the pressure drop observed in the DDG single channel tests was attributed to the orifice. The poor agreement for the finned tube data with orifice was attributed to poor positioning of the test module. Upon disassembly after test completion, it was noted that the orifice sat slightly above the flow outlet rather than snugly in the annulus. As a consequence, the orifice plug did not provide the predicted flow resistance. Therefore, the flow characteristics would be expected to more closely resemble those in the non-orificed test, as observed in Fig. 5.45(b). Better care will be taken in subsequent tests to assure proper positioning of the central tube and orifice to maintain the integrity of all experimental results.

General observations during testing indicated a more stable flow for the spiral wrapped flow annulus. During testing of the finned design, some chugging of the flow was audible. It is likely that the source of this chugging was the lack of insufficient back pressure in the annulus that resulted from testing at atmospheric pressure. However, the pressure drop in the spiral channel was significantly higher than for the finned annulus design, such that the finned design was more desirable for the full core configuration. This annulus design was adopted for the 37-channel DDG core.

6.1.4 Future Enhancements

Future testing should consider higher pressure tests of the flow channel. Testing at 150-200 psi (1.0 – 1.4 MPa) will better reflect the actual conditions in the full 37 pin design and will eliminate compressible flow effects that may be present in atmospheric pressure testing. At high pressure, the calculated pressure drops along the annulus are significantly smaller than for atmospheric outlet pressure. To accurately determine the pressure drop, a differential pressure gauge should be installed, which will allow for

rapid, multiple differential pressure measurements along the channel length and will greatly improve the measurement accuracy.

When testing of the DDG concept was initiated, three reactor concepts were being considered for an NEP vehicle intended for a deep space mission: direct-drive gas cooled, heat pipe cooled, and liquid metal cooled reactor cores. However, due to a shift in the focus of the NASA requirements, the gas cooled concept fell out of favor and testing of the DDG concept was temporarily halted. As a result, higher pressure tests and fully instrumented tests of the DDG single channel were not conducted. Instead, the lessons learned in the DDG test series were applied to single channel testing of the SAFE-100a heat exchanger design.

6.2 SAFE-100a Heat Exchanger

The single channel experiment for the HPR-HX was designed to characterize the coolant gas flow in a nominal flow channel of the heat exchanger that will be coupled to the SAFE-100a heat pipe-cooled reactor (HPR). Due to the similarity in the configuration of the annular coolant flow channels in the HPR-HX and the annular flow channels in the DDG, it was possible to test a nominal heat exchanger flow channel with only minor modifications to the test stand. In the HPR-HX, the size of the coolant annulus also varies with radial position across the core to maintain adequate cooling radially across the bank of heat pipes emerging from the reactor core. For testing purposes, only the central flow channel was constructed.

The test article for the HPR-HX, called the SAFE-100a, contains the center 19 modules of the full 61 module SAFE-100 design, thus creating a “partial-array”. The SAFE-100a is fitted with a single 19 channel heat exchanger (Kapernick and Guffee 2003). The SAFE-100a core, heat exchanger, and test stand were previously shown in

Figure 5.40. Figure 6.7 shows the partially constructed core and heat exchanger assembly to demonstrate how the heat pipe modules fit into the heat exchanger.

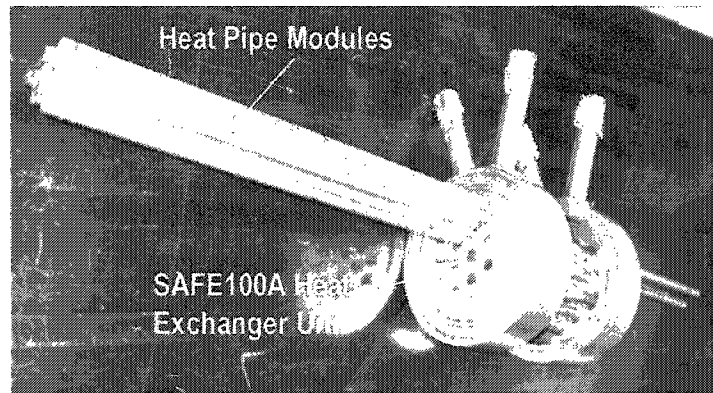


FIGURE 6.7. SAFE-100a Heat Exchanger with Four Heat Pipe Modules.

The dimensions of the inlet and exit flow geometry for the HX flow channels were designed to provide similar flow characteristics in the 19 module test concept and the full 61 module SAFE-100 concept. Specifically, the flow areas in the inlet/exit pipes, flow distribution rings, and plenums were reduced in the 19 module design to maintain the same ratio of flow areas in these passages to that in the flow annuli of the 61 module design. The modifications were made to keep the distribution of Reynolds numbers, pressure drops, and heat transfer coefficients approximately the same as in the full 61 module design. Additionally, this design better simulates the effect of the inlet and exit plenums in producing flow distributions among the annular flow passages in the heat exchanger. A cross section of the (a) full 61 channel heat exchanger and (b) the 19 channel heat exchanger are shown in Figure 6.8.

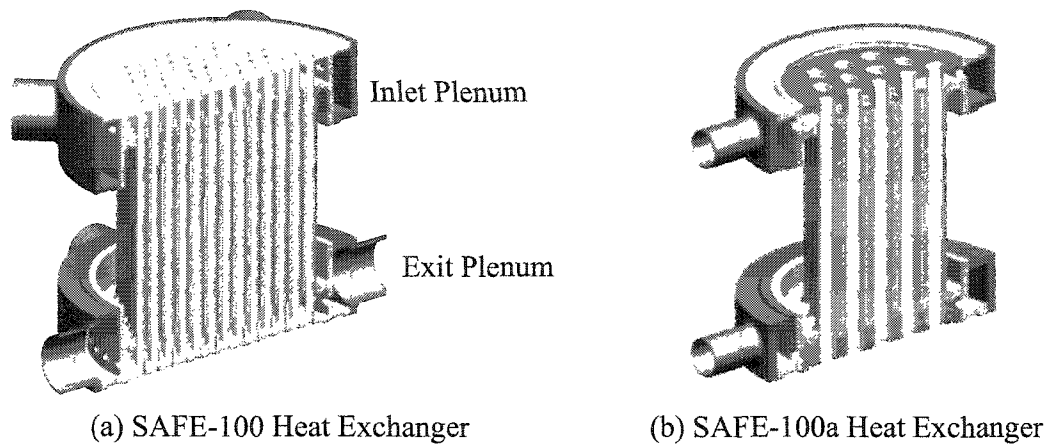


FIGURE 6.8. Cross Section of the Proposed HPR-HX Assemblies.

6.2.1 Test Article Description

The full test stand for the HPR-HX single channel tests is shown in Figure 6.9. DDG single channel testing provided some background experience for the HPR-HX tests, and many of the “lessons learned” in the DDG tests were applied. To take advantage of existing hardware, some modifications had to be made to the HX single channel design. In the actual HX, the length of the flow channel is only 8” (20.3 cm). To take advantage of existing resistance heater hardware, the designed test article was approximately 18.5” (47.0 cm), the same length as the DDG flow channel. Although this was considerably longer than the nominal HX flow path, the channel was fully instrumented along its length, such that detailed information about the pressure drop and film temperature rise could be obtained for any length section of the test article.

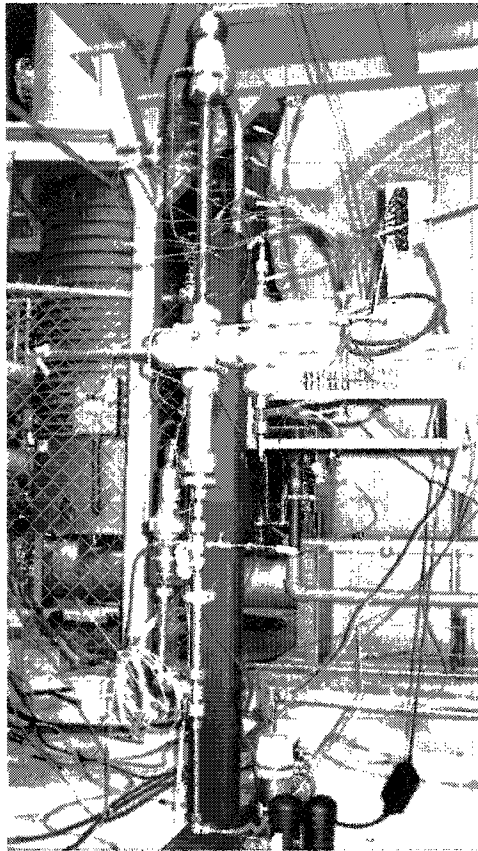


FIGURE 6.9. Test Stand for the Single Channel Heat Exchanger Test Article.

The similarity in the DDG and HX test articles (e.g. length and diameter) allowed use of much of the same hardware in the test stand, but major modifications were required to perform testing at higher inlet pressure. Because compressible flow effects were present when testing was performed at atmospheric pressure, modifications were made to operate the single channel HX tests at up to 1.0 MPa (150 psi) inlet pressure. This pressure maintained the exit Mach number of the gas flow at less than 0.2, well below the limit for compressible flow effects (Fox and McDonald 1992). Necessary modifications included: addition of a high pressure relief valve (set to 165 psi) for safety concerns; a pressure controller operated by proportional-integral-derivative (PID) control to maintain the desired inlet pressure; a variable position valve (VPV) to provide computer controlled mass flow rate; and piping to direct nitrogen outside the building (at

atmospheric pressure, the nitrogen was outlet inside the building). The VPV was adjusted to attain the desired Reynolds number for each case at a constant inlet pressure. Small adjustments to the valve position were required to avoid overshoot of the desired pressure by the PID controller. As in the single channel DDG tests, nitrogen was selected as the working fluid for purposes of cost, ease of use, and availability.

The HX flow channel was characterized by an inner wall roughened by square ribs having a rib pitch-to-height ratio of ten. This square-ribbed design prevented a boundary layer from developing and enhanced the turbulent heat transfer in the flow annulus relative to a smooth channel design. Figure 6.10 shows a close-up view of one of the fabricated parts for the annulus. The ribbed section of the module is 17" (43.2 cm) in length, with 0.725" (1.84 cm) unribbed on either end. The cartoon of the flow annulus provided in Figure 6.11 provides the details of the flow path.

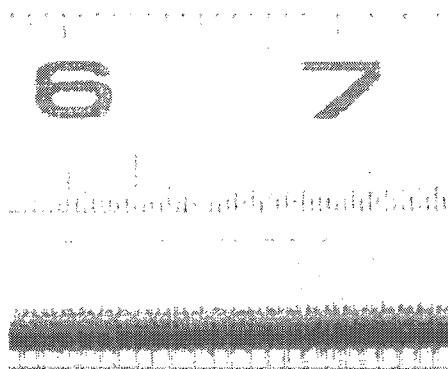


FIGURE 6.10. Close Up Detail of Fabricated Part for HX Inner Flow Path.

A full break-out of the test article construction is provided in the series of photos in Figure 6.12. Although not shown, the graphite heater, representing the nuclear fuel pin, fit into the component labeled “HX Module,” which then fit inside the “Outer Pressure Vessel” component, cut to the dimensions of the center channel of the monolithic HPR-HX. The outer diameter of the HX module was 0.715” (without ribs) and the inner diameter of the outer pressure vessel was 0.805”, creating a maximum flow

channel width of 90 mils (minimum of 83 mils between top of rib and channel wall). At the very center of the HX module, a slight discoloration marks the spot at which two ribbed modules were welded together. Due to the fine detail of the ribbed surface, limitations of the machine used to cut the ribs prevented the module from being constructed using a single stainless steel tube. The welded section extended approximately 0.25" (0.635 cm) and was ground smooth (no ribbed surface or rough weld material). The center weld was lined up with the centermost diagnostics for measurement of flow temperature and differential pressure. This was done to minimize the number of flow perturbations caused the penetrations necessary for diagnostics and the variation in the surface properties introduced by the center weld.

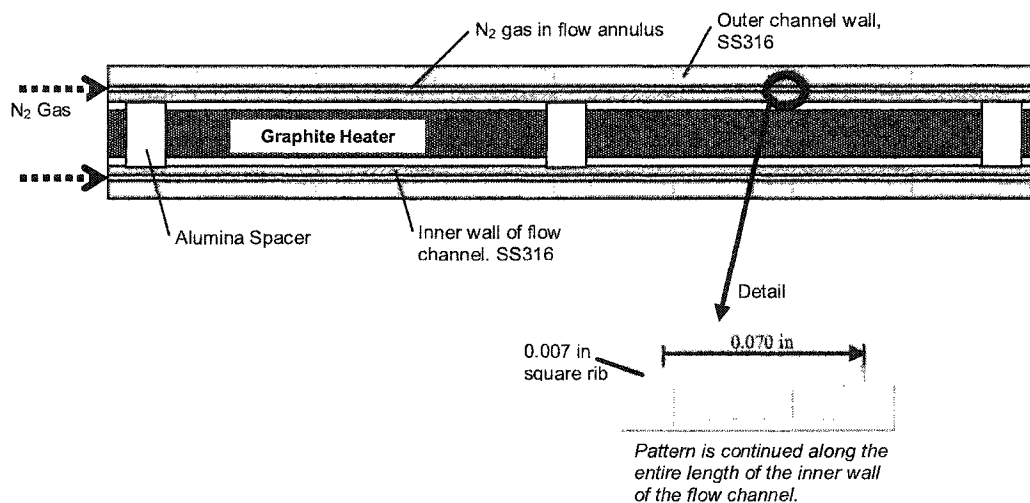


FIGURE 6.11. Illustration of the HPR-HX Flow Channel.

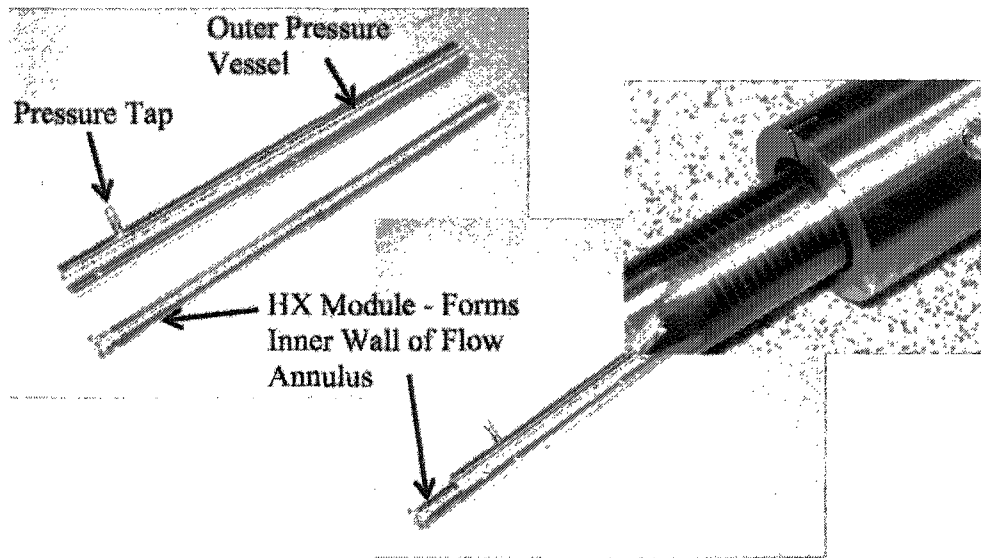
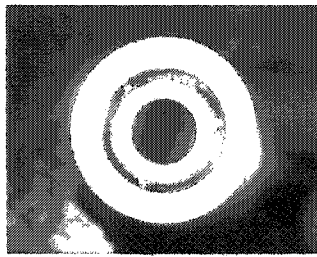
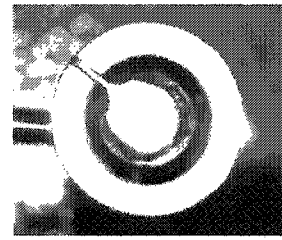


FIGURE 6.12. Break-out of the HPR-HX Flow Channel.

For the proposed DDG flow channel designs, concentric positioning of the fuel tube was provided by either the wire wrap or the wire fins. For the HX design, the square ribs did not extend to the outer wall of the annulus. As a result, a separate mechanism was developed to provide concentric location of the HX module. Figure 6.13(a) shows the inlet to the flow annulus. When the module was machined, three small tabs (~1.5 mm width) were left at 120° intervals. These tabs provided minimal flow resistance while maintaining concentric positioning. At the channel exit, module positioning was provided by the tab shown in Figure 6.13(b). The outlet positioning tab maintained module alignment and clocked the module to a known position. Module clocking was required to line up external diagnostic ports for wall thermocouples with the correct wall locations; thermocouple placement will be discussed in the description of the channel diagnostics.



(a) Flow Channel Entrance.



(b) Positioning Tab at Flow Outlet.

FIGURE 6.13. Flow Channel Entrance/Exit Showing Positioning Tabs.

To simulate the heat that would be transferred from the reactor core to the heat exchanger via liquid metal heat pipes, a 17" long (43.2 cm), constant 0.375" (0.95 cm) diameter graphite heater was inserted into the heat exchanger module. Three 0.422" (1.07 cm) diameter, hollow cylindrical alumina pieces were fit over the graphite heater (at the center and both ends) to electrically isolate the heater from the HX module. To simplify and expedite the single channel experiment, testing was conducted at significantly lower gas temperature than the intended, design temperature for the 19 channel HX demonstrator, where the coolant gas will be preheated to approximately 650 K at the HX inlet. Instead, as in the DDG single channel tests, nitrogen gas entered the flow channel at line temperature, which was slightly below standard room temperature ($\sim 17^{\circ}\text{C}$). As in the DDG test, power was applied to the resistance heater with a goal of matching the axial temperature rise expected for the HX flow channel of 180 K (180°C). The prototypic outlet temperature for the HPR-HX is approximately 850 K (577°C). The temperature rise in the channel was measured by type K thermocouples (TCs). Two TCs were placed before the entrance to the flow channel, just upstream of the inlet, and three were placed at the outlet of the flow channel, just downstream of the annulus. Thermocouples at the channel exit were placed with the assistance of real-time image guidance using a borescope. Final outlet TC positioning is shown in Figure 6.14.

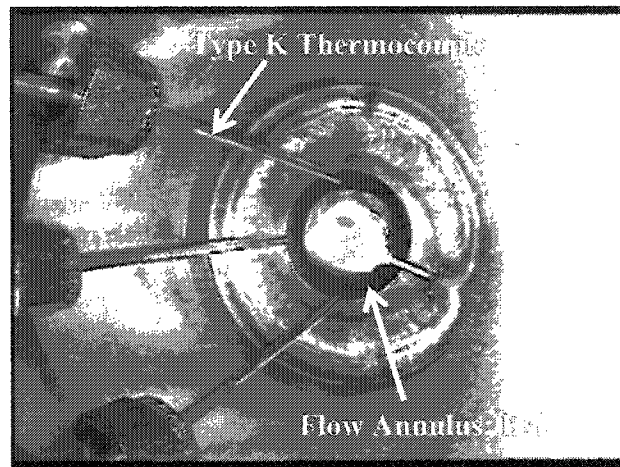


FIGURE 6.14. Thermocouple Placement at Flow Channel Exit.

Additional thermocouples along the length of the flow annulus provided detailed information on the flow temperature and the internal wall temperature as a function of distance along the channel. Six thermocouples were positioned in the flow path, each rotated 120° from the adjacent flow thermocouple. Table 6.2 provides details on the position of all flow and wall thermocouples; Figure 6.15 illustrates the complexity of the diagnostics for the channel. To obtain wall temperature measurements, small divots were made in the internal wall of the flow annulus without fully penetrating the module wall. Thermocouples were inserted through swage fittings in the outer wall of the flow annulus and were seated in the divots cut in the interior wall. No verification of wall or flow thermocouple positioning was possible along the annulus. At the most upstream position, approximately 1" from the channel exit, three wall thermocouples were located at 120° intervals to assess any azimuthal variations in the wall temperature.

As opposed to the simple instrumentation applied in the DDG tests, the HX flow annulus was fully instrumented with differential pressure taps at the positions indicated in Table 6.2. Axial pressure taps along the length of the test article measured the differential pressure at each measurement point, relative to a common reference pressure. Differential pressure measurements were accomplished using a multichannel pressure

scanner. Manufactured by Pressure Systems, the NetScanner Model 9116 consists of 16 silicon piezoresistive pressure sensors and is capable of scanning 16 differential pressures, all referenced to a single pressure measurement with a 0.05% full scale accuracy. The 9116 was instrumented with ± 30 psid (0.2 MPa) pressure transducers, but could be operated at a line pressure up to 600 psi (4 MPa). Seven pressure taps were located along the length of the flow annulus (using 7 of the available 16 channels), with the reference pressure tap located just upstream of the channel inlet.

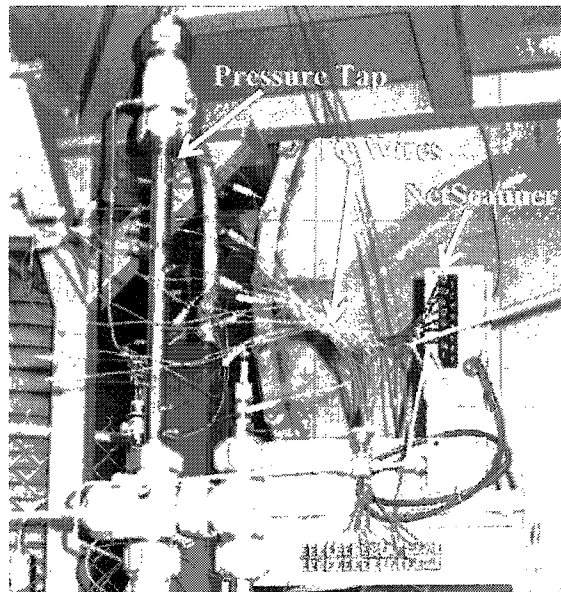


FIGURE 6.15. Detailed Flow Channel Diagnostics.

Flow Temperature	Inner Wall Temperature	Differential Pressure Tap
Inches (cm)	Inches (cm)	Inches (cm)
1.0 (2.5)	2.4 (6.1)	1.0 (2.5)
3.8 (9.7)	5.0 (12.7)	4.0 (10.2)
6.3 (16.0)	7.8 (19.8)	6.6 (16.8)
9.6 (24.4)	10.7 (27.2)	9.6 (24.4)
12.1 (30.7)	13.7 (35.8)	12.1 (30.7)
14.9 (37.8)	16.3 (41.3) – 3 total	14.7 (37.3)
		17.7 (45.0)

TABLE 6.2. Thermocouple Locations Along HX Flow Annulus.

6.2.2 Experiment Design & Test Matrix Development

The objective of the HPR-HX single channel test was to determine the pressure drop along the length of the annulus for laminar to turbulent flow (Reynolds number 1500-15000), maintaining a specified temperature rise along the channel with varying power and flow rate. Flow measurements were again acquired with the turbine flow meter and frequency-to-DC converter that were used in the earlier DDG tests. A test matrix was calculated in the same fashion as for the DDG tests, making the appropriate modifications for the annulus design and the operational pressure.

Although the same pressure drop equation used in the DDG calculations (Eq. 6.2) was applicable, modifications were necessary for the channel friction factor and predicted heat transfer coefficient. For the square-ribbed flow annulus of the HPR-HX, the smooth tube correlation applied for the DDG flow channel was no longer appropriate. Instead, the empirical relationship developed by Takase (2000) for the friction factor in an annular flow channel roughened by square ribs was applied:

$$f = C1 \cdot (\text{Re})^{-C2} \left(\frac{Tk_w}{Tk_b} \right)^{-0.5}, \quad (6.4)$$

where Tk_w and Tk_b are the wall and bulk temperatures, respectively (Takase 2000). Based on the empirical relation developed from experimental measurements by Takase (2000) for a pitch-to-height ratio of ten, the coefficient $C1$ and the exponent $C2$ were taken to be 0.13 and 0.5, respectively, in the calculation of the expected pressure drop. The reported error for the empirical constants was 8%. Under fully developed flow conditions, Takase (2000) also provides a general expression for the relationship between the Nusselt (Nu) number and the Reynolds number for a square-ribbed annulus:

$$Nu = C3 \cdot (\text{Re})^{C4} \text{Pr}^{0.4}, \quad (6.5)$$

where Pr is the Prandtl number. The Prandtl number, defined as the ratio of the kinematic viscosity to the thermal diffusivity ($\text{Pr} = \nu/\alpha$), is a dimensionless parameter

that relates the relative thickness of the hydrodynamic layer and the thermal boundary layer (Holman 1981). The Nusselt number was used to compute the channel heat transfer coefficient (h) using the relation $Nu = h*D_h / k$, where k is the conductivity of the working fluid. Takase (2000) determined that for a pitch-to-height ratio of ten, the appropriate constants $C3$ and $C4$ in Eq. (6.5) were 0.085 and 0.74, respectively. As in the DDG tests, all single channel flow tests were conducted using nitrogen as the working fluid.

Reynolds Number	Power (kW)	Mass Flow (g/s)	T _{in} (K)	T _{out} (K)	ΔP (kPa)	ΔT _{radial} (K)
1500	0.210	1.121	295	475	0.97	48.4
2000	0.280	1.495	295	475	1.86	29.6
2500	0.350	1.869	295	475	2.83	26.0
3000	0.420	2.243	295	475	3.96	27.2
3500	0.490	2.617	295	475	5.27	28.3
4000	0.559	2.991	295	475	6.74	29.3
4500	0.629	3.364	295	475	8.38	30.3
5000	0.699	3.738	295	475	10.18	31.1
6000	0.839	4.486	295	475	14.27	32.6
7000	0.979	5.234	295	475	19.00	33.9
8000	1.119	5.981	295	475	24.35	35.1
9000	1.259	6.729	295	475	30.33	36.2
10000	1.399	7.477	295	475	36.92	37.2
11000	1.538	8.224	295	475	44.14	38.2
12000	1.678	8.972	295	475	51.99	39.0
13000	1.818	9.720	295	475	60.46	39.9
14000	1.958	10.467	295	475	69.57	40.6
15000	2.098	11.215	295	475	79.33	41.4

TABLE 6.3. Single Channel HX Test Matrix at 1.0 MPa Inlet Pressure.

The sample test matrix in Table 6.3 corresponds to an inlet pressure of 1.0 MPa (150 psi) and a constant axial temperature rise of 180° K (180° C). In each implementation of the test matrix, data were acquired from high mass flow rate (high Re) to low mass flow rate (low Re). The VPV was adjusted to obtain the approximate mass flow indicated in the test matrix, and the power to the heater was adjusted to provide the desired temperature rise. The power level indicated in the test matrix was used as an

initial estimate of the required power. Note that the ΔT shown in the table reflects the average *radial* temperature rise across the coolant.

6.2.3 Results & Discussion

The measured and calculated pressure drop along the length of the flow channel is shown in Figure 6.16. Although relatively good agreement was noted for low Reynolds numbers, the discrepancy between the measured and calculated pressure drop increased as the Reynolds number was increased. At $Re = 15000$, the measured pressure drop was only two-thirds the expected pressure drop. This result was observed for tests conducted at an inlet pressure of 100 psi and at 150 psi, as shown in the figure below, and was repeatable for all test implementations.

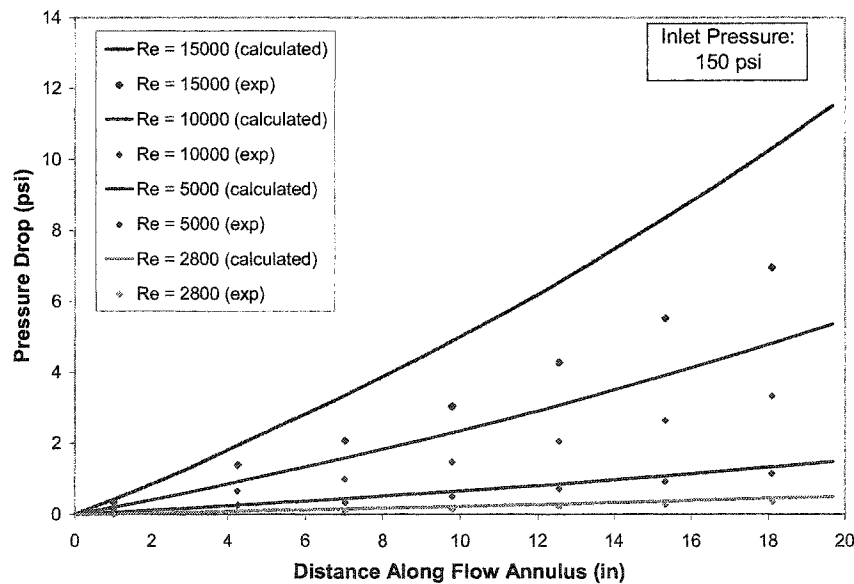


FIGURE 6.16. Experimental and Calculated Pressure Drop for the HX Flow Annulus.

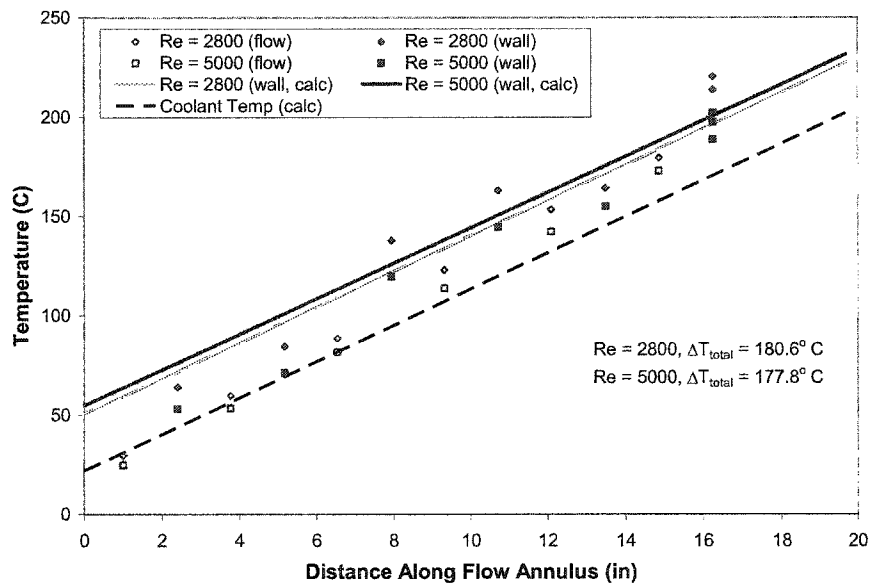
As discussed in the test article description, the HX flow channel was fully instrumented along its length with type K thermocouples placed in the flow and inserted into the inner module wall. Figure 6.17 shows the measured and predicted temperatures

along the flow annulus. The open data points correspond to flow temperatures; the solid data points correspond to wall temperatures. Analysis of the measured wall temperatures indicates that some wall thermocouples were not well placed. Results for these TCs indicate results more closely aligned with the predicted flow temperature at the given axial location than the wall temperature, suggesting that some TCs were not properly seated in the wall. The thermocouples at 7.8", 10.7", and 16.3" were better positioned than the other wall TCs, reporting higher temperatures than that predicted for the coolant flow at the same axial location. Results for these three wall TCs better matched the predicted temperatures at low Re than at high Re, in which all measured values were lower than expected. Future tests of the single channel configuration should determine a more reliable method of positioning wall thermocouples to ensure that the reported values correspond to the wall temperatures rather than the temperature of the nitrogen coolant.

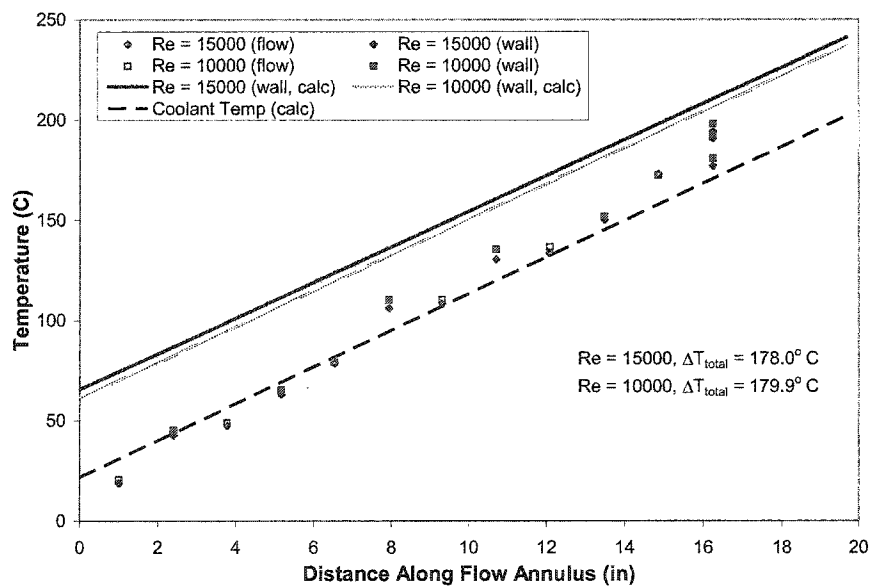
The measured flow temperatures approximately matched the expected coolant temperature at each distance along the flow channel, but the measured temperatures were somewhat high for the low Reynolds number measurements. The measured flow rate at low Re, however, was somewhat suspect. The turbine flowmeter provided jumpy measurements at very low flow rates and considerable patience was required to attain a stable value. Although data were only recorded when the flowmeter reading had stabilized, the level of accuracy at low flow rates was uncertain.

In the test results presented, the flow channel was not insulated, such that a large amount of heat was lost to the environment. A simple energy balance was calculated by comparing the energy transmitted to the heater with the energy transmitted to the flow, where $E_{flow} = \dot{m} c_p (T_{out} - T_{in})$. A comparison of these values throughout the 1.0 MPa test indicated that approximately 350 W was lost to the environment during steady state operation at each flow condition. Theoretical calculations assumed that the outer pressure vessel served as an adiabatic boundary, such that no heat was lost to the environment. As a result, because significant heat was lost to the surroundings, the

predicted pressure drop may not have been accurate for the experimental configuration. Follow-on testing will be performed with insulation wrapped around the outer surface of the flow channel to prevent unnecessary heat loss.



(a) Re = 2800, 5000



(b) Re = 10000, 15000

FIGURE 6.17. Flow and Wall Temperatures for the HX Flow Annulus.

Because the measured pressure drop at high Re was significantly less than anticipated, the theoretical pressure drop calculation was revisited to determine the empirical coefficient in Eq. (6.4) that would apply at high Reynolds numbers. The plot shown in Figure 6.18 demonstrates the effect of reducing the constant C_1 in Eq. (6.4) from 0.13 to 0.085 for $Re = 15000$ and 10000 . The calculation was performed using the theoretical wall and bulk flow temperatures rather than experimentally measured values, which suggested poor positioning of the wall thermocouples. According to Takase (2000), the lower coefficient of 0.085 better applies to a square-ribbed annulus with a pitch-to-height ratio of 20.

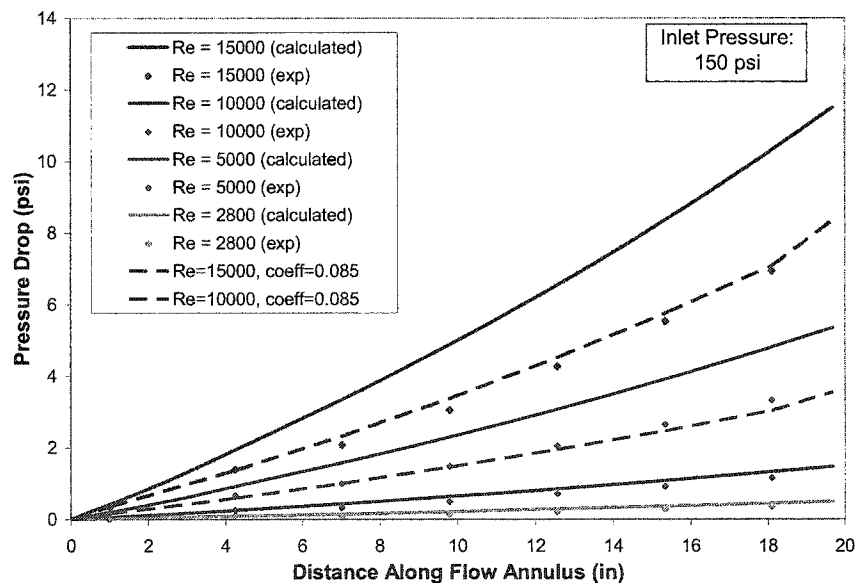


FIGURE 6.18. Experimental and Calculated Pressure Drop for the HX Flow Annulus; Adjustment of the Coefficient in the Takase (2000) Correlation.

6.2.4 Future Enhancements

Work is currently being performed to further assess the source of the discrepancy in the measured and calculated channel pressure drop. Initial corrections to the test apparatus will be the addition of a 2" thick layer of insulation wrapped around the outside

surface of the flow channel. Due to the significant number of diagnostic ports on the outer surface of the test article necessary for temperature and pressure measurement, proper insulation of the flow channel is somewhat challenging.

Re-positioning of the wall thermocouples to ensure measurement of the wall temperature (rather than the flow temperature) will likely require redesign of the test article. In the current configuration, wall TCs were inserted through Swage fittings in the outer pressure vessel, crossed through the flow annulus, and seated in the inner wall. Positioning was performed by “feel” and could not be verified due to the small annulus size. An alternative thermocouple placement might feed thermocouple wires along the inside of the HX module (between the outer surface of the heater and the inner surface of the HX module) and fix the TC head in the inner wall of the HX module (rather than the outer wall, as in the present configuration). To prevent the TC wires from touching the heater or from making contact with the HX wall at an arbitrary axial location, this method would require shielded TC wires. This possibility was investigated, but it was quickly determined that significant challenges existed. The distance between the heater and the module wall is on the order of 0.1” (2.5 mm), significantly limiting the size of the shielded TC wires, especially when it is considered that multiple thermocouples would be required along the channel length. Additionally, typical shielding materials used for thermocouple wires (e.g., Kapton, Teflon®, Glass braid) may not withstand the high temperatures that would exist in the immediate vicinity of the heater. This and other alternative designs for TC placement are still being investigated for future tests of the single channel.

Finally, it was noted in the description of the HPR-HX test article that the test configuration was slightly altered from the actual HX design. The primary alteration was the length of the module, which was increased to approximately twice the length of the prototypic HX to accommodate the available graphite heaters. To prevent re-design of the test stand and single channel test article, and to avoid locating and acquiring shorter

resistance heaters, subsequent tests will seek to match the desired temperature rise of 180° K (180° C) over an 8" (20.3 cm) section of the test article. This test will allow determination of the expected pressure drop and film temperature rise in the flow annulus over a section equivalent to the prototypic HX, without the additional effects of the inlet and outlet plenums.

Due to program milestones and pending deadlines for deliverables for the full SAFE-100a testing with heat exchanger, the single channel tests have temporarily been placed on hold. It is anticipated that additional tests will commence in mid FY 2004.

CHAPTER 7

HEAT PIPE OPERATION: EXPERIMENTAL AND COMPUTATIONAL ANALYSES

The basic theory of heat pipe operation was discussed in Section 2.1.1.2. For nominal operation of a heat pipe reactor, the in-core heat pipes must be properly thawed as the reactor power is slowly increased. A slow start-up prevents potentially problematic events such as entrainment, departure from nucleate boiling in the evaporator region, or evaporator dryout. Computational models have been developed to assess the expected operation of a specific heat pipe design both during start-up and steady state operation. While some codes provide complete, detailed analyses of heat pipe thaw that are computationally intensive (Cao and Faghri 1992; Hall and Doster 1990; Tournier and El-Genk 1995; Tournier and El-Genk 1996; Tournier and El-Genk 2003; Tournier and El-Genk 1993), a relatively simple, concise routine can also be applied to approximate the response of a heat pipe to changes in the evaporator heat transfer rate during start-up and power transients (e.g., modification of reactor power level) with reasonably accurate results (Reid 2002; Reid et al. 2001). In this dissertation a simplified model of heat pipe start-up will be developed by extending the work of Reid (2002), and comparing the results to experimental measurements for a SAFE-100 type heat pipe design.

7.1 Steady-State Operation of a Sodium Heat Pipe

The HTPPIPE code developed by Los Alamos National Laboratory was designed to assess the steady state operation of a specified heat pipe design (Woloshun et al. 1988).

Figure 7.1 shows the axial temperature distribution and the power into and out of a SAFE-400 heat pipe design, calculated using HTPIPE. Although it does not include a more complex analysis of the transient operation of a heat pipe, this calculation using HTPIPE is included to demonstrate the nominal heat transfer characteristics of a fully operational liquid metal heat pipe.

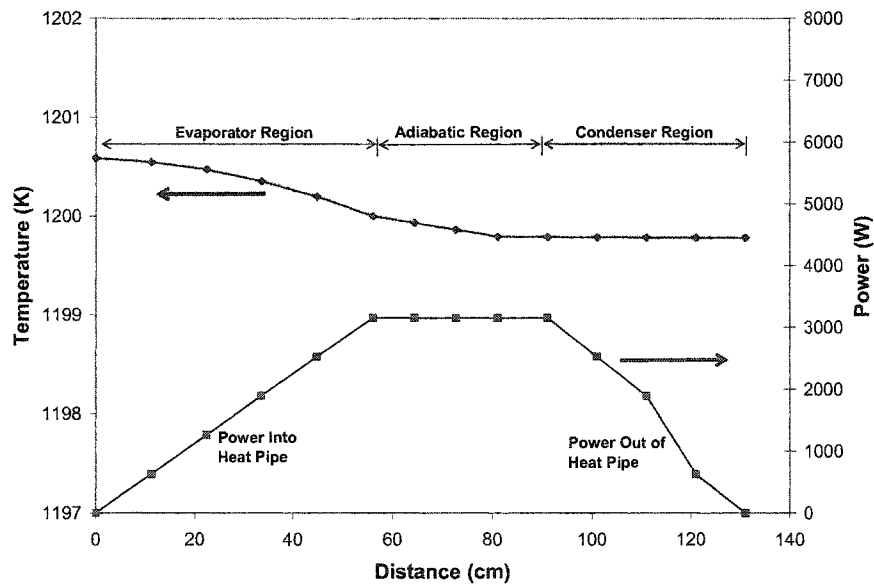


FIGURE 7.1. Steady State Operation of a SAFE-400 Heat Pipe.

Parameters included a Mo-44%Re wick structure with a crescent annular gap and sodium working fluid; dimensions for the SAFE-400 heat pipe design were provided in Table 2.1, Section 2.1.1.2. The analysis was performed for the average heat input of 3150 W for a single heat pipe in the SAFE-400 core (total input by the end of the evaporator region) at an evaporator exit temperature of 1200 K. During nominal operation at a steady state power input, the heat pipe is approximately isothermal, deviating less than ± 1 degree from the selected evaporator exit temperature along the length.

7.2 Transient Operation of a Sodium Heat Pipe

Computational analysis of heat pipe start-up requires a detailed description of the module geometry and boundary conditions. In preparation for the SAFE-100a tests, each heat pipe module underwent multiple start-ups as the final stage in the standard heat pipe processing procedures, which included fill, leak check, vacuum processing, weld closeout, and high temperature “wet-in” (Martin and Salvail 2004). The data presented in this dissertation, for comparison to computational results, correspond to the second start-up of SAFE-100a Module 12 that was performed at the EFF-TF facility in October 2003. The heat pipe was operated in air, with heating provided by three cartridge heaters (Boraelectric Heater elements, Advanced Ceramics Corporation) that fit into the three fuel tubes surrounding the central heat pipe in the module. Due to the limited length of the heater elements, the evaporator region was effectively limited to 16” (40.64 cm). This configuration is illustrated in Figure 7.2.

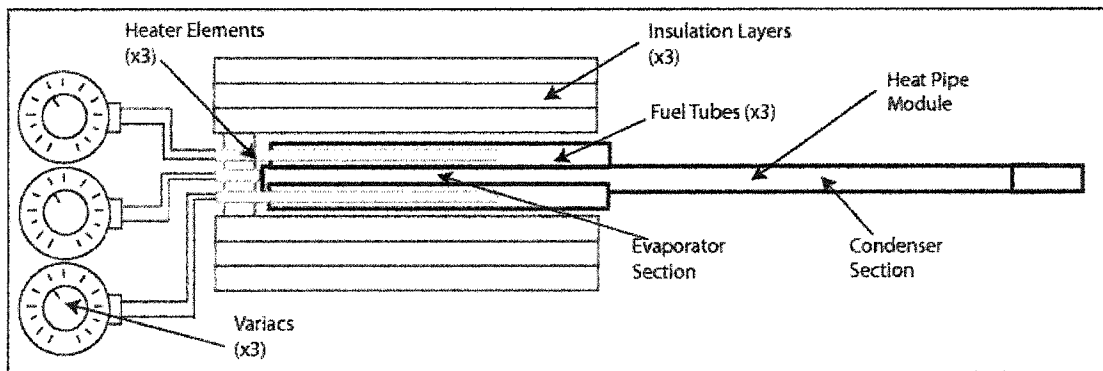
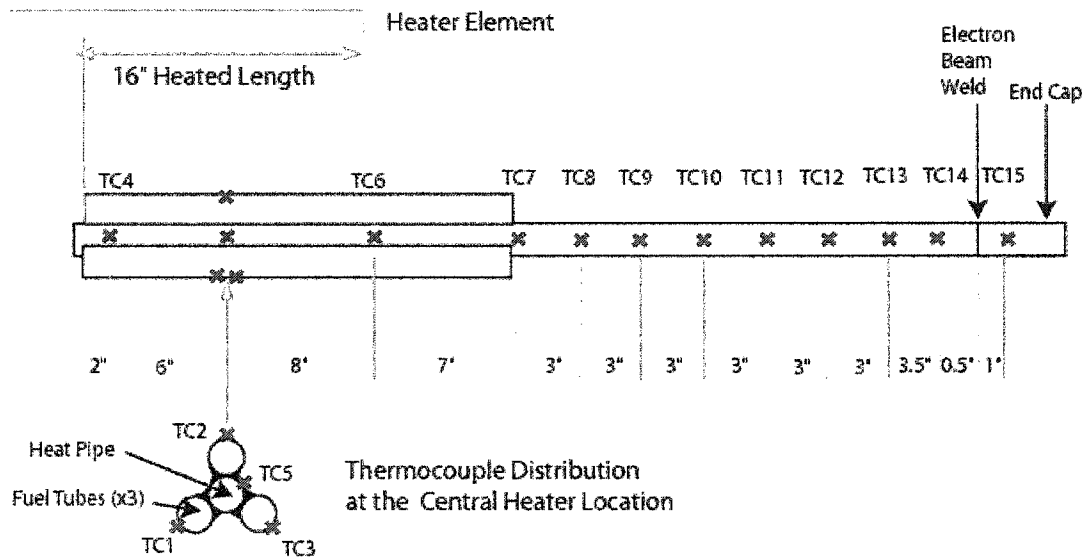


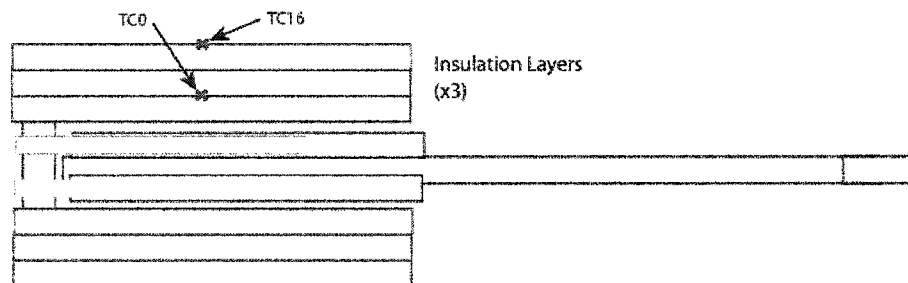
FIGURE 7.2. General Component Layout for Heat Pipe Testing.

To monitor the start-up process, the module was instrumented with 15 type K thermocouples (TCs) that were distributed along its length and spot welded to the desired tube surface. Thermocouples were placed such that 3 were located on the external surfaces of the fuel tubes, 3 were located on the evaporator section, 8 were located on the

condenser section, and one was located on the end cap. The location of each thermocouple is noted by an “x” in Figure 7.3.



(a) TC Locations Along Heat Pipe Module.



(b) TC Locations in Insulation Blanket.

FIGURE 7.3. Illustration of Heat Pipe Module Instrumentation Layout.

For module testing, the fuel tubes and evaporator section were wrapped with three layers of 1" thick insulation material (Insulfrax) with an outer layer of foil to decrease the thermal losses to the environment. The evaporator insulation was equipped with two thermocouples to provide an estimate of the ambient losses. The complete test set-up is

shown in Figure 7.4. Heat pipe start-up was performed over approximately one hour to an average condenser temperature of 930°K (660°C), which was maintained for approximately 6 hours. During testing, the heat pipe condenser section was also loosely covered with a single layer of aluminum foil.

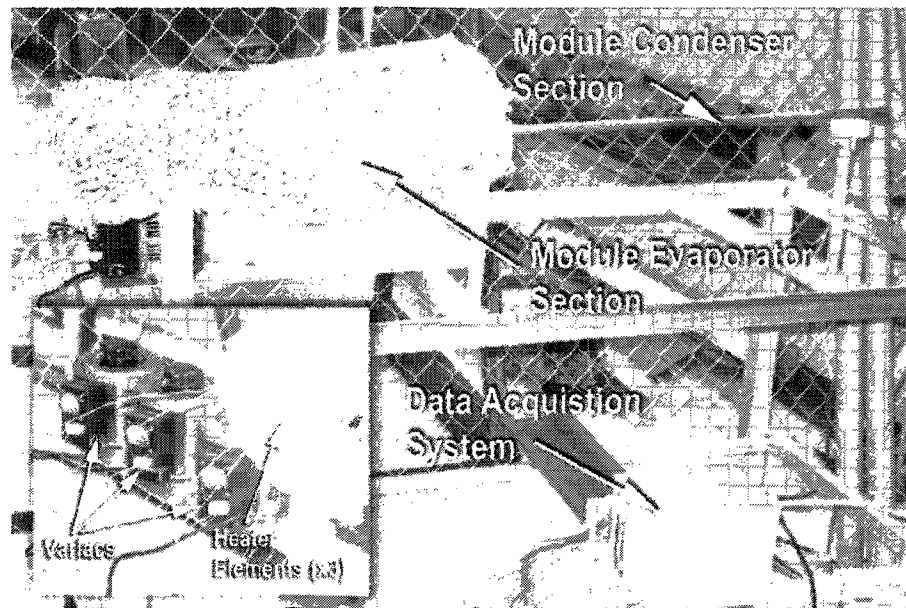
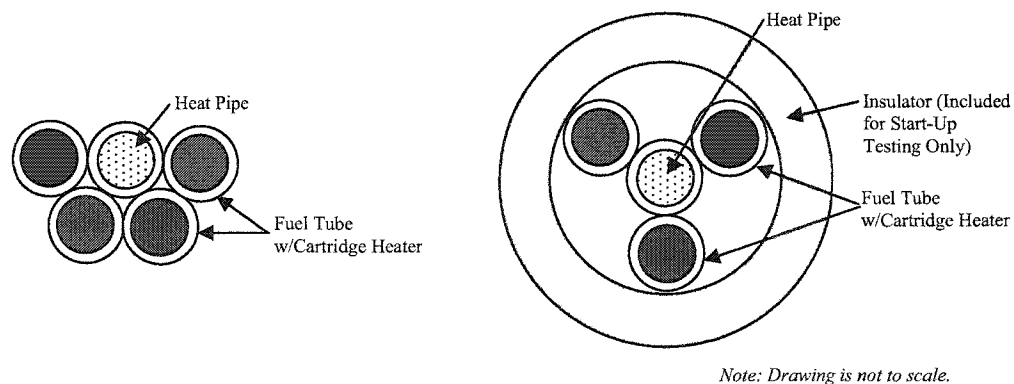


FIGURE 7.4. Heat Pipe Module Test Set-Up.

A relatively simple approach was taken in developing a computational model of heat pipe operation during start-up. Computational analysis of a SAFE-100a heat pipe was based on the transient heat pipe response approximation, HPAPPX, that was developed by Reid (2002) for the SAFE-30 heat pipe design (Reid et al. 2001). The original HPAPPX code, written in Fortran, made several simplifying assumptions to minimize the computation time, yet maintain reasonably accurate results. The approach first assumed that the alkali metal heat pipe maintained a fixed conductance, and that changes in the evaporator heat transfer rate were quasi-steady. In this one-dimensional approach to heat pipe start-up and operation, a lumped capacitance solution was applied. Additional assumptions included viscous limited, laminar, incompressible flow. The

modeled heat pipe was not sonic limited during start-up, so this limitation to the heat transfer rate was not included. Although HPAPPX provides a reasonable approximation to heat pipe transients, it was not designed to predict the transient response in a rapid heat pipe start-up from a frozen state.

Several modifications were required to apply HPAPPX to the SAFE-100a geometry. In the earlier SAFE-30 design, each individual module was constructed from a central heat pipe with one half of its circumference (π radians) surrounded by four fuel tubes (see Fig. 7.5a), providing significantly improved coupling between the heat pipe and fuel tubes (cartridge heaters) than in the tri-node SAFE-100 and -400 designs (see Fig. 7.5b). Additionally, SAFE-30 module testing was performed in vacuum, such that no heat was lost due to convective heat transfer, and the heated region was not surrounded by insulation, such that heat loss from the fuel tubes and the evaporator region was possible by radiation.



(a) SAFE-30 Heat Pipe Module.

(b) SAFE-100a Heat Pipe Module.

FIGURE 7.5. Heat Pipe Modules Modeled by HPAPPX.

The cartridge heater and fuel tube configuration was modeled as a simple two-zone enclosure with series resistances. Although the cartridge heaters were slid into the fuel tubes and simply allowed to rest in the tube, the model assumed that the heaters were

centered in the fuel tubes (not touching the walls). Hence, it was assumed that conduction between the heater and fuel tube was minimal and only radiation heat transfer from the heater to the tube wall was included. Applying these assumptions, the expression for the heat transfer from the heater (CH) to the fuel tube (FT) was determined using Eqs. (7.1), where ε , A , and T correspond to the emittance (unitless), area (m^2), and temperature (K) of each component. The energy at each node is given by σT^4 , where σ is the Stefan-Boltzmann constant ($5.67\text{E-}8 \text{ W/m}^2\text{-K}^4$).

$$Q_{CH} = \frac{\sigma(T_{CH}^4 - T_{FT}^4)}{R} = G_{RI}(T_{CH}^4 - T_{FT}^4) \quad (7.1a)$$

$$R = \frac{1}{A_{CH}} \left(\frac{1}{\varepsilon_{CH}} + \frac{A_{CH}}{A_{FT}} \left(\frac{1}{\varepsilon_{FT}} - 1 \right) \right) \quad (7.1b)$$

Note that in the derivation of the equivalent resistance between the cartridge heater and the fuel tubes, the view factor from the cartridge heater to the fuel tube was assumed to be unity.

The fuel tube, evaporator region, and insulator in the test configuration were modeled as a three node radiation network, approximated by the network of resistors shown in Figure 7.6 (Ozisik 1985). In the initial analysis, the insulator was assumed to be a “reradiating” surface. This translates to a net radiation heat flux of zero in the insulated zone, implying that the zone emitted as much radiation as it received by radiation from the surrounding zones. In the computational model that was developed, this implies that all radiative energy emitted by the fuel tubes and evaporator that was “seen” by the insulator was subsequently re-emitted by the insulator back to the fuel tubes and evaporator.

The amount of radiation each component sees is a function of the view factor. The view factor physically represents the fraction of the radiative energy leaving one surface that directly strikes the other surface of interest. The view factors (F) between

each component and the emittance of each component in the network are included in the equivalent resistances that appear in the network in Fig. 7.6.

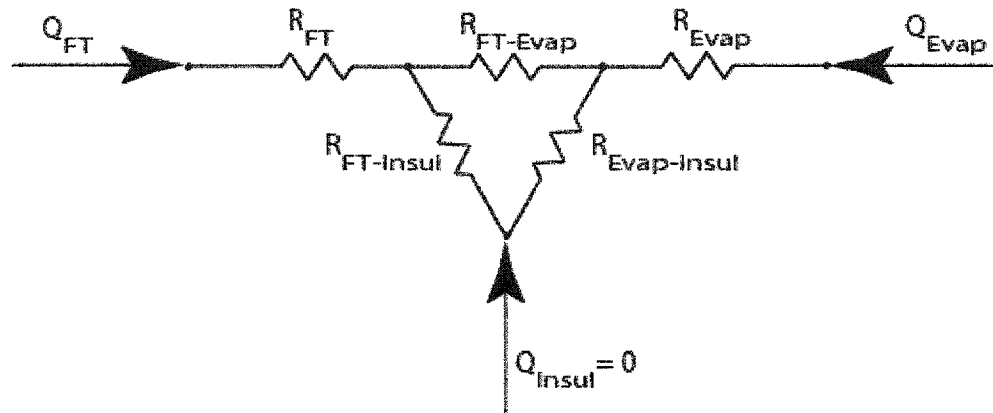


FIGURE 7.6. Three Node Radiation Network Applied for SAFE-100a Heat Pipe Module Analysis.

Analysis of the 3-node radiation network using relationships for series and parallel resistors results in a total resistance (R_{FEI}) between the fuel tubes, evaporator, and insulator:

$$R_{REI} = R_{FT} + \left[\frac{1}{R_{FT-Evap}} + \frac{1}{R_{FT-Insul} + R_{Evap-Insul}} \right]^{-1} + R_{Evap} \quad (7.2a)$$

where the individual resistances are given by:

$$\begin{aligned} R_{FT} &= \frac{1 - \varepsilon_{FT}}{A_{FT} \varepsilon_{FT}} & R_{Evap} &= \frac{1 - \varepsilon_{Evap}}{A_{Evap} \varepsilon_{Evap}} \\ R_{FT-Evap} &= \frac{1}{A_{FT} F_{FT-Evap}} & R_{FT-Insul} &= \frac{1}{A_{FT} F_{FT-Insul}} \\ R_{Evap-Insul} &= \frac{1}{A_{Evap} F_{Evap-Insul}} \end{aligned} \quad (7.2b)$$

F represents the view factor between each component and each area (A) in the denominator corresponds to the outer surface area of the fuel tube or evaporator. Given the initial assumption that $Q_{\text{Insul}} = 0$, and initially ignoring conductive and convective

losses, conservation of energy implies that $Q_{FT} = -Q_{Evap}$. Therefore, the net heat flow rate from the fuel tubes to the evaporator can be expressed as:

$$Q_{FT} = -Q_{Evap} = \frac{\sigma(T_{FT}^4 - T_{Evap}^4)}{R_{FEI}} = G_{FEI}(T_{FT}^4 - T_{Evap}^4). \quad (7.3)$$

Thermocouple measurements during heat pipe operation indicated that the assumption of a perfect thermal insulator, such that $Q_{insul} = 0$, was poor. As the module temperature increased, a significant amount of heat was lost by conduction through the insulator. This time and temperature dependent heat loss was added to the computational model as a loss from the fuel tubes (and, subsequently, a loss from the evaporator) utilizing measured insulator temperatures (TC0 and TC16 in Fig. 7.3b) at each measurement time to establish boundary conditions. The conductivity of the insulation was approximately given by:

$$k(T_{insul,avg}) = 4.3 \cdot 10^{-7} T_{insul,avg}^2 - 2.0 \cdot 10^{-5} T_{insul,avg} + 0.0503, \quad (7.4)$$

where the temperature used to assess the conductivity was the average insulator temperature. The approximate heat loss through the insulator was therefore given by:

$$Q_{Insul,loss} \cong k(T_{insul,avg}) \frac{A_{Insul}}{\Delta x_{insul}} (T_{insul,outer} - T_{insul,inner}). \quad (7.5)$$

Because the heated region was also exposed at its ends, allowing a small amount of air flow along its length, a convective loss term was also approximated:

$$Q_{Conv} \cong hA_{FT,outer}(T_{FT} - T_{ambient}). \quad (7.6)$$

The ambient temperature was roughly approximated as an average of the fuel tube and insulator temperatures, and the convection coefficient was assumed to be half that of the air flowing on the outside of the condenser region due to the presumed reduction in the air flow in this region. However, results of the HPAPPX calculations indicated that inclusion of this convective term was insignificant relative to the radiative heat transfer.

The unsteady, one-dimensional diffusion equation with a heating source, radiation, and convection is given by:

$$C \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial x^2} + \dot{q} - G(T^4 - T_\infty^4) - hA(T - T_\infty), \quad (7.7)$$

where

- C = specific heat capacity (J/K),
- k = conductivity (W/m²-K),
- G = conductance (W/K) = σ/R ,
- R = effective resistance of the radiation network (m⁻²),
- h = convection coefficient (W/m-K), and
- A = surface area (m²).

The expression in Eq. (7.7) is essentially a balance of the heat input and the heat output in any given node. Written in difference form about the cartridge heaters (CH), fuel tubes (FT), and evaporator (indicated as node (1)), this becomes:

$$C_{CH} \frac{T_{CH} - T_{CH}^P}{dt} = Q_{electric} - G_{RI}(T_{CH}^4 - T_{FT}^4), \quad (7.8)$$

$$C_{FT} \frac{T_{FT} - T_{FT}^P}{dt} = G_{RI}(T_{CH}^4 - T_{FT}^4) - G_{FEI}(T_{FT}^4 - T_{(1)}^4) - Q_{insul} - Q_{Conv}, \quad (7.9)$$

$$C_{(1)} \frac{T_{(1)} - T_{(1)}^P}{dt} = G_{FEI}(T_{FT}^4 - T_{(1)}^4) - Q_{(2)}. \quad (7.10)$$

The energy balance for the condenser nodes was computed using the form:

$$C_{(i)} \frac{T_{(i)} - T_{(i)}^P}{dt} = Q_{(i)} - G_{R(i)}(T_{(i)}^4 - T_\infty^4) - hA_{Cond}(T_{(i)} - T_\infty). \quad (7.11)$$

$Q_{(i)}$ represents the heat transferred into node i from the previous node $i-1$; this is a function of the status of the heat pipe start-up, and whether the heat pipe is viscous or radiation limited. A single node was used to represent the heaters, the fuel tubes, and the evaporator; the condenser was divided into 8 nodes along the axial dimension. In each of the expressions above (Eqs. 7.8-7.11), a superscript P corresponds to the temperature of the indicated node at the previous time point.

The original HPAPPX code only considered radial heat transfer, and the heat transfer rate from the evaporator to the first condenser node, $Q_{(2)}$, was determined by the minimum of the radiation, sonic and viscous limits, as discussed in Section 2.1.1.2. Addition of axial conduction through the sodium trapped in the annular gap in the heat pipe adds an additional term to the evaporator and condenser nodes:

$$Q_{axial} = kA \frac{\partial T}{\partial z} = \frac{T(j+1) - T(j)}{z(j+1) - z(j)} . \quad (7.12)$$

Based on the current temperature of the node relative to the sodium melting temperature, the conductivity of solid or liquid sodium is selected. For the evaporator region, this represents an additional loss term (from node 1 to node 2); each of the condenser nodes sees heat addition due to axial conduction from the previous node, as well as heat loss to the subsequent node.

The Newton-Raphson method was employed to solve each of the energy balance equations at each time. The Newton-Raphson method is an iterative root-locating method, where the root at time $n+1$ is determined from an initial guess at time n , given that the function f is a differentiable function (Chapra and Canale 1985):

$$x_{n+1} = x_n - \frac{f(x_n)}{f'(x_{n+1})} . \quad (7.13)$$

Because it is a derivative method that is based on some previous knowledge of the function (e.g., an initial guess is required), the Newton-Raphson method can find a local minimum as opposed to the global minimum that is sought. Hence, it is important that an appropriate initial guess be selected.

7.3 Results: Comparison of Computational and Experimental Analyses

The accuracy of the computational model developed for SAFE-100a module operation was assessed by comparison to experimentally measured temperatures on the second start-up of SAFE-100a Module 12. Several changes in the geometry, material

properties, and environmental boundary conditions were made to the original HPAPPX code to both make it applicable to the SAFE-100a geometry and testing conditions and to improve the details in the modeling. The modified HPAPPX code, also written in Fortran, will be referred to as SF100RADNtwk. Computational results for Module 12 are shown in Figures 7.7-7.10. The time at which each measurement was taken and the electrical power input to the heaters were required inputs to the computational model that were taken from the experimental data. The heat loss through the insulator was estimated by measured values reported by TC0 and TC16 at each measurement point. The electrical power input, measured power loss through the insulator, and calculated power into the evaporator and first condenser nodes are plotted in Figure 7.7.

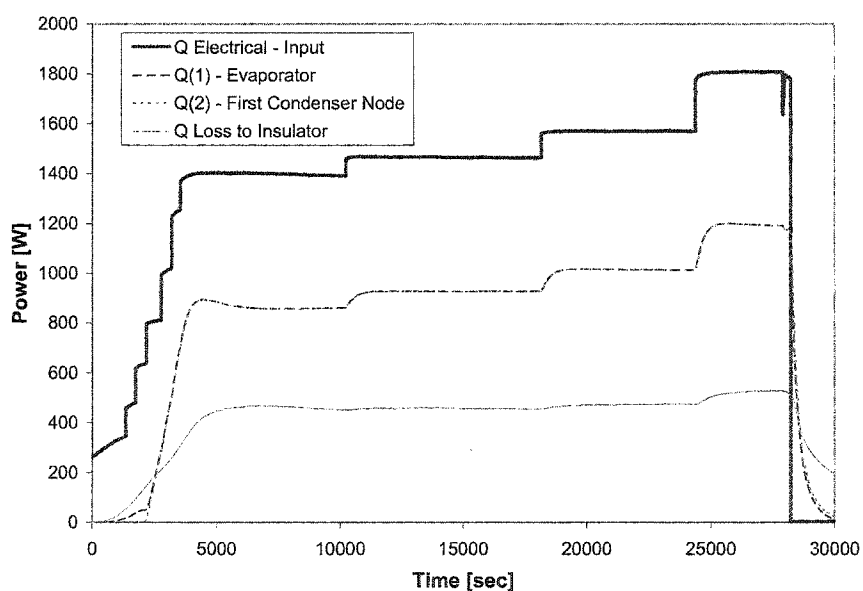


FIGURE 7.7. Input Power and Calculated Output Power for SAFE-100a Module 12.

In Figure 7.8, the average measured temperature is shown for thermocouples 1, 2 and 3 as compared to the predicted heater, fuel tube, evaporator, and condenser (node 2, evaporator exit/condenser entrance) temperatures. Because these thermocouples were located on the outside of each fuel tube (Fig. 7.3a), these measured values would be

expected to be lower than the calculated fuel tube temperature. However, one would expect the shape of the measured and calculated fuel tube temperatures to match somewhat better than what was obtained.

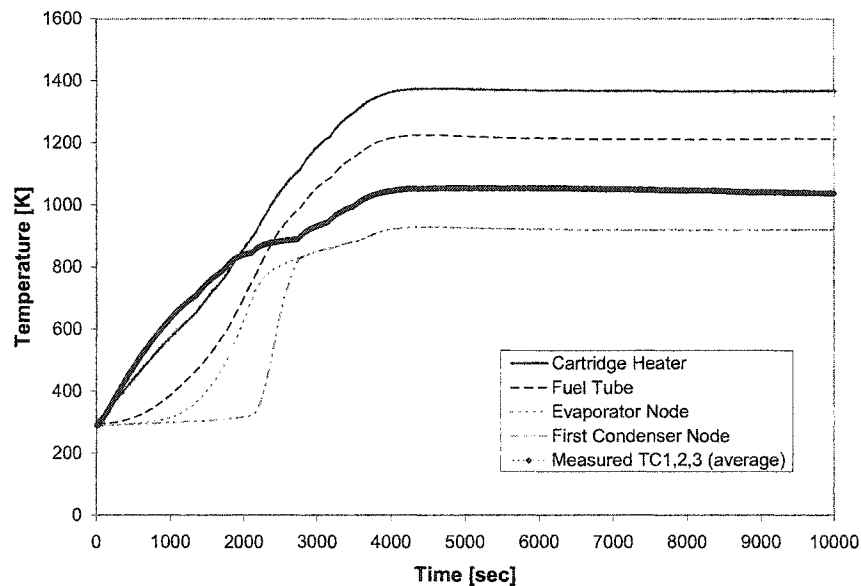


FIGURE 7.8. Calculated Module 12 Temperatures: Heater, Fuel Tubes, Evaporator, and First Condenser Nodes.

Because the trend in the calculated heater temperature matches the measured trend for the fuel tube temperature as a function of time, and the calculated fuel tube temperatures were lower than the average measured temperature on the outside surface of the fuel tube, it is likely that the modeled heat transfer from the heater to the fuel tube was insufficient. Recall that Eqs. (7.1, 7.8) assumed that heat was only transferred by radiation from the cartridge heater to the fuel tube. The heaters were assumed to be centered in the fuel tube; in reality, each heater rested in the fuel tube (due to gravity), making contact with the fuel tube wall and providing an avenue for conduction that was not modeled. This actual configuration is shown in Figure 7.9. All heat pipe start-up tests were performed in air for the SAFE-100a module, rather than in vacuum, such that a small amount of additional conduction may have been present between the heater and

fuel tube, using air as a conduction medium. Although the total contribution from the conduction paths was expected to be small, experimental results suggest that conduction was the dominant means of heat transfer from the heater to the fuel tube (and, subsequently, to the evaporator) early in the start-up transient. This discrepancy in the model may have been sufficient to account for the modified shape in the fuel tube and evaporator temperature as a function of time. Additional error may have been introduced in the estimated heat loss through the insulator, or in the approximated convective losses around the fuel tube.

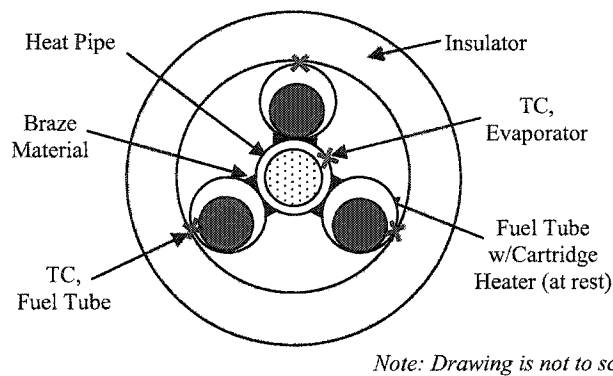


FIGURE 7.9. SAFE-100a Test Module Configuration.

The calculated and measured temperatures for the evaporator node and the first condenser node over the full test duration are shown in Figure 7.10. Analysis of these results indicates that although the calculated evaporator and condenser temperatures were approximately correct during steady state, the temperature rise for each of these nodes was significantly delayed relative to the measured temperatures. Some fraction of this discrepancy may be attributed to the errors in the modeled heat transfer from the heaters to the fuel tubes, as discussed above. In addition, due to the relatively small contact area between the fuel tubes and the evaporator, it was assumed that conduction was negligible

between these components. However, as shown in Fig. 7.9, braze material was used to bond the fuel tubes to the heat pipe, providing an avenue for conduction that was ignored in the model. As indicated in the illustration, the evaporator thermocouple was located on the outside of the evaporator, near the heater in the fuel tube. The physical location of this thermocouple also may have contributed to the higher measured temperature. Had the thermocouple been located between the bottom two fuel tubes, at a slightly greater distance from the cartridge heaters, it is likely that a lower temperature would have been measured. The combination of these effects (excluding conduction in the model and the physical location of the evaporator TC in the experiment) likely contributed to the delayed heating of the evaporator node (and, subsequently, the condenser nodes) in the calculated model versus experimental measurements.

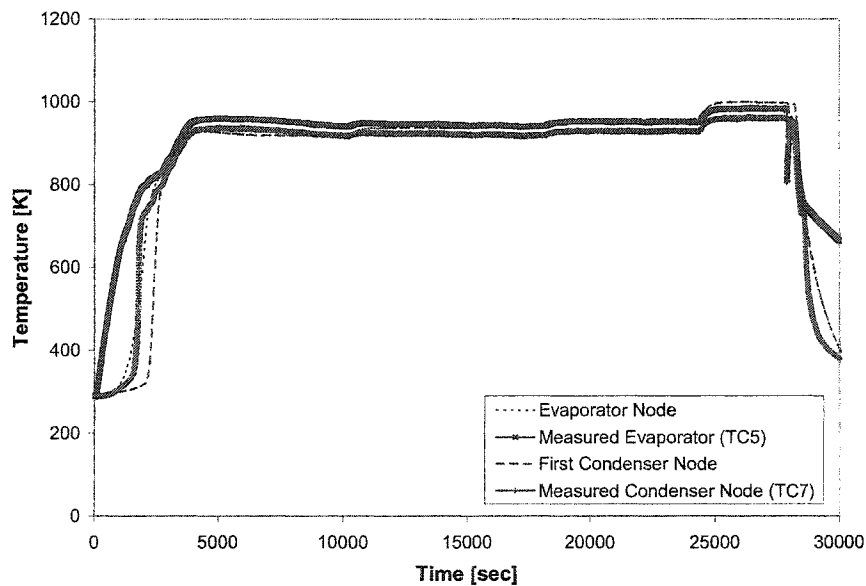


FIGURE 7.10. Calculated and Measured Module 12 Temperatures: Evaporator and First Condenser Nodes.

Comparison of the computational results presented here using SF100RADNtwk for the SAFE-100a module to the earlier results obtained by Reid (2002) using the original HPAPPX for the SAFE-30 module demonstrates the importance of adding axial

conduction to the heat balance model. Results presented by Reid (2002) predicted sharp increases in the condenser temperatures as the working fluid in these nodes was thawed. The addition of axial conduction to the heat balance smoothes the temperature rise over time. Although the calculated temperature rise for the first condenser node was slightly delayed relative to the measured data as a result of the modeling inaccuracies discussed above, the rate of the temperature rise (i.e. shape of $T_{(2)}(t)$) was identical to the measured rate of temperature rise for node 2.

Figure 7.11 shows a comparison of the measured and calculated temperatures along the condenser for Module 12. Again, the temperature rise in each node is somewhat delayed relative to the measured values, but each maintains the appropriate rate of change and, with the exception of the final condenser node (node 9, TC14), each node in the condenser increases to the evaporator temperature at steady state. The final node (identified as $T_{(9)}$) in the computational model failed to reach the evaporator temperature, reporting a value that was approximately 80° K lower than anticipated. This result may be due to the convection term along the insulator being too strong, or due to loss of heat by axial conduction to the stagnant sodium pool at the end of the condenser region. The final curve shown in Fig. 7.11 corresponds to the measured temperature of the heat pipe end cap. Because no sodium can flow into this region, the end cap temperature can only increase by conduction from the condenser, causing the end cap temperature to be significantly lower than the other condenser temperatures; the end cap temperature was not calculated in the computational model. The calculated temperature for node 9, which corresponds to the final condenser node adjacent to the end cap may be lower than anticipated due to modeling assumptions that reduce the effective length of the condenser region, such that in the model, node 9 appears to be a part of the stagnant sodium pool at the end of the heat pipe.

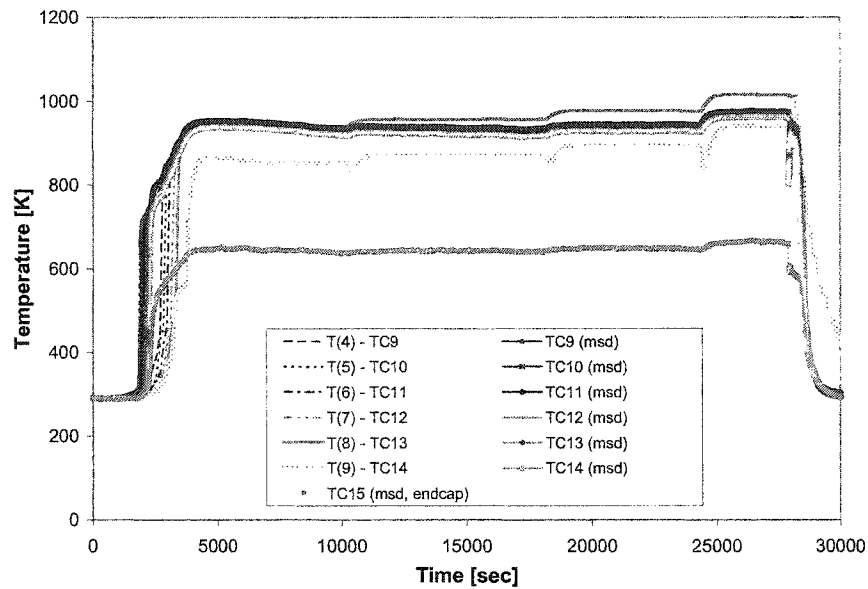


FIGURE 7.11. Calculated and Measured Module 12 Temperatures: Condenser Nodes.

7.4 Future Enhancements to Computational Heat Pipe Analysis

The modifications to the HPAPPX code for application to the SAFE-100a modules, which resulted in SF100RADNtwk, attempted a simple, straightforward approach to modeling the transient operation of a SAFE-100a type heat pipe. Although the results that were obtained were reasonably accurate, several modifications could be applied to make the code more rigorous in its application.

First, it was noted that the heat transfer between the cartridge heaters and the fuel tubes was likely insufficient. Greater diligence should be applied to determine the magnitude of conduction from the heater to the fuel tube to better capture this behavior in the computational model. Other contributions to radial heat conduction should be assessed, including potential conduction between the fuel tubes and the heat pipe evaporator region.

Second, a more rigorous analysis of the radiation/conduction/convection heat transfer network between the fuel tubes, evaporator, and insulator should be completed.

The current model incorporated heat loss through the insulator using measured boundary conditions; attempt should be made to dissociate the computational model from the experimental results as much as possible to avoid any potential biasing of the computational results.

Third, failure of the final condenser node to reach the evaporator temperature indicates a need to reassess the convection and axial conduction terms for the condenser nodes to determine the source of the error. It is likely that one or both of these terms is too high, causing the active region of the heat pipe to effectively be shortened.

Despite the small inaccuracies that result from simplifying assumptions, the SF100RADNtwk code provides a reasonable estimate of the heat pipe response under transient conditions. With the proper adjustments made to the boundary conditions, this code will be applied to heat pipe transient operation under vacuum conditions. In a vacuum environment, the simplifying assumptions made for the insulator heat loss and for the convection heat loss terms will be less significant, and it is anticipated that the computational results will better match the experimental data. Lifetime testing of the SAFE-100a modules and testing of the heat pipe modules in the electrically heated SAFE-100a core will commence in FY2004; all in-core and lifetime testing will be performed under vacuum conditions.

CHAPTER 8

CONCLUSIONS AND FUTURE WORK

This dissertation covered an eclectic body of work that has been conducted to advance the development status of a space nuclear fission system for application to nuclear electric propulsion. While a significant amount of work remains to advance to flight testing, many questions have been answered and capabilities demonstrated through both computational and non-nuclear experimental work.

Computational analyses were applied to quantitatively assess the effect of the space radiation environment on reactor operation in near Earth orbits and in the Jovian system. Available models were applied to characterize the space radiation environment. Calculations indicated that while the space radiation environment was negligible during steady state operation of a 400 kW_t heat pipe cooled reactor, the secondary neutron population produced by incident protons could in fact provide a start-up source for reactor operation. Terrestrial reactors generally employ a neutron source to ensure that reactor start-up will be achieved in a reasonable, predictable time period. The SAFE-400 reactor has a fuel inventory of only 141.7 kg, producing just 85.5 spontaneous fission neutrons per second, such that start-up by spontaneous fission alone could take a considerable amount of time. However, due to the increased radiation environment in space, calculations have demonstrated that a space fission reactor will not require a start-up source, simplifying launch approval procedures. During start-up from zero power to a low, steady state power level, fluctuations in the secondary neutron source generated a

noticeable effect in the simulated reactor dynamics. A controller developed using model-based predictive control was demonstrated to be sufficiently robust to account for large temporal fluctuations in the external source without overshoot of the desired power level. Despite the demonstrated success of the MBPC controller, future enhancements were suggested, including additional constraints on the control drum movement and further decoupling of the controller from the user-selected control parameters. Future analysis should also consider the effect of probable failure scenarios on the core reactivity as a function of drum position and the capability of the control algorithm to account for this potential failure. This might be accomplished by developing separate expressions for the reactivity worth of each of the six control drums. A sum of the reactivity worth for all six drums would then yield the total reactivity in the reactor core at a given time.

Due to limitations in the available transport codes, the work presented in this dissertation only characterized the effect of incident protons on the reactor operation, with a crude estimate of the alpha particle induced neutron production. While ions having a higher atomic mass ($Z \geq 2$) are not expected to significantly affect reactor operation, as demonstrated by calculations performed for incident alpha particles, future analysis should seek to quantify these effects more thoroughly. Although the Jovian radiation environment that was considered was somewhat incomplete, considering only trapped protons and an approximate contribution from galactic and anomalous cosmic ray protons, the intensity of this environment under nominal conditions indicated that care should be taken in designing any system for operation in the Jovian system should low power operations (e.g., reactor shutdown and restart) become necessary or desirable in this region.

Recognizing that the development of a nuclear reactor for space applications requires a significant amount of testing prior to flight testing, several non-nuclear experiments were presented for a prototypic heat pipe cooled reactor core and a direct gas cooled reactor core. The first of these tests provided a unique demonstration of how non-

nuclear, electrically heated reactor tests might be coupled with nuclear simulations to more accurately simulate the behavior of a nuclear system. This test integrated neutronic calculations with the real time measurement of the dominant reactor feedback mechanism, i.e. core deformation caused by thermal expansion of the core materials. Off-line criticality calculations were employed to quantify the reactivity worth of the simulated reactor control drums and the measured core deformations. During test, the reactor point kinetics equations were solved in real time to assess the response of a nuclear system to the measured reactor state. While significant enhancements to this experiment are planned in the near future, including the incorporation of autonomous control and application to an electrically heated core with heat extraction by heat pipes and an annular flow heat exchanger, the initial tests successfully demonstrated the capability of non-nuclear tests to be coupled to computational simulations of nuclear behavior.

Taking advantage of the similarity in design of the annular flow paths of the heat exchanger for the heat pipe cooled reactor concept and the direct gas cooled reactor concept, testing and characterization of a nominal heat exchanger flow channel and a nominal coolant flow channel in the DDG core were conducted. Simple, low cost tests were performed to validate the predicted flow characteristics prior to construction of the multi-channel heat exchanger or the gas cooled reactor module. Single channel testing for the DDG resulted in the redesign of the coolant flow path, and these modifications were applied prior to initiating the 37-channel DDG core testing; tests of the 37 channel core are currently being performed. Additionally, results of the DDG tests offered significant insight in the development of the single channel tests for the nominal heat exchanger flow channel for the HPR. HPR-HX tests provided significantly more detailed data than the tests performed for the DDG single channel, and a greater understanding of the flow dynamics was obtained. Although some discrepancy was noted in the experimental results relative to the predicted results, the heat exchanger flow tests were

considered a success, and work is on-going to further assess the source of these differences. Due to schedule constraints, the full 19-channel heat exchanger tests have commenced prior to completion of the single channel tests, and additional single channel tests will be performed in parallel.

Computational analysis of a sodium-filled, liquid metal heat pipe was also performed for steady state and transient operation. To determine the accuracy of a relatively simple computational model, these analyses were compared to experimental heat pipe operation data obtained during the check-out tests of the heat pipe modules for the SAFE-100a core. Despite the small inaccuracies that result from simplifying assumptions, the SF100RADNtwk code provided a reasonable estimate of the heat pipe response under transient conditions, and provided excellent prediction of the steady state operation of the sodium filled heat pipe for the SAFE-100a. Lifetime testing of the SAFE-100a modules and testing of the heat pipe modules in the electrically heated SAFE-100a core will be performed under vacuum conditions. Future application of the SF100RADNtwk code will make the necessary adjustments to the heat pipe boundary conditions to predict heat pipe performance under vacuum conditions. In a vacuum environment, the impact of the simplifying assumptions are expected to be less significant, and it is anticipated that the computational results will better match the experimental data during transient heat pipe operation.

As discussed in the introduction to this work, various space fission development programs have been conducted in the United States. With the single exception of the SNAP-10A in 1965, none of these programs resulted in a flight test. Heavy reliance on nuclear testing for system development was a primary contributor to the early demise of these programs. Hence, the current space reactor development program has adopted a strong emphasis on non-nuclear testing, utilizing existing nuclear databases and experiences in system design and testing, to advance more rapidly toward the development of a flight system. To ensure the success of this reactor development

program, innovative approaches have been adopted to minimize or eliminate the reliance on nuclear ground tests through the development of a reliable non-nuclear test program. The validation of this testing approach will be in the in-flight demonstration of a nuclear electric propulsion system, hopefully in the very near future.

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