99 cup
8/13/73

UC-80

Rev: 287

AI-AEC-13069, VOL II

SNAP 4 SUMMARY REPORT
VOLUME II. SYSTEMS DESCRIPTION

AEC Research and Development Report

Atoms International Division
Rockwell International

P.O. Box 309
Canoga Park, California 91304

MASTER

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

SNAP 4 SUMMARY REPORT
VOLUME II. SYSTEMS DESCRIPTION

A. R. MARCHESE, Ed.

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

Atoms International Division
Rockwell International

P O Box 309
Canoga Park, California 91304

CONTRACT: AT(04-3)-701
ISSUED: JUNE 18, 1973

DIST. STATEMENT OF WORK - CLASSIFIED
24

DISTRIBUTION

This report has been distributed according to the category "Systems for Nuclear Auxiliary Power (SNAP) Reactor — SNAP Program," as given in the Standard Distribution for Classified Scientific and Technical Reports, M-3679.

CONTENTS

	Page
Abstract	17
I. Introduction	19
II. Summary Description of SNAP 4 Prototype Reactor System (S4PR) . .	21
A. Introduction	21
B. General	21
C. Reactor	22
1. Reactor Core	23
2. Reflector Assembly and Control Mechanism	33
3. Reactor Vessel and Shield Plug	41
4. Reactor Shield	46
D. Process System	47
1. Steam Separator	49
2. Recirculation Pump	49
3. Condenser	52
4. Feedwater and Condensate Pump	56
5. Regenerative and Nonregenerative Heat Exchangers	56
6. Deionizer	58
7. Recombiner	59
8. Valve Operating Subsystem	59
9. Pressure Relief Subsystem	61
10. Poison Injection Subsystem	61
11. Decay Heat Removal Subsystem	62
E. Control and Instrumentation System	62
1. Control System	62
2. Nuclear Instrumentation System	63
3. Plant Protective System	65
4. Radiation Monitoring Systems	65
5. Process Control Monitoring and Diagnostic Instrumentation Components	68

CONTENTS

	Page
Appendix A. Reactor Design	71
A. Core Design	71
B. Core Structure Design - Background	75
1. Fuel Cluster Supports	75
2. Grid Plate	76
3. Fuel Rod to Grid Plate Attachment	76
4. Fuel Rod End Plugs	76
C. The Reactor Vessel	79
D. Reactor Vessel Configuration - Background	87
E. Control Mechanism	89
Appendix B. Reactor Physics	97
A. Core Composition Analyses	97
1. Burnable Poisons	97
2. Fission Product Poisons	104
3. Optimum Fuel Loading	105
4. Power Flattening	108
B. Reactivity Requirements	109
1. Coefficients of Reactivity	109
2. Control Requirements	113
C. Control Drum Design Analyses	116
D. Critical Experiments	121
1. Objectives and Description of Critical Tests	121
2. Results and Conclusions of Critical Tests	123
References	126
Appendix C. Fuel Technology	129
A. Description of the Fuel Element	129
B. Requirements and Analysis of Fuel Element Performance	130
1. Heat Transfer	130
2. Structural	133

CONTENTS

	Page
3. Hydrogen Redistribution Within the Fuel	135
4. Thermal Cycling	138
5. Fuel Integrity - Corrosion Stability	140
C. Irradiation Tests of Fuel	141
1. NAA 43 Series of Irradiation Experiments	141
2. NAA 75 Series of Irradiation Experiments	143
3. Irradiation Damage	150
D. Fuel Properties	150
1. Physical Properties	150
2. Mechanical Properties	157
E. Fuel Material Fabrication	158
1. Melting and Casting	158
2. Extruding	159
3. Addition of Poisons	159
4. Modifiers	161
5. Hydriding	161
6. Chemical Analysis and Quality Control	161
7. Current Specifications	162
F. Fabrication of the Fuel Element	162
1. Shrink Fitting	162
2. Die Sizing	166
References	170
Appendix D. Reactor Heat Transfer and Hydrodynamics	171
A. Core Hydraulics	171
B. In-Core Heat Transfer	181
C. Columbia Boiling Heat Transfer Experiments	187
1. Description of the Twelve-Rod Test Sections	190
2. Burnout Characteristics of Test Sections 3 and 4	191

CONTENTS

	Page
3. Burnout Characteristics of Test Section 2.	191
4. Burnout Characteristics of Test Section 5.	194
5. Physical Status of Test Section 5.	200
6. Comments and Recommendations	201
References	203
Appendix E. Shielding, Equipment, and Corrosion Product Activation Analyses	205
A. Shield Plug Design	205
B. Activation Shield Design	213
C. Operating Shield	219
D. Corrosion Product Activation	227
E. Deposition of Corrosion Products	230
References	235
Appendix F. Process System	237
A. Definition and Requirements	237
B. Background	237
1. General	237
2. Process System Design Objectives	237
C. Subsystem Descriptions	238
1. Recirculation Subsystem	238
2. Steam Subsystem	242
3. Feedwater and Condensate Subsystem	244
4. Valve Operating Subsystem	247
5. Decay Heat Removal Subsystem	249
6. Pressure Relief Subsystem	251
7. Poison Injection Subsystem	252
8. Recombiner Subsystem	252
9. Coolant Makeup Subsystem	253
10. Shield Water Subsystem	254
11. System Studies	255

CONTENTS

	Page
D. Heat Rejection System	265
1. Performance Requirements	265
2. Suitable Methods	266
3. Control of Condenser Pressure	269
4. Component Design Features	272
E. Water Chemistry	275
1. Corrosion	275
2. Selection of pH Conditions	279
3. Coolant Radiolysis and Hydrogen Suppression	279
4. Catalytic Recombination	280
5. The Potential Problem of Fouling	281
6. Water Purification System	283
F. Waste Gas Handling	286
1. In-Leakage of Air	286
2. Gaseous Fission Product Activity	286
G. Compact, High-Speed Turbo-Pumps	287
1. Shaft Seal Leakage	287
2. Pump Drivers	288
3. Flow Control	288
4. Cycle Analysis	289
References	296
Appendix G. Plant Control System and Instrumentation	301
A. Control System Functional Description	301
1. Neutron Flux Control	303
2. Pressure Control	303
3. Recirculation Flow Control	303
4. Feedwater Flow Control	303
5. Steam Separator Liquid Level Control	304
6. Condenser Heat Removal Control	304

CONTENTS

	Page
B. Component Description of the Process Control Monitoring and Diagnostic Instrumentation Subsystems	304
1. Pressure Measurement and Transmission	305
2. Flow Elements	305
3. Level Elements	306
4. Temperature Elements	306
5. Control Room Receivers and Power Supplies for the Detectors and Transducers	307
6. Radiation Monitoring Subsystem	307
7. Nuclear Instrumentation Subsystem	311
8. Plant Protective Subsystems Description	315
9. Control Room Description	317
C. Analysis of System Dynamics and Stability	320
D. Analysis of Reflector (Control) Drum Dynamics	328
References	329
Appendix H. Safety	331
A. Reactor Characteristics	331
1. Reactivity Coefficients	331
2. Fuel Properties	332
B. Operational Behavior	334
1. System Response at Power	334
2. Reactor Stability	335
C. Accident Conditions	343
1. Startup	343
2. Loss of Coolant	348
3. Pressure Loss	349

TABLES

	Page
II-I. Characteristics of the S4PR	24
B-I. S4PR Parameters	98
B-II. Coefficients of Reactivity About Operating Reactor Conditions .	112
B-III. Nuclear Control Requirements	115
B-IV. Comparison of Experiments on SCA-4B with 16-Neutron Group, One-Dimensional Diffusion Calculations and SNAP 4 Transport Calculations	117
B-V. Comparison of Control Drum Experiments on SCA-3 with 4- Neutron Group, Two-Dimensional Diffusion Calculations and SNAP 4 Transport Calculation	117
B-VI. Available Compacted Powder Fuel Pellets	123
C-I. Kinetics of Hydrogen Redistribution	138
C-II. Thermal Cycling Data	139
C-III. NAA 75-1 and 75-2 Irradiation Experiments, In-Pile Temper- atures and Calculated Burnup Levels	144
C-IV. NAA 75-1 and 75-2 Irradiation Experiments, Pre-to-Post- Irradiated Dimensional and Density Changes	149
C-V. NAA 75-1 and 75-2 Irradiation Experiments, Change in Hydro- gen Content	149
C-VI. Fuel Properties, Experimental Errors	157
C-VII. Transverse Rupture Strength, Variation with Temperature . .	158
C-VIII. Fuel Composition by Weight	158
C-IX. Preliminary Fuel Specifications	162
C-X. Cladding Theoretical Stress Cycle Data	163
C-XI. Room Temperature Gap Measurement	166
C-XII. Finished Assembly Dimensions (Die-Sizing)	166
C-XIII. Change in Clearance Between Fuel and Cladding as a Function of Temperature for Die-Sized Specimen	167
C-XIV. Cladding Summary	168
D-I. Effect of Coolant Passage Thickness on Pressure Loss	176
D-II. 12-Mwt Sources of Control Drum Coolant Enthalpy Rise	180
D-III. "Hot Rod" Instrumented Thin Wall Burnout Heat Transfer Data for SNAP 4 Test Sections 3 and 4	193

TABLES

	Page
D-IV. Predicted Relative Local Exit Qualities	194
D-V. Predicted Relative Local Exit Qualities, Section 2 Data	195
D-VI. Annular Test Sections Dimensions	201
D-VII. Heat Generation for Each Annular Test Section	202
E-I. Neutron Shielding Properties of Materials	206
E-II. Figure of Merit for Shield Plug Materials	206
E-III. Nuclear Data for Isotopes Contributing to Activation Dose . . .	214
E-IV. Activation Dose Rates at Containment Vessel Surface	217
E-V. Activation Dose Rates 1 Meter from Containment Vessel Surface	217
E-VI. Coolant Activity Due to Cyclic Activation	229
E-VII. Coolant Activity from Core Corrosion Products	229
E-VIII. Estimate of Piping Maintenance Dose Rates	231
E-IX. Demineralizer Radioactivity Estimates	234
F-I. Instantaneous Power (At Time, t) After Shutdown	250
F-II. Summary of Water-Cooled Reactor Operating Conditions	256
F-III. S4PR Operating and Design Pressures and Temperatures . . .	258
F-IV. Process Fluid Temperatures	262
F-V. Valve Operating System Comparison	264
G-I. Radiation Monitoring Systems Weight and Cost Breakdown Chart	310
G-II. Nuclear Instrumentation Weight and Cost Chart	312
G-III. Scram and Setback Logic Amplifiers	317
G-IV. Control Equipment Cost and Weight	320

FIGURES

	Page
II-1. Reactor Vessel Cross Section	27
II-2. Nuclear Steam Generator and Container Assembly, Module No. 1	28
II-3. Water Purification Equipment, Module No. 2	29
II-4. Power Conversion Equipment, Module No. 3	30
II-5. Plant Arrangement	31
II-6. Fuel Element Assembly	32
II-7. Support — Core Structure	34
II-8. Core Structure Layout	35
II-9. Core (Design Layout)	37
II-10. Reflector and Control Drive (Design Layout)	39
II-11. Installation Assembly Control Mechanism	43
II-12. System Heat Balance Diagram	48
II-13. Steam Separator (Conventional), Plan View	50
II-14. Steam Separator (Conventional)	50
II-15. Condensate and Feedwater Turbopump Assembly	53
II-16. Turbine Driven Feed and Condensate Pump	55
II-17. Deionizer Unit	60
II-18. Block Diagram - Title I Nuclear Instrumentation System	64
II-19. Block Diagram - Plant Protection System	66
II-20. Block Diagram - Radiation Monitoring System	67
A-1. SNAP 4 Reactor	70
A-2. Basic Dimensions of the Core Structure	72
A-3. Core Cross Section (Design Layout) 7564-0317	77
A-4. Reactor Vessel	80
A-5. Shield Water Vessel	85
A-6. SNAP 4 Reactor Vessel	88
A-7. Spherical Chamber-Type Reactor Vessel with Manifold Nozzles and Internal Separator	90
A-8. Installation Assembly Control Mechanism	91

FIGURES

	Page
B-1. Endurance Calculations with Hafnium Poison	100
B-2. Endurance Calculations with Europium-Hafnium Poison Mixtures	102
B-3. S4PR Design Composition Design Survey	106
B-4. Power Flattening Optimization-I	110
B-5. Power Flattening Optimization-II	111
B-6. Summary of S4PR Control Effects	114
B-7. Effect of Hydrogen Moderation on Optimum Poison Thickness .	119
B-8. Generalized Critical Machine (SCA-5)	120
B-9. SCA-5 Core and Drum Configuration	122
C-1. S4PR Fuel Rod	130
C-2. Schematic of Fuel Element	131
C-3. Fuel Element Temperature Distribution	132
C-4. Effect of Temperature on the Thermal Conductivity of Hydrogen at Subatmospheric Pressures	134
C-5. Short-Time Tensile Properties of AISI Type 347 Stainless Steel as a Function of Temperature	136
C-6. Effect of Neutron Radiation at 80°C on Room-Temperature Tensile Properties of Annealed Type 347 Stainless Steel	136
C-7. Partial Phase Diagram of Zirconium-Hydrogen System	137
C-8. Rate of Hydrogen Redistribution	139
C-9. Pre-Irradiated Condition of NAA 75-1 Experiment	145
C-10. NAA Series 75 Specimens	146
C-11. NAA 75-1 and 75-2 Irradiation Experiment Results, Variation of Diameter Change with Burnup	147
C-12. Correlation of Radiation Damage in Hydride Fuels	151
C-13. SNAP 4 Isochore	152
C-14. Isochore Diagram for SNAP 4 Fuels	152
C-15. Thermal Conductivity	153
C-16. Thermal Diffusivity	153
C-17. Heat Capacity	154
C-18. Electrical Resistivity	154

FIGURES

	Page
C-19. Linear Thermal Expansion	155
C-20. Density of SNAP 4 Fuel Hydride	155
C-21. Relationship Between H/Zr and N_H for Fueled Zirconium Hydride	156
C-22. Induction Melting Furnace	160
C-23. Cladding Stress Cycle Annealed 18-8 Stainless Steel — 90% Probability	164
D-1. Total Core Pressure Drop vs Channel Flow Rate for Various Core Powers	172
D-2. Axial Power Distribution	172
D-3. Effect of Inlet Subcooling on Various Core Parameters	174
D-4. Effect of Reactor Clearances on Coolant Bypassing the Core . .	178
D-5. Forced Convection Nucleate Boiling Film Coefficient	182
D-6. Effect of System Pressure on Burnout Heat Flux	183
D-7. Effect of Steam Quality on Burnout Heat Flux	183
D-8. Effect of Coolant Velocity on Burnout Heat Flux	184
D-9. Effect of Hydraulic Diameter on Burnout Heat Flux	185
D-10. Predicted Burnout Performance at 1225 psia and 0.10 in.	185
D-11. Burnout Heat Flux Correlation	186
D-12. Heat Transfer Test Section Components	188
D-13. Heat Transfer Test Section Assembled	189
D-14. Columbia 12-Rod Test Section	190
D-15. Burnout Heat Flux as a Function of Exit Quality	192
D-16. Unheated Wall Burnout Heat Flux as a Function of Mean Exit Quality	196
D-17. Unheated Wall Burnout Heat Flux as a Function of Local Exit Quality	198
D-18. Experimental Heat Flux as a Function of Local Exit Quality of the Interior Channels	199
E-1. Cross Section of Reactor Through Shield Plug	207
E-2. Cross Section of Shield Plug	208

FIGURES

	Page
E-3. Fast Neutron Axial Flux Distribution for 12-Mw Thermal Power Level	210
E-4. S4PR Reactor Assembly	212
E-5. Schematic Representation of Activation Shield	216
E-6. Supplementary Shielding for Coolant Pipes	219
E-7. Heat Generation Due to Radiation Absorption in Beryllium . . .	220
E-8. Heat Generation Due to Radiation Absorption in Steel	220
E-9. Heat Generation Due to Radiation Absorption in Lead	222
E-10. Heat Generation Rates in Shield Plug for 12-Mwt/Power Level .	224
E-11. Operating Neutron Fluxes Outside SNAP 4 Activation Shield . .	225
E-12. Normalized Fast Flux Above Radial Centerline in Water Shield	226
E-13. Heat Generation in Ice Wall vs Wall Shield Thickness — SNAP 4 (12 Mwt)	228
E-14. Gamma Source of Deposited Corrosion Products on Pipe Walls	232
E-15. Dose Rate/lb of Accumulated Corrosion Products	233
F-1. Recirculation Subsystem	238
F-2. System Heat Balance Diagram	239
F-3. Steam Subsystem	242
F-4. Effect of Condenser Conditions on Cycle Efficiency	243
F-5. Feedwater and Condensate Subsystem	245
F-6. Valve Operating Subsystem	248
F-7. Decay Heat Removal Subsystem	249
F-8. Decay Heat Removal Subsystem Installed in Steam Separator . .	251
F-9. Recombiner Subsystem	252
F-10. Closed Circuit, Intermediate Cooling Loop with Final Heat Rejection from an Airblast Heat Exchanger to the Air	266
F-11. Direct Air-Cooled Condenser and Heat Exchanger	267
F-12. Closed Circuit, Intermediate Cooling Loop with Final Heat Rejection from a Cooling Tower to the Air	267

FIGURES

	Page
F-13. Open Circuit, Intermediate Fluid, Cooling Tower Loop	268
F-14. Intermediate Coolant, Natural Circulation, Heat Rejection Loop	269
F-15. Fan Performance vs Load for Various Inlet Vane Settings . . .	271
F-16. Fan Performance vs Load for Various Damper Settings	271
F-17. Cycle Efficiency vs Feedpump Turbine Exhaust Pressure . . .	290
F-18. Reactor Coolant Flow and Inlet Temperature vs Feedpump Turbine Exhaust Pressure	292
F-19. Cost Savings Due to Increased Cycle Efficiency	293
F-20. Efficiency, Reactor Power, and Flow vs Recirculation Pump Turbine Inlet Pressure for Various Feedwater Pump Turbine Inlet Pressures	294
F-31. Cost Comparisons for Nonregenerative Turbopump Systems (10 psia Exhaust on All Turbines) Based on Reference Canned Pump System	295
G-1. Schematic Diagram of S4PS Control System	302
G-2. Control Room Arrangement	318
G-3. Reactivity Step of \$1.00	322
G-4. Reactivity Core Inlet Velocity Ramp 4 ft/sec-sec	324
G-5. System Natural Response to a 20% Step in Power Demand . . .	326
H-1. Cladding Burst Conditions	333
H-2. \$0.50 Reactivity Step - Controlled	336
H-3. \$1.00 Reactivity Step - Controlled	337
H-4. \$1.00 Reactivity Step - Uncontrolled	338
H-5. Velocity Ramp - Controlled	339
H-6. Velocity Ramp - Uncontrolled	340
H-7. Power Oscillation	341
H-8. Reactor Stability - Nyquist Diagram, $Q = 1.12$	342
H-9. Reactor Stability - Nyquist Diagram, $Q = 2.154$	342
H-10. Reactor Stability - Nyquist Diagram, $Q = 3.58$	342
H-11. Startup Accident, 2¢/sec to \$1.00	344

FIGURES

	Page
H-12. Startup Accident, 2¢/sec to \$12.00	345
H-13. Startup Accident, \$2.00/sec to \$1.00	346
H-14. Startup Accident, \$2.00/sec to \$12.00	347
H-15. Loss of Coolant Accident	350

ABSTRACT

This report summarizes the technical activities conducted by Atomics International for the Atomic Energy Commission, during the period from 1960 to 1965, on the SNAP 4 Program. The overall objective of this was to develop a multipurpose, automatically controlled, transportable, compact nuclear power system. The concept consists of a uranium - zirconium hydride alloy fueled and moderated, beryllium reflector controlled, forced convection boiling water cooled, compact reactor. The reactor is directly coupled, through a steam separator, to a high-speed hermetically sealed turbine generator unit. The turbine exhausts to a compact high-pressure condenser, and small steam driven turbo-pumps return the condensate through the system to the reactor. The resulting hermetically sealed compact system may reject heat to air or water.

The SNAP 4 nuclear power system was designed to be readily transportable by air, land, rail, or water, and suitable for multipurpose applications which require power in the range of 500 to 4000 kwe. The system was designed to be capable of unattended remote operation and automatic startup and shutdown, and to be compatible with all terrestrial environments, either water or atmospheric. When the program was closed out in 1965, the SNAP 4 system had progressed through the conceptual and preliminary design stages, and work was starting on final design of a prototypic system.

BLANK

I. INTRODUCTION

This report summarizes the technical activities conducted by Atomics International for the Atomic Energy Commission during fiscal years 1960-1965 on the SNAP 4 Program. Since no work is presently being performed on the SNAP 4 system, this report provides a complete, up-to-date documentation of the AEC program status.

The overall objective of the SNAP 4 Program was to develop a multipurpose, compact, nuclear power system which incorporates a reflector-controlled, boiling-water-cooled, uranium - zirconium hydride-fueled and moderated reactor in a direct cycle saturated steam power conversion system. A compact, high-speed, 12,000- rpm turbogenerator would provide 400 cps electrical power, or the reactor-steam source could be connected to a 60-cps turbogenerator unit.

Initial activities under the program consisted of feasibility and conceptual design studies, development program planning, and the start of fuel material irradiations and fuel element fabrication process development. At mid-year FY 1962, the AEC requested that Atomics International undertake the design of a SNAP 4 system capable of demonstrating the potential of the reactor in an operating system which would utilize conventional components external to the reactor, and which could achieve operation of the reactor in the shortest possible time. An early report, NAA-SR-Memo-7307, "SNAP 4 Experimental Reactor," dated May 7, 1962, presented a summary of the reactor, fuel element, and process component redesign studies, and described the S4ER as it was conceived at that time.

Design and fuel development activities were continued through FY 1963. Upon further review, during the last half of FY 1963, it was recognized that the S4ER system could be modified to include compact, lightweight, hermetically sealed process loop components (e.g., steam separator, valves, pumps) which would be more consistent with the advanced design of the reactor and with the ultimate system requirements of air transportability, minimum site erection effort, and relocatability after extended nuclear operation. These ultimate system requirements, together with the requirement for a significant cost reduction

relative to earlier packaged power plants, indicated that a modular arrangement be selected for the SNAP 4 system.

The SNAP 4 system described in this summary report consists of a number of modules, readily interconnected in the field, and packaged within containment shells to assure against the uncontrolled release of radioactivity. As defined and described herein, the reactor system utilizes conventional heat rejection and system control and instrumentation equipment. In place of the power-generating equipment of a power plant, the system employs a throttling valve for steam flow control. In all other respects the system is identical with the prototype SNAP 4 nuclear power plant. Thus, the reactor system as described in this report is in actuality a prototype, and has, therefore, been designated the SNAP 4 Prototype Reactor System (S4PR).

When the program was terminated in 1965, the SNAP 4 system had progressed through the conceptual and preliminary design stages, and work was starting on final design of a prototypic system.

II. SUMMARY DESCRIPTION OF SNAP 4 PROTOTYPE REACTOR SYSTEM (S4PR)

A. INTRODUCTION

The analysis, development, and design activities described in the Appendix Sections of this report have provided sufficient information to permit preliminary design selection of the S4PR system and its components. The program was at a stage where final design, fabrication, assembly, and operation of a demonstration system was the next logical step. The system to be constructed would have provided a self-sufficient demonstration of the basic features and performance of the nuclear steam source, and would have allowed for ultimate demonstration of the complete power system capability.

This section is devoted to a summary physical and performance description of the system and components which would have been constructed to provide the operational demonstration. In the Appendix Sections of this report, the detailed analysis and design activities performed for the SNAP 4 system are described.

B. GENERAL

The complete SNAP 4 Power Generating system consists of a reflector controlled reactor, fueled with uranium - zirconium hydride, in a direct cycle saturated steam power conversion system. A compact, high-speed, 12,000-rpm turbogenerator would provide 400 cps electrical power, or the reactor-steam source may be connected to a 60-cps turbogenerator unit or to a marine propulsion turbine. Process loops required to support operation of the system provide the following:

- 1) Separation of the water-steam mixture from the reactor and recirculation of the high-pressure water to the reactor
- 2) Condensation of the steam after power extraction and return of the condensate to the reactor
- 3) Filtration and deionization of the process fluid to prevent buildup of corrosion products in the process system and subsequent fouling of the heat transfer surfaces, and gas handling to recombine the radiolytically dissociated H_2 and O_2 and return the water and excess H_2 to the system to eliminate the need for water makeup and maintain the desired hydrogen overpressure to minimize the radiolytic decomposition rate.

The basic system which was to be constructed would have been a complete, self-sufficient nuclear steam demonstration of the components and subsystems which would be common to all ultimate applications of the power system. This will include the reactor, the basic activation and shutdown shield, and the recirculation, condensate, purification, and gas handling process systems. In order to operate the demonstration system, conventional reactor experiment controls and ultimate heat rejection equipment would have been provided. The basic system was designed for a thermal power of 12 Mw, would contain compact lightweight valves and turbine-driven pumps, and would be hermetically sealed. Components and packaging of the prototype system would be as nearly identical to a deliverable prototype power system as possible; however, it would be necessary to provide certain special design features to gather experimental data and to make adjustments to developmental equipment. In addition, the design of the system would incorporate features to permit the adaptation of developmental power conversion, compact air-cooled heat rejection, and fully automatic control equipment concurrent with, or subsequent to, the installation of the basic reactor-steam source.

C. REACTOR

The reference SNAP 4 reactor was designed for a thermal power of 12 Mw, and a life of 12 Mwt-years. This power is generated in the form of 1200 psia, 10% quality steam. The basic fuel moderator material is a homogeneous alloy of uranium and zirconium which is hydrided to a nominal N_H of 5.35. This stable, hydrided alloy results in an extremely compact lightweight reactor with an unusually large fuel volume fraction. The compact configuration permits reflector control over a broad range of reactivity, and the solid homogeneous moderator circumvents the reactivity fluctuation problem associated with coolant density variations experienced by other boiling water reactors.

Radial and axial power flattening are accomplished by the incorporation of a nuclear poison in the fuel alloy prior to hydriding. The fuel rods are segmented within the cladding to permit axial flattening. By varying the composition of fuel and poison in two regions within the core, a peak-to-average power ratio of 1.3 can readily be obtained. Flattening the power enhances the worth of reflector control elements, permits a higher average core power density, and results in better overall fuel utilization. Burnable poisons are also used to reduce reactivity swing due to burnup to less than 2% $\Delta k/k$ over core life.

Table II-I summarizes the physical and operating characteristics of the reactor, and Figures II-1 through II-5 show the general arrangement of the reactor and associated process equipment for this basic system.

1. Reactor Core

The reactor core occupies a volume of approximately 1 ft³ and has a length-to-diameter ratio of 1.5. The core contains 583 stainless-steel clad, 0.440-in. OD, cylindrical fuel rods with 17 in. of active length. Each element contains four slugs of hydrided fuel, 0.408 in. in diameter; and the cladding is thin-wall (0.015 in.) Type 347 stainless-steel tubing. The fuel material is described in AI fuel element specification NES4ER1-18-001 which was released in June 1962. Thermal bonding of the fuel to the cladding is obtained by maintaining an interference (shrink) fit throughout the operating cycle. Tubular end plugs are welded to the cladding at each end of the element, and portions of the tubular walls are machined away to allow coolant to flow through these openings and out the end of the tubular element. Minimum spacing of 0.022 in. between adjacent fuel elements is maintained under operating conditions by spirally wrapped wires on alternate fuel elements. The fuel element assembly is shown in Figure II-6, and the fabrication and assembly process is described in AI specification NA0622-003.

The top tubular end plugs are expanded into holes in the top grid plate and lock the fuel elements in place. The bottom end plugs are free to expand axially into the lower grid plate. The core is subdivided into seven fuel element clusters; a central hexagonal cluster containing 127 fuel elements, and six outer, trapezoidal-shaped clusters each containing 76 fuel elements. The central cluster contains 37 fuel elements which are replaceable and may be used for criticality shimming. The central cluster is positioned and supported structurally by the outer clusters, and the outer clusters are located and supported by the control drum bearing ring. The lower grid plate of each cluster is secured to the ends of widely spaced fuel elements. This construction permits independent thermal expansion of each fuel element during reactor operation, and permits removal and replacement of individual fuel element clusters.

The core structure unit includes fuel elements and grid plates, control drums and their associated hardware, snubbing devices, and reflector cusps and filler pieces around the control drums. The entire unit is encased in a

TABLE II-I
CHARACTERISTICS OF THE S4PR
(Sheet 1 of 3)

Operating power	12 Mwt
Core life	12 Mwt (years)
Core volume	1.07 ft ³
<u>Core Configuration</u>	
Effective diameter	11.77 in. (with cusps)
Active length	17.0 in.
Number of fuel rods	583
Fuel diameter	0.410 in.
Cladding thickness	0.015 in.
Fuel element diameter	0.440 in.
Pitch of triangular lattice	0.462 in.
Rod separation (at closest approach)	0.022 in.
<u>Core Composition</u>	
Fuel volume fraction	0.714
Coolant volume fraction	0.180
Cladding volume fraction	0.106
U ²³⁵ loading (fully enriched)	20.7 kg
Zirconium loading	112. kg
Burnable poison - hafnium	4.8 kg
- europium oxide	160 gm
Hydrogen density in fuel	5.35×10^{22} atoms/cc fuel
Hydrogen density in cold, flooded core	5.02×10^{22} atoms/cc core
Average hydrogen density in hot, full-power core	4.42×10^{22} atoms/cc core
Burnup (peak)	2 metal at. %
<u>Reflector Composition</u>	
Axial - stainless steel	1 in.
water - bottom plenum	10 in.
water-steam - top plenum	4 in.
Radial - control drum diameter	3.95 in.
thickness of beryllium	2.80 in.
thickness of poison zone	1.15 in.
cusp and filler material	nickel

TABLE II-I
CHARACTERISTICS OF THE S4PR
(Sheet 2 of 3)

<u>Operating Conditions</u>	
Coolant flow rate	4.1×10^5 lb/hr
Coolant inlet pressure	1235 psia
Coolant outlet pressure	1212 psia
Coolant inlet temperature	540 °F
Coolant outlet temperature	570 °F
Recirculation ratio	10:1 (10% exit quality)
Average cladding temperature	667 °F
Average fuel temperature	879 °F
Maximum fuel temperature	1056 °F
Heat transfer area	95 ft ²
Average heat flux	431,000 Btu/hr-ft ²
Maximum heat flux	540,000 Btu/hr-ft ²
Burnout safety factor (minimum)	3
Average power density in fuel	15.7 Mw/ft ³
Coolant mass velocity	2.85×10^6 lb/hr-ft ²
Coolant passage hydraulic diameter	0.0937 in.
Reactor pressure drop	22.8 psi
Reactor pumping power	21.7 water HP
<u>Nuclear Control Requirements</u>	
	<u>% $\Delta k/k$</u>
Minimum shutdown margin	2.0
Transition from cold to hot-and-boiling (core N _H change)	4.81
Transition from cold to hot-and-boiling (axial reflector)	0.50
Doppler effect (core resonance absorption)	0.57
Fuel element elongation	0.16
Grid plate expansion	0.12
Thermal flux spectrum temperature shift	0.04
Fuel element cell effect	negligible
Hydrogen loss from fuel	negligible
Equilibrium xenon poisoning (midlife)	2.0
Burnup control requirement	1.4
Total	11.60

TABLE II-I
CHARACTERISTICS OF THE S4PR
(Sheet 3 of 3)

Nuclear control availability (12 reflector drums)		12.50 - cold 13.2 - hot
<u>Coefficients of Reactivity (Reactor Operating Conditions)</u>		
	<u>$\Delta k/k - ^\circ F$</u>	<u>$\phi / ^\circ F$</u>
Fuel elongation coefficient	-0.20×10^{-5}	-0.025
Core coolant expulsion coefficient	-0.57×10^{-5}	-0.071
Neutron spectral coefficient	-0.03×10^{-5}	-0.004
Doppler coefficient	-0.54×10^{-5}	-0.069
Net Prompt Coefficient	-1.3×10^{-5}	-0.17
Lower grid plate coefficient	-0.055×10^{-5}	-0.007
Upper grid plate coefficient	-0.133×10^{-5}	-0.017
Power coefficient	$-0.0044 \Delta k/k - \text{Mwt}$	-55 ϕ / Mwt
Steam void coefficient	$-0.165 \Delta k/k$ per fractional void change	
Core hydrogen concentration coefficient (N_H in terms of $10^{24}H$ atoms/cc)	$+9.0 \Delta k/k - \Delta N_H$

6531-5014

Figure II -1. Reactor Vessel Cross Section

5-7-63

7564-0218

Figure II-2. Nuclear Steam Generator and Container Assembly, Module No. 1

5-7-63

7564-0219

Figure II-3. Water Purification Equipment, Module No. 2

5-7-63

7564-0220

Figure II-4. Power Conversion Equipment, Module No. 3

5-8-63

Figure II-5. Plant Arrangement

7564-0221

6-4-63

Figure II-6. Fuel Element Assembly

7564-0241

cylindrical plate of 5/8-in. -thick, Type 304 stainless steel, and bearing rings are fastened to each end of this cylinder to provide the necessary support and rigidity required. The design permits replacement of the entire core and reflector assembly, replacement of the core only, or replacement of a single fuel element cluster. The design features of the core structure unit are shown in Figures II-7 through II-10.

2. Reflector Assembly and Control Mechanism

Twelve control drums are arrayed around the reactor core in a circular pattern. Each drum is approximately 4 in. in diameter and 17-3/8 in. long. Beryllium reflector material fills two-thirds of the drum cross section, and the remaining one-third is filled with hafnium hydride nuclear poison material. The drum is shrink-fit clad with a nickel sheath, and end caps are welded to the cladding and to the drive shafts which extend axially from the ends of the drums. The drum shafts are flanged and fastened to the beryllium sector by bolts and dowels.

The upper drum shaft has a mechanical sleeve-type coupling to minimize radial movement of the mating shaft from the motor drive unit. This coupling allows relative axial movement between the drum shaft and the motor shaft and is required to accommodate component elongation due to thermal expansion. The upper and lower drum shafts are supported by pivot bearings, which are in turn supported by the upper and lower bearing rings of the core structure assembly.

To complete the reflector assembly, stationary cusp-shaped nickel reflector segments are located between and outboard of the drums, inside the core structure cylinder. The 12 cusps are secured between the upper and lower bearing rings by fasteners and dowel pins, thus assuring positive clearance between the cusps and the control drums.

A shaft which extends through the shield plug engages the upper drum shaft through the sleeve-type coupling. This shaft connects the drive mechanism to each of the drums. One control drive mechanism is provided for each drum. The 12 control drum drive mechanisms are mounted on a single base plate in the space provided between the shield plug and the reactor vessel cover. Each control drive mechanism consists of the following components: a scram spring, a scram clutch, a drive motor with gearing, a drum position sensing device, and their associated power and instrumentation wiring.

Figure II-7. Support - Core Structure

BLANK

BLANK

BLANK

The scram spring provides rapid rotation of the control drum to the shutdown position. The spring is a flat coil spring with the inner end attached to the drive shaft and the outer end attached to the control drive mechanism housing. The spring is preloaded to assure sufficient positive torque to move the drum to the least reactive position. An electromagnetic clutch is provided to engage the drive mechanism to the drive shaft. When shutdown is required or loss of power occurs, the holding magnet is de-energized and the clutch is released. In connection with the scram mechanism, a snubber is provided on the underside of the lower bearing support ring. This snubber, essentially a dashpot, consists of a vane attached to the drum shaft and a cavity below the lower pivot bearing. As the scram spring rotates the drum, the vane displaces water trapped in the cavity. Energy is dissipated by performing work on the trapped water in forcing it through the clearance space around the snubber vane.

The input end of the scram clutch is coupled to the output shaft from the drive motor gear box. Selection of the drive motor and gear box is based upon the need to overcome the windup torque of the scram spring and to provide controlled rate of rotation of 1/20 rpm to the control drum. A stepper motor, with integral planetary gearing, has been selected. With a 60-cps input, the motor shaft speed is 72 rpm; and the gear box reduction of 1400:1 provides the required 1/20 rpm output. Shaft torque is approximately 50 in.-lb at the gear box output or less than 1/2 in.-oz at the motor shaft, neglecting gear box friction.

Indication of drum position is from a linear variable differential voltage transformer coupled to the control drive mechanism output shafts, below the scram clutch. Rotation of the shaft moves the position sensor in direct proportion to drum position.

The entire control drive mechanism is located within the reactor pressure vessel, and is immersed in 200°F water at 1200 psia. The electrical power and instrumentation leads are brought out of the pressure vessel through a high-pressure sealed conductor disconnect plug. The design layout of the control mechanism is shown in Figure II-11, and is described in AI specification NC4-11-001 and NAA-SR-TDR-7723.

3. Reactor Vessel and Shield Plug

The reactor vessel is a vertically arranged, conventional pressure vessel of Type 347 stainless steel, and is of welded construction consisting of a

BLANK

Figure II-11. Installation Assembly Control Mechanism

BLANK

cylindrical shell, an elliptically dished bottom head, and a bolting flange at the upper end. The vessel is 6-1/2 ft long by 25 in. OD, and is supported at the bottom by a cylindrical skirt which is welded to the vessel body. The lower edge of the skirt is fitted with a flange plate to provide bearing area and space for anchor bolts. Top closure of the vessel is provided by a bolted, spherically dished, top head. The vessel is designed for an internal pressure of 1400 psia and a temperature of 600°F.

Inside the vessel is fitted a liner which serves as both the thermal (neutron absorbing) shield and the reactor core support member. This liner extends from a point near the bottom of the reactor vessel to an elevation just above the coolant nozzle, and is attached around the top by welding to the reactor vessel. Four bosses welded to the outside of the liner near the bottom provide for proper spacing and centering of the liner within the vessel, and permit differential thermal expansion between the liner and the vessel. A ring extends inward near the bottom edge of the liner to serve as the core structure support. The annular space between the liner and the reactor vessel provides a down-comer passage for reactor inlet coolant.

The vessel is fitted with a single nozzle, located on the side about two-thirds of the vessel height up from the bottom of the vessel. This nozzle provides both the inlet coolant passage and the outlet steam-water mixture passage. The arrangement is that of two concentric passages with the inner, circular passage carrying the steam-water mixture, and the outer, annular passage carrying the inlet coolant. The inner pipe penetrates the vessel liner and is attached by welding. The two concentric pipes are divided into separate pipes just outside the vessel activation shield, for connection into appropriate process piping.

A shield plug is provided inside the vessel liner, above the core, of sufficient thickness to limit activation of structure and equipment to that required for maintenance access in the chamber provided to house the control drive mechanisms. A layer of thermal insulation is provided at the top surface of the shield plug to thermally isolate the equipment chamber. Steel sleeves penetrate the shield plug and serve as conduits for the reactor control drum activating shafts. At the bottom of the shield plug, a streamlined reentrant steam-water flow passage provides for relatively unimpeded flow of the steam-water mixture from the upper plenum to an eccentric collecting passage.

The top cover for the reactor vessel consists of a bolting flange, a ring section from a spherically dished plate, and a center conduit port. The port provides for the passage of a conduit for electrical and instrumentation conductors to the control drive mechanisms. The conductors terminate at the upper end of the conduit in high-pressure, sealed, multipin disconnect plugs. The port cover is underneath the reactor vessel cover, and is bolted to the cover to permit removal of the reactor cover without disturbing the electrical conductors.

The reactor vessel is thermally insulated at the bottom and sides by a layer of fibrous insulating material. The cover plate is not insulated, since its temperature is essentially that of the surrounding shield water.

The reactor vessel layout is shown in Figure II-1, and is described in AI specification NC4-12-001.

4. Reactor Shield

The design of the reactor shield for operational systems will be highly dependent upon the maintenance and access requirements of the specific application. Any application would have as a minimum requirement that the area surrounding the reactor vessel be accessible with the core removed, and that the surrounding structure and components not become activated to the point that contact maintenance access would be denied. These two criteria have been selected as the shielding criteria for the SNAP 4 Prototype System.

The first criterion is met by a layer of lead immediately outside the reactor vessel thermal insulation. The thickness of the lead is proportioned to match the amount of gamma activity in the vessel wall and in other vessel and reactor components. This layer of lead is placed under the support skirt and bearing plate, and extends upward around the reactor vessel to the bolting flange.

The second criterion is met by the water contained within the aluminum shield water and containment vessel. The shield water vessel is an 8-ft diameter cylinder with two flanged and dished pressure vessel heads. The shield water will provide a means of quenching any steam released as a result of reactor vessel or piping system failure, and the thickness of the shield water vessel is designed to contain the resultant pressure increase from such a failure.

The combined lead and water shield will permit access by maintenance and operating personnel to the area outside the shield water vessel with the reactor shutdown. This area will be inaccessible with the reactor operating, and concrete biological shielding will be provided in the facility to bring dose rates in operating areas to permissible levels.

The reactor vessel and lead shield are located and secured within the shield water vessel by an arrangement of two concentric cylinders which extend upward from the bottom of the shield water vessel. The inner cylinder mates with the reactor vessel support skirt bearing plate, and the outer cylinder mates at the mean diameter of the lead shielding.

The arrangement of shielding is shown in Figures II-1 and II-2. This arrangement provides the simplest possible shielding fabrication, yet allows the conduct of shielding experiments with the operating SNAP 4 reactor to obtain design information for subsequent operational shields.

D. PROCESS SYSTEM

The process system for the basic SNAP 4 Prototype System is composed of all the fluid (liquid and gas) subsystems which are required to support the safe operation of the reactor heat source. The primary functions of the process system are to: (1) dissipate the power generated by the reactor, and (2) control the process fluid chemistry. Figure II-12 is a preliminary piping diagram and heat balance for the system. The entire process system is hermetically sealed to prevent contamination of the environment and eliminate the need for external gas and liquid waste handling and water makeup equipment. The subsystems which make up the process system are as follows:

- 1) Recirculation Subsystem: Steam separator and recirculation pump
- 2) Steam Subsystem: Condenser, auxiliary (pump) turbines, steam flow control station, steam surge volumes, and steam dumping line
- 3) Feedwater and Condensate Subsystem: Feedwater and condensate pump, first and second regenerative heat exchangers, nonregenerative heat exchangers, filter, and deionizer
- 4) Recombiner Subsystem: Recombiner, eductor, and mixing chamber
- 5) Valve Operating Subsystem

Figure II - 12. System Heat Balance Diagram

- 6) Pressure Relief Subsystem
- 7) Poison Injection Subsystem
- 8) Reactor Decay Heat Removal Subsystem.

1. Steam Separator

At design thermal power of 12 Mw, the reactor coolant will leave the reactor at a flow rate of 410,000 lb/hr in the form of a steam-water mixture consisting of 10 wt % steam and 90 wt % water. A steam separator is provided to separate this mixture and provide dry saturated steam to the power and auxiliary turbines, and to return the high-pressure water to the reactor. The requirement to remove at least 99% of the vapor from the liquid is as necessary as is the requirement to remove at least 99% of the liquid from the vapor, to prevent cavitation-erosion of the recirculation pump impeller. Two stages of separation are required to meet these requirements, and may be a combination of gravity-mechanical or mechanical-mechanical. A conventionally designed, commercially available unit of the type shown in Figures II-13 and II-14 will meet the performance requirements for this unit, as described in AI specification NC4-86-001. Although the performance requirements for the steam separator can be met by such a unit, the physical size, 2 ft OD by 7 ft, and weight, 3000 to 4000 lb of this conventional component are incompatible with the overall objectives of minimum size and weight. A design and engineering test program was to be carried out by AI and a selected steam separator supplier to produce a suitable unit with a more compact, lightweight, physical arrangement.

2. Recirculation Pump

The recirculation pump provides the means by which the high-pressure water is removed from the steam separator and is returned to the reactor. The condensate being returned to the reactor from the condensate and feedwater pump joins the recirculation stream ahead of the recirculation pump to provide subcooling and prevent cavitation; thus, the recirculation pump must handle the entire reactor flow rate of 410,000 lb/hr. The head developed by the recirculation pump must equal the pressure drop in the recirculation loop, or 45 psi at full flow.

The pump will be a single stage axial flow type impeller operating at 9800 rpm. The impeller diameter will be approximately 4-1/2 in. The driving unit will be a partial admission, single-stage, steam turbine of the Terry type

6531-5439

Figure II-13. Steam Separator (conventional), Plan View

Figure II-14. Steam Separator (Conventional)

or conventional axial flow velocity compounded. The turbine wheel diameter will be approximately 6 in. in diameter. Steam flow rate to the turbine will be approximately 2600 lb/hr.

The combined turbine and pump will be mounted on water-lubricated bearings, and the complete unit will be hermetically sealed. The overall envelope for this turbine-driven recirculation pump will be 8 in. by 8 in. by 14 in., and the unit will weigh approximately 200 lb. The recirculation pump is described in AI specification NC4-81-001; and although no drawings are available for this unit, its construction would be similar to the condensate and feedwater pump shown in Figures II-15 and II-16.

3. Condenser

The condenser described in AI specification NC4-86-002, will receive 41,000 lb/hr of steam from the reactor at full power. The actual flow rate to the condenser will be controlled by a steam flow control valve located between the steam separator and the condenser. The function of the condenser is to condense the steam to a saturated liquid, thereby dissipating the reactor heat. The condensed steam will be slightly subcooled, 7 to 10°F, and collected in the condenser hotwell. The hotwell serves as a storage and surge volume for the condensate and feedwater system.

For experimental reasons, the condenser will be designed to condense the specified amount of steam at condensing pressures varying between 1 and 25 psia. The envelope for the condenser will be approximately 4 ft in diameter by 12 ft overall length, and will be designed and fabricated in accordance with conventional nuclear practices. The tube bank will consist of approximately 1650 5/8-in. OD stainless-steel tubes arranged in a U-tube configuration. The unit will be of all-welded construction. Steam will be condensed on the outside of the tubes, and an inlet transition section and baffles will be provided to reduce steam entrance velocity to prevent excessive tube erosion. Provision will be made for removal of noncondensable gases during operation to prevent gas blanketing of the heat-transfer surfaces.

In addition to the normal steam inlet to the condenser from the steam flow control station, all high-pressure relief systems will discharge to the condenser, and the condenser is designed to condense the resulting vapor and contain the associated minor pressure surge.

REVISIONS			
SYM	DESCRIPTION	DATE	APPROVED
1	MAY BE REWORKED		
2	CANNOT BE REWORKED		
3	RECORD CHANGE		
4	NOW SHOP PRACTICE		
5	PARTS MADE OK		

HEAT TREAT	UNLESS OTHERWISE SPECIFIED, DIMENSIONS ARE IN INCHES AND APPLY PRIOR TO FINISH.	DR BY	ROCKETDYNE A DIVISION OF NORTH AMERICAN AVIATION, INC. CANOGA PARK, CALIFORNIA • MCGREGOR, TEXAS
FINISH	125 MACH. SURF. ROUGHNESS TOLERANCES ON DECIMALS ANGLES XX = ± .03 XXX = ± .010 ANGLES ± 30° HOLES NOTED "DRILL": 013 THRU 040 + .001 - .001 041 THRU 130 + .002 - .001 131 THRU 229 + .003 - .001 230 THRU 500 + .004 - .001 501 THRU 750 + .005 - .001 751 THRU 1000 + .007 - .001 1001 THRU 2000 + .010 - .001	CHK BY H.J. Rejcek DES 46-63 H. ROSCHAK MAT DEV	CONDENSATE AND FEED WATER TURBO-PUMP ASSEMBLY (PRELIMINARY LAYOUT)
MAT'L	DO NOT SCALE PRINT		CODE IDENT NO 02602 SIZE D SCALE FULL
			SHEET

Figure II-15. Condensate and Feed Water Turbopump Assembly (Preliminary layout)

BLANK

TURBINE DRIVEN FEED & CONDENSATE PUMP			
WORTHINGTON CORPORATION ADVANCED PRODUCTS DIVISION NEWARK, NEW JERSEY			
ORDER	DATE 3-22-63	SCALE 6	INCHES TO FOOT
<i>J. J. Kelly</i>			P-1541-1

4-30-63

7564-0177

Figure II-16. Turbine Driven Feed and Condensate Pump

4. Feedwater and Condensate Pump

The condensate and feedwater pumps are mounted on a common shaft and driven by a single turbine driver. The condensate stage will be a 2-in.-diameter centrifugal flow impeller which will handle 46,000 lb/hr of condensate and increase the pressure from that of the condenser to 125 psia. After leaving the condensate pump stage, the condensate passes through the water purification equipment and returns to the inlet of the feedwater pump. A small quantity, approximately 2500 lb/hr, of the condensate pump discharge is recirculated to an eductor which removes noncondensibles from the main condenser, and the fluid then returns to the condensate pump section. Additional condensate recirculation may have to be provided to operate a jet-boost pump to suppress cavitation, or a mechanical inducer may be provided.

The feedwater pump will consist of four equal stages of 3-in.-diameter centrifugal flow impellers, and will operate at 18,000 rpm. The feedwater pump receives the condensate stream after it has passed through the water purification equipment, and raises the pressure to the 1220 psia required to inject the fluid into the recirculation stream.

The turbine driver will be mounted between the two pumps, and will be the same turbine wheel as used on the recirculation pump. Although still operating as a partial admission turbine, the feedwater and condensate pump turbine will have four inlet nozzles as contrasted with a single nozzle for the recirculation pump turbine.

The turbine and pumps will be mounted on water-lubricated bearings, and the complete unit will be hermetically sealed. The overall envelope for this turbine driven feedwater and condensate pump will be 8 by 8 by 24 in., and the unit will weigh approximately 250 lb. The feedwater and condensate pump is described in AI Specification NC4-81-002, and Figures II-15 and -16 show units which have been proposed for this service but which do not meet present overall efficiency criteria.

5. Regenerative and Nonregenerative Heat Exchangers

The water purification subsystem requires one nonregenerative and two regenerative heat exchangers to reduce the condensate temperature to one that will not induce resin damage in the demineralizer, and at the same time to minimize the amount of waste heat which must be rejected from the cycle.

The arrangement of these heat exchangers in the flow cycle is such that condensate from the condenser hotwell passes through the tube side of the first regenerative exchanger where its temperature is dropped to 140°F, and then enters the suction of the condensate pump. A small side stream, approximately 2000 lb/hr, is drawn from the recirculation loop and passed through the tube side of the second regenerative exchanger, where its temperature is reduced from 568 to 240°F. After leaving the second regenerative exchanger, the pressure of the fluid is reduced and the stream mixes with the condensate pump discharge before the combined stream passes through the tube side of the non-regenerative heat exchanger. The temperature of the fluid is reduced to 130°F in the nonregenerative exchanger, and the fluid then passes through the demineralizer and guard filter, the shell side of the first and second regenerative exchangers and enters the feedwater pump suction at 240°F.

The three heat exchangers have many common design features and are therefore discussed together. These units will be of relatively conventional nuclear design and construction. They will be fabricated of welded, stainless steel, and will utilize 3/8-in. or 1/2-in. tubing in a baffled, counterflow, U-tube configuration. The design pressure for the shell and tube sides of the first regenerative exchanger, the shell and tube sides at the nonregenerative exchanger, and the shell side of the second regenerative exchanger will be 150 psia. The design pressure for the tube side of the second regenerative exchanger will be 1500 psia. The overall envelopes and weights of these units will be as follows:

- a) First regenerative: 3 ft OD by 10 ft OAL by 1850 lb,
- b) Second regenerative: 6 in. OD by 6 ft OAL by 300 lb,
- c) Nonregenerative: 12 in. OD by 10 ft OAL by 500 lb.

In addition to these heat exchangers, which are associated with the water purification subsystem, there will be an additional regenerative-type heat exchanger which will condense the pump turbine exhaust steam, and return the heat energy and fluid to the process stream. This exchanger will be of the "Helicoil" type which is a compact shell and tube exchanger in which the tubing is arranged in a coil between header pipes rather than the conventional straight or U-tube configuration. The unit will be fabricated of stainless steel, and will be all-welded construction. The turbine exhaust steam from both the turbine

driven pumps will be condensed in the shell of the unit at 330 psia and subcooled to approximately 250°F. The condensed and subcooled steam is then dumped to the condenser hotwell. The discharge stream from the feedwater pump passes through the tubes of this regenerative exchanger, and is heated from 240°F to approximately 420°F. The design pressure for this heat exchanger will be 1500 psia, and the unit which weighs 1200 lb will have an envelope of 2 ft OD by 5-1/2 ft OAL.

The regenerative and the nonregenerative heat exchangers are described in AI specification NC4-82-001.

6. Deionizer

To prevent fouling of heat-transfer surfaces, particularly in the reactor, it has been established that the process water must be maintained within the following limits:

Oxygen	0.4 ppm (max)
pH	7.0 ± 2
Corrosion Product Concentration	10 ppb (max)
Conductivity	10 megohm-cm
Additives	None

To maintain the required pH, corrosion product level, and conductivity, a mixed-bed deionizing unit of nuclear grade, strongly acid cation resin in the hydrogen form and a nuclear grade, strongly basic anion resin in the hydroxyl form will be used. Approximately 7 ft³ of resin will be required to provide the exchange capacity to remove the corrosion products generated in the system in one year of operation. Flow loading of 50 gpm/ft² will permit packaging this amount of resin in a cylinder approximately 2 ft OC by 3 ft OAL, and the complete deionizer unit will be approximately 2 ft OD by 5 ft OAL weighing 350 lb. The deionizer casing will be stainless steel. Resin retention elements will be provided to hold all resin fines within the unit. A guard filter will be installed downstream of the deionizer, to protect the system in the event of failure of the downstream retention element. Appropriate valving will be provided to allow charging and discharging of the resin by a sluicing technique. A permanently installed 2-1/2-in. -thick lead shield will be provided around the deionizer to permit maintenance access in the immediately adjacent area.

The deionizer is described in AI Specification NC4-85-001, and Figure II-17 is an illustration of a suitable unit. This unit differs from the unit to be used in subsequent systems, which will be of the disposable cartridge type. The guard filter is described in AI Specification NC4-84-001.

7. Recombiner

An estimated 4.5 lb per day of hydrogen and oxygen will be produced in the system due to radiolytic decomposition of the reactor coolant. To eliminate the need for off-gas handling and a makeup feed system, a catalytic recombiner will be provided, which will continuously recombine the hydrogen and oxygen and reinject the resultant water and excess hydrogen back into the system.

The catalytic recombiner will consist of a cylindrical chamber approximately 3 in OD by 12 in. OAL filled with platinum coated alumina pellets. The pellets will be held in place by screens at each end of the chamber. Since the catalyst will not function when wet, a small cartridge type electrical heater will be provided in the catalyst bed near the inlet.

The recombiner, which is similar to those which have been used in the AI water boiler reactors, is described in AI Specification NCA-85-002.

8. Valve Operating Subsystem

The valve operating subsystem provides the means by which process system valves are positioned in response to control and system flow signals. A typical process valve control and operating loop will consist of a controller which electrically compares the desired flow to the measured flow and generates an appropriate error or null signal which will control the position of a piston in a hydraulic servo valve. A supply of 800 psia water, taken from the high pressure feedwater pump, serves as the actuating fluid. The position of the servo valve determines to which side of the process valve actuating piston the actuating fluid will flow. The servo valve also allows water from the opposite side of the actuating piston to be bled to the condenser hotwell, as it is displaced by actuating piston motion. The flow capacity of each servo valve and valve actuator will be determined by the size and response characteristics of the valve to be controlled. Flow control valves and servo valves are described in AI Specifications NC4-34-001, NC4-34-002, NC4-34-003, and NC4-34-004.

Figure II-17. Deionizer Unit

An emergency and/or startup mode of operation of the valve actuating system will be provided by the inclusion of a water reservoir which can be pressurized by means of a high-pressure gas accumulator (bottle). This system will have sufficient capacity to allow initial valve operation for startup to put the recirculation loop in a condition for self pressurization.

9. Pressure Relief Subsystem

A pressure relief system is provided to insure that the system pressure can not exceed the design pressure, and thereby create a potentially hazardous situation. The pressure relief system consists of two, main, self-actuating, bellows-sealed, adjustable set point and blowdown control valves. One of these valves is installed in parallel with the main steam flow control station and will be actuated if steam pressure rises to 1300 psia. The valve capacity will be equal to 120% of steam system design flow, and will discharge directly to the main condenser. The second valve is connected to the steam separator, and will actuate if separator pressure rises to the design pressure of 1400 psia. This valve also will be designed to handle a flow rate which corresponds to the maximum possible system transient flow rate, and will also discharge to the main condenser. Actuation of the steam separator pressure relief valve will also provide a power level scram input signal to the reactor control system.

The pressure relief valves will be constructed of stainless steel and the valve seats and discs will be hard-surfaced to prevent wiredrawing or damage due to "valve chatter."

10. Poison Injection Subsystem

Due to the experimental/developmental nature of the S4PR prototype system, a third safety has been provided in the form of a nuclear poison injection system. This system will inject a concentrated borate solution into the reactor inlet if other means of reactor shutdown have failed or malfunctioned.

Borated water is contained in a pressure vessel and is maintained at a pressure of 1300 psia by means of a hydrogen gas overpressure. The hydrogen gas pressure is supplied from a bottled supply of gas, and pressure is controlled in the borated water tank by a pressure regulating valve. Two shutoff-isolation valves are installed in the piping between the water tank and the reactor inlet. A drain line is provided between these two valves to prevent leakage of the poison solution into the reactor inlet. When the system is actuated, the drain line

isolation valves are closed, and the shutoff-isolation valves open allowing the gas overpressure to inject the solution into the reactor. A check valve is provided in the water tank outlet to prevent reverse flow as the tank is emptied.

The poison injection system is recharged by venting the pressure, filling with additional borate solution, and then re-pressurizing with the hydrogen cover gas. The poison injection system will be designed for a pressure of 1750 psia, and a pressure relief valve will be provided to prevent the pressure from exceeding 1500 psia.

11. Decay Heat Removal Subsystem

A simple decay heat removal system is located within the water shield, and utilizes the shield water as a heat sink. The heat exchanger consists of a length of finned tubing which is connected between the inlet and outlet reactor piping. A check valve on the reactor inlet piping side of the exchanger prevents flow through the decay heat exchanger during normal system operation, but opens and permits flow through the decay heat exchanger whenever reactor inlet pressure is equal to the outlet pressure. The arrangement of the decay heat exchanger system is also shown in Figure II-2.

E. CONTROL AND INSTRUMENTATION SYSTEM

The complexity of control and instrumentation components and subsystem is held to a level which is dictated only by the safety and operational needs of the reactor-steam system. This simplicity lends itself advantageously to eventual programmed plant startup and shutdown and to adaptation of a fully automatic, self diagnostic control system.

1. Control System

The S4PR control system will be a conventional experimental type of system and will require attendance during system operation. Manual operations will be required for startup and shutdown, setting output power level, diagnosing system behavior, and correcting operational irregularities. Automatic control is provided for normal operation about the desired operational power level. Automatic scram and setback will be provided as well as operator manual override of these automatic functions.

The plant control system will consist of a number of subsystem or single control loops, some of which are mutually coupled. Each loop operates by

comparing a reference signal to a signal derived from the controlled variable. The differences between the measured and reference signals generates an error signal which actuates a controller to reduce the error to zero. In some loops, rate of change of the error signal will be detected and provided as an additional control signal to enhance stability. The control system, shown schematically in Appendix G (Figure G-1), consists of the following subsystems:

- a) Neutron Flux Control
- b) Pressure Control
- c) Recirculation Flow Control
- d) Feedwater Flow Control
- e) Steam Separator Liquid Level Control
- f) Condenser Heat Removal and Pressure Control.

2. Nuclear Instrumentation System

The nuclear instrumentation system (Figure II -18) provides reactor neutron flux level and period information to the plant protective system, the plant control system, and the control room. This system monitors reactor flux through seven detector channels which cover at least 10 decades of power in three overlapping ranges. The startup (source) range detection system consists of two channels employing fission chambers, the intermediate range detection system consists of two channels employing compensated ion chambers; and the power range detection system consists of three channels employing uncompensated ion chambers.

All detectors will be housed in waterproof containers located in the shield water below the reactor. The detectors will be of standard type units which comply with military specifications for shock and vibration, MIL-S-901 and MIL-STD-167 (Type I).

The nuclear signal conditioning and power supply equipment for these detectors will be standard, commercially available equipment using solid-state components in a "pull-out" modular type of construction. The startup range signal conditioning equipment contains a linear pulse amplifier with two outputs, one for readout instrumentation and the other for the plant control and plant protective systems. The intermediate and power range signal conditioning amplifier outputs connect to the plant control and plant protective systems.

Recording instrumentation consists of the following:

- a) Log countrate recorder to record either startup channel
- b) Long N recorder to record either intermediate range channel
- c) Power level recorder to record any one of the three power range channels.

3. Plant Protective System

The plant protective system protects the reactor and system from damage or functions to minimize the extent of damage during all phases of plant operation. The plant protective system, illustrated in Figure II-19, consists of startup interlocks, alarms, automatic reactor setbacks or scrams, activation of the decay heat removal loop, and poison injection into the core. The plant protective system is completely separate from the normal plant control and instrumentation systems except for their common use of nuclear detectors, amplifiers, and control drum actuators.

Reactor scram is activated by any of the following:

- a) Excessive period (reactor)
- b) Excessive power level (reactor)
- c) High core temperature
- d) High reactor outlet steam pressure
- e) Low reactor inlet pressure
- f) Low separator water level
- g) Low flow to condenser
- h) Low reactor shield water level
- i) Low reactor inlet flow
- j) Manual.

4. Radiation Monitoring Systems

The radiation monitoring system, shown in Figure II-20, is provided to protect plant personnel and equipment from excessive radiation and to provide information required for the health physics services.

Figure II-19. Block Diagram-Plant Protection System

Figure II-20. Block Diagram-Radiation Monitoring System

Standard dosimeters, film badges, sealers, hand and foot counters, and portable survey meters will be utilized. In addition, the following installed systems will be provided:

- a) Area Radiation Monitors at ten locations throughout the test facility, with a common power supply and recorder located in the control room. Any off-normal radiation level will be announced in the control room by an annunciation and alarm light system.
- b) Stack Monitor – Gaseous effluent from the system operating area will be drawn off the main exhaust and monitored in a sampling system. Any excessive radiation level will actuate a control room alarm system.
- c) Process Fluid Monitoring – Samples of reactor coolant, and water entering and leaving the purification equipment will be monitored for fission product activity level. The presence of abnormal levels of fission product activity in the reactor coolant will be a direct indication of fuel element failure or excessive deterioration, and will require a shutdown/scram decision by the operating crew. Activity level will be recorded in the control room at all times, and abnormal levels will activate an annunciator alarm.

5. Process Control Monitoring and Diagnostic Instrumentation Components

One standard type of variable reluctance type of pressure transducer has been selected for all pressure transmission including differential pressure head, level, and flow measurements. This type pressure transducer is low in weight, highly reliable, and long lived. Each transducer, complete with connections and wiring, weighs approximately 1 lb.

Flow-tube type elements will be used for all flow measurements except for extremely low flows. These units are highly accurate, have a low permanent head loss, and permit shorter straight run piping. Extremely low flows will be measured by means of precision small-bore orifice elements. The differential pressure produced by the flow tubes or orifices will be sensed and transmitted by the differential, variable reluctance pressure transducers.

All temperature sensing elements will be stainless-steel-sheathed, M_gO insulated, chromel-constantan thermocouples. Thermocouples for the

measurement of fluid temperatures will be protected by immersion wells, constructed of the same material as the piping or vessel into which it is installed.

Recording receivers will be all electronic, miniature, 4-in. -strip-chart types, with 6-in. by 6-in. face dimensions.

5-3-63

Figure A-1. SNAP 4 Reactor

7564-0161A

APPENDIX A. REACTOR DESIGN

The reactor (Figure A-1) includes fuel, supporting structures for the fuel, a radial thermal shield, a shield plug which limits the radiation (activation) dose rate to the control drive mechanism, the pressure vessel, lead shutdown shielding, reflector control drums and drum drive mechanism, the vessel closure cover, and vessel support structure. The reactor assembly is installed in a cylindrical shield water vessel, which also serves as a containment shell.

A. CORE DESIGN

The objective of the core support design is to provide the alternatives of unit replacement of the core and reflector assembly, replacement of the core only in subcritical units, or replacement of a single fuel cluster. This objective makes it possible to stock a partial core for replacement of fuel elements which may have failed prematurely, permits the shipment of a minimum weight core for refueling, and allows replacement of depleted or defective reflector parts after several core lives.

The core structure unit (Figure II-8) includes control drums, snubbing devices, fuel elements and grid plates, reflector cusps and filler pieces around the drums, and the hardware associated with the drums themselves.

The core structure body (Figure II-7) consists of a cylindrical plate, 5/8-in. thick, of Type 304 stainless steel. This cylinder encases the 12 control drums which are equally spaced around the fuel rod lattice. Bearing rings are fastened to each end of the cylinder to provide the necessary support and rigidity required by the components. The basic dimensions of the core structure are presented in Figure A-2.

The top bearing ring supports the individual grid plate segments and their fuel rods. It also serves as a housing for pivot and thrust bearings required by the drums. Trapezoidal grid plate segments are fastened to this bearing ring to form a rigid bracket which in turn has the center grid plate mounted and positioned to it. The trapezoidal grid plate segment is a flat plate, which fastens to the upper bearing ring, centering itself around two male plugs that extend out from the control drum centerline. The grid plate extends to the edge

DIMENSION	HOT (570°)	COLD (70°)
R_p	7.6856	7.6483
R_D	1.9742	1.9647
R_{W1}	9.6898	9.6429
R_{W2}	10.3180	10.2680
R_{S1}		$10.3985^{+0.015}_{-0.000}$
R_{S2}		11.000 ± 0.015
R_{V1}		$11.300^{+0.062}_{-0.000}$
T_v		1.125
S	0.462	0.4598

7-3-63

7564-0315

Figure A-2. Basic Dimensions of the Core Structure

of the bearing ring and then angles downward to about 1 in. above the upper fuel boundary. This plate then becomes a segment of the upper grid plate and is perforated with holes spaced on a triangular pattern.

The bottom bearing ring has a large central opening to provide a water passage for flow directly to the lower grid plates. Coolant flows to the upper side of the grid plate through openings in the fuel rod end plugs. This bearing ring also serves as the housing for the lower bearing and the snubbing device of each control drum. The lower drum shaft bearing locates the axis of the drum and resists radial loads. The snubbing device consists of a rotary arm keyed to the lower drum shaft. It is free to move axially on the shaft. This rotary arm will displace water in a precisely machined, cam-shaped housing. At the end of 180-degrees movement, the housing wall has a machined constriction to achieve a snubbing action when the arm swings by the constricted area. This restriction of the water passage reduces the rotational speed of the rotary arm and in turn, the control drum.

The fuel rod clusters (Figure II-8) in the core are of two basic shapes. The central cluster is hexagonal, while the six outer clusters, which are identical, are of a trapezoidal shape. Each cluster is contained between upper and lower grid plates with all the fuel rods secured by roll-expanding the tubular end plug extensions into the upper grid. The lower grid plate of each cluster is secured to the ends of widely spaced fuel rods. The remaining fuel rod end fittings are permitted to slide freely in holes in the grid plate. This construction allows independent thermal expansion of each rod during reactor operation.

The center, hexagonal fuel cluster contains 127 rods; of these, 90 rods are fueled to a standard composition and 37 are replaceable and may be used for criticality shimming. The center cluster grid plate is supported by a 1/4-in.-thick flange which overlaps the inner edges of the 6 peripheral trapezoidal fuel clusters grid plates. The center hexagonal upper grid plate has a high boss in its center in which a lifting notch is machined. This fitting provides a lifting attachment for remotely removing or replacing this assembly.

The trapezoidal fuel clusters contain 76 fuel rods in each of the 6 segments. Thus, the core contains a total of 583 fuel rods.

The fuel rods consist of fuel slugs clad in stainless-steel tubing. At each end, identical tubular end plugs are welded to the cladding. These end fittings

have portions of their side walls machined away so that water may flow through the openings. Alternate fuel rods have a wire spirally wrapped around them to prevent contact between rods. Each individual rod is positioned into a 1/4-in.-diameter hole in the grid plates. Midway through the upper grid plate thickness, a circumferential groove is located in each hole. A portion of the end plug slips into the hole in the grid plate and an expanding tool is inserted in this hollow end plug area. The metal of the end plug is forced into the grooves by the expander tool securing the fuel rod to the grid plate.

As indicated above, 37 of the 127 fuel rod locations in the central cluster are adaptable to accept shim rods. The shim rods are for criticality shimming. The end plugs of the shim rods are made sufficiently long to protrude above the grid plate. A sleeve bushing is fitted over the extended end of the plug and a cotter pin is installed through both parts. During the critical experiment the reactivity worths of the shim rods are established. After core reactivity requirements are confirmed, the cotter pins and bushings are removed and the end plugs of the shim rods are machined down to the same lengths as the other end plugs. Reinsertion of these fuel rods into grid plate follows, and the expander tool is used to secure all shim rods.

When the six trapezoidal fuel clusters are located and fastened to the upper bearing ring, the unfilled opening in the center accepts the central fuel cluster. The boss, which is centered in the center grid plate, comes in contact with the surface of the hold-down bracket. This bracket has a center boss from which three equally spaced arms extend outward. The arms are mounted and fastened down to the upper bearing ring.

"Filler" pieces of nickel are positioned between the outer fuel rods of the core and the control drums. These stationary pieces are shaped to minimize the amount of water space between the drums and fuel rods and thus, prevent coolant bypassing. They are positioned between upper and lower grid plates and are firmly fastened to prevent contact with the movable control drums.

Twelve control drums completely surround the core and are arrayed in a circular pattern. Each drum is driven (rotated) by a motor drive unit located vertically above the drum and connected to it via a shaft extension. Rotation of the drum exposes either poison or reflector material to the core upon signal.

The reflector material filling a sector two-thirds of the drum cross-section is beryllium and the remaining one-third sector is occupied by a nuclear poison, e.g., hafnium-hydride. The drum is clad via shrink-fitting with a nickel sheath. Caps are welded to the cladding and to the shafts which extend axially from the ends of the drums. This fabrication method encases the control drum to give it rigidity and to exclude water. Each drum is approximately 4 in. in diameter and 17-3/8 in. in height. The drum shafts are flanged and fastened to the beryllium sector by bolts and dowels. The upper drum shaft has a mechanical sleeve-type coupling to minimize the radial movement of the mating shaft from the motor drive unit. This coupling allows relative axial movement between the drum shaft and motor drive shaft. This axial movement is required to accommodate component elongation due to thermal expansion.

The outer cusp-shaped reflector segments are stationary pieces of nickel that are located between and outboard of the drums inside the core structure cylinder. The twelve cusps are mounted between the upper and lower bearing rings by fasteners and dowel pins. A secured location is required to provide positive clearance between the cusps and the control drums.

Hold-down bolts are used to attach the core structure unit to the lower flange of the thermal shield. These are long nickel bolts, extending through holes in six of the nickel cusps into the flange extending inward at the base of the thermal shield.

B. CORE STRUCTURE DESIGN — BACKGROUND

1. Fuel Cluster Supports

Earlier investigations included an arrangement (Figure II-9) which used a tongue and groove connection between the core support structure and the trapezoidal fuel cluster. The inner hexagonal fuel cluster was indexed by having its upper grid edges bear against the six mating edges of the outer fuel cluster plates. Vertical stability was achieved by the use of a support fixture whose outer edges were bolted to the core support structure. A series of clamping devices extended to attach to the fuel cluster in the center of the support fixture. It was felt that both the tongue and groove arrangement and the clamping fixture with matching projections were somewhat more complicated than necessary to meet the functional requirements, and the reference design which evolved accomplishes the desired result with an acceptable degree of simplicity.

2. Grid Plate

The close spaced triangular lattice pattern for the fuel rods posed a problem in providing a grid plate which is not unduly weakened by penetrations for both fuel rod support and coolant flow. It can be demonstrated that if a holding pin is provided in the center of each lattice position, two flow passages will be required for each fuel rod position. Furthermore, if it is desired that the pressure drop through the grid plates be minimized, the web of the grid plate structure between adjacent flow passages becomes vanishingly thin. Through design layout studies it became increasingly obvious that a means must be found to combine flow passages with the fuel rod supports, this has now been achieved in the reference design.

3. Fuel Rod to Grid Plate Attachment

Several methods of fuel rod attachment to the grid plate were investigated. The use of snap rings proved feasible and simple, however, because the possibility exists that a snap ring could break or dislodge, enter the coolant stream, and release the fuel element, this method was abandoned. A second scheme investigated secured the rods by positioning balls in a groove machined into the grid plate using the principle of commercial ball lock pins. This scheme, although reliable and sound in working principle, occupied coolant flow space in the fuel rod and fitting, and was therefore impractical for this application. A third scheme investigated (Figure A-3) used washers bolted to the upper grid to retain the fuel elements. It was decided that this device required that more parts be handled than was necessary for the function, and also suffered from the potential hazard of permitting pieces to enter the coolant stream.

4. Fuel Rod End Plugs

Only one alternative end plug design was examined. This design (Figure II-6) used three longitudinal slots milled into the extension of the end plug to provide coolant flow passages. This design was readily adaptable to all functional requirements except that of being secured to the grid plate. The best means of securing this end fitting to the grid plate required welding. It was felt that a design could be achieved which avoided welding to the grid plate avoiding possible distortion from heat and difficult removal and, therefore, should be favored for the reference design.

7-3-63 7564-0317
Figure A-3. Core Cross Section
(Design Layout)

BLANK

C. THE REACTOR VESSEL

The reactor vessel is a vertically arranged conventional pressure vessel of welded construction having a cylindrical shell, an elliptically dished bottom head, and a bolting flange at the upper end. It is designed to comply with the requirements of Section VIII of the ASME Boiler and Pressure Vessel Code and with the applicable nuclear cases of interpretation of the Boiler Codes. The design conditions are 1400 psi pressure at a temperature of 600°F. The vessel (Figure A-4) is supported at the bottom end by a cylindrical skirt, welded to the vessel and fitted with a plate at the lower edge to provide bearing area and space for anchor bolts. Closure at the top is provided by spherically dished bolted cover. The vessel is fitted with a multipurpose internal liner with an annular space between the liner and the vessel wall. The liner serves both as the thermal (neutron absorbing) shield and the reactor core supporting member. The space between the thermal shield and the vessel defines a downcomer passage for the incoming reactor coolant. The thermal shield extends from a point near the bottom of the reactor vessel to an elevation just above the coolant nozzle. The thermal shield is fastened all around its top to the vessel wall by welding. This is the only place of attachment between the vessel and the thermal shield; however, four bosses welded to the outside of the thermal shield near the bottom satisfy the spacing and centering requirements and permit differential thermal expansion between the vessel and shield. An inward extending flange ring near the bottom edge of the thermal shield serves to support the core structure and also provides tapped bolting holes for holding down the core structure.

The inside of the thermal shield is sized to provide an annular space all around between the core structure cylinder and the thermal shield. This space is closed at the bottom by the core structure support flange. Cooling for this space is provided by gravity flow from the top and the boiling of this coolant within the annular space.

The vessel is equipped with one nozzle located on the side at about two-thirds of the height of the vessel. The single nozzle opening serves as a passageway for both the inflow of coolant and outflow of the steam-water mixture by means of concentric channels. The inner channel of circular cross-section provides the passageway for effluent coolant while the outer channel, an annular passage surrounding the steam outlet pipe, provides a path for the incoming water. The concentric pipes are divided into separate pipes just outside the

7-3-63

7564-0319

Figure A-4. Reactor Vessel

vessel activation shield and terminate in high-pressure-gasketed bolting flanges for connection at installation to adjoining power plant components. The inner channel or steam outlet pipe has a bell-mouth entrance which penetrates and is welded to the thermal shield. The annular passage for incoming coolant communicates with the annular space between the vessel wall and the thermal shield. This space is increased in width around the entire vessel at the elevation of the entrance pipe to provide adequate cross sectional area (to avoid high-flow velocities) and to provide distribution of coolant all around the annular downcomer passage.

Within the thermal shield above the core, the shield plug is mounted to provide neutron shielding for the chamber above the plug near the top of the vessel. The shield plug is of adequate thickness to limit activation of the equipment in the chamber above the plug to levels acceptable for manual maintenance operations.

At the top surface of the shield plug, a layer of insulation and an insulation cover plate are provided to isolate thermally the equipment chamber.

At the bottom of the shield plug, a streamlined re-entrant steam-water flow passage permits relatively unimpeded flow but provides neutron shielding. This flow passage leads to an eccentric collecting channel. Steel sleeves extend vertically through the shield plug and serve as conduits for the reactor control drum actuating shafts. The shafts and sleeves are stepped to a larger diameter just above the eccentric collecting space to reduce radiation streaming up the channel between the shaft and sleeve. Twelve sleeves are provided, equally spaced in a circular arrangement, one for each control drum.

The control drum drive mechanisms are located within the chamber above the plug at the top of the vessel. Each control drum has its own drive mechanism and all mechanisms are mounted on a single plate so that all drives may be removed as a unit when maintenance, repair, or replacement is required. The motor mounting plate is bolted to the insulation cover plate on the plug.

The top cover for the reactor vessel consists of a bolting flange ring, a ring section from a spherically dished plate, and a center conduit port, all welded together. The port provides for the passage of a conduit for electrical conductors to the control drum drive motors, clutches, position indicators, etc. The electrical conductors terminate at the upper end of the conduit in several

high-pressure, sealed, multipin disconnect plugs. The port cover is underneath the reactor vessel cover and is equipped with bolting studs which extend up through the vessel cover and is sealed with the gasket placed outside the bolt circle. This arrangement permits the reactor vessel cover to be removed without disturbing the electrical conductors. Gaskets for both the reactor vessel cover and for the port cover are of nickel, or any other metallic material compatible with the high-purity water employed as reactor coolant.

The reactor pressure vessel is insulated at the bottom and sides by means of a layer of fibrous insulating material, e. g., Fiberglas. At the bottom, the insulating material is enclosed in the space between the lower end of the vessel and the bearing plate at the bottom of the supporting skirt. On the sides, the insulation is contained in an annular space enclosed by a stainless-steel wrapper sheet. The top of the wrapper sheet is welded to the underside of the reactor vessel bolting flange. The cover plate is not insulated since its temperature is essentially that of the shield water. An extension of the insulation wrapper plate surrounds the nozzle and connects to separate wrapper sleeves around the individual coolant inlet and steam outlet pipes. The insulation space around the pipes is sealed to the shield water containment vessel by means of flexible metal bellows welded to the sleeves and bolted and gasketed to a companion flange on the shield water vessel. The purpose of the bellows connections is to permit differential expansion between the reactor vessel and pipes and the shield water vessel, since the shield water will operate at temperatures from 100 to 200°F while the vessel and pipes mentioned are in the 500 to 600°F temperature range.

The insulation mentioned in the description of the shield plug is of the same material as is employed on the reactor vessel and pipes. The insulation at the top of the shield plug consists of a horizontal layer just below the insulation cover plate and of a circumferential layer extending all around just inside the vessel wall. The top surface of the circumferential layer extends upward to the same elevation as the bottom surface of the reactor vessel bolting flange. With this insulation arrangement, the reactor vessel wall above the thermal shield attachment point and the bottom of this bolting flange is insulated both inside and outside. The temperature variation in the wall of the vessel will be approximately linear and the length of the vessel wall in this region is sized to avoid unacceptable thermal stresses for the temperature gradient which exists in this region, 600°F at the steam outlet to 150/200°F in the drive motor chamber and at the bolting flange.

The activation shield consists of a layer of lead surrounding the reactor vessel on the sides and bottom. It is placed under the support skirt and bearing plate, and upward around the vessel to the bottom surface of the bolting flange. The thickness of the lead is not uniform but is proportioned to match the amount of gamma activity in the vessel wall and in other vessel and reactor components. Its purpose is to restrict gamma radiation intensity to permissible levels for personnel access to the region around the shield water tank when the core has been removed. A layer of lead is also wrapped around and over the end of the inlet-outlet nozzle. The lead is supported on the inside by the insulation wrapper plate and on the outside if and where required by aluminum clamping bands.

The reactor vessel and shield are located and secured by means of a supporting structure extending up from the bottom of the shield water vessel. It consists of two cylinders arranged concentrically and is capped by a top bolting and bearing plate. It is of all-welded construction and the material is an aluminum alloy (e. g., 52S) that does not become unacceptably activated by neutron radiation from the reactor core that penetrates the thermal and activation shield. The inner cylinder will be placed directly under the reactor vessel support skirt and the outer cylinder directly under and coinciding with the mean diameter of the lead activation shield. The reactor vessel will be fastened to the structure by means of aluminum bolts to reduce the amount of unshielded activated material.

The shield water vessel (Figure A-5) is constructed of aluminum and is 8 ft in diameter. It is arranged to provide shielding water all around the reactor vessel so that the space outside the shield water vessel is accessible to maintenance and operating personnel. The shield water vessel consists of a cylinder and two flanged and dished pressure vessel heads. The vessel serves not only as a shield water tank, but also as a containment vessel in the event of reactor vessel or piping system failure. The thickness of the shield water vessel is designed to withstand the pressure increase that might result from a reactor coolant system containment failure.

The decay heat exchanger, steam separator, and recirculation pump are all located within the shield water vessel. The decay heat loop consists of a length of finned pipe or tubing, arranged back and forth around approximately 40% of the circumference of the vessel. The inlet to the heat exchanger is provided by an insulated riser taking off from the steam-water outlet pipe as it leaves the

BLANK

REVISIONS			
STN	ZONE	DESCRIPTION	DATE

PACKAGE #1
REACTOR, STEAM SEPARATOR AND RECIRCULATION PUMP

7-3-63 7564-0320
Figure A-5. The Shield Water Vessel

BLANK

reactor. The heat exchanger tube then zigzags downward to the nozzle elevation where it enters the cooling water supply pipe through a check valve. Since the water inlet pressure is greater than the outlet pressure, the check valve will prevent flow during normal operation, but in the event of a break in a pipe line, the pressure will become equalized and down-flow will occur because of the pressure on the check valve resulting from the head of condensed liquid in the heat exchanger coil.

The steam separator and recirculation pump together with the reactor pressure vessel and interconnecting piping complete the recirculation loop. All parts of this loop are submerged in the shield water pool as shown in Figure A-6. This arrangement was finally selected after studying many possible configurations to achieve the advantages described below:

- 1) Minimum containment volume is required under accident conditions since the release of reactor coolant can only take place through the shield water pool in which condensation and quenching will occur.
- 2) The probability of a meltdown of the core, following a loss-of-coolant accident via a system failure (e. g., line rupture), is reduced essentially to zero. (See discussion in Appendix H, Safety.)
- 3) The need for quick-acting isolation valves between components of the recirculation loop is eliminated providing assurance that the natural circulation mode of cooling the core will always be available. The separator and pump are placed at an elevation which assures a continuous, natural circulation of coolant to the core, so long as the water level in the separator is maintained above the minimum specified value, i. e. approximately the midplane as illustrated in Figure A-5.

The arrangement described above provides the reactor module with its own containment provisions and permits a remarkably compact and safe configuration.

D. REACTOR VESSEL CONFIGURATION—BACKGROUND

The development of the arrangement described resulted from studying several concepts to achieve a reactor of optimum size and weight. These concepts will be described in chronological order.

Initially the reactor vessel (Figure A-6) was conceived as being of the smallest possible diameter necessary to enclose the core. Inlet coolant flowed

10-26-62

7564-0040

Figure A-6. SNAP 4 Reactor Vessel

from a funnel-shaped inlet fitting which formed the vessel lower-end closure. Effluent coolant passed through a funnel and elbow formed within the upper shield plug. The shield plug was integral with the upper closure, and accommodated sealed control drum drives on the upper face. Shield coolant flow was vertically upward in the annulus between the reactor vessel and surrounding activation shield. This concept was abandoned because:

- 1) The low temperature shield coolant adjacent to the reactor vessel wall created severe temperature gradients and thermal stresses in the vessel wall.
- 2) A thermal neutron shield was required between the reactor vessel and core to minimize the effects of radiation damage on the vessel wall.
- 3) The design was inherently inflexible.

An attempt was made to achieve an overall plant weight reduction by including the steam separating function within the reactor vessel and placing shielding material inside the pressure vessel adjacent to the reactor core (Figure A-7). This concept was abandoned because placing shielding adjacent to the core provided insufficient neutron attenuation to prevent activation of the pressure vessel. Therefore, an additional amount of gamma shielding, placed outside the reactor vessel, was required. Another reason for rejecting this concept stems from the lack of sufficiently reliable data on steam separation to provide the desired confidence in the design.

E. CONTROL MECHANISM

The control mechanism (Figure A-8) consists essentially of two units; the control drums with their support pivots and scram snubbers, and the drum drive mechanism packages that are activated by an appropriate signal to cause rotation of the drums.

Each control drum is a right, circular cylinder, approximately 4 in. in diameter and 17-3/8 in. long. Shafts, which extend beyond the ends of the drums, are installed in bearings to locate the drums on their pivot axes. The drums are built up of a poison section and a reflector section, each in the form of cylindrical segments. One-third of the cylindrical cross-section of the drum consists

7-3-63 7564-0322
 Figure A-7. Spherical Chamber-Type Reactor
 Vessel with Manifold Nozzles and Internal
 Steam Separator

7-3-63 7564-0323
 Figure A-8. Installation
 Assembly Control Mechanism

BLANK

of a neutron absorber or poison material. The remaining two-thirds of the cylinder is a neutron reflector material, viz. beryllium. The drums provide control of reactivity by positioning reflector and absorber material in the reflector region just outside the core. Poison material is positioned nearest the core to achieve the least reactive condition, and reflector material is positioned nearest the core for the most reactive position.

Twelve control drums are used in the reactor. They are supported by bearings at each end. Two circular bearing rings, one at each end of the core, secured to the ends of the core structure cylinder surrounding the core, contain the pivot bearings for the drums. The two rings, securely attached to the structure cylinder, form a rigid structure which permits accurate location of the drum pivot bearings and simultaneously provides the fuel element cluster assemblies' and control drums' mounting structure. At the underside of the lower ring, a space is provided for an integral snubber essentially a dashpot. The snubber (Figure II-7) consists of a vane, attached to the drum shaft, which displaces water trapped in a cavity below the bearing in the circular ring at the bottom. Energy is dissipated by performing work on the trapped water in forcing it through the clearance space around the snubber vane.

A shaft, which extends through the shield plug, connects the drum drive mechanism to each of the drums. Slip-on couplings are provided at each end of the shaft to allow removal of the shield plug or the drive motor without disturbing the control drum.

One control drum drive mechanism is provided for each drum. The 12 control drum drive mechanisms are mounted on a single-base plate in the space provided between the shield plug and reactor vessel cover and may be removed as a unit for servicing or repair. Each control drum drive mechanism consists of the following components; a scram spring, a scram clutch, a drive motor with gearing, a drum position sensing or readout instrument, and power and instrumentation leads. The entire mechanism is immersed in water at 200°F or less and 1200 psi pressure.

The scram spring provides rapid rotation of the drum to the shutdown position. This spring is a flat coil or clock spring with the inner end attached to the drive output shaft, and the outer end attached to the control drive mechanism

housing. The spring is preloaded to assure that it can always supply sufficient torque to move the drum to the least reactive position.

A clutch, electromagnetically engaged, is provided to release the shaft from the drive motor when an immediate reactor shutdown is required. Loss of power to the holding electromagnet releases the clutch; hence, it is a fail-safe device, since it will release on loss of power supply. The input end of the scram clutch is coupled to the output shaft from the drive motor gearbox. Selection of the drive motor and gearbox is based on the need to overcome the windup torque of the scram spring and to provide controlled rate of rotation at an output speed of 1/20 rpm to the control drum. A stepper motor, with an integral planetary gearbox, has been selected to satisfy these requirements. With a 60-cycle input, the motor shaft output is 72 rpm. The gearbox provides a reduction of 1400:1 to achieve an output speed of 1/20 rpm. Shaft torque is approximately 50 lb-in. at the gearbox output, or less than 1/2-in. -oz at the motor shaft, neglecting gearbox friction.

Indication of drum position is from a linear variable differential voltage transformer coupled to the control drive mechanism output shaft. Rotation of the shaft moves the position sensor in direct proportion to drum position. The position sensor remains coupled during scram.

Since the drive motor, clutch, and readout instrument operate within the 1200-psi pressure vessel, while the control room (where the operation of the mechanism is controlled) is at ambient atmospheric conditions, the electrical leads must cross the pressure barrier. This is accomplished by collecting all leads in a conduit and terminating them at a sealed conductor, disconnect plug.

It is believed that the control mechanism described represents a minimum of compromise and a maximum of desirable characteristics for a compact reactor. Alternate approaches were considered in this effort, and rejected. The use of shaft seals was ruled out primarily because none have been proved to be of sufficient reliability to operate without maintenance for over one year. Secondary considerations which dictate against the use of shaft seals are:

- 1) Undesirable consequences of premature failure, with possible contamination of the reactor interior, and
- 2) Variations in friction losses and possibility of seizing in driving through a seal, which are both particularly undesirable for a reliable scram system.

A detailed study was made of a control drive mechanism capable of driving two control drums from a single motor. This system (Figure II -10) provided better utilization of the space available for the drive package, since only 6 drive units were required instead of 12. It was, however, considered less safe, since a single component failure could cause the loss of control over two control drums and components whose reliability is of prime importance. In this scheme, the scram clutch, gears and scram spring all operate in an undesirable environment, that is, a steam-water mixture at 570°F, whereas the selected design provides a water environment at 200°F.

BLANK

APPENDIX B. REACTOR PHYSICS

The SNAP 4 reactor is zirconium hydride moderated and boiling water cooled. High power density, at reasonably low temperature, is achieved by the phase change in boiling heat removal and by relying primarily on the hydrogen in the fuel-moderator for neutron moderation. Optimum utilization of core volume is maintained by axial and radial power flattening using a non-uniform distribution of poison. Long core life is achieved by using burnable poisons to minimize the long-term reactivity control requirements. A high radiation damage limit is achieved by combining the fuel with a solid moderator to provide a very large fuel volume fraction that minimizes atom percentage burnup. Minimum fuel inventory is achieved by forming an epithermal physics design with hydrogen moderation. Compact design of the reactor shield is made possible by utilizing reflector control. The inclusion of these design features in an optimized system requires considerable analytical sophistication due to multiple interaction effects; however, the promise of the final design, once achieved, is well worth the effort.

A. CORE COMPOSITION ANALYSES

The following sections describe the analytical development of the current S4PR core composition as it has evolved through successive refinements in computational techniques. The areas of effort described include burnable poisons, fission product poisons, optimum fuel loading, and power flattening. The detailed operating characteristics of the S4PR core are presented in Table B-I.

1. Burnable Poisons

Due to the limited control available from reflector control drums, the reactivity control requirements of the S4PR must be minimized. The reactivity loss of 11% $\Delta k/k$ during life due to fuel depletion and fission product poison accumulation is the major control requirement that can be significantly reduced. Fortunately the choice of burnable poison is not constrained by considerations of residual poison blackness after a full core life, because the introduction of burnable poisons did not require a complementary increase in fuel loading to maintain criticality. The initially low hydrogen content of the fuel, with $N_H = 4$, permitted the reactivity to be regained by a simple increase of N_H to

TABLE B-I
S4PR PARAMETERS
(Sheet 2 of 2)

<u>Operating Conditions</u>	
Coolant flow rate	4.1×10^5 lb/hr
Coolant inlet pressure	1235 psia
Coolant outlet pressure	1212 psia
Coolant inlet temperature	540°F
Coolant outlet temperature	570°F
Recirculation ratio	10:1 (10% exit quality)
Average cladding temperature	667°F
Average fuel temperature	879°F
Maximum fuel temperature	1056°F
Heat transfer area	95 ft ²
Average heat flux	431,000 Btu/hr-ft ²
Maximum heat flux	540,000 Btu/hr-ft ²
Burnout safety factor (minimum)	3
Average power density in fuel	15.7 Mw/ft ³
Coolant mass velocity	2.85×10^6 lb/hr-ft ²
Coolant passage hydraulic diameter	0.0937 in.
Reactor pressure drop	22.8 psi
Reactor pumping power	21.7 water HP

about $N_H = 5.5$. The higher fuel hydrogen content also served to reduce the reactivity change from the cold to the hot, boiling water condition and further reduce control requirements. However, the increased core blackness and scattering cross section reduce the worth of control drums, indicating that an optimization analysis on hydrogen content should be performed, as was done in selecting the optimum fuel loading.

The initial burnable poison calculations considered a uniform dispersion of boron in the fuel. A poison loading of 120 gm of boron yielded the minimum reactivity change of 5.5% $\Delta k/k$ during the S4PR core life. To achieve a further reduction in reactivity change requires an effective reduction in poison isotope cross section below the 4000 barn boron-10 isotope value. The physics problem in the S4PR is similar to that encountered in the naval reactor technology which involves a similar fractional burnup of fuel. A reduction of effective boron

12-10-62

7564-0105

Figure B-1. Endurance Calculations With Hafnium Poison

cross section may be achieved by heterogeneous configurations of self-shielded boron in plates, rods or particles. In the S4PR such heterogeneous designs are not desirable because of the resulting penalty in peak-to-average power distributions. In addition, boron in any form is not desirable because of the complications of helium gas formation from the n, α reaction cross section of boron-10.

Among the many possible substitute burnable poisons, hafnium has been evaluated to be the most favorable. Hafnium is metallurgically compatible with zirconium and also is readily hydrided. The four major isotopes of hafnium are stable and detailed resonance parameter data are available in the resonance energy region. The major isotope is hafnium-177 which burns to form the higher stable hafnium isotopes. Thus hafnium remains as hafnium as it burns out and causes no metallurgical complications.

From the nuclear standpoint, resonance absorbers as burnable poisons present some additional complications. The effective epithermal absorption cross sections must be carefully determined from the resonance parameters. In the S4PR about half the reactivity effect of elemental hafnium is due to the epithermal neutron captures by these resonances.

Initial burnup calculations using hafnium employed hafnium isotope cross sections calculated by assuming effectively infinite macroscopic scattering cross sections at the resonance energies. This initial approximation was significantly misleading and lead to these conclusions:

- a) Hafnium had large epithermal cross sections
- b) Hafnium burned out too fast
- c) A mixture of fast-burning hafnium and a slow-burner like dysprosium yielded the optimum burnable poison.

Detailed calculations of hafnium epithermal cross sections in the S4PR moderating material were made using ARES-1, a resonance integral calculation code.¹ In the finite macroscopic scattering cross section medium, typical of S4PR fuel, the hafnium isotope epithermal cross sections were significantly decreased from previous estimates. The resulting reactivity changes in the S4PR with various loadings of hafnium are shown in Figure B-1, with hafnium cross sections individually calculated for each initial hafnium loading. At each

12-10-62

7564-0106

Figure B-2. Endurance Calculations With Europium-Hafnium Poison Mixtures

of the hafnium loadings to achieve reasonable, initial values of k_{eff} for the reactor, the curve of reactivity vs time monotonically decreases, indicating that hafnium burns up too slowly. However, the 4.2% $\Delta k/k$ long-term reactivity control requirement, using a hafnium burnable poison, is an improvement over the 5.5% $\Delta k/k$ required using boron as the burnable poison.

An additional refinement yet to be included is the recalculation of hafnium isotope epithermal cross sections during the core life. The major isotope, hafnium-177, will leave a residue of unburned hafnium-177 that has an epithermal cross section that increases with time (as the core loses blackness) but also burns out more rapidly. How this effect will shift the burnout curves remains to be determined, but the change will probably be small. A significant flattening of the reactivity curves should result when the fission product poison representation is improved to include burnup of the fission product poisons.

An absolute manipulation of the shape of the burnout curves can be achieved by the addition of small amounts of a higher cross-section element, e.g., europium. The result of various partial substitutions of europium for hafnium are shown in Figure B-2. A dramatic reduction of long-term control requirements to 1.4% $\Delta k/k$ is indicated as possible at the optimum poison mixture.

Europium is an optimum additive to hafnium from the nuclear standpoint but involves complications. Metallurgically the low boiling point of europium makes it difficult to alloy with zirconium. The inclusion of europium oxide could be achieved with time control of the melting process to avoid uncontrolled vaporization of the reduced europium.

The nuclear analysis of europium burnup is complicated because resonance parameter data are available only for the two stable europium isotopes, 151 and 153. Only the thermal cross-section values are known for the major unstable europium isotopes 152, 154, and 155. The calculations performed to date with europium have assumed only $1/v$ cross sections for the unstable isotopes. Detailed comparison with experiments^{2,3} on europium burnup is a needed refinement to the calculations.

In general, the status of burnable poisons is that remarkably small long-term reactivity changes are possible but careful, experimentally verified calculations are required to achieve design certainty. In the meantime,

conservative control margins must be retained in the reactor designs to cover burnup uncertainties in early reactor prototypes.

2. Fission Product Poisons

The major source of reactivity loss in the S4PR during one year of full-power operation is due to the accumulation of fission product poisons. The depletion of fuel material also contributes significantly to the net reactivity loss.

Using a value of 190 Mev/fission for energy retained in the reactor core, the depletion of U^{235} is 0.48 kg U^{235} per Mwt-year developed in the reactor core due to U^{235} fissions and the conversion to U^{236} . After 12 Mwt-years, the U^{235} lost is 5.76 kg. In the S4PR with an initial fuel inventory near 20 kg of fully enriched uranium, the reactivity loss due to fuel depletion is 4.0% $\Delta k/k$.

The reactivity loss due to fission product poisons is appreciably more difficult to calculate due primarily to uncertainty in knowledge of cross sections. A further difficulty is that the multitude of fission product isotopes have extreme differences in cross sections. This causes the isotopes to burn up at different rates during core life and significantly influences calculations of reactivity loss. The natural decay process of the unstable fission product isotopes has only a slight effect on calculations for normal reactor operational practice which minimizes reactor shutdown times.

Traditionally, the fission products are separated into the Xe^{135} and Sm^{149} fractions, leaving the remainder of the isotopes lumped together as one equivalent isotope, sometimes named "fishpium." In two-group calculations, the cross section of fishpium has been made to decrease with fuel depletion to represent the partial burnup of fission products.^{4,5} Constant fishpium cross sections have also been used in many multigroup calculations.⁶

For the S4PR, the choice of calculational method was strongly influenced by the desire to observe spatial power distribution changes during core life because a flat power distribution is a prime design objective. The one-dimensional burnup code, SIZZLE,⁷ was selected for initial approximations of spatial variation with time. The SIZZLE code contains four isotope burnup chains. Samarium accumulation and burnup, and the equilibrium xenon accumulation burnup and decay may be represented. A single-atom fishpium model permits accumulation but not burnup or decay of fishpium. Two other chains describe series, mother-daughter isotope decay chains which were used to represent the burnable poisons.

The calculated reactivity worth for the S4PR core at 12 Mwt-years was 7.0% $\Delta k/k$ for the fission product poisons, excluding xenon. This calculation utilized the fission product poison cross-section from Reference 6 for initial life conditions and therefore overestimated the effect.

To achieve an improved value for the reactivity effect of fission product poisons, a 3 fishpium-atom model was to be employed.⁸ The high cross-section fishpium atom is a composite of 5 isotopes of samarium, gadolinium, and cadmium. The medium cross section fishpium atom is a composite of 10 isotopes. The remaining low cross section isotopes appear as the third fishpium atom. A modification of the SIZZLE code, utilizing the former xenon, samarium, and fishpium locations, will permit the calculation with the 3 fishpium-atom model.

Significant uncertainties will remain in the reactivity effect of fission product poisons due to cross section uncertainties. The fast absorption cross sections are due primarily to resonances. The absorption effect of a resonance varies with the macroscopic scattering cross section of the region and the atomic concentration of the absorbing isotope. Since accurate calculations are not readily made, the reactor control design must be able to tolerate these uncertainties in control requirements.

The reactivity worth of equilibrium xenon increases during core life due to the general reduction in blackness of the core as U^{235} and the hafnium burnable poison is burned up. Equilibrium xenon reactivity worth is 1.7% $\Delta k/k$ at 6 Mwt-years, and 2.4% $\Delta k/k$ at 12 Mwt-years. A slight reduction in these values may be expected after the xenon thermal cross sections are improved by flux spectral weighting.

3. Optimum Fuel Loading

The selection of fuel loading was influenced by many factors. The dominant consideration was the long-term reactivity change during core life due to mismatch between the reactivity loss from fuel depletion and accumulation of fission product poisons and the opposite reactivity gain due to loss of burnable poisons. At low fuel loading appreciable reactivity mismatch occurred. At about a 12 kg (of uranium) loading in the S4PR, the fuel alloy contains 10 wt % uranium corresponding to the fuel alloy used in SNAP 2/10 reactors. At about 20 wt % of uranium, metallurgical difficulties in the fuel rod extrusion process

7-2-63

7564-0305

Figure B-3. S4PR Core Composition Design Survey

are anticipated due to increasing alloy hardness. A rational selection of fuel element loading within these bounds was made on the basis of optimizing the reactor control margin.

The major control requirements were calculated at several fuel loading conditions of the S4PR and are presented in Figure B-3. The reactivity change from the cold, flooded to the hot, boiling reactor condition gradually decreased at increasing reactor fuel loadings. This trend is due to the gradual increase in the median fission energy at higher fuel loadings with a resulting decrease in reactivity importance of the moderating properties of the hydrogen in the coolant.

The long-term reactivity change was calculated at each fuel loading by varying the burnable poison composition and loading to obtain the minimum reactivity mismatch between fuel depletion and burnable poison depletion reactivity effects. The hafnium and dysprosium cross sections used for this study were evaluated at infinite dilution and were later shown to be invalid. However, the trend of the reactivity mismatch with fuel loading is believed to be representative of the phenomena. An updated optimization study using improved hafnium and europium cross sections would yield similar curves of the variation of control requirements with various fuel loadings.

The reactivity worth of equilibrium xenon was calculated for each fuel loading and is seen to decrease with increasing fuel loading, i. e., blackness of the core.

The reactivity requirements due to the various temperature and power coefficients were assumed to be constant as was the shutdown margin. The total control requirement was then summed up for each fuel loading condition.

The available reflector control as a function of fuel loading was estimated by simple one-dimensional, radial diffusion calculations in which control drum rotation was represented by inversion of reflector zones of beryllium and poison. The results were normalized to agree with two-dimensional calculations.

The major conclusion to be drawn from the fuel loading parameter survey is that a broad optimum fuel loading does exist in the region of about 15 wt % fuel. This design condition optimizes the control margin of the reactor. A similar survey with hydrogen content as the prime variable is yet to be done.

4. Power Flattening

A significant advance in compact reactor performance requires power flattening. Power flattening can be achieved by nonuniform fuel distribution, nonuniform moderator distribution, or nonuniform poison distribution. Since appreciable burnable poison loadings were already desired to minimize long-term reactivity changes, the nonuniform poison method of achieving flattened power was the obvious choice.

The inclusion of poison in a heterogeneous design, viz, in individual rods, was avoided so that a more uniform power distribution could be achieved and to increase the safety aspects of fuel fabrication and reactor operation. Also, rod-type control devices were ruled out to avoid flux peaking in flooded rod-channels and to minimize reactor structural and shielding weight.

With a burnable poison utilized to flatten power, the degree of power flattening later in core life, as the poison burnup, must be considered. In general, with a nonuniform burnable poison, if the local poison burns up more rapidly than the fuel, the power level rises with time in that region until the fuel is depleted. This effect was observed in calculations with boron dispersed in the central region of the reactor core. However, with slow-burning hafnium, zoned to achieve power flattening, the residual peak power points decreased with time. Thus, in this case, the degree of power flattening improved with time.

Power flattening must compensate for the effects of the asymmetrical axial water distribution due to boiling, natural power peaking in the center of the core, and local thermal flux peaking near the reflectors. The degree of power flattening that can be achieved using only two zones of different poison composition was examined. A high poison concentration region was restricted to a centrally-located cylindrical zone in the interior of the core and a low poison concentration region was arranged around the periphery of the core. A parameter survey was then conducted to determine the optimum radius, length, and axial position of the central high poison zone and the optimum ratio of high and low poison concentrations to minimize peak-to-average power ratio in the core. The total poison loading was held constant at 6 kg of hafnium to represent a reasonable burnable poison design. The axial hydrogen concentration was distributed to represent a reasonably flat power, boiling configuration as a first approximation. The radial reflector represented a condition in which all control drums were fully inserted, i. e., in a fully-reflected position. This

atter condition underlies the absolute values of the radial peak-to-average power distribution presented.

The results of the optimization study are presented in Figures B-4 and B-5. The best results yield a net reactor peak-to-average power ratio of 1.20. When details of reflector control drum programming during operation are included, the ratio may rise somewhat, with an effective peak-to-average power ratio of 1.30 entirely reasonable.

Potential improvements may be made by resorting to three or more poison composition zones. Also, poisoning the bottom end fittings of the fuel elements will reduce local thermal flux peaking. However, future improvements in power flattening, to a ratio of less than 1.30, are certain to be small and therefore, may be economically unjustifiable.

B. REACTIVITY REQUIREMENTS

1. Coefficients of Reactivity⁹

The various coefficients of reactivity for the S4PR are presented in Table B-II. They were calculated for a reactor model that included a hafnium-dysprosium burnable poison in a power-flattened distribution. Where significant, as in the Doppler broadening calculation, the model was updated to an all-hafnium poison loading. The results are close approximations and were to be further refined after reflector control drum design calculations were completed.

a. Coolant Boiling and Expulsion Effect

The change in water density in the transition from room temperature to the hot, boiling condition causes the largest reactivity change in the S4PR. This reactivity change was calculated including the small coolant expulsion effect due to fuel rod radial expansion. The reactivity defect, from room temperature to the operating condition of 700°F average fuel element surface temperature, was $-4.81\% \Delta k/k$. The slope near operating conditions was $-0.57 \times 10^{-5} \Delta k/k$ per °F and was found to be linear in the average fuel element surface temperature range from 560 to 840°F.

b. Power Variations Effect

A calculation of the effect of perturbations in power level was made for constant inlet coolant weight flow conditions. The coolant densities and

BASIS: 6 kg OF HAFNIUM AS POISON
REFLECTOR CONTROL DRUMS FULLY INSERTED
BERYLLIUM
CONSTANT $Z = 0.15 L$

5-1-63

7564-0168

Figure B-4. Power Flattening Optimization-I

5-1-63

7564-0169

Figure B-5. Power Flattening Optimization-II

TABLE B-II
COEFFICIENTS OF REACTIVITY
ABOUT OPERATING REACTOR CONDITIONS

	$\Delta K/K - ^\circ F$	$\epsilon / ^\circ F$
Fuel elongation coefficient	-0.020×10^{-5}	-0.025
Core coolant expulsion coefficient	-0.57×10^{-5}	-0.071
Neutron spectral coefficient	-0.03×10^{-5}	-0.004
Doppler coefficient	-0.54×10^{-5}	-0.069
Total Prompt Coefficient	-1.3×10^{-5}	-0.17
Lower grid plate coefficient	-0.055×10^{-5}	-0.007
Upper grid plate coefficient	-0.133×10^{-5}	-0.017
Power coefficient	$-0.0044 \Delta K/K - \text{Mwt}$	$-55 \epsilon / \text{Mwt}$
Steam void (α) coefficient	$-0.165 \Delta K/K - \text{fractional change in void}$	
Core hydrogen concentration (N_H) ($N_H - 10^{24}$ H atoms/cc)	$+9. \Delta K/K - N_H$	

steam void distributions corresponding to 9-, 12-, and 15-Mwt operating power were determined using the FUGUE¹⁰ code. The power coefficient was computed as $-0.0044 \Delta k/k (1/\Delta P)$, with ΔP in Mwt.

c. Doppler Broadening

The Doppler effect was calculated for the U^{238} contained in the fuel and a 5000 gm loading of hafnium as the burnable poison. The neutron group absorption cross sections were calculated using the ARES-1 code.¹

The Doppler reactivity defects from room temperature to a volume-average fuel temperature of 885°F were as follows:

$$\begin{array}{rcl}
 -0.47 & \text{due to Hafnium} & \\
 -0.10 & \text{due to } U^{238} & \\
 \hline
 -0.57 & \% \Delta k/k \text{ total} &
 \end{array}$$

The total Doppler coefficient was $0.54 \times 10^{-5} \Delta k/k (1/^\circ F)$ at operating conditions. The effect of U^{235} resonances was estimated to be small compared to the above

values. The results were not sensitive to the water inventory in the core from room temperature to the boiling condition.

d. Fuel Elongation Effect

The axial thermal expansion of fuel elements from room temperature to a volume-average fuel temperature of 885°F yielded a reactivity defect of -0.16% $\Delta k/k$. The coefficient near operating conditions was $-0.20 \times 10^{-5} \Delta k/k$ (1/°F).

e. Grid Plate Effect

The top grid plate expansion, from room temperature to the effluent water temperature of 570°F, yielded a reactivity defect of -0.08% $\Delta k/k$ and a coefficient of $-0.13 \times 10^{-5} \Delta k/k$ (1/°F) at operating conditions. The expansion of the lower grid plate from room temperature to the inlet water temperature of 540°F results in a reactivity defect of -0.075% $\Delta k/k$ and a coefficient of $-0.055 \times 10^{-5} \Delta k/k$ 1/°F. The reactivity effects were calculated assuming no radial movement of reflector, but included the change in water gap between core and reflector.

f. Neutron Spectrum Effect

The thermal spectrum changes with temperature were evaluated using 10 thermal neutron groups with neutron upscatter which are included in a 23 neutron group cross section library. The bound hydrogen cross sections¹¹ for water and $ZrH_{1.8}$ were used in the QUICKIE code¹² to calculate reactivity changes.

The reactivity defect from room temperature to a fuel volume-average temperature of 885°F was -0.04% $\Delta k/k$. The spectral coefficient was $-0.03 \times 10^{-5} \Delta k/k$ (1/°F) at operating conditions and was found to be nonlinear decreasing with increasing temperature.

2. Control Requirements

The control requirements for the S4PR are listed in Table B-III. As an aid in correlating the various reactivity conditions of the reactor as a function of control drum positions, water inventory, burnup condition, and xenon status, Figure B-6 was prepared. A line has been drawn to show the reactivity condition after the full rotation of each of the 12 reflector control drums, neglecting

5-3-63

7564-0108A

Figure B-6. Summary of S4PR Control Effects

TABLE B-III
NUCLEAR CONTROL REQUIREMENTS

	$\% \Delta k/k$
Minimum shutdown margin	2.0
Transition from cold to hot and boiling moderator expulsion (core N_H change)	4.81
Transition from cold to hot and boiling axial water reflector expulsion	0.50
Doppler, core resonance absorption U^{238} -0.10, burnable poisons -0.47	0.57
Fuel element elongation	0.16
Grid plate expansion	0.15
Thermal flux spectrum temperature shift	0.04
Fuel element cell effect	negligible
Hydrogen loss from fuel	negligible
Equilibrium xenon poisoning at maximum reactivity, midlife condition	2.0
Burnup control requirements (partially compensated with burnable poisons)	1.4
Total Control Requirement	11.63
Available nuclear control from reflector drums	12.50 - cold 13.2 - hot
Control contingency (in addition to 2% shutdown margin)	0.9

drum interaction effects. The reactivity status of the dry core is shown on the left. The reactivity rise at the cold, wet condition and reactivity fall at the hot, boiling condition is then indicated. An abbreviated curve of the long-term reactivity change is shown at the right with the top curve corresponding to complete decay of the equilibrium xenon. The maximum reactivity status near midlife with zero xenon was used to relate the previous curves at various water inventories. Thus, if the reactor were shut down and cooled off at the midlife, maximum reactivity condition, the reactor would be 2.9% $\Delta k/k$ subcritical with all drums fully rotated to the poison-in position. With one drum stuck in its most reactive position, the reactor would still be 1.9% $\Delta k/k$ subcritical, neglecting drum interaction effects. Measurements on drum interaction effects performed with the critical assembly SCA-3 show that a single drum is worth 144% of the

average drum worth when all remaining drums are in their most reactive position, but is worth only 51% of the average drum worth when all remaining drums are in their least reactive position, as during a reactor shutdown. Therefore, the reactor would actually be more nearly 2.4% subcritical when shut down with one drum stuck.

C. CONTROL DRUM DESIGN ANALYSES

The calculation of the physics of reflector control drums is a most difficult aspect of reactor analysis. Elaborate two-dimensional diffusion and transport theory reactor codes are currently being employed to improve calculational accuracy. Detailed comparison with critical assembly experiments has shown the degree of precision to be expected from successive levels of calculational refinements as indicated in Tables B-IV and B-V. These tables show that two-dimensional four-neutron group S4-transport calculations agree reasonably well with experimental results on reflector control drums. The inclusion of anisotropic hydrogen scattering cross sections has proven to be a significant factor in improving calculation accuracy.

Transport theory effects were evaluated using the one-dimensional codes DSN,¹³ DTK¹⁴ and the two-dimensional codes TDC¹³ and DDK.¹⁴ One-dimensional diffusion theory calculations were accomplished with FAIM.¹⁵ The slowing down and thermalization calculations, and cross section group reductions were accomplished using ULCER¹⁶ and QUICKIE.¹² The one-dimensional and two-dimensional criticality calculations were accomplished using 16 and 4 groups, respectively. The spectrum calculations (ULCER and QUICKIE), were done with 23 groups, 14 fast and 9 thermal. In general, the cross sections used were from the 16-group set generated by Hansen and Roach.¹⁷ Changes were made to the U^{235} cross sections of Reference 17 to give a fission resonance integral of 271¹⁸ barns. Thermal cross sections were modified to include averaging over the calculated thermal spectra. Thermalization cross sections used were those available from the ULCER library, and include the representations for zirconium hydride and water reported in Reference 11.

The one-dimensional calculations were performed using the buckling iteration techniques described in Reference 19. The two-dimensional calculations resulted in reactivity increases of as much as 1.5% above the one-dimensional cases. In addition to this geometrical effect, the problem of representing the

TABLE B-IV

COMPARISON OF EXPERIMENTS ON SCA-4B WITH 16 NEUTRON GROUP,
ONE-DIMENSIONAL DIFFUSION CALCULATIONS
AND S4 TRANSPORT CALCULATION
(Assumed $\beta_{\text{eff}} = 0.008$ for all cases)

	Diffusion Calculation (\$)	S4 Transport Calculation		Experiment (\$)
		Isotropic Scattering (\$)	Anisotropic Scattering (\$)	
Water in core	4.93	4.96	5.66	7.3 ± 1.9
Borated water in core and re- flector	18.0	14.8	13.98	13.7 ± 1.4
B ₄ C sleeve be- tween core and reflector	20.60	15.0	12.80	12.1 ± 1.9

TABLE B-V

COMPARISON OF CONTROL DRUM EXPERIMENTS ON SCA-3
WITH 4-NEUTRON GROUP, TWO-DIMENSIONAL DIFFUSION
CALCULATIONS AND S4 TRANSPORT CALCULATION
(Assumed $\beta_{\text{eff}} = 0.008$ for all cases)

Drum Poison Material	Diffusion Calculation (\$)	S4 Transport Calculation		Experiment (\$)
		Isotropic Calculation (\$)	Anisotropic* (\$)	
One drum B ₄ C + ZrH _{1.93}	1.33	0.97	1.01	1.00 ± 0.02
Four drums Boron and 304 SS	3.74	3.45	2.90	2.37 ± 0.05

*Obtained by adjusting isotropic results.

hexagonal core in a cylindrical geometry was shown to introduce as much as a 2.0% difference in reactivity depending upon the choice of homogenization techniques, i. e., cell or rubber band model. (The cell model resulted in higher reactivities.) The final method was a combination of the above techniques and reproduced the surface-to-volume ratio at the core vessel and the correct void fractions in the central regions of the core.

The hydrogen content of the core, the relatively large surface-to-volume ratio of these assemblies, and the use of a poison sleeve indicated the importance of an anisotropic flux representation, particularly at the boundaries of the cores. In addition, it appeared that the anisotropy of the neutron scattering with hydrogen would also be important. These effects were investigated using the S_n codes. The reactivity effect of the anisotropy of the flux varied from 3.0 to 6.0% for the various assemblies. The largest difference was in the poison sleeve cases for which consideration of the anisotropy of the flux led to an increase in reactivity of 6.0%. The remaining cases all resulted in increases in reactivity of about 3.0%. The use of anisotropic scattering cross sections for hydrogen introduced additional 1.0 to 4.5% differences in reactivities, although opposite in sign to those of the anisotropic flux effects. Table B-IV presents a comparison of the differences between transport and diffusion theory calculations. These results were obtained with one-dimensional, 16-group calculations. A value of β_{eff} for each configuration was calculated and was used to convert to units of dollars.

Because of the differences in the chemical bonding characteristics of hydrogen in zirconium hydride and water, the spatial dependence of the spectrum was considered. This was accomplished using ULCER, a one-dimensional diffusion theory calculation which allows neutron upscatter. It was found that at the core edge the spectrum was significantly distorted from the characteristic hydride spectrum established in the core interior. These results were incorporated into the two-dimensional calculations by defining separate core regions for the center and edge of the core and using the appropriate spectrum weighting indicated by ULCER.

In further support of the analytical treatment of control drum worths, the procedure described above was followed in the analysis of the SCA-3 critical assembly control drum experiments. Table B-V presents the results of this analysis and illustrates the precision attained by the newly-developed calculational tools.

The optimum design of the S4PR control drums has been partially investigated using a reflector drum poison material of boron dispersed in zirconium hydride. Preliminary results on the optimum poison zone thickness are shown on Figure B-7. For an unmoderated poison material, e.g., hafnium metal, the optimum poison zone thickness is one-fourth of the control drum diameter.

For a moderated poison material, such as boron in zirconium hydride, the optimum poison zone thickness increased to one-third of the control drum diameter.

A hafnium hydride drum poison was found to be less effective than the boron material. A "blackier" poison may be obtained by using europium oxide which has been experimentally shown to be the most effective potential poison in other water-cooled reactors.²⁰

The final drum design was to be fully tested by experiments to be conducted in the critical assembly SCA-5 and again during the initial critical assembly of the reactor core.

7-2-63 7564-0306
Figure B-7. Effect of Hydrogen Moderation on Optimum Poison Thickness

Figure B-8. Generalized Critical Machine (SCA-5)

7569-5401

D. CRITICAL EXPERIMENTS

1. Objectives and Description of Critical Tests

The cross section data mentioned above, as well as the control drum reactivity worth had to be checked experimentally in the SNAP Critical Assembly (SCA-5). The planned objectives of the SCA-5 experimental program were: (1) to determine experimentally the effect of power shaping and fuel cusping on control drum worth, (2) to measure the reactivity worth of the proposed poisons, hafnium and europium oxide, and (3) to substantiate the general analytical techniques used in predicting criticality, power profile, control drum worth, and poison worth in the SNAP 4 reactor design analysis.

A diagram of the SCA-5 critical assembly is shown in Figure B-8. The initial loading configuration consisted of 559 fuel tubes. Of these, 547 are in a regular hexagonal array, while the remaining 12 are positioned in the triangular cusp regions between the control drums. A diagram of the core and drum configuration is shown in Figure B-9. Two other configurations can be achieved by repositioning the control drums farther from the core centerline and adding additional rows of rods between the drums and the core array.

The Type 304 stainless-steel fuel tubes are 0.443 in. in diameter and positioned on a 0.462-in. pitch. The fuel tubes, lower grid plate, and four adjacent control drums are surrounded by an aluminum core tank. This tank can be filled with water as desired.

The four control drums are 4 in. in diameter and are positioned along two adjacent flats of the hexagonal core, as can be seen in Figure B-9. Two-thirds, measured along the diameter, of each drum is beryllium, while the remaining one-third consists of a strong neutron absorbing poison material. Stainless-steel cans are available for use as the poison backing portion of the drums; they can be filled with any desired poison material. The drums are driven by gearhead motors at a rate of about $0.7^\circ/\text{sec}$.

The remaining four faces of the hexagonal core are surrounded by two movable, 3-in.-thick, beryllium reflector pieces. These reflector pieces serve as safety elements. They are held against the core tank by electromagnetic clutches. When the critical assembly is scrammed, the clutches release the

5-20-63

7569-0594

Figure B-9. SCA-5 Core and Drum Configuration

reflector pieces and they fall away from the core under the combined action of spring compression and gravity.

Compacted powder fuel pellets were prepared for use in the SCA-5. The number and compositions of these pellets are given in Table B-VI. The pellets are 0.41 in. in diameter and 0.43 in. in length.

TABLE B-VI
AVAILABLE COMPACTED POWDER FUEL PELLETS

Number of Pellets Available	Pellet Composition (wt %)			
	UO ₂	ZrH _{1.93}	HfH _{1.93}	Eu ₂ O ₃
13,344	10	90		
4,900	10	89	1	
461	10	88	2	
479	10	87	3	
506	10	84	6	
490	10	89.9		0.1
464	10	89.7		0.3
435	10	89.4		0.6
470	10	86.7	3	0.3
427	10	83.4	6	0.6

The critical water height for the SCA-5, "flux-flattened" 12-in. core was predicted to be 11.7 in. $\pm$ 0.6 in. The drum worth for the initial configuration with beryllium inserts and three boron stainless-steel drums and one boron carbide, powder-compacted drum was calculated to be \$4.53 $\pm$ 20¢. Measurements performed in the SCA-5 would have been employed to verify the adequacy and accuracy of nuclear analytical methods. The nuclear design of the core would then be checked with the verified techniques.

2. Results and Conclusions of Critical Tests

The results and conclusions of the SCA-5 critical experiments are summarized as follows.

a. Control Drum Worth

The reactivity worth of drums with the available two different backings were measured: a B_4C compacted powder backing at about 50% theoretical density, and a backing using 2.27% natural boron dispersed in stainless steel.

The maximum and minimum worth of the B_4C drum was determined to be \$1.87 and \$1.46, respectively. The maximum worth was measured with the adjacent drum in its most reactive position and fuel rods in the cusp region between the drums. The minimum worth was measured with the adjacent drum in its least reactive position. The effect of loading fuel in the cusp region was estimated to result in an approximate 5% increase in the drum worth. The effect of water level on drum worth was 4.3¢ decrease in worth per inch increase in core water level.

The maximum and minimum worth of the boron- stainless backed drum was determined to be \$1.58 at the optimum geometrical conditions described above, and \$1.17 for the less favorable geometrical configuration.

The most significant conclusion that can be drawn from these experiments is that power shaping and fuel cusping increased control drum worth by over 50% as compared to nonflattened, noncusped configurations as, for example, studied in the SCA-3 12-in. core experiments completed in 1962. These experiments investigated the reactivity worth of these identical drum backings.

b. Poison Reactivity Worth

The reactivity worth of 1 wt % hafnium in fuel was measured relative to unpoisoned fuel and water. The worth of europium was measured as a function of concentration to study self-shielding effects. These results agreed quite well with what was expected. In particular, previous SNAP 4 analysis indicated a very large self-shielding effect for europium. The danger coefficient experiments substantiated this. The effective cross section was reduced by about 40% on going from 1.0 to 2.0 wt % Eu_2O_3 in graphite samples.

c. Substantiation of Analytical Techniques and Data

The analytical techniques and data used in the SNAP 4 nuclear analysis were felt to be adequate. The as-built experiment differed from the specified, calculated experiment primarily only in the increased amount of hydrogen

introduced in the fuel binder. When this increased hydrogen loading was properly accounted for in the post-analysis, the criticality, drum worth, power profile, and poison worth calculations were in good agreement with experiment. In particular: (1) it was apparent that analysis under-predicted reactivity by about 2% $\Delta k/k$, as was expected from previous critical assembly analyses; (2) the effect of power shaping and fuel cusping was expected to increase control drum worth by over 50% and this was substantiated by the experiment; (3) the radial peak-to-average power ratio was predicted to be 1.09, the experimental result was about 1.1; and (4) the analysis predicted significant self-shielding effects on the effective cross section of europium oxide and this was also experimentally verified.

APPENDIX B REFERENCES

1. F. L. Fillmore, "ARES-1, A Resonance Integral Code," AMTD-130-A, October 1962
2. J. C. Corelli and F. Feiner, "The Burnup of Europium and Dysprosium Under Neutron Irradiation," KAPL-M-JCC-2, October 29, 1959
3. J. L. Russell, Jr., M. R. Carrothers, W. V. Mosgovag, "Absorber Burnup Experiment - Final Report," GEAP-4017, April 1962
4. C. R. Greenhow and E. C. Hansen, "Reactor Spectra Effects on Fission Fragment Cross Sections," KAPL-2057, December 1, 1959
5. C. R. Greenhow and E. C. Hansen, "Thermal and Resonance Fission Product Poisoning for U-235 Systems," KAPL-2172, October 19, 1961
6. J. R. Stehn, "Physics of Intermediate Spectrum Reactors," Vol. 3, p 152
7. D. P. Satkus and H. P. Platt, "SIZZLE," February 1961 (An internal NAA Program Description)
8. J. D. Garrison and B. W. Roos, "Fission Product Capture Cross Sections," Nuclear Science and Engineering, Vol. 12, pp 115-134, 1962
9. R. D. Doctor, "S4ER Coefficients of Reactivity," NAA-SR-MEMO-7961, November 1962
10. R. C. Noyes, et al, "FUGUE, Calculation of Steady-State Temperature, Pressure and Void Fraction in Pipe Flow With or Without Boiling," NAA-SR-5958, May 1961
11. J. Miller, W. J. Roberts, and R. L. Brehm, "Temperature Coefficients and Spectra in the Hydride-Moderated SNAP Reactors," NAA-SR-7140, December 30, 1962
12. W. A. Rhoades and G. A. Long, "Multi-Group Reactivity Calculations for One-Region Systems," NAA-SR-MEMO-7515, June 1962
13. B. C. Carlson, C. Lee, and J. Worlton, "The DSN and TDC Neutron Transport Codes," LAMS-2346
14. Los Alamos Neutron Transport Code (to be published)
15. D. C. Baller, "The FAIM Code - A Multi-Group, One-Dimensional, Diffusion Equation Code," AMTD-118, 1962
16. D. Vargofeak, "The ULCER Code - A Multi-Group, One-Dimensional, Diffusion Equation Code with Upscatter," NAA-SR-7146, 1962
17. Gordon E. Hansen, William H. Roach, "Six and Sixteen Group Cross Sections for Fast and Intermediate Critical Assemblies," LAMS-2543

18. R. L. Macklin and H. S. Pomerance, "Resonance Capture Integrals," Progress in Nuclear Energy, Series I, Physics and Mathematics, p 179, McGraw-Hill, New York, 1956
19. Meghreblian and Holmes, "Reactor Analysis," p 479-482, McGraw-Hill, New York, 1960
20. R. A. Becker and J. L. Russel, Jr., "Relative Effectiveness of Reactor Control Materials," GEAP-3201, July 1959

BLANK

APPENDIX C. FUEL TECHNOLOGY

A. DESCRIPTION OF THE FUEL ELEMENT

The S4PR fuel element, depicted in Figures C-1 and C-2, measures 20-in. in length and has a diameter of 0.438 in. The calculated weight of one element is 0.80 lb (~13 oz.). Basically, the fuel element consists of four components:

- 1) The segmented fuel
- 2) Cladding
- 3) End caps, and
- 4) Wire wrap.

The reactor core consists of two regions, a high-poison region and a low-poison region, (Figure B-4). Approximately 25% of the 583 fuel elements per core are located in the high-poison region; of these rods only the central section, measuring 10.20 in. in length, contains the high-poison material (Figure C-2); the end portions of these elements and the remaining 75% of the reactor fuel elements are of low-poison composition. Fuel elements in the low-poison region will also contain segmented fuel for the following reasons. Past experience at AI indicates that fuel warping during hydriding is minimized with shorter fuel lengths. Minimum warping results in a direct fuel saving since the fuel may be extruded to a diameter approaching that of its finished ground diameter. Machining tolerances will be more easily met on several short segments than on one continuous 17-in. length.

Cladding dimensions of the finished element are:

Length — 18.375 in.

Inside Diameter — 0.408 in.

Wall Thickness — 0.015 in.

Studies of material compatibility of fuel and cladding in simulated reactor environment have indicated that the Type 300 series stainless steels are completely inert. Firm selection of a Type 300 series stainless, viz. Type 347, however, were not yet made, since its use prohibits application of the die-sizing fuel assembly method. The Type 400 series stainless steels, in particular, Type 446, were also being studied since they would permit these relatively inexpensive fabrication techniques to be employed (i.e., die-sizing). The reader is directed to Section F, "Fabrication of the Fuel Element," for a more comprehensive discussion of this subject.

4-30-63

7564-5104B

Figure C-1. S4PR Fuel Rod

The basic end cap configuration is shown in the inset of Figure C-1. The grid plates fit over the follow end cap to the collar. Slots milled in the end cap provide paths for the flow of coolant to the core. Material used for the end caps will be identical with that of the cladding.

The purpose of the wire wrapping is to eliminate contact between any two adjacent fuel elements. Approximately one-half of the fuel rods in the core will be wrapped. The "wire" employed for this purpose will be hypodermic needle tubing, approximately 0.020-in. diameter, wound around the element on a 6-in. pitch, in a manner similar to that employed in the EBR-II core. The wire will be welded to the end caps at both ends.

B. REQUIREMENTS AND ANALYSIS OF FUEL ELEMENT PERFORMANCE

1. Heat Transfer

The uniqueness of the SNAP 4 reactor stems largely from the high-performance fuel elements employed. The entire core, 583 elements, contains approximately 95 ft^2 of heat transfer area. A power output of 12 Mwt results in

Figure C-2. Schematic of Fuel Element

POINT	TEMP. °F	
	AVG.	MAX.
①	643	654
②	690	713
③	708	736
④	879	949
⑤	964	1056

NOTES:

(1) A BOILING FILM ΔT OF 30°F WAS ASSUMED.

(2) "CRUD FILM" $h = 10,000 \text{ Btu/hr, ft}^2, \text{F}$

(3) k (347 S.S. @ $\sim 660\text{F}$) = 11.4 Btu/hr, ft², F/ft

$$\therefore k/l = \frac{11.4}{0.015} \times 12 = 9130 \text{ Btu/hr, ft}^2, \text{F}$$

(4) $k_{\text{H}_2} = 0.178 + 1.4 \times 10^{-4} (t - 572) \text{ Btu/hr, ft}^2, \text{F/ft}$

@ $t = 700$, $k = 0.196$ & $k/l = 23,500 \text{ Btu/hr, ft}^2, \text{F}$

$$(5) \Delta T = \frac{(Q/A)d}{4k_f}; k_f = 14.4; \frac{4k_f}{d} = \frac{4(14.4)12}{(0.410)} = 1685 \text{ Btu/hr, ft}^2, \text{F}$$

7-2-63

7564-0287

Figure C-3. Fuel Element Temperature Distribution

an average heat flux of 430,000 Btu/hr-ft², and a maximum heat flux of 540,000 Btu/hr-ft², based on a peak-to-average flux ratio of 1.25. Results of an analysis of the temperatures within the fuel element and the basic thermal data used are shown in Figure C-3. This analysis is based on the best thermal property data currently available and where uncertainties are encountered, conservative estimates have been made. One such instance occurs at the fuel-cladding interface. Any void region between the fuel and cladding will contain hydrogen gas at low pressure (approximately 5×10^{-3} atmosphere). A graph of experimental data¹ was used to determine the thermal conductivity of the hydrogen (Figure C-4). A radial clearance of 0.0001 in. between the fuel and cladding results in a temperature difference of about 18°F. This magnitude of the clearance was selected by taking twice the maximum observed profilometer reading (50 micro inches) for ground fuel slugs and is probably an order of magnitude greater than that which will occur in an interference-assembled fuel element. The fuel-cladding clearance and the associated temperature rise at this interface is one of the points at which the heat transfer performance of the actual fuel elements may very well exceed analytical estimates.

Minimizing the temperature rise through the fuel lowers the central temperature of the element further below the threshold fuel swelling temperature of 1200°F. The desirability of reducing the maximum fuel temperature below the threshold swelling temperature led directly to the shrink-fit fuel assembly development work. Conservatism employed in the heat transfer analysis and the success of the shrink-fitting experiments indicate that satisfactory fuel element performance can readily be obtained. Further research may indicate that a more economical fabrication process may also prove satisfactory.

2. Structural

A general stress analysis of the S4PR fuel element was completed. The analysis indicated that sources of stress and instability are primarily related to: assembly techniques, dimensional tolerances, temperature gradients, dynamic loads, and system pressure. The completed analysis includes the following topics:

- a) Thermal stress in the fuel slugs
- b) Thermal stress in the cladding

7-2-63

7564-0288

Figure C-4. Effect of Temperature on the Thermal Conductivity of Hydrogen at Subatmospheric Pressures

- c) Mechanical stresses caused by thermal expansion
- d) Elastic buckling of the cladding
- e) Elastic buckling of the fuel element
- f) Transverse vibration of a fuel element
- g) Transverse vibration of a fuel element caused by a shock load

Numerical solutions to the above analysis have been obtained on the basis of preliminary fuel properties. However, since the fuel property data for the carbon-modified hydride have but recently become available, additional and final calculations have not been made. Improved structural properties were realized with the addition of carbon to the fuel. When recomputation is completed, it is anticipated that a very low level of stress will be predicted; i.e., cracking of the fuel due to thermal stresses or thermal shocks is highly unlikely. This result arises from the low value of Young's modulus ($<7 \times 10^6$ psi) obtained in the recent measurements.

Continued effort in the area of stress analysis is primarily directed toward the problem of possible fuel element bowing as a result of radial flux and hence, thermal gradients. Preliminary calculations predict less than 0.002-in.

al bow for an assumed 30% radial heat flux differential. These calculations were based on a simplified solution to the problem and therefore can be regarded as a first approximation.

Type 347 stainless steel has been selected as the preferred fuel cladding material. Its excellent irradiation behavior summarized in the graphs of Figures C-5 and -6, provides assurance of maximum fuel element integrity. The selection of Type 347 stainless steel necessitates an interference-fit assembly technique. Therefore, some additional research was to be directed toward other materials which would allow potentially more economical fabrication techniques.

3. Hydrogen Redistribution Within the Fuel

The SNAP 4 fuel element is subject to a maximum radial temperature gradient of 320°F under full-power operating conditions. With this temperature gradient as the driving force, the distribution of hydrogen in the fuel will gradually change as hydrogen diffuses from regions of high temperature to those at lower temperatures. If redistribution of the hydrogen were of sufficient magnitude to cause a phase transformation within the fuel, density changes would occur and would create stresses which could in turn cause fuel cracking. To assess the probability of such phase change, calculations were made to determine both the equilibrium N_H gradient in the fuel and the rate at which the redistribution could be expected to proceed. These computations are based upon the most conservative experimental value for the activation energy for hydrogen diffusion.

Axial hydrogen redistribution was neglected in this analysis as the axial temperature variation over the full length of the fuel element is computed not to exceed $\pm 45^\circ\text{F}$ from the mean. Applying the parabolic radial temperature distribution from Figure C-3, the equilibrium hydrogen concentration profile was calculated assuming that the radial vapor pressure gradient for hydrogen in the fuel is zero at final equilibrium. Details of this analysis are presented in Reference 7. From an initial uniform hydrogen concentration of 5.5×10^{22} atoms/cc, the radial temperature gradient will cause a redistribution of the hydrogen resulting in an N_H of 6.23 at the outer surface and an N_H of 4.80 at the centerline of the fuel element. This change does not lead to phase transformation; Figure C-7 depicts the physical conditions described superimposed upon the Hydrogen-Zirconium phase diagram. Thus, with the selection of an initial N_H of about

7-2-63

7564-0289

Figure C-5. Short-Time Tensile Properties of AISI Type 347 Stainless Steel as a Function of Temperature

7-2-63

7564-0290

Figure C-6. Effect of Neutron Radiation at 80°C on Room-Temperature Tensile Properties of Annealed Type 347 Stainless Steel

7-2-63

7564-0291

Figure C-7. Partial Phase Diagram of Zirconium-Hydrogen System

5.5 for the SNAP 4 fuel, the final equilibrium hydrogen distribution (computed on the most conservative basis) will not cause any of the fuel to be transformed into the $\beta + \delta$ or $\alpha + \delta$ regions. Therefore, the structural stability of the fuel element is unaffected by the migration of hydrogen under the influence of the thermal gradients imposed by full-power operation.

The data of Sommer and Dennison² were applied to the calculation of the rate of hydrogen redistribution for the SNAP 4 fuel conditions. Results are presented in Table C-I below and in Figure C-8. These results show that more than one year of full-power operation is required to attain the equilibrium condition.

TABLE C-I
KINETICS OF HYDROGEN REDISTRIBUTION

Percent Equilibrium Value Attained	Time to Attain Percentage Redistribution – Hours	
	Data of Sommer & Dennison	Estimated for SNAP 4*
30	112	566
40	172	870
50	268	1355
60	385	1945
70	562	2840
80	860	4343
90	1450	7320

*For the hottest fuel

An estimate of the maximum vapor pressure of hydrogen within the fuel element yields a value of 1.7×10^{-2} psia. Since hydrogen will also be dissolved in the reactor coolant, with a predicted vapor pressure of 0.12 psia, it seems highly unlikely that any loss of hydrogen from the fuel will occur under the conditions specified for SNAP 4 reactor operation. Since the permeability of all of the stainless steels to hydrogen is extremely low at SNAP 4 temperatures, it is also unlikely that hydrogen will be absorbed by the fuel from the coolant.

4. Thermal Cycling

Thermal cycling experiments were conducted to confirm the dimensional stability of the fuel, of fundamental importance to the maintenance of the

7-2-63

7564-0292

Figure C-8. Rate of Hydrogen Redistribution

interference fit between fuel and cladding. Specimens of 90% Zr-10% U alloy hydrided to an N_H of 6.0, were cycled between the conditions summarized in Table C-II.³ The results revealed no change in dimensions to an accuracy of approximately 1 part in 2000. Examination of the microstructure showed no significant change between the pre-cycled condition and after thermal cycling.

Since fuel specimens of higher N_H were subjected to higher temperatures than those corresponding to SNAP 4 conditions, variability of dimensions is not anticipated under SNAP 4 conditions due to thermal cycling.

TABLE C-II
THERMAL CYCLING DATA

Laboratory Designation	No. of Cycles	Cycling Range		
		Upper	Lower	
"A" (modified)	325	650°C	400°C	All temperatures $\pm 30^\circ$
"C" (modified)	6	690	250	Ampoule shattered
"B" (modified)	404	690	210	
"A" (unmodified)	402	690	300	Ampoule not properly sealed
"B" (unmodified)	400	730	300	

Modified specimens showed equiaxed structure; unmodified specimens were columnar. Dimensions 0.53 in. diameter, 1.25 in. long.

5. Fuel Integrity - Corrosion Stability

Massive zirconium-uranium hydride is a stable material under all normal handling conditions. The addition of carbon, manifested as a dispersion of minute zirconium carbide particles throughout the matrix, resulted in a highly refined grain structure and eliminated the tendency of preferred orientation observed after extrusion in the unmodified alloy. The modified alloy has proved more stable during hydriding and post-hydriding machining operations with the virtual elimination of the tendency for cracking to occur.

The stability of the fuel in high-temperature water was demonstrated via static corrosion tests. Bare fuel specimens were subjected to a water environment of 570°F and 1230 psi pressure during a 400-hr period in an autoclave. The average corrosion rate observed was $+353 \text{ mg/dm}^2, \text{ mo.}$ The gain in weight accompanies the conversion of the surface layer of the hydride to an adherent oxide film. This rate categorizes the hydride as a Class III, corrosion resistant material, as reported in the Corrosion and Wear Handbook for water-cooled reactors.⁴ Thus, this fuel falls into the same class of materials as Inconel, columbium and aluminum. The maximum extent of corrosion penetration after 400 hr was less than 2 mils.

Experiments⁵ were conducted to evaluate the compatibility of SNAP 4 fuel (hydrided 85-15 Zr - U) with Types 304 and 446 stainless steel in a boiling water environment (570°F and 1230 psia for 400 hr). High purity water at a pH of 7 was employed at the start of the experiment. No reaction was observed between sandwiched wafers of the fuel and Type 304 cladding in either the water-free contact area or in the area where water came in contact with both materials. The water-free area for Type 446 SS and fuel showed no reaction, but a slight reaction was observed between the fuel and cladding where the boiling water was in direct contact with both materials.

Dynamic (high velocity) corrosion experiments were planned in which prototype fuel elements assembled by both fabrication processes would be subjected to typical reactor environmental conditions.

Thermal shock tests were performed using 90% Zr - 10% U unmodified fuel with an N_H of 6.5.⁶ The fuel specimens were 1/2 in. OD x 2-1/2 in. long. The experiment consisted of plunging the cylinders from a furnace at 1200°F

into a stagnant pool of eutectic NaK. The temperature of the NaK was reduced incrementally until a temperature difference of 900° F existed between the fuel and NaK. No cracking occurred nor were other effects observable. Calculations predicted that a thermal stress of 23,000 psi was attained during the shock test. Since the carbon-modified fuel is structurally superior to the material used in these tests, cracking of the SNAP 4 fuel due to thermal stress during reactor operation is unlikely. However, confirmation was to be sought via additional tests to be conducted with modified material of SNAP 4 composition.

C. IRRADIATION TESTS OF FUEL

1. NAA 43 Series of Irradiation Experiments

The NAA 43 series of irradiation experiments was initiated in 1959 to determine the irradiation stability of the original reference fuel. These experiments, utilizing 90% Zr - 10 U alloy hydrided to an N_H of 4.0, consisted of seven assemblies designated 43-1 through 43-7. Of these, only 43-3, 43-5, and 43-6 were completely examined. The 43-1 and 43-2 experiments were not given a post-irradiation examination since design information could be obtained from the higher burnup 43-3 capsule. The 43-4 experiment aborted because of a test reactor transient which induced temperatures $\sim 2000^\circ\text{F}$ in the fuel. The 43-7 experiment was not assembled because of funding limitations.

In the NAA 43 experiment series, the fuel was selectively ground to slip into honed cladding tubes under normal conditions of assembly. The nominal clearance was approximately 0.0003 in.

The post-irradiation examinations of the 43-5 and 43-6 capsules have been completed. Results of the 43-3 post-irradiation examination are described in Reference 7.

a. NAA 43-6 Experiment (Design Burnup of 2 met. at. %)

Examination of the irradiated 43-6 capsules, consisting of one specimen, was initiated in November 1962, but was suspended until GFY 64 after the fuel slugs had been partially declad. In the initial phase of the post-irradiation examination, visual observation of the cladding surface revealed no evidence of unusual changes due to irradiation. Diameter measurements indicated that the

cladding remained within as-fabricated tolerances. Mass spectrometer analysis indicated a burnup of 3.35 met. at. %.

A length measurement of the fuel pin was made in the second phase of the examination. This measurement was made optically on the partially declad fuel because of its fragile condition, and further, it became quite obvious during the examination that further mechanical removal of the cladding would fracture the fuel. Comparison of the pre- and post-irradiated length measurements indicated an axial fuel growth of 1.33%.

Metallographic examination revealed that the fuel was cracked along radial lines; however, this may have occurred in the cladding stripping operation. No diffusion zone existed between the fuel and cladding. There was, however, a very thin reaction zone at the fuel-cladding interface, which was to be expected because of their mutual contact. The lack of diffusion zone was significant in that cladding strength and integrity was maintained despite its intimate contact with the fuel.

Diameter measurements of the fuel were made optically on the mounted fuel. These indicated a change in diameter from the pre-irradiated values ranging from 0 to 0.96% with an average of 0.55%. The corresponding numerical change was 0 to 0.0018 in., and the average value was 0.0010 in. This average value corresponds closely to the unirradiated cladding diameter within the accuracy of these measurement.

Density measurements were made by Archimedes' principle on three pieces of fuel from which the cladding had been chemically removed. The method utilized monobromobenzene as the liquid because of its relatively high density (~ 1.5 gm/cc) and excellent wetting and low surface tension characteristics. Comparison of the pre- and post-irradiation values indicate a change in density ranging from 5.9 to 11.4% with an average of 8.3%. The pre-irradiation values of density used to obtain these changes were inferred from data on the hydrogen analysis and thus there is a large uncertainty in the above density changes.

b. NAA 43-5 Experiment (Design Burnup 2.3 met. at. %)

Examination of the 43-5 capsules, containing an upper and lower specimen, commenced in May 1964. Microscopic observations and dye penetrant

tests revealed that the cladding remained intact without evidence of defects. Mass spectrometer analyses indicate a burnup of 1.89 met. at. % in the 43-5 lower specimen and 2.05 met. at. % in the upper specimen.

Diameter measurements of the clad fuel indicated a change from pre-irradiated values for the upper pin ranging from 0.43 to 2.7% with an average of 1.6%. Corresponding values for the lower pin indicated zero change.

To make length measurements of the fuel, a chordal cut was made on both clad specimens along their full length. Length measurements were made optically. The resultant change from pre-irradiation values was zero for the upper specimen and 2.1% for the lower specimen.

After chemically decladding the fuel, density measurements were made in the same manner as described for the 43-6 examination. The density change from pre-irradiation values was 8.6% for both upper and lower fuel slugs. As with the 43-6 data, the pre-irradiated values of density were inferred from the hydrogen content at the time of assembly.

Hydrogen analysis performed by vacuum extraction techniques indicated a concentration of 1.05 wt % hydrogen in the lower fuel slug and 0.95 wt % in the upper slug. The concentration in the upper slug was determined from two fuel samples. Essentially no loss of hydrogen occurred in the lower slug.

2. NAA 75 Series of Irradiation Experiments

During 1963, the NAA 75 series of fuel irradiation experiments was designed, fabricated, and inserted in the MTR. This series, consisting of two capsules, each of which contains two fuel specimens, was designated 75-1 and 75-2. The fuel specimens were clad by shrink-fitting into a Type 304 stainless-steel jacket. The fuel contained 15 wt % U, 6.5 wt % Hf, 0.3 wt % C, and the remainder Zr, hydrided to an N_H of 5.35. Each capsule contained fuel specimens of 0.408 in. and 0.288 in. in diameter and 4 in. in length. The larger diameter specimens were designed to represent an exact simulation of reactor conditions, while the 0.288-in.-diameter specimens were designed for accelerated irradiation conditions to obtain levels of burnup twice that of the larger specimen. Target burnups for the 75-1 specimens were 1 and 2 met. at. %, while those of the 75-2 specimens were 0.5 and 1 met. at. %. The capsules were designed to achieve a temperature of 1075° F at the fuel center and 725° F at the

fuel surface. Figure C-9 depicts the 75-1 clad specimens prior to capsule assembly and irradiation.

a. In-Pile Performance

The 75-1 and 75-2 experiments operated satisfactorily with no evidence of cladding or other failure during their irradiation periods with the exception that four of the twenty-four installed thermocouples failed or operated erratically at various times in-pile. The operating thermocouples provided sufficient data to permit a successful completion of the irradiation experiment.

Table C-III presents the time-averaged centerline and maximum temperatures observed for the four fuel slugs, together with an estimate of the burnup experienced as calculated from the fuel temperatures and irradiation time; recent experimentally measured burnup values are also included.

During the in-pile operating period, the fuel-cladding temperature difference in all specimens remained very close to the values calculated from the observed fuel centerline temperatures and pre-irradiated values of thermal conductivities. Towards the end of the irradiation period, the observed temperature difference decreased slightly. Thus, irradiation does not appear to have adversely influenced the heat transfer performance of the clad specimens, but rather it may have slightly improved the conductivity of the assembly. This slight improvement was probably the result of a better transfer of heat across the fuel-cladding interface as caused by fuel growth.

TABLE C-III
NAA 75-1 AND 75-2 IRRADIATION EXPERIMENTS, IN-PILE
TEMPERATURES AND CALCULATED BURNUP LEVELS

Specimen	Time-Averaged ☉ Fuel Temperature (° F)	Maximum Observed Fuel Temperature (° F)	Calculated Burnup (met. at. %)	Experimental Burnup (met. at. %)
75-1 Large Diameter	1075	1270	1.05	0.68*
75-1 Small Diameter	1011	1225	2.12	2.15
75-2 Large Diameter	1059	1235	0.63	0.50
75-2 Small Diameter	914	1020	1.12	1.26

*A second sample should be submitted for experimental burnup determination because of the large discrepancy between this value and the corresponding calculated value.

3-18-63

7564-5106

Figure C-9. Pre-Irradiated Condition of
NAA 75-1 Experiment

b. Post-Irradiation Examination

Visual macro-examination of the specimens revealed that the cladding remained intact without evidence of cracking or unusual appearance. Dye-penetrant examination verified this observation. Figure C-10 shows photos of the irradiated specimens.

Diameter measurements of the clad specimens exhibited a fairly consistent pattern of radial growth with burnup, yielding about a 0.9% diameter change per met. at. % burnup as inferred from the slope of the variation of diameter change with burnup, Figure C-11. The 2 met. at. % burnup specimen (75-1 small-diameter specimen) displayed an average tangential strain of 2.23% in the cladding which received an estimated dose of 10^{21} nvt ($E \geq 1$ Mev).

The cladding was removed along a chordal section longitudinally to expose the fuel for length measurements. The removal was accomplished with a sanding disc onto which the specimens could be held in a hardened steel mount.

10-9-64

75-1 Large Diameter Specimen

7564-4703

10-9-64

75-1 Small Diameter Specimen

7564-4704

10-9-64

75-2 Large Diameter Specimen

7564-4705

10-9-64

75-2 Small Diameter Specimen

7564-4706

12-23-64

7564-4711

Figure C-10. NAA Series 75 Specimens

NOTES:

1. LIMITS ON $\Delta D/D$ VALUES OBTAINED FROM RANGE OF MEASUREMENTS
2. VALUE OF MEASURED BURNUP OF 75-1 LOWER BELIEVED TO BE LOW BY A FACTOR OF 2
3. POSITIVE VALUE OF INTERCEPT IS DUE TO DIFFERENCE IN MEASUREMENT TECHNIQUES IN PRE- AND POST-IRRADIATION MEASUREMENTS

SYMBOLS

- 75-1 UPPER
- △ 75-1 LOWER
- 75-2 UPPER
- ▲ 75-2 LOWER

6531-5632

Figure C-11. NAA 75-1 and 75-2 Irradiation Experiment Results, Variation of Diameter Change with Burnup

The specimens were fixed into their mounts with dissolvable adhesive. The mounting was designed to limit the fuel removal to 5 to 10 mils.

After longitudinal grinding, severe cracking was evidenced on three of the four specimens, and end effects indicating abnormal conditions were present on all specimens. Subsequent examination of a transverse section of the specimens revealed that the major crack extended circumferentially at a mid-radial location.

Whether the cracking occurred during irradiation and was opened by relaxation of the slit cladding, or was caused by the cladding relaxation itself cannot be ascertained. There is some evidence that a bond was created between the fuel and cladding based on cladding diameter measurements made on the slit cladding. Residual stresses on the order of 20,000 psi were inferred from a comparison of these measurements with those made of the intact cladding. Stresses greater than 20,000 psi were anticipated based on the amount of strain in the irradiated cladding induced by fuel growth. Thus, one could speculate that some degree of restraint remained in the cladding as caused by the fuel-cladding bond. As mentioned previously, no adverse effect of the cracks was noted in the in-pile thermal performance of the specimens, if indeed they were present in-pile.

Detailed examination of the in-pile temperature history of the capsules indicated that the ends of the fuel pins may have been subjected to temperatures in excess of 1400° F. The capsules were designed with heat barriers which overlapped the ends of the fuel slugs in order to minimize the axial temperature gradient. One thermocouple which was inadvertently positioned 3/16 in. from the upper end of the 75-2 large diameter specimen indicated cladding temperatures 100 to 150° F higher than the others. It is possible, therefore, that the abnormal appearance of the ends of the specimens is the result of phase changes in locally severe hot spots.

Length measurements of the exposed fuel were made optically using a Filar eyepiece mounted on a stereomicroscope. Unlike the pre- to post-irradiation diameter measurements, no consistent pattern of growth with burnup level was discernible. In this case, the true longitudinal growth of the fuel, as caused by fissioning, may have been masked by the end effects and/or irregular cracking.

Density measurements were made on the center portions of the specimens obtained by removing approximately 1 in. from each end of the fuel column. Density measurements were performed both on the clad fuel and on bare fuel from which the cladding had been removed chemically. The fuel density of the clad specimens was obtained using the dimensions of the cladding to provide a check on the densities measured on the bare fuel. The two values of densities compared well for all specimens, being within 0.3% in all cases.

Table C-IV lists the observed changes in diameter, length, and density of the four specimens, and compares volume changes inferred from the density and from the dimensional measurements. The data are plotted on Figure C-11 with an indication of the experimental error.

TABLE C-IV
NAA 75-1 AND 75-2 IRRADIATION EXPERIMENTS, PRE-TO-POST-IRRADIATED DIMENSIONAL AND DENSITY CHANGES

Specimen	$\Delta D/D$ (%)	$\Delta L/L$ (%)	$2(\Delta D/D) + \Delta L/L$	$3(\Delta D/D)$	$\Delta \rho/\rho_{\text{post}}$
75-1 Large Diameter	1.25	0.5	3.0	3.75	3.92
75-1 Small Diameter	2.23	0.95	5.4	6.69	7.01
75-2 Large Diameter	0.94	1.64	3.5	2.82	4.34
75-2 Small Diameter	1.31	1.45	4.1	3.93	2.77

Considering the inaccuracies inherent in inferring $\Delta V/V$ from dimensional measurements, the density changes compare reasonably well with the dimensional changes.

Hydrogen analysis performed by vacuum extraction techniques indicated results given in Table C-V.

TABLE C-V
NAA 75-1 AND 75-2 IRRADIATION EXPERIMENTS
CHANGE IN HYDROGEN CONTENT

Specimen	Pre-(Vacuum Extraction)	Pre-(Weight Gain)	Post-(Vacuum Extraction)	Hydrogen Loss (wt %)
75-1U	1.44	1.35	1.22	0.13
75-1L	1.46	1.34	1.48	0
75-2U	1.41	1.32	1.33	0
75-2L	1.40	1.32	1.45	0

3. Irradiation Damage

Successful operation of the SNAP 4 reactor to high fuel burnup depends to a great extent upon the assurance that a minimum of fuel growth or swelling will be encountered during irradiation. In addition to the favorable experimental evidence cited above, a report⁸ from the Argonne National Laboratory confirms the facts that: (1) thin (0.010 in.) Type 304 stainless-steel cladding provides sufficient restraint to inhibit swelling of alloy fuels to temperatures of 760°C and to burnups in excess of 2 met. at. %, and (2) fuel swelling is a temperature-dependent phenomena; i.e., there appears to be, for each type of fuel, a threshold temperature below which swelling does not occur.

A correlation of irradiation damage results for zirconium hydride fuels is presented in Figure C-12. The data plotted were obtained from irradiations of unmodified, 10 U - 90 Zr hydrided specimens at various temperatures and to varying levels of burnup. This correlation clearly indicates that the threshold swelling temperature for this material lies between 1150 and 1250°F, well above the maximum SNAP 4 fuel temperature of 1056°F. A further reason for confidence in the suitability of S4PR fuel lies in the fact that with the refinement of grain size attendant with the incorporation of carbon as a fuel modifier, it is anticipated that the threshold temperature will be found to increase significantly above the range indicated by the results of Figure C-12. These results pertain to the behavior of unrestrained fuel specimens; with the restraint afforded by the interference fit cladding, growth and/or swelling rates should be even less.

D. FUEL PROPERTIES

1. Physical Properties

a. Structure

Above an H/Zr ratio of 1.4, Zr - 10 wt % U hydride consists essentially of a face-centered-cubic zirconium hydride with a fine dispersion of metallic α -uranium. In the composition region H/Zr = 1.6 to 1.7, the matrix becomes a mixture of δ and ϵ (a face-centered, tetragonal bonded structure).⁹ Figure C-7 summarizes the zirconium-hydrogen phase relationships. The effect of the uranium addition is to shift the phase boundaries to the left.¹⁰

Figure C-12. Correlation of Damage in Hydride Fuels

7-2-63

7564-0293

b. Isochore

The isochore diagram, the composition-vapor pressure-temperature relationship (Figure C-13) was obtained for SNAP 4 fuel hydrided to an N_H of 5.72 and of the following composition: 15% U, 6.5% H_f , and 78.5% Zr.* The range of carbon concentration in the samples was from 0 to 0.6%. The effect of carbon is to increase the hydrogen dissociation pressure. Additional work was to be performed to obtain a complete isochore diagram starting with fuel samples of varying hydrogen content. Figure C-14 shows the extrapolated isochore diagram for SNAP 4 conditions based on the data of Figure C-3 and the family of isochores obtained from compiled data.¹¹

c. Other Physical Properties

Figures C-15 through -20 show the various fuel properties experimentally measured for the SNAP 4 fuel composition with an N_H of 5.6 to 5.8. The experimental errors, estimated for the various properties, were found to be as given in Table C-VI.

*For hafnium-bearing fuels, the H-to-Zr ratio is actually H-to-Zr + H_f .

7-2-63

7564-0294

Figure C-13. SNAP 4 Isochore

7-2-63

7564-0295

Figure C-14. Isochore Diagram
for SNAP 4 Fuels

7-2-63

7564-0296

Figure C-15. Thermal Conductivity

7-2-63

7564-0297

Figure C-16. Thermal Diffusivity

7-2-63

7564-0298

Figure C-17. Heat Capacity

7-2-63

7564-0299

Figure C-18. Electrical Resistivity

7-2-63

7564-0300

Figure C-19. Linear Thermal Expansion

7-2-63

7564-0301

Figure C-20. Density of SNAP 4 Fuel Hydride

AI-AEC-13069, Vol II

7-2-63

7564-0302

Figure C-21. Relationship Between H/Zr and N_H
for Fueled Zirconium Hydride

TABLE C-VI
FUEL PROPERTIES EXPERIMENTAL ERRORS

Figure	Property	Maximum Error (%)
C-15	Thermal Conductivity ¹²	±4
C-16	Thermal Diffusivity ¹²	±3
C-17	Heat Capacity ¹²	±4
C-18	Electrical Resistivity ¹²	±1
C-19	Linear Thermal Expansion	±5
C-20	Density	±1/2

Figure C-21 shows the relationship between N_H and H/Zr ratio for hydrided fuel containing uranium and hafnium.

2. Mechanical Properties

Due to the concentration of effort expended in obtaining the physical properties of the SNAP 4 fuel, the experimental determination of mechanical properties was not performed. However, much of the data assembled by the SNAP space projects is applicable to SNAP 4 since measured mechanical properties appear to be relatively insensitive to minor variations in composition. Basically, the hydride fuel is a brittle material similar in structural behavior to a ceramic in some respects and to cast iron in others.

a. Elastic Modulus

Preliminary tests on 10 U - 90 ZrH_X containing 0.15% carbon¹³ indicate that Young's Modulus decreases with an increase in hydrogen content and also decreases with increasing temperature. For an H/Zr ratio of 1.6, Young's Modulus varies between 4.3 and 5.9×10^6 at 1350°F. For an H/Zr ratio of 1.74 at 1350°F, Young's Modulus varies between 3.9 and 4.2×10^6 and at 1450°F from 2.0 to 2.1×10^6 psi. At room temperature, Young's Modulus, for an H/Zr range of 1.60 to 1.77, is 9.10×10^6 psi. It is estimated that at SNAP 4 operating conditions Young's Modulus should lie between 6.5 to 7.5×10^6 psi.

b. Ultimate Tensile Strength

Due to the notch sensitivity of the fueled hydride, large scatter is evident in data reported for tensile strengths. These data show the tensile strength to fall in the range of 8,000 to 11,000 psi, from room temperature to 1000°F.

c. Transverse Rupture Strength

Measurements¹⁴ disclose variations in this property with temperature for 10 U - 90 Zr alloy hydrided to a range of H/Zr = 1.65 to 1.74 (Table C-VII).

TABLE C-VII
TRANSVERSE RUPTURE STRENGTH
VARIATION WITH TEMPERATURE

Test Temperature (°F)	Average Transverse Rupture Strength (psi)
70	16,000
500	16,000
1000	20,000
1200	15,000

Rupture strength increases with temperature up to 1000° F, and then decreases rapidly. With hydride containing 7% uranium, the average transverse rupture strength is 18,000 psi at 70° F indicating a decrease in strength with increasing uranium content.

d. Compressive Yield Strength

Room temperature compressive strengths, for 10 U - 90 Zr with an H/Zr ratio of 1.6 to 1.8, lie in the range 43,000 ± 9,000 psi. Values for the 7 wt % U fuel hydrided to the same H/Zr range vary from 76,000 to 83,000 psi.

E. FUEL MATERIAL FABRICATION

1. Melting and Casting

In order to obtain experimental data on the melting and extrusion of SNAP 4 fuel, a series of developmental melts was made at Nuclear Metals Inc. Seven billets were melted to the following compositions by weight (Table C-VIII).

TABLE C-VIII
FUEL COMPOSITION BY WEIGHT
(wt %)

Number of Billets	U	Zr	C	Dy	Hf	Sm
2	15	84.8	0.2	0.0	0.0	0.000
3	15	78.8	0.2	1.0	5.0	0.015
1	15	72.8	0.2	2.0	10.0	0.015
1	15	71.8	0.2	3.0	10.0	0.015

The melting and casting was carried out in a bottom pour, vacuum induction melting furnace. Virgin charges of zirconium and hafnium, are initially placed in the graphite crucible and melted at approximately 1865°C. The temperature was rapidly increased to 1925°C and the uranium and poisons introduced in the crucible. The melt was held at 1900 to 1925°C for 1 min at which time it was poured from the furnace to cool. Each casting weighed about 13 kg.

An enriched billet of composition 15% U, 6.5% H_f, 0.2% C and the remainder zirconium was also melted and cast. The apparatus, employed by Nuclear Metals Inc. in the preparation of these melts, is presented in Figure C-22.

2. Extruding

Billets for extrusion were obtained by machining a simple right circular cylinder, measuring 2.860 in. in diameter by about 8 in. in length, from each of the castings. These billets were sealed in evacuated, 16-gage copper cannisters. Extrusions were carried out at 1400°F, to effect a 35X reduction in area. The extruded rods were pickled to remove the copper and ground to finish size.

3. Addition of Poisons

The addition of poisons to the melt presented some problems. To ensure proper mixing of all components in the melt, the temperature was kept at 1900°C. Due to the low boiling point of samarium (1630°C), this poison could not be added in the elemental form. Dysprosium and hafnium were successfully introduced into the melt and evenly dispersed. It was found difficult to obtain the desired amount of dysprosium by charging the elements individually to the furnace. However, by preparing master alloys in premelts, the proper concentration of poisons can be obtained.

Europium was also being considered as a burnable poison for SNAP 4 based on more recent nuclear analyses. The boiling point of europium is 1440°C, which is even lower than that of samarium previously attempted. Since, with europium, the homogeneity requirements in the fuel are not stringent, this poison could be introduced as the oxide which is not volatile at 1900°C.

Additional melting experiments were planned to evaluate optimum process conditions.

7-2-63

7564-0303

Figure C-22. Induction Melting Furnace

4. Modifiers

In order to refine the grain size of the extruded alloy and subsequently of the hydrided material, and to eliminate the tendency to preferred orientation, the addition of carbon to the melt has been proven beneficial in the concentration range of 0.15 to 0.30 wt %.¹³ However, since in the induction melting process the crucible is made of graphite, contact of the melt with the walls of the crucible has been found to permit sufficient carbon to be absorbed to supply this requirement.

5. Hydriding

A limited amount of fuel of the S4PR composition was hydrided. A number of pieces of the enriched fuel, prepared by Nuclear Metals Inc. were hydrided to an N_H of 5.8.

Prior to hydriding, the alloy was examined metallurgically and the carbon was found to be uniformly dispersed as zirconium carbide particles throughout the U-Zr matrix and to a slight degree preferentially oriented in the direction of extrusion. After the material was vacuum annealed at 1600°F for several hours, the preferred orientation was significantly reduced.

After hydriding, samples showed the uranium to be dispersed along the grain boundaries and within the grains. The carbides generally had a larger particle size than the uranium and displayed a "corded" appearance. Although no large cracks were apparent in the specimens, a few microcracks were observed in two of the four samples. The hydrided material possessed a fine grain structure.

The hydriding process used was similar to the process employed in the production of fuel for the SNAP 2, 8, and 10A reactor projects. Hydriding experiments, performed at AI with unpoisoned, normal uranium zirconium alloys (containing 15 wt % U), have proven the adequacy of the hydriding process and cycle conditions perfected for the other SNAP project reactors.

6. Chemical Analysis and Quality Control

Chemical analyses of the alloys prepared revealed composition variations in the following ranges: 6.3 to 6.5 wt % hafnium, 15.39 to 15.50 wt % uranium, and 0.25 to 0.30 wt % carbon.¹⁴

Nuclear Metals, Inc., after receiving the results on these melts, concluded that the variations in composition detected in these alloys could be achieved routinely. They also predicted that the carbon content could be lowered by decreasing the time of contact between the melt and crucible.

7. Current Specifications

Utilizing the results obtained from the Nuclear Metals, Inc., study and from nuclear calculations of core performance, preliminary specifications for the S4PR core have been selected (Table C-IX).

TABLE C-IX
PRELIMINARY FUEL SPECIFICATIONS

High Poison Fuel (wt %)		Low Poison Fuel (wt %)	
U	15.0 ± 0.1	U	15.0 ± 0.1
H _f	6.77 ± 0.2	H _f	3.07 ± 0.1
Eu ₂ O ₃	0.54 ± 0.02	Eu ₂ O _e	0.24 ± 0.019
C	0.20 ± 0.05	C	0.20 ± 0.05
Zr	77.49	Zr	81.49

These specifications can be satisfied using the available alloy fabrication process described above. Since melting is essentially an art, additional refinements can be achieved as development continues.

F. FABRICATION OF THE FUEL ELEMENT

Two methods of fabrication of the S4PR fuel element remain as candidate processes following the completion of scouting work in this area. The other methods, e. g., stretch-forming, magneforming, and in-site hydriding, are less promising and have therefore been abandoned in favor of shrink-fitting and die-sizing. Details of these fabrication processes are described in the following, and the prerequisite cladding properties which determine the selection of process are outlined.

1. Shrink-Fitting

The shrink-fitting process of fuel assembly consists of heating the stainless-steel cladding to a sufficiently high temperature, thereby providing the radial clearance necessary for insertion of the fuel. Subsequent cooling of the cladding provides an interference or shrink-fit between the fuel and cladding. Interference fitting implies close tolerances which in turn result in a somewhat

higher relative cost per element when compared to other possible methods of fabrication. Preliminary studies indicate that the benefits obtained from this assembly method justify the procedure.

Probably the most obvious benefit derived from the shrink-fitting process pertains to the basic function of the fuel element — the transfer of heat to the working fluid. Virtual elimination of any "gas-gap" between the fuel and cladding minimizes this very important thermal resistance. Experimental evidence¹ indicates that at reactor operating conditions a fuel temperature increase of 20°F/0.0001-in. of "gas-gap" will occur. Ability to operate the core at minimum temperature minimizes the possibility of fuel swelling and in addition minimizes thermal stresses in both the fuel and cladding. Secondary benefits derived from an interference fit are especially important. Investigations by Atomics International and others⁵ indicate that a relatively thin cladding in intimate contact with the fuel can restrain swelling. Structurally, a shrink-fit fuel element is analogous to a prestressed member. Stresses caused by thermal gradients are minimized and other problems, viz., cladding buckling, are negligible.

The magnitude of the shrink or interference required was determined with the aid of a stress-strain diagram, the fuel element temperature distribution (Figure C-3) and known physical properties of fuel and cladding. Figure C-23 is the theoretical stress cycle for the cladding material superimposed on a stress-strain diagram for 18-8 stainless steel. The diagram may be interpreted as shown in Table C-X.

TABLE C-X
CLADDING THEORETICAL STRESS CYCLE DATA

Point	Path	Process	Stress (psi)	Clearance (in.)
0	0-1	Cladding hot at assembly temperature	0	+0.0035
		Cladding cooling to room temperature	-	-
1	1-2	Cladding at room temperature	32,000	-0.0010
		Heating element to 0 reactor power	-	-
2	2-3	Element at 0 reactor power (560°F)	0	+0.0005
		0 to full reactor power	-	-
3		Full reactor power (Figure C-24)	3,000	-0.0001

7-2-63

7564-0304

Figure C-23. Cladding Stress Cycle Annealed
18-8 Stainless - 90% Probability

Optimum tolerances cannot be assigned until experimental stress-strain cladding data as a function of operating temperature are known. Generally, the effect of irradiation and fuel growth should be beneficial in retaining the desired interference fit;¹⁵ however, since the effect is a direct function of time, it is of negligible aid during and following initial reactor startup. Decreased ductility of the cladding under irradiation will certainly contribute to fatigue-type failure of the element; this, however, is applicable to any type of reactor fuel element. Present studies indicate that cyclic (ratchetting) loadings in the element are negligible; therefore, fatigue failure is highly improbable.

The economic and engineering evaluations of the shrink-fit process of assembling fuel elements cannot be completed without further experimental data. The process to date appears very promising for the following reasons:

- a) Increased heat transfer capabilities
- b) Decreased chance of any fuel swelling
- c) Increased structural capabilities.

A feasibility evaluation was conducted by Atomics International to establish a preliminary assembly process and to verify analytical predictions. Three separate 6-in. specimens were prepared. Eutectic NaK, sodium-potassium alloy, at 1200°F ensured uniform heating of the cladding and provided an excellent heat source during assembly. Each assembly consisted of three, 2-in. zirconium slugs clad with annealed Type 304 stainless steel. Following assembly, the test samples were x-rayed to determine the magnitude of the butt-gap between adjacent slugs. No such gap was detected. One of the specimens was then sectioned and samples were polished. The other remaining specimens were instrumented with strain gages prior to slitting of the cladding longitudinally. The residual strain measurements were used to determine the hoop-stress in the cladding resulting from the shrink-fit process. The experimentally determined stresses were in close agreement with the calculated stresses; an average value being in the neighborhood of 30,000 psi.

2. Die Sizing

Fuel element assembly studies indicate that die-sizing is feasible for the fabrication of S4PR fuel elements. The cladding in this process is mechanically forced into close proximity with the fuel by means of ring dies. While the resulting element does not have the high thermal performance and mechanical integrity of a shrink-fit element, the assembly cost per element may be substantially reduced. Tolerances on the OD of the fuel and the ID of the cladding are virtually independent of the assembly method (i.e., either shrink-fitting or sizing). Additional tight tolerances on the tubing wall thickness and concentricity are mandatory for a successful sizing operation.

Results of preliminary die-sizing experiments are presented in Tables C-XI through -XIII.

TABLE C-XI
ROOM TEMPERATURE GAP MEASUREMENT

Speciman Number	Smallest Gap	Largest Gap (in.)	Average Gap* (in.)
1	Contact	0.0013	0.0006
2	Contact	0.0024	0.0007
3	Contact	0.0015	0.0007
4	Contact	0.0015	0.0004
5	Contact	0.0018	0.0006

*Average of eight readings

TABLE C-XII
FINISHED ASSEMBLY DIMENSIONS (DIE-SIZING)

Total Indicator Reading (over 17 in.)	Maximum Diameter (in.)	Minimum Diameter (in.)
0.0006	0.4062	0.4060
0.0008	0.4062	0.4060
0.0005	0.4062	0.4060

TABLE C-XIII
CHANGE IN CLEARANCE BETWEEN FUEL AND CLADDING
AS A FUNCTION OF TEMPERATURE
FOR DIE-SIZED SPECIMEN

Temperature (°F)	Clearance (0.001 in.)
R.T.	0.47
150	0.77
200	0.76
290	
300	0.90
350	0.85
400	0.85
410	
450	0.89
500	0.95
580	
600	1.15
650	1.23
600	1.16
590	
510	
500	0.96
450	0.96
400	0.90
300	0.78
200	0.77
R.T.	0.45

The dimensions used in the die-sizing operation were those of convenience. Materials employed were Type 304 stainless-steel cladding and Zr-U hydride as fuel. A fuel-to-cladding gap, or spring-back, will always exist in elements employing a sizing operation for assembly; however, the gap may be minimized by annealing the tubing after successive draws and by making a "light" final draw, prior to the final assembly of the element. Ideally, minimum cladding "spring-back" would be achieved by barely exceeding the elastic limit of the cladding. Variations in tubing wall thickness and concentricity require a heavier draw than the ideal minimum and attempts to maintain fuel-to-cladding contact during an experimental sizing operation have resulted in fuel cracking. Therefore, a non-uniform radial gap, ranging from possible contact to 0.0004 in. or more, is obtained with the sizing operation.

Selection of a cladding material (such as Type 446 stainless steel) having a low coefficient of thermal expansion will minimize the fuel-to-cladding gap at reactor operating conditions. Table C-XIV summarizes the above discussion.

TABLE C-XIV
CLADDING SUMMARY

Assembly Method	Material	Fit At 0 Reactor Power (570° F)	Fit At Full Reactor Power
Shrink fitting*	Type 304, 347 stainless steel	+0.0001	-0.0002
Die-sizing [†]	Type 304, 347 stainless steel	+0.0011	+0.0004
Die-sizing [†]	Type 446 stainless steel	+0.0003	-0.0003

Notes: + indicates a clearance (loose fit)

- indicates a tightness (interference fit)

*Assumes 0.0009 in nominal interference at room temperature

†Assumes line-to-line fit at room temperature

The acceptability of fuel rod fabrication by die-sizing, using Type 446 (or equivalent) stainless steel, depends on further fuel-cladding compatibility studies. A preliminary study⁵ consisted of boiling sample wafers of SNAP 4 fuel (hydrided 85-15 Zr-U) sandwiched between wafers of Type 446 stainless

steel for 400 hr at 570°F and 1230 psia. Although some interaction was observed, gross degradation of either the fuel or the stainless steel was not observed. The program of testing was suspended due to budget limitations.

Summarizing, the method of fuel element fabrication finally selected must ensure a low central temperature, thereby minimizing any chance of fuel swelling. The optimum fabrication method cannot be selected without further development.

APPENDIX C REFERENCES

1. J. D. Rogers, et al, "Hydrogen Transport Property Correlations, Part 2," LA 2719, University of California Los Alamos Scientific Laboratory, September 17, 1962
2. A. W. Sommer and W. F. Dennison, "Thermal Diffusion of Hydrogen in Nonstoichiometric Zirconium-Dihydride," NAA-SR-5066
3. D. J. Klein, "Growth Measurements of Thermal-Cycled Hydrided U-Zr Alloys," NAA-SR-TDR-7842, February 1963
4. "Corrosion and Wear Handbook," TID 7006, U. S. Atomic Energy Commission, Division of Reactor Development, Naval Reactors Branch, March 1957
5. D. H. Stone, "Corrosion Tests of Candidate SNAP 4 Dual Element Material," NAA-SR-TDR-8068, February 1963
6. J. G. Spraul, "Thermal Shock Tests of SNAP Fuel and Their Applicability to SNAP 4," NAA-SR-MEMO-7350, April 1962
7. L. Bernath, "SNAP 4 Summary Report," NAA-SR-8590, July 1963
8. J. A. Horak, et al, "Irradiation Behavior of Restrained and Vented Uranium-2 wt % Zirconium Alloy," ANL-6431, June 1962
9. D. F. Atkins, "Dissociation Pressures of Hydrided Zirconium-Uranium Alloy Hydrides," NAA-SR-4245, February 1, 1960
10. A. Bauer, "An Evaluation of the Properties and Behavior of Zirconium Uranium Alloys," BMI-1350, September 1959
11. SNAP Fuel Technology Handbook
12. R. A. Finch, "Thermal and Electrical Properties of a SNAP 4 Fuel Sample," NAA-SR-Memo-8134, March 12, 1963 (Confidential)
13. J. Raymond, "Development of a High Yield Zr - 10 U Massive Hydride," NAA-SR-7305, April 1962 (Secret)
14. Letter from Nuclear Metals, Inc. to AI, March 1, 1963
15. "Stress Relaxation in Stainless Steel During Irradiation," DP-369, E. I. du Pont de Nemours and Co., 1959

APPENDIX D. REACTOR HEAT TRANSFER AND HYDRODYNAMICS

A. CORE HYDRAULICS

The SNAP 4 reactor differs from other boiling water-cooled reactors in that the coolant is pumped through the core at high velocity; the velocity of the subcooled water at the core inlet is about 20 ft/sec, while that of the two-phase mixture at the outlet is about 40 ft/sec. An additional, significant difference arises from the use of hydride fuel-moderator, viz., that coolant passages may be of small hydraulic diameter (0.0937-in.) since the incremental neutron moderation provided by the coolant is but a fraction of that required for the nuclear reaction. Thus, this combination of high velocity and small hydraulic diameter produces both excellent heat transfer capability and rather high pressure losses (about 15 psi/ft of length). Although the latter exacts its penalty in the pumping power required, the flow stabilization resulting from the high frictional loss completely masks the acceleration pressure loss that attends boiling, two-phase flow; this phenomenon has also been reported by others.^{1,2} It is the latter that causes flow instability in natural circulation boiling reactors. High coolant velocity also assures homogeneity of the two-phase stream (bubble-flow regime), imparting to the coolant the behavior and properties of a compressible, single-phase fluid.

The steady-state hydraulic characteristics of the reactor were evaluated via a computer code which subdivides the core into twenty axial nodes and performs a compressible flow balance in each node using averaged local fluid properties. This method of computation treats the entire S4PR core as a single boiling channel — an assumption that is very nearly rigorous in view of the high degree of radial power flattening (see Appendix B, Reactor Physics). The code also utilizes the homogenous fluid model for the representation of the high velocity, two-phase coolant. This model assumes that the slip ratio, i. e., the ratio of vapor and liquid velocities in a two-phase mixture, is unity. A review of the effects of the primary parameters that influence the magnitude of the slip ratio (e. g., pressure, mass velocity, hydraulic diameter, and steam quality) reveals that under SNAP 4 coolant conditions the slip ratio is indeed very nearly unity. The homogeneous representation permits an excellent first approximation to the hydraulic performance of the core in that the pressure drop computation yields more conservative (higher) results than are calculated when a slip ratio greater than unity is applied.

Figure D-1. Total Core Pressure Drop vs Channel Flow Rate for Various Core Powers

5-7-63

7564-0199

Figure D-2. Axial Power Distribution (peak/avg. = 1.13)

Figure D-1 summarizes the effects of coolant flow and reactor power on core pressure drop computed as described above. The positive slope, exhibited by the curves for reactor power greater than 1 Mwt, is confirmation of flow stability throughout the two-phase flow regime, i. e., at all values of steam quality. The low power curves illustrate that at very low flow rates, e. g., <10% of design value, instability could theoretically occur if insufficient pressure head were supplied by the recirculation pump. The curves, presented in Figure D-1, are based on the axial power distribution depicted in Figure D-2.

In a core composed of a closely-spaced bundle of cylindrical rods, coolant can flow readily laterally between the fuel elements. With the high axial velocity specified, the pressure difference between coolant channels to cause cross-flow is orders of magnitude smaller than the axial pressure gradient. Furthermore, since each coolant channel can communicate along its full length with the six adjacent channels, the axial pressure profile must be identical everywhere throughout the core under steady-state conditions. Thus, "boiling disease," the flow starving of "hot" channels, cannot occur in the open matrix core of the SNAP 4 reactor. Furthermore, inlet flow orificing, a necessity for cores composed of isolated coolant channels, is not required.

Despite the high degree of confidence in the hydraulic stability of the reactor core derived from the homogeneous, single-channel representation described above, the development of a multichannel, two-dimensional, two-phase hydraulics code for the IBM 7090 computer was performed. This code permits evaluation of coolant density profiles throughout the core. Because of the large void coefficient of reactivity, detailed design of the burnable poison distribution in the core, accurate evaluation of the dynamic response of the reactor to process system transients, and the accurate prediction of the reactivity decrement associated with the transition from the cold-flooded to the hot-boiling condition all require detailed knowledge of the coolant density distribution as a function of operating conditions within the reactor. A description of this code is presented in Reference 3. Data from electrically-heated, multirod test sections, viz., Columbia University and Hanford experimental results, were applied to check out and calibrate the code.

5-9-63

Figure D-3. Effect of Inlet Subcooling on Various Core Parameters

7564-0198

To determine the interaction between optimized process system design parameters and core performance, the effect of the degree of coolant inlet subcooling on the nuclear, thermal and flow parameters within the reactor was evaluated. Figure D-3 summarizes these effects graphically and illustrates that major performance penalties do not accompany changes in subcooling within the range of interest. These results were used to evaluate variation coefficients for application to the analog computer study of system stability under transient operating conditions. The latter study is described in Appendix G.

Several hydraulic analyses were conducted in support of structural design activities; in these, the effect of geometric arrangements on the operating performance of the reactor was evaluated.

In order to assess the pumping power penalty attendant with the use of narrow inlet coolant passages, pressure drop calculations were made for several annular flow passage thicknesses, using the following formulae:³

$$\frac{\Delta P}{L} = \frac{2fV^2}{g_c D_e}$$

$$f = 0.00140 + 0.125/(N_{Re})^{0.32}$$

Pressure losses were calculated for three values of passage thickness. The following coolant flow and physical property values were used:

$$\rho = 45.5 \text{ lb/ft}^3$$

$$\mu = 0.242 \text{ lb/hr-ft}$$

$$W = 408,500 \text{ lb/hr.}$$

The calculated data are summarized in Table D-I.

The core coolant also flows through the passages within the reflector region; these coolant paths parallel those between the fuel elements and therefore represent core bypass flow, except for the heat absorbed from the reflector components. In order to balance the loss in thermal efficiency due to the bypass flow against the difficulty of achieving the manufacturing tolerances required to assure narrow coolant passages, the effect of reflector coolant passage thickness on the extent of flow bypassing was evaluated as described in the following. Certain idealizations were made to determine the amount of core coolant

TABLE D-I
EFFECT OF COOLANT PASSAGE THICKNESS ON PRESSURE LOSS

Passage Thickness (in.)	1/4	5/16	3/8
Flow area, ft ²	0.1214	0.1522	0.1831
Velocity, ft/sec	20.5	16.55	13.6
($\Delta P/L$) _{fric} , psi/ft	0.633	0.322	0.187
(ΔP) _{fric} , psi (based on L = 55.5 in.)*	2.92	1.49	0.86
(ΔP)/K _L [†]	2.06	1.31	0.91
(ΔP) _{accl} , etc. for K _L = 1.5, psi [§]	3.09	1.97	1.37
(ΔP) _{total} = (ΔP) _{fric} , psi (based on L = 55.5 in.) + (ΔP) _{accl} , etc. for K _L = 1.5, psi	6.01	3.46	2.23

*55.5 in. = 38 in. (distance from inlet to bottom of thermal shield skirt) + 17.5 in. (1/2 distance around flow passage to compensate for spiral path)

[†]K_L is the expansion, contraction, and acceleration loss coefficient

[§]K_L = 1.5 = 0.5 (head loss at inlet) + 1.0 (head loss at bottom of thermal shield skirt)

that will be bypassed through the 12 control drum flow channels. It was assumed that the control drum flow region is a closed annular flow passage. Hence, cross-flow between the core and this single control drum region is not permitted.

Assuming single-phase (homogeneous model), turbulent flow, the pressure drop through a flow passage is given by:

$$\frac{\Delta P}{\Delta L} = \left(\frac{0.092 W^{1.8} \mu^{0.2}}{g_c D_{eq}^{1.2} A^{1.8} \rho} \right)$$

Since there is no cross-flow and the flow paths are interconnected at both ends, the pressure drops across the reflector and the core must be equal. Hence,

$$\left(\frac{\Delta P}{\Delta L} \right)_{core} = \left(\frac{\Delta P}{\Delta L} \right)_{Refl}$$

and

$$\left(\frac{W_R}{W_c} \right) = \left(\frac{\mu_c}{\mu_R} \right)^{0.111} \left(\frac{\rho_R}{\rho_c} \right)^{0.555} \left(\frac{D_R}{D_c} \right)^{0.667} 12 \frac{A_R}{A_c}$$

Assuming $\rho_C = \rho_R$ and $\mu_C = \mu_R$, the ratio of the bypass coolant to that of the core is

$$\frac{W_R}{W_C} = 12 \left(\frac{D_R}{D_C} \right)^{2/3} \frac{A_R}{A_C} .$$

Using the relationships

$$W_T = W_R + W_C$$

and

$$r = \frac{W_R}{W_T} ,$$

the ratio of bypass to total coolant flow may be written,

$$r = \frac{1200}{\left[1 + 12 \left(\frac{D_R}{D_C} \right)^{2/3} \frac{A_R}{A_C} \right]} \left(\frac{D_R}{D_C} \right)^{2/3} \frac{A_R}{A_C} , \%$$

where

A_C = cross-sectional area of the core, 19.6 in.²

A_R = cross-sectional area of one reflector coolant channel, in.²

$A_R = \pi \left[(R_D + \tau)^2 - R_D^2 \right] , \text{ in.}^2$

D_C = core equivalent diameter, 0.0937 in.

D_R = control drum channel equivalent diameter, 2τ in.

r = bypass flow fraction, %

R_D = outside radius of the control drum, 1.974 in.

W_C = core coolant flow rate, lb/hr

W_R = total control drum coolant flow rate, lb/hr

W_T = total flow rate, lb/hr

τ = coolant channel thickness, in.

The percentage of flow bypassed, r , is plotted as a function of channel thickness, τ , in Figure D-4. Inspection of the graph indicates that holding

5-9-63

7564-0196

Figure D-4. Effect of Reactor Clearances on Coolant Bypassing the Core

The control drum coolant passage thickness to 0.015 in. or less, will limit the bypass flow to approximately 5% of the total flow, thereby incurring only a slight penalty in increased quality of the coolant from the core.

For the no bypass condition, the enthalpy rise per pound of coolant is

$$\Delta h = \frac{\chi}{100} h_{fg} + C_p \Delta T$$

where

C_p = average specific heat, 1.3 Btu/lb-°F

h_{fg} = heat of vaporization at exit conditions of 570°F and 1220 psia,
606 Btu/lb

ΔT = degrees of entrance subcooling, 30°F

χ = exit quality, 10%

Therefore

$$\Delta h = 60.6 + 39.9 = 100.5 \text{ Btu/lb}$$

Since

$$Q = 12 \text{ Mwt} = W_T \Delta h ,$$

$$W_T = \frac{12 \times 10^6 \times 3.415}{100.5} = 408,500 \text{ lb/hr}$$

For flow with bypassing, the enthalpy balance is given by

$$Q = \Delta h W_T = (\chi h_{fg} + C_p \Delta T) W_C + Q_\epsilon$$

Since

$$W_C = W_T - W_R$$

and

$$r = \frac{W_R}{W_T} ,$$

$$\chi = \frac{100}{h_{fg}} \left[\frac{\Delta h - Q_\epsilon / W_T}{1 - r} \right] - C_p \Delta T , \%$$

TABLE D-II

12 Mwt SOURCES OF CONTROL DRUM COOLANT ENTHALPY RISE

Source	Total Volume (ft ³)	Average "Q" (Btu/hr-ft ³)	Q (Btu/hr)	Assumptions
Control Drum	1.445	165,000	238,000	Slab equations for H _f -hydride and B _e portions of control drum.
Nickel Cusps	0.348	305,000	106,000	Slab equations; considered triangle 2-in. high with equivalent area; assumed energy deposition rate on inner surface of cusp to vary linearly from 527,000 Btu/hr-ft ³ at apex of cusp to 407,500 Btu/hr-ft ³ where cusp blends into the core structure cylinder; numerically integrated over surface of equivalent triangle to obtain Q.
Core Structure Cylinder	0.368	326,000	30,000	Slab equations; did not consider the angular variation of energy flux; assumed 1/4 of heat generated in core structure cylinder to be deposited in reflector drum coolant.
Total Q _c			374,000	

The above expression represents the core exit quality required, when bypassing occurs, such that $Q = 12$ Mwt. Q_{ϵ} is the energy added to the bypass coolant as the result of energy deposition in the adjacent structures. Table D-II lists the sources of Q_{ϵ} and the various assumptions made in evaluating its terms. In the limit when $r \rightarrow 0$, it is assumed that $\lambda \rightarrow 10\%$ for $Q_{\epsilon} = rW_T(\Delta h)_R$. Upon insertion of the appropriate quantities

$$\lambda = \frac{16.43}{1 - r} - 6.43, \quad \%$$

Inspection of Figure D-4 shows that, for the specified width of reflector coolant passage (0.015 in.), 5% of the total flow through the reactor cools the reflector and that the quality of the effluent from the core need only be 10.9% to provide a net 10% quality in the effluent from the reactor.

B. IN-CORE HEAT TRANSFER

Thermal conditions within the fuel elements are presented in Appendix C and summarized in Figure C-3. The evaluation of the boiling film heat transfer coefficient was based on the experimental results reported by Columbia University.^{5,6} In this investigation of boiling heat transfer performance in 19-rod, electrically heated and spiral wire-wrapped bundles of simulated fuel elements, heat transfer coefficients were measured that ranged between 20,000 and 30,000 Btu/hr-ft²-°F. Interestingly, as shown in Figure D-5, the effect of increased coolant mass velocity on the coefficient was insignificant and the coefficient appeared to be proportional only to the heat flux, i.e., the slope of the heat flux vs ΔT curve proved to be essentially infinite throughout the entire nucleate boiling regime, up to the burnout condition. Measurements of surface temperature both in the vicinity of the spiral wire wrapping and in remote regions revealed that the wire acted as a fin causing the temperature of the surface beneath it to be slightly lower than in the other regions. Based on these results, it is concluded that the SNAP 4 core will be characterized by essentially uniform cladding temperatures throughout.

A search was made of the literature for applicable data to predict the conditions under which heat transfer burnout would occur in the SNAP 4 core. Although a limited number of experiments had been performed at the system pressure and in the range of mass velocities of interest, very few data points were

7-2-63

7564-0285

Figure D-5. Forced Convection Nucleate Boiling Film Coefficient

available for the relatively low steam qualities and small hydraulic diameter of the SNAP 4 core. Accordingly a graphical correlation of all available data^{7,8} was undertaken to clarify the role of each of the primary variables in the burnout phenomena.

The effects of system pressure on the magnitude of the burnout heat flux, steam quality, coolant mass velocity and hydraulic diameter of the coolant flow path are summarized in Figures D-6, -7, -8 and -9, respectively. In Figure D-6, the effect of system pressure is illustrated for several values of mass velocity and hydraulic diameter, all at a constant steam quality of 10%. It is obvious from these data that both coolant velocity and hydraulic diameter exert a primary effect on the burnout heat flux. Figures D-8 and -9 further clarify the interaction of these parameters and depict the effect of coolant steam quality (at the burnout site) on burnout heat flux, while Figure D-7 further illustrates this effect for a single value of hydraulic diameter and for two levels of pressure and mass velocity.

Figure D-6. Effect of System Pressure on Burnout Heat Flux

6531-5443

4-30-63

7564-0163

Figure D-7. Effect of Steam Quality on Burnout Heat Flux

4-30-63

7564-0164

Figure D-8. Effect of Coolant Velocity on
Burnout Heat Flux

6531-5444

Figure D-9. Effect of Hydraulic Diameter on Burnout Heat Flux

4-30-63

7564-0165

Figure D-10. Predicted Burnout Performance at 1225 psia and 0.10-in.

The data of Figure D-8 are particularly noteworthy since they unequivocally demonstrate the pronounced effect on the burnout heat flux of reducing the hydraulic diameter, particularly at the higher mass velocities and steam qualities. Figure D-9 confirms the influence of the hydraulic diameter and shows that the improved burnout performance is attainable over a wide range of system pressures.

To arrive at the burnout prediction, shown in Figure D-10, all parameters except hydraulic diameter were interpolated from graphs of experimental data; the effect of hydraulic diameter was obtained by extrapolation from graphs similar to those depicted in Figure D-9. It had been shown earlier,⁹ in a comprehensive study of subcooled burnout phenomena, that the hydraulic diameter was the unique correlating factor for comparing results from a wide range of geometrical configurations, e.g., tubes, rectangular ducts, internally and/or externally heated annuli, twisted ribbons, etc. A test of the bulk boiling data was performed to determine whether the hydraulic diameter accurately correlated these data as well. Figure D-11 presents a comparison of two experimental geometries, at essentially one value of hydraulic diameter, and of these data with the correlation derived in this study. The agreement between the three is excellent, and it

4-30-63

7564-0166

Figure D-11. Burnout Heat Flux Correlation

is reasonable to conclude that the correlation adequately accounts for geometrical configuration. As shown in Figure D-10, the SNAP 4 core, operating at mass velocities between 2 and 3×10^6 lb/hr-ft², is adequately protected from burnout; burnout heat fluxes range between 3 and 4 times the operating heat fluxes throughout the core.

However, since experimental verification of the burnout, heat transfer and hydrodynamic behavior of the SNAP 4 core presented in the foregoing is highly desirable, a program of research in these areas was conducted at Columbia University and is described in the next section.

C. COLUMBIA BOILING HEAT TRANSFER EXPERIMENTS

A series of tests has been conducted at Columbia University to aid in the evaluation of the thermal and hydraulic characteristics of the SNAP 4 reactor. In particular the study of geometrical effects was emphasized in the determination of the burnout characteristics of the SNAP 4 core. Core environment was simulated in a high-pressure, boiling-water loop by an electrically heated, 12-rod bundle with vertical upflow at 1200 psia. The influence of mass velocity, mean exit quality, power distribution, and wall-to-rod separation on the burnout heat flux were studied in five different test sections. Figures D-12 and D-13 are photographs of components and assembly of the test section.

A significant improvement in the analysis and interpretation of the data resulted from the development of a multichannel, two-phase hydraulics code.³ Such analyses have shown that test section geometry has a crucial influence on the burnout heat fluxes attainable from multirod test sections. Interpretation of the gross behavior of a multirod test section in the prediction of core performance is misleading; detailed knowledge of the flow distributions among the various coolant channels, both within the test section and the core, are required. Although an "unheated wall" effect does appear to exist in the SNAP 4 reactor core, the experimental data confirm the conclusions that more than adequate burnout safety factors exist in the interior channels, and may be designed for in the peripheral channels.

6-11-63

Figure D-12. Heat Transfer Test Section Components

7564-5406

6-14-63

7564-5409

Figure D-13. Heat Transfer Test Section Assembled

1. Description of the Twelve-Rod Test Sections

The five 12-rod test sections were close simulations of the SNAP 4 core geometry. The active heated length of each rod was 17 in. and its outside diameter was 0.440 in. Each rod was wrapped with 0.022-in. OD hypodermic needle tubing with a 6-in. pitch. The lattice-rod pitch was 0.4625 in. A cross-sectional view of the test section is shown in Figure D-14.

Test Sections 1 and 2 were identical except for slight differences in instrumentation. The power distribution produced by direct resistance heating in each of these test sections was uniform. In Test Sections 3 and 4, the interior rods (rods 6 and 7) were designed to produce more power than produced in the peripheral rods. Test Section 5 differed from the other geometries in that the minimum wall-to-rod distance was increased from 0.022 to 0.032 in. in the side-wall and acute-corner channels and to 0.040 in. in the obtuse-corner channels; as in Test Sections 1 and 2, the power distribution was uniform. Test Sections 3 and 4 yielded data most representative of the SNAP 4 core configuration.

Figure D-14. Columbia 12-Rod Test Section

2. Burnout Characteristics of Test Sections 3 and 4

In order to determine the burnout heat flux in the hot channel regions of the core, cold wall effects had to be eliminated. To achieve this, the two central rods, Numbers 6 and 7, were built to operate at higher heat flux conditions (thinner wall). These central elements would then be the probable elements to experience burnout. The first series of these "hot rod" Runs 66 to 69 (Table D-III) was characterized by a hot-to-edge rod power ratio of 2.6/1 (Test Section 3). A second series (Runs 78 on, Table D-III) had a power ratio reduced to 1.55/1 (Test Section 4) to better simulate reactor conditions which are less than 1.1/1 between adjacent rods. These tests show that heat fluxes in excess of 1.5×10^6 Btu/hr-ft² were achieved at near SNAP 4 operating conditions. The best fit line for these data is plotted as a function of local quality in Figure D-15. The test conditions for the data are presented in Table D-III as "Columbia Data." The data were analyzed using the multichannel, two-phase hydraulics code, to predict the local qualities and flow rates in order to yield specific burnout conditions. The local conditions covered the range of the SNAP 4 core operating at 12 to 20 Mwt. Considerable redistribution of flow took place in these tests due to the drastic thermal profile. Of further interest are the last four data points in Table D-III. These data were taken during a test where the elements were covered with a thin crud film as will be experienced in reactor operation. The data yielded the expected higher heat flux values.

Examination of the data from Test Sections 3 and 4 when modified to local conditions, indicated that the burnout heat flux in the SNAP 4 configuration is well above core operating conditions. The comparison of the worst burnout condition in the hottest channel is shown on Figure D-15, and compared to the local data from Test Sections 3 and 4. The hot rod data yielded no detectable trend with mass flux in the range tested.

3. Burnout Characteristics of Test Section 2

The burnout data obtained from Test Section 2 are summarized by the curves presented in Figure D-15. There are two distinct features to these curves: (1) at a given mass velocity, a limiting value of burnout heat flux is approached as the mean exit quality is decreased (i.e., as the degree of inlet subcooling is increased), and (2) a definite mass velocity effect exists for the

6531-5445

Figure D-15. Burnout Heat Flux as a Function of Exit Quality

TABLE D-III

"HOT ROD" INSTRUMENTED THIN WALL BURNOUT HEAT TRANSFER DATA FOR
SNAP 4 TEST SECTIONS 3 AND 4

Columbia Run No.	Mass Flux, Water and Steam (lbm/hr-ft ² x 10 ⁻⁶)	Mixed Exit Quality (wt %)	Cold Rod Flux (Btu/hr-ft ² x 10 ⁻⁶)	Hot Rod Flux (Btu/hr-ft ² x 10 ⁻⁶)	AI Analysis Local Quality (wt %)
<u>Test Section 3</u>					
66	3.04	11.2	0.422	1.09	17.6
67	3.03	7.7	0.434	1.150	14.4
68	2.96	5.2	0.485	1.285	12.0
69	2.98	1.6	0.524	1.409	9.5
<u>Test Section 4</u>					
78	1.71	26.8	0.466	0.723	31.3
79	1.73	18.4	0.534	0.829	22.0
80	1.73	15.9	0.606	0.941	19.0
81	1.73	14.4	0.695	1.078	~19.5
82	1.76	12.8	0.804	1.244	11.0
83	2.84	19.5	0.567	0.879	23.2
84	3.02	14.2	0.711	1.101	18.3
85	3.00	10.5	0.865	1.341	15.3
86	3.01	6.0	0.985	1.529	10.9
87*	1.74	12.2	0.924	1.435	8.7
88*	1.77	10.3	1.019	1.580	22.8
89*	3.01	6.2	1.187	1.842	18.2
90*	2.96	19.8	0.651	1.010	23.2

*Corrosion Film Tests

entire range of the data. The results indicated an apparent "unheated wall" effect* since the location of a burnout occurred invariably on a peripheral rod which faced an unheated shroud wall. In addition, the preponderance of burnout indications (33 of 36 runs) on Rod 4, which is in an obtuse corner, suggests that there was an unusual condition of flow "starvation" in this corner.

The modified multichannel, two-phase hydraulics code,³ developed subsequent to the design of the test section, was used to analyze the experimental data. The predicted local exit quality from the obtuse corner, around Rod 4, (see Tables D-IV and D-V) was in the range of 2 to 3 times as great as the qualities from the other channels. Typically, the predicted local qualities in Channel 4 were sufficiently high to suggest that the burnout heat fluxes were not indicative of the average behavior of the entire test section but of the "abnormal" flow channel in the obtuse corner. The results from Test Section 2 do not define conclusively an "unheated wall" effect. Rather, they indicate a need for geometry modifications.

TABLE D-IV
PREDICTED RELATIVE LOCAL EXIT QUALITIES

Channel Number	Channel Designation [†]	Relative Local Exit Qualities	
		Test Section 2	Test Section 5
1	Internal	1.10	2.4
2	Side-wall	1.05	1.2
3	Acute-corner	1.00	1.0
4	Obtuse-corner	2.5	1.0

[†]See Figure D-6

4. Burnout Characteristics of Test Section 5

Test Section 5 was designed to emphasize a wall effect, if it indeed exists. That is, based upon burnout heat flux limits for uniformly heated circular tubes and those of Test Section 2, the dimensions of the interior and peripheral channels were sized so that the predicted local qualities would result in burnout in each channel simultaneously. This calculation was based upon the assumption

*The flow maldistribution that occurs when coolant is accumulated on an unheated wall facing a heated surface in vigorous nucleate boiling

TABLE D-V
PREDICTED RELATIVE LOCAL EXIT QUALITIES
TEST SECTION 2 DATA

Run No.	G/10 ⁶ (lb/hr-ft ²)	Q"/10 ⁶ (Btu/hr-ft ²)	Local Exit Quality (%)				Mean Quality (%)
			<u>Channel Numbers</u>				
			1	2	3	4	
29	1.06	0.485	25.8	23.4	24.6	77.8	27.0
36	2.03	0.700	19.3	17.5	18.3	58.7	20.0
43	2.96	0.860	9.96	8.88	9.59	47.1	11.7

that the local behavior in a channel in which all walls are heated is identical to that in uniformly heated single tubes. The relative values of the predicted local qualities are given in Column 4 of Table D-IV.

Three salient features of the burnout data from Test Section 5 may be noted. First, all burnouts were located on peripheral rods even though the predicted local qualities in the interior channels were much greater than those in the peripheral channels. Therefore, an "unheated wall" effect does appear to exist. Second, the burnout heat flux increased monotonically with decreasing mean exit quality (see Figure D-16). An asymptotic value of burnout heat flux was not reached within the range of the experimental data as was noted in Figure D-15. These data are more typical of those obtained from uniformly heated circular tubes. Third, no mass velocity effect was evident for the test section as based upon its gross behavior. From single-tube experiments it is more usual to expect a mass velocity effect. In the low quality or bubble-flow regime, the burnout heat flux would increase with increasing mass velocity whereas an inverse effect would be found in the high quality or annular-flow regime. This will be discussed below. Significantly, the trend of the data has been reversed from that of Test Section 2 by a change of approximately 0.010 in. in the shroud separation.

Another indication of the sensitivity of the data to geometry changes is the difference in behavior of Setups 1 and 2 of Test Section 5 (see Figure D-16). Setup 1 was disassembled in order to replace two rods which had developed

Figure D-16. Unheated Wall Burnout Heat Flux as a Function of Mean Exit Quality

slight leaks. Two identical sound rods were used to replace these damaged rods and the test section was reassembled as Setup 2. Any difference in behavior of the test sections may be attributed to differences in geometry due only to assembly techniques.

The behavior of the supposedly identical test sections was markedly different. First, the pressure drop across the test section was approximately 10% greater for Setup 2 than for Setup 1. Second, the primary burnout indications occurred in an obtuse-corner channel (Rod 4) for Setup 2. In Setup 1, the primary burnout indications appeared in a side-wall channel (Rod 11) and in an acute-corner channel (Rod 12). Third, the burnout heat fluxes attained in Setup 2 were consistently about 20% higher than those in Setup 1. Upon disassembly of Setup 2, due to a physical burnout of Rod 5, two distinct sets of wire-wrap indentations on the phenolic-asbestos shroud walls were noted. The above behavior may be explained by the existence of a slight degree of eccentricity of the rod bundle with respect to the shroud walls in the first setup of Test Section 5. Both the lower pressure drop and the lower burnout heat fluxes at Rods 11 and 12 are consistent with the view that a greater amount of fluid than had been expected bypassed the rod bundle on the side opposite the burnout locations. If the results for Setup 2 may be considered reliable, the burnout safety factor for the core design condition of a mean exit quality of 10% and a heat flux of 500,000 Btu/hr-ft² is 2.36.

Before an interpretation of the data is complete, a detailed analysis must be made with the multichannel, two-phase hydraulics code. Although a mass velocity effect was not noted in Figure D-16, Figure D-17 shows it to exist at the local burnout conditions. The burnout heat fluxes are lower than those obtained from uniformly heated single-tube data, due to the "unheated wall" effect, but they are consistent with the trends of such data.

It was anticipated that the qualities in the interior channels would be sufficiently high to cause burnout. In Figure D-18, the predicted local qualities for the interior channels of Test Section 5 are shown at those heat fluxes corresponding to the burnout heat fluxes observed for the peripheral rods. For comparison, the average burnout limit of uniformly heated circular tubes is shown. In addition, the local "heated wall" burnout conditions recorded on the interior

6531-5447

Figure D-17. Unheated Wall Burnout Heat Flux as a Function of Local Exit Quality

Figure D-18. Experimental Heat Flux as a Function of Local Exit Quality of the Interior Channels

rods of Test Section 4 (Q'' interior/ Q'' outer = 1.55) are shown. The conditions predicted for the interior channels of Test Section 5 reached and exceeded the limits of the circular tube data and of some of the multirod "heated wall" data without the occurrence of a burnout. Interior channel burnouts, therefore, may be conservatively predicted by uniformly heated circular tube burnout criteria. The results, however, lend emphasis to the existence of an "unheated wall" effect for the peripheral channels.

5. Physical Status of Test Section 5

The operation of Test Section 5 was plagued by a series of equipment failures. Two O-ring failures occurred — each at the end of one day's operation. Modifications were made to the equipment in order to extend the life of the O-rings. High-pressure cold water was diverted from the loop to the low-temperature side of the O-ring plug. Hopefully, any tendency for leakage past the O-rings would be that of cold water into the test section rather than hot water or steam out of the test section. At this time it was discovered that Rods 9 and 10 had minute leaks. The test section (Setup 1) was disassembled, the rods were replaced with identical sound rods, and the test section (Setup 2) reassembled. After one day of operation, Setup 2 was shut down because of a leak in the system.

An examination of the disassembled test section revealed that a physical burnout had occurred on Rod 5. Also, the adjacent rod, Rod 9, was severely burned along its length and its internal instrumentation was burned out. Since Rod 5 was an uninstrumented rod and because of the failure of instrumentation on Rod 9, the burnout condition went undetected until physical failure took place. The tubing of four of the other rods was found to have been wrinkled. Six of the twelve rods, therefore, were lost in this test.

The fact that the physical burnout occurred on the opposite side of the rod bundle from which all previous incipient burnouts had been detected (Rod 4) suggests that this burnout condition was not ordinary. A malfunction of some component may have occurred. The two phenolic-asbestos shroud pieces in the corner adjacent to the physical burnout location were burned and damaged. In particular, on the shroud piece facing Rods 5 and 9, a segment of the "hill" between these two rods was splintered. It is suspected that the splintering of the

wall and the resultant flow disturbance which it caused may have led directly to the physical burnout condition. The failure of the phenolic-asbestos shroud may have been due to an intrinsic failure of the material or due to unusual stresses put on the pieces when assembled. During assembly the fit of the shroud pieces in each corner was sufficiently tight as to require the "jacking-in" of the individual pieces. The resultant stresses and the possible bowing of the shroud may have caused its failure.

6. Comments and Recommendations

In anticipation of the scheduled termination of the 12-rod experiments, the design of several annular test section geometries was initiated. The purpose of these experimental investigations was to provide basic experimental information on the effect of rod-to-rod spacing, rod-to-unheated wall spacing, and rod-to-partially-heated wall spacing on burnout heat flux limits and two-phase pressure drop. These data can be attained by using a series of four annular test sections (Table D-VI).

TABLE D-VI
ANNULAR TEST SECTIONS DIMENSIONS
(in.)

Heated Length	Annular Gap	Rod OD	Tube ID
6	0.010	0.400	0.420
12	0.020	0.400	0.440
24	0.040	0.400	0.480
42	0.070	0.400	0.540

The test sections were designed so that the internal and external heat transfer surfaces can be heated individually. Plans were made to run five different series of tests with each configuration (Table D-VII).

The gap spacings were selected to cover a wide range of variables. The heated lengths were chosen holding a constant value of (L/D_e) in order to give identical or near identical performance when they are doubly heated or either internally or externally heated. The annular test program was specifically designed to provide detailed information concerning the "unheated wall" effect

TABLE D-VII
HEAT GENERATION FOR
EACH ANNULAR
TEST SECTION

Test No.	Heat Generation (%)	
	Internal Heater	External Heater
1	100	0
2	100	10
3	100	25
4	100	100
5	0	100

and detailed methods of alleviating its adverse effects. Future extensions of the program were to include the study of the effect of spiral wraps and other turbulence promoters.

The main results of the 12-rod tests are summarized as follows:

- 1) A closely-spaced arrangement of small rods may be taken to high burnout heat fluxes.
- 2) The gross behavior of a multirod geometry must be interpreted in terms of the local behavior in the individual channels.
- 3) The burnout characteristics of a multirod geometry are typical of those of a single tube geometry when interpreted in terms of local conditions.
- 4) Via the multichannel, two-phase hydraulics code, the local flow distributions in a multirod geometry may be estimated. Maldistributions of flow may be avoided by proper design.
- 5) An "unheated wall" effect exists.
- 6) The burnout characteristics of small-gap geometries are highly sensitive to changes in the geometry.
- 7) The burnout location may be driven from the peripheral rods by a nonuniform power distribution (Q'' interior/ Q'' wall = 1.55).

APPENDIX D REFERENCES

1. "Pathfinder Atomic Power Plant — Technical Progress Report — July 1961 to September 1961," ACNP-6123, February 1962, p 108
2. P. A. Lottes, et al., "Boiling Water Reactor Technology — Volume I," ANL-6561, February 1962
3. "SNAP 4 Compact Reactor Program," NAA-SR-10699, January 8, 1965
4. McAdams, Heat Transmission (3rd Ed., McGraw Hill, New York, 1954)
5. Columbia University Progress Report MPR-X-11-61, TXD-14439
6. Columbia University Progress Report, MPR-X111-2-63
7. H. Firstenberg, et al., "Forced-Convection Quality Burnout Data," NDA-2131-16, June 30, 1960
8. R. A. DeBortoli, et al., "Forced-Convection Heat Transfer Burnout Studies for Water, etc.," WAPD-188, October 1958
9. L. Bernath, "A Theory of Local Boiling Burnout and Its Application to Existing Data," Chem. Eng. Prog. Symp. Series, Vol 56, November 30, 1960

BLANK

The design objectives of the SNAP 4 reactor shielding are specified as follows:

- a) Minimum weight of shield external to the pressure vessel to permit transportation, (i. e., relocation) of the reactor after full-power operation and with the core removed, via aircraft or surface transport;
- b) Minimum weight of shield between the core and the control drum mechanisms to permit manual contact maintenance of the drive assemblies; and,
- c) Flexibility to accommodate a variety of SNAP 4 applications, in the addition of removable (for shipping) operational shielding to the basic standardized reactor assembly.

The S4PR shield is designed to serve as a prototype for those of deliverable SNAP 4 systems and, therefore, is required to meet the ICC shipping regulations for radioactive materials whenever the reactor package is shipped. These criteria (200 mr/hr at any accessible surface and 10 mr/hr one meter from any accessible surface) must be met at any time during the expected 20-yr life of the system. In addition, the shield plug is designed to minimize neutron induced activity in the control drum drive motors, located in the head of the pressure vessel; the activation of these motors was specified to be low enough to permit manual maintenance 24 hours after reactor shutdown.

The problems of equipment activation during reactor operation, heat generation in reactor components, and the deposition of activated corrosion products have also been examined.

A. SHIELD PLUG DESIGN

The shield plug is designed to protect the control drum drive motors and to minimize streaming of neutrons through the inlet and outlet coolant nozzles. In the selection of the material for the shield plug, stainless steel, water, and zirconium hydride ($\text{ZrH}_{1.9}$ at 80% theoretical density) were considered. Lithium hydride was not included both because it is costly and because of the potential hazard of a reaction with water. The fast neutron removal cross sections

and densities^{1, 2} of these materials are given in Table E-I. (The gamma ray shielding properties are not included here because the primary function of the shield plug is to attenuate neutrons.)

TABLE E-I
NEUTRON SHIELDING PROPERTIES OF MATERIALS

Material	Density (gm/cc)	Removal Cross Section $R(\text{cm}^{-1})$
304SS	7.94	0.159
ZrH _{1.9}	4.92	0.130
H ₂ O (room temp)	1.00	0.103

A comparison of these materials, based on system weight, is given in Table E-II. The weight-per-unit height of a slice through the reactor at the shield plug elevation was calculated. This was then divided by the removal cross section of the plug material, which is inversely proportional to the thickness required, and a relative weight was found. The cross section at the elevation considered is shown in Figure E-1. It was assumed that the shield water would be drained prior to shipment so its weight was not included in the calculation.

TABLE E-II
FIGURE OF MERIT FOR SHIELD PLUG MATERIALS

Plug Material	Plug Weight* (lb/in.)	P.V.† Weight (lb/in.)	Pb Shield Weight (lb/in.)	Total Weight* (lb/in.)	Relative Weight (Total wt · Σ_r)
304SS	94.4	22	114	230	1446
ZrH _{1.9}	56.1	22	114	192	1477
H ₂ O	0	22	114	136	1320

*Shipping Weight

†P.V. = Pressure Vessel

On a weight basis, water appears to be superior to the other materials. This weight advantage is questionable, however, since certain design aspects of a water shield plug were not taken into consideration. For instance, the shield

Figure E-1. Cross Section of Reactor Through Shield Plug

water must be thermally insulated from the reactor outlet steam, and piping must be provided to circulate the shield water to a heat sink to maintain it at a low temperature. The weight of the water system will certainly increase when these design considerations are taken into account.

The substitution of ordinary carbon steel for stainless steel would appear practical for two reasons: (1) the lower cost of carbon steel, and (2) the induced activation of stainless steel would be about 13 times that of carbon steel (for saturation activity with a 10-day decay period). However, 25-kev neutrons have been found to stream through iron, but not through stainless steel.

The reason for this is that nickel has a small resonance at the neutron energy (25 kev) where the iron cross section has a "valley." In addition, a carbon-steel plug would have to be encased in stainless steel, or an equivalent corrosion-resistant material, to protect it against corrosion in the water-steam environment. With the addition of a 1/16-in. -thick 304 SS cladding to the carbon steel plug, the ratio of activation of the stainless-steel plug to the carbon-steel plug is reduced from 13 to approximately 4. There would also be a corresponding

7-2-63

7564-0273

Figure E-2. Cross Section of Shield Plug

decrease in cost differential. Thus, the necessity for a cladding and the consequences of neutron streaming counterbalance the arguments in favor of carbon steel.

On the basis of shield plug compactness and simplicity, stainless steel is the outstanding material. A reactor package with a stainless-steel plug will be approximately 1 ft shorter than a reactor package with a water plug. Also, a stainless-steel shield plug requires no thermal insulation around the outlet pipe and the reactor effluent coolant stream provides an adequate heat sink for dissipation of the heat generated within the stainless-steel shield plug by nuclear radiation.

The coolant flow channel at the base of the shield plug (Figure E-2) is contoured to prevent excessive neutron leakage. This is necessary to minimize activation of the nozzles which penetrate the radial lead activation shield. Neutron leakage calculations were made using the Simon-Clifford equation given in Rockwell's Reactor Shielding Design Manual.³ The neutron attenuation through the flow channel will be about nine orders of magnitude, which is more than adequate.

Initially, activation calculations were made on the basis that the motor drive assembly and the shield plug would be removed from the reactor vessel simultaneously and would remain together while maintenance operations were being conducted on the assembly. Since the shield plug will become radioactive, a shield cask or shadow shield around the plug would be necessary to protect personnel working on the motors. In addition, a lead shield approximately 2-1/2 in. thick would be required between the motor mounting plate and the shield plug to reduce the dose rate in the motor compartment to an acceptable level. In order to eliminate the problem it was proposed that, for maintenance purposes, provisions be made to permit removal of the motor drive assembly separate from the shield plug. This change eliminated the requirement for the lead shield as well as the need for an external shadow shield.

This design modification introduced a new problem, however, in that the control drum drive shafts would be exposed when the motor drive assembly was separated from the shield plug. Rough calculations indicated that the dose rate in the motor compartment from 12 exposed shafts would be on the order of 12 r/hr. This problem was circumvented by the addition of a shaft coupling at the base

7-2-63

7564-0274

Figure E-3. Fast Neutron Axial Flux Distribution for 12 Mwt Thermal Power Level

of the motor package that permits removal of the motors and motor mounting plate from the reactor without removing the drive shafts.

Since construction details of the motors were not available, it was assumed that Type 304 SS and copper were the main constituents. The estimated weight of stainless steel and of copper was 9 lb and 6 lb, respectively, for each motor.

The fast neutron flux was computed at several points in the reactor package using the IBM 7090 computer shielding code 14-0. The axial distribution of the fast neutron flux along the centerline of the reactor vessel is shown in Figure E-3. Activation by both fast and thermal neutrons was included in the calculated dose rate. The fast neutron fluxes computed by shielding code 14-0 were used to determine the induced activity due to fast neutrons. The thermal neutron flux in the control drum motors and in the motor mounting plate was assumed to be 10 times greater than the fast flux computed by code 14-0 at these positions.

Hand calculations were made of the neutron transmission through the control drum drive shaft penetrations in the shield plug. Thermal flux attenuation was found to be on the order of 7×10^{-6} based on experimental data given in the Radiation Shielding Textbook.⁴ Calculations for fast neutrons, based on a theoretical equation given in Rockwell,³ yield an expected attenuation of 5×10^{-4} . Both calculations were made for a 22-in.-long penetration, a 3/4-in.-OD shaft, and a 1/8-in. shaft clearance filled with water of 0.3 g/cc density. The estimate of fast neutron attenuation is probably conservative, but nearly coincides with the attenuation through the weakest part of the shield plug, which is 2.5×10^{-4} . This comparison indicates that the current shaft design is adequate for fast neutrons. The primary source of thermal neutrons in the water (in the motor compartment above the plug) is probably from the slowing down of intermediate energy neutrons coming through the steel plug. This fact, coupled with the large attenuation of thermal neutrons through the shaft clearances, indicates that problems will not be encountered due to thermal neutron streaming.

The shutdown dose rate after 10,000 hr of full-power reactor operation, at a point on the centerline of the motor compartment and 6 in. above the mounting plate, will be approximately 138 mr/hr when the motor assembly is separated from both the reactor vessel and the shield plug. Exposure for approximately 22 hours in this radiation field yields the quarterly tolerance of 3R, or 43 minutes of exposure is required for the weekly tolerance of 100 mr.

6531-5015

Figure E-4. S4PR Reactor Assembly

B. ACTIVATION SHIELD DESIGN

The activation shield is provided to reduce the dose rate to an acceptable level for plant relocation operations outside the reactor package due to neutron induced activation of the various components shown in Figure E-4. Since shielding of the containment vessel and the activation shield itself would impose a very large weight penalty, it was necessary to choose materials for these applications which would not contribute significantly to the dose rate, after the specified 2-week shutdown period. For this reason, the following materials are recommended:

- 1) Activation shield-lead
- 2) Shield support structure — 52S aluminum
- 3) Containment vessel — 52S aluminum
- 4) Reactor support structure — 3S aluminum

Using the materials listed above, the only activation sources that remain are the pressure vessel and its steel appendages. The 3S aluminum alloy was chosen for the support structure because the orientation of some of the members would be such that streaming through cross section "valleys" in the aluminum might be harmful. Alloy 3S contains 1.2% by weight of manganese which possesses a relatively high cross section for high-energy neutrons and also has a resonance at 25 kev, where aluminum exhibits a reduced cross section.

It was established that if the core were removed and shipped separately a substantial reduction in the shield weight could be realized. This condition can be attained via the usual refueling procedures and by bolting the shield plug back in place in the empty pressure vessel. Once the core is removed, the remaining sources of radiation are created by neutron activation of the various components. The primary contributors are shown in Figure E-4; they are:

- 1) Shield plug
- 2) Pressure-vessel base
- 3) Pressure-vessel wall
- 4) Flange
- 5) Nozzle
- 6) Top plate
- 7) Motor support plate.

Activation sources were calculated using data obtained from the activation handbook⁵ and from the compositions of the various materials used. All product isotopes with half-lives short compared to the 2-week shutdown period were ignored. The remaining isotopes were considered to be at saturation activity with the exception of Co⁶⁰ which was assumed to be at 92% of saturation. This is the condition anticipated at the end of the 20-yr system life; therefore, the resultant shield design will be adequate at any time during this period. It was found that most of the isotopes produced by reactions with fast neutrons emitted gamma rays of approximately 0.9 Mev energy and that those produced by thermal reactions emitted gamma rays of about 1.25 Mev energy. The activation dose was therefore subdivided into two energy groups corresponding to fast and thermal activation. In most cases, the thermal activation sources proved dominant. The isotopes considered are listed in Table E-III. Gamma ray attenuation coefficients and buildup data were obtained from Grotenhuis⁶ and Rockwell.³

TABLE E-III
NUCLEAR DATA FOR ISOTOPES CONTRIBUTING TO ACTIVATION DOSE

Reaction	Cross Section	Half-Life	Gamma Energy
Fe ⁵⁴ (n, p) Mn ⁵⁴	11 mb	300 d	0.84 Mev
Fe ⁵⁸ (n, γ) Fe ⁵⁹	0.9 b	45.1 d	1.2 Mev
Cr ⁵⁰ (n, γ) Cr ⁵¹	13.5 b	27.8 d	0.32 Mev
Co ⁵⁹ (n, γ) Co ⁶⁰	36 b	5.3 y	1.25 Mev
Ni ⁵⁸ (n, p) Co ⁵⁸	66.4 mb	72 d	0.814 Mev
Ni ⁵⁸ (n, 2n) Ni ⁵⁷	2.9 mb	36.4 h	0.854 Mev

Fast neutron fluxes, calculated with code 14-0, were used to describe source shapes in the various regions. The gamma ray volumetric source in mev/cc, sec per unit flux was used in conjunction with the 14-0 fluxes to give the activation source. The source was calculated from

$$S_v \left(\frac{\text{Mev}}{\text{cc-sec}} \right) = \phi \sum_{i=1}^n N_i \sigma_i E_i \quad \dots (1)$$

where

- ϕ = neutron flux n/cm^2 , sec
- N_i = number density of i^{th} isotope
- σ_i = cross section of i^{th} isotope
- E_i = energy of emitted gamma ray, i^{th} isotope
- n = total number of isotopes considered.

Two values of S_v were calculated, one each for the thermal and fast reactions. The thermal flux in regions near the core was estimated from diffusion theory and in the shield water region, external to the activation shield, was estimated from the equation

$$\phi_{th}/\phi_f \approx [\lambda_o \Sigma_a (1 - L^2/\lambda_o^2)]^{-1} \exp(L_s^2/\lambda_o^2) \quad \dots (2)$$

where

- ϕ_{th} = thermal neutron flux
- ϕ_f = fast neutron flux
- L_s = thermal diffusion length
- L = fast diffusion length
- λ_o = fast neutron mean free path
- Σ_a = thermal macroscopic absorption cross section.

The value for ϕ_f was taken from the 14-0 calculations. Equation 2 was obtained from Price, Horton & Spinney,⁴ and approximates the ratio of ϕ_{th} to ϕ_f at equilibrium.

The configuration and dimensions of the proposed activation shield are shown in Figure E-5. The calculated dose rates at the accessible surface and at 1 meter from the accessible surface are presented in Tables E-IV and E-V, respectively.

Shielding is also required around the reactor inlet and outlet piping to limit radiation streaming through the pipe penetrations in the radial shield. Calculations, made with shielding code 14-0, established the following requirements:

- 1) Lead, 2 in. thick, positioned normal to the nozzle centerline and directly opposite the penetrations in the activation shield
- 2) Lead, 1-1/2 in. thick, around the piping concentric with the nozzle centerline

7-2-63
 Figure E-5 Schematic Representation of Activation Shield

7564-0275

TABLE E-IV
ACTIVATION DOSE RATES AT CONTAINMENT VESSEL SURFACE

Point	1	2	3	4	5	6	7	8	9	
Source	Below	Below and Side	Side Opposite Base	Side Opposite Core	Side Opposite Nozzle	Side Opposite Plug	Side Opposite Flange	Above	Motor Compartment	Contributing Component
1	39.7	34.4	16.2	2.6	0.17	-	-	-	-	Pressure Vessel Base
2	13.0	11.3*	8.1	15.5	20.2	19.3	8.5	1.48	81.1	Pressure Vessel Wall
3	-	-	-	-	-	-	0.08	0.22	6.9	Pressure Vessel Flange
4	0.8	0.7*	0.08	0.9	5.3	9.7	4.9	4.6	150.	Shield Plug
5	-	-	-	-	-	-	-	1.51	87.0	Motor Support Plate
6	-	-	-	-	-	-	-	0.06	0.29	Top Plate
7	-	-	2.6	5.6	9.7	9.7	6.6	-	-	Nozzle
8	-	-	-	-	-	-	-	0.72	51.	Drum Drive Motors
Totals	53.5	46.4	26.98	24.6	35.37	38.7	20.08	8.59	376.29 138.29†	

The total dose radially outward through the nozzle is: 20.2 P.V. Wall
32.2 Plug
8.3 Nozzle
60.7 mr/hr

*Extrapolated values

†Total dose in motor compartment including only those sources present when unit has been removed for maintenance

TABLE E-V
ACTIVATION DOSE RATES 1 METER FROM CONTAINMENT VESSEL SURFACE

Point	1	2	3	4	5	6	7	8	9	
Source	Below	Below and Side	Side Opposite Base	Side Opposite Core	Side Opposite Nozzle	Side Opposite Plug	Side Opposite Flange	Above	Motor Compartment	Contributing Component
1	12.03	12.87	3.13	2.74	1.38	-	-	-	-	Pressure Vessel Base
2	1.30	1.40*	3.86	6.17	6.29	6.75	5.24	-	-	Pressure Vessel Wall
3	-	-	-	-	-	-	-	-	-	Pressure Vessel Flange
4	0.34	0.36*	0.34	0.94	1.64	4.24	3.84	-	-	Shield Plug
5	-	-	-	-	-	-	-	-	-	Motor Support Plate
6	-	-	-	-	-	-	-	-	-	Top Plate
7			0.65	1.4	2.4	2.4	1.6			Nozzle
8	-	-	-	-	-	-	-	-	-	Drum Drive Motors
Totals	13.67	14.63	7.98	11.25	11.71	13.39	10.68			

*Extrapolated

- 3) Lead, 1-1/2 in. thick, on the outside of each vertical pipe and extending vertically above the nozzle centerline 15-3/4 in. and 12-1/2 in. on the inlet and outlet pipes, respectively.

A diagram of the pipe shield is included as Figure E-6.

Figure E-6. Supplementary Shielding for Coolant Pipes

C. OPERATING SHIELD

The design of the shield, required during operation of the SNAP 4 reactor, must consider three basic problems, in addition to special considerations created by the particular application.

- 1) Heat generation in the reactor and external components
- 2) Possible activation of external components due to neutron leakage from the reactor,
- 3) Environmental radiation hazards.

Since the environmental dose requirements are to be specified for each of the different applications, only the first two areas will be examined here.

7-2-63 7564-0276
Figure E-7. Heat Generation Due to
Radiation Absorption in Beryllium
(12 Mwt)

7-2-63 7564-0277
Figure E-8. Heat Generation Due to
Radiation Absorption in Steel
(12 Mwt)

Evaluation of heat generation in structural and shield materials was performed so that thermal stress conditions for these components could be evaluated. Sources of heat due to attenuation of radiation can be broken down into four main parts:

- 1) Attenuation of fission and fission product gamma rays
- 2) Attenuation of gamma rays resulting from thermal neutron capture in various materials
- 3) Attenuation of gamma rays resulting from inelastic scatter, and
- 4) Elastic scatter of high-energy neutrons.

In the walls of the pressure vessel, the sources of significant magnitude were the fission and fission product gamma attenuation and the attenuation of capture and inelastic scatter gamma rays born in the steel. The core structure cylinder, thermal shield, and pressure vessel were treated as one region in these calculations. The water gaps between these regions are not expected to cause serious error because of their small size and because of the low cross sections for neutron and gamma attenuation in these regions.

The sources of heat in the lead activation shield were the same as those in the steel plus a contribution from the attenuation of capture and inelastic scatter gammas born in the lead.

Heat generation in the control drums was also calculated and found to be due primarily to the attenuation of fission and fission product gamma rays and the elastic scatter of fast neutrons in the beryllium. Elastic scatter is significant in this region because of the low mass of the Be nucleus. Heating in the poison (HfH_x) portion of the drum, due to gamma attenuation, is approximately 7.5 times as great as in the beryllium. Elastic scatter and capture gamma heating become insignificant. Heat generation curves for the beryllium, steel and lead regions discussed here are presented as Figures E-7, -8, and -9.

Shielding code 14-0 was used to compute heat generation in the stainless-steel shield plug due to prompt fission and fission product gamma rays. Hand calculations were made of the heat generation from fast neutron elastic and inelastic scattering and from capture gammas. The predominant heat source in the shield plug is the iron capture gamma rays. The thermal flux incident on

7-2-63

7564-0278

Figure E-9. Heat Generation Due to Radiation Absorption in Lead (12 Mwt)

the bottom face of the plug was determined, from diffusion theory, to be about 1.52×10^{13} n/cm² sec. This value was then assumed to be exponentially attenuated according to

$$\phi = 1.52 \times 10^{13} e^{-\Sigma_a x}$$

A plot of the heat generation in the stainless-steel shield plug is presented as Figure E-10.

Activation of components external to the reactor package will depend upon the neutron leakage and thus upon the amount of water between the activation shield and the outside of the containment vessel. The fast flux through this region was calculated with code 14-0 and the thermal flux, assumed to be in equilibrium with the fast flux, was calculated from Equation 2. The results are plotted in Figure E-11. The neutron flux, above the core midplane and displaced 3 ft from the core centerline, has been calculated with shielding code 14-0 and the results plotted in Figure E-12. This curve gives the ratio of the flux at any elevation to the flux at the horizontal midplane, for water shield thicknesses in the neighborhood of 22 in.

The activation of a 4000-lb stainless-steel steam separator vessel, located 6 ft above the core midplane and outside a water shield 22 in. thick, was calculated to be 314 mr/hr at a distance of 1 ft. A 4-in. -thick belt of polyethylene, 6 ft wide, wrapped around the containment vessel at core elevation, should be sufficient to reduce the activation dose of the steam separator to 100 mr/hr at 1 ft, which would permit 1-hr access if the weekly radiation tolerance is acceptable.

If the steam separator is located within the same containment vessel as the reactor and approximately at core elevation, then the required thickness of water is 37 in. for an activation dose of 100 mr/hr at 1 ft. Since about 80% of this activation is due to thermal neutrons, the interposition of a boron plate, or other absorbing material, would reduce the required water thickness considerably. For example, the required water thickness is reduced to 21 in. when only fast neutron activation is considered.

For an arctic application, the heat generated in the wall of an ice tunnel outside the primary containment vessel is important. Heat generation in the ice due to neutron elastic scatter, and fission and fission product gammas has

7-2-63

7564-0279

Figure E-10. Heat Generation Rates in Shield Plug for 12 Mwt Power Level

7-2-63

7564-0280

Figure E-11. Operating Neutron Fluxes Outside SNAP 4
Activation Shield (12 Mwt)

7-2-63

7564-0281

Figure E-12. Normalized Fast Flux Above Radial Centerline in Water Shield (12 Mwt)

been calculated for an ice wall 1 ft away from the outer boundary of the containment vessel. A curve of heat generation rate at the face of the ice wall vs water shield thickness is presented as Figure E-13.

D. CORROSION PRODUCT ACTIVATION

Corrosion products may enter the coolant stream at various rates from numerous parts of the system. It is likely that some of the corrosion products from the core will be radioactive. These will be referred to as core corrosion products. Material which enters the coolant stream from more remote parts of the system are not initially active, but may become so by being circulated through the core by the coolant. This process will be referred to as cyclic activation.

Impurities in the coolant stream of the SNAP 4 system come primarily from stainless steel and from Stellite valve components. The coolant purification system is designed to maintain the purity of the coolant at a level of 10 ppb or less. For this reason the activity level of the coolant due to corrosion products is expected to be very low. In the SNAP 4 system, 0.5% of the reactor coolant stream, as well as all of the condensate, passes through the demineralizer and filter system with an impurity removal efficiency of at least 95%. Thus, the mean number of cycles through the reactor prior to removal is approximately 210.

The equation used to calculate the specific activity due to cyclic irradiation is

$$A(k) = \left[\frac{\lambda N \sigma \phi}{\phi \sigma + \lambda} \right] \frac{\{1 - \exp[-(\phi \sigma + \lambda)t_c]\} \{1 - \exp[-(k+1)(\phi \sigma t_c + \lambda T)]\}}{\{1 - \exp[-(\phi \sigma t_c + \lambda T)]\}} \quad \dots (3)$$

where

A = specific activity of fluid (dis/sec-cc)

t_c = time fluid is in core

$A(t_c)$ = activity of fluid at core exit

T = cycle time

λ = decay constant of active isotope

ϕ = neutron flux ($n/cm^2 \text{ sec}$)

N = number density of parent isotope

7-2-63

7564-0282

Figure E-13. Heat Generation in Ice Wall vs Water
Shield Thickness - SNAP 4 (12 Mwt)

σ = microscopic cross section of parent isotope

k = number of circulation cycles

Assumptions made in the calculation of cyclic activity were: (1) no initial activity, and (2) complete solution of the impurities so that they flow around the loop at the same rate as the coolant. Table E-VI presents the specific activity of the coolant as a function of impurity concentration and number of cycles.

TABLE E-VI
COOLANT ACTIVITY DUE TO CYCLIC ACTIVATION

Impurity Concentration	100 Cycles	1000 Cycles	Equilibrium (∞ cycles)
ppb	$\mu\text{c/cc}$	$\mu\text{c/cc}$	$\mu\text{c/cc}$
10	1.77×10^{-8}	1.69×10^{-7}	6.1×10^{-5}
100	1.77×10^{-7}	1.69×10^{-6}	6.1×10^{-4}
1000	1.77×10^{-6}	1.69×10^{-5}	6.1×10^{-3}

The calculation of core corrosion product activity was based on the assumption that corrosion from the core itself could produce no isotope irradiated for more than one full-power year (expected core life). The active isotopes coming from the pressure vessel, however, could produce saturation activities for almost any of the isotopes near the end of the expected 20-year system life. Based on these assumptions, the coolant specific activity due to core corrosion products was calculated as shown in Table E-VII. It is apparent that the core

TABLE E-VII
COOLANT ACTIVITY FROM CORE CORROSION PRODUCTS

Impurity Concentration	Core	1000 Cycles	Equilibrium (∞ Cycles)
ppb	$\mu\text{c/cc}$	$\mu\text{c/cc}$	$\mu\text{c/cc}$
10	4.7×10^{-4}	1.69×10^{-7}	6.1×10^{-5}
100	4.7×10^{-3}	1.69×10^{-6}	6.1×10^{-4}
1000	4.7×10^{-2}	1.69×10^{-5}	6.1×10^{-3}

activities are much higher than the 1000-cycle, cyclic irradiation case and a factor of 10 higher than the infinite cycle case. The total coolant activity will therefore be governed by the core corrosion products.

Values of coolant activities quoted in Tables E-VI and E-VII apply after a two-day shutdown period. The isotopes considered in this analysis are the same as those listed in Table E-III.

E. DEPOSITION OF CORROSION PRODUCTS

Calculations were performed to assess the magnitude of the hazard to maintenance personnel of radioactive corrosion products that have deposited on pipe walls and in the demineralizer system. The following methods were used to analyze this problem.

The cumulative amount of corrosion product deposition was assumed to follow the square root-time relationship observed for the buildup of corrosion films:

$$y = C_1 t^{1/2}$$

where t = operating time

y = accumulated deposit

C_1 = a constant.

The activity of the material deposited was then calculated from

$$A_d = A \int_0^{T_o} y'(t) e^{-\lambda(T_o + T_s - t)} dt \quad \dots (4)$$

where

A_d = activity of deposited material

A = activity per unit weight at time of deposition

$y'(t)$ = rate of deposition at time t

λ = isotope decay constant

T_o = operating time

T_s = shutdown time after operation

Activities were calculated as in the preceding section for coolant activity, except that the number densities corresponding to the deposited solid material were used. Curves of deposited activation source vs operating time for one year are presented in Figure E-14. These curves represent the 100, 1000, and infinite cycle cases of cyclic irradiation plus a curve for core corrosion products. Figure E-15 presents curves for the unshielded dose rate per pound of accumulated corrosion products, at 1 ft for the same cyclic cases and core corrosion products. Self-attenuation has not been included in calculation of these dose rates.

The curves, describing the deposited activation source, are conservatively based upon a film layer of 120 mg/dm^2 at the end of the year, all of which is assumed to have precipitated from the coolant. This last assumption is not true since some of the film material will come directly from the pipe wall. In fact, due to the extremely low concentration of impurities in the coolant (10 ppb), the film will more than likely contain only a small percentage of material absorbed from the coolant. Table E-VIII summarizes the deposited activation source after one year as a function of the percentage of the deposited material that had been activated. The estimated dose, 1 ft from a 6-ft length of 6-in. pipe, is also given.

TABLE E-VIII
ESTIMATE OF PIPING MAINTENANCE DOSE RATES

Material Absorbed From Coolant (%)	Source Strength S_A (Mev/cm ² sec)	Pipe Dose (mr/hr at 1 ft)
1	3.18×10^2	0.13
10	3.18×10^3	1.3
100	3.18×10^4	13.0

For the case of corrosion product accumulation in the demineralizer, the core corrosion products represent the most significant source. Table E-IX lists the dose/lb of accumulated corrosion products for two cyclic cases combined with the products from core corrosion.

7-2-63

7564-0283

Figure E-14. Gamma Source of Deposited Corrosion Products on Pipe Walls (12 Mwt, 2-week Shutdown Period)

7-2-63

7564-0284

Figure E-15. Dose Rate/lb of Accumulated Corrosion Products
(12 Mwt, 2-week Shutdown Period)

TABLE E-IX
DEMINERALIZER RADIOACTIVITY ESTIMATES

Irradiation Cycles	Cyclic Irradiation (mr/hr-lb at 1 ft)	Core Irradiation (mr/hr-lb at 1 ft)	Total Dose Rate (mr/hr-lb at 1 ft)
∞	1.51×10^3	5.11×10^3	6.62×10^3
1000	0.98×10^{-1}	5.11×10^3	5.11×10^3

Corrosion of the core and pressure vessel may amount to 0.45 lb in a year. The rest of the system should contribute another 2.5 lb, or about 85% of the total. The values in Tables E-VIII and E-IX are based on the composite material, 15% from the core and pressure vessel and 85% from the rest of the system. In all cases, further activation of material from the core due to cyclic irradiation has been ignored. This approximation is justified by the fact that cyclic irradiation does not contribute significantly except in the overly conservative case of an infinite number of cycles.

APPENDIX E REFERENCES

1. J. A. Belcher and L. K. Zoller, "Ott Laminated Shield Configuration Analysis Method," DC 61-4-15 (February 1960)
2. V. Keshishian, "Radiation Shielding," NAA-SR-5966 (December 1960)
3. T. Rockwell, "Reactor Shielding Design Manual," TID-7004 (March 1960)
4. B. T. Price, C. C. Horton, and K. T. Spinney, Radiation Shielding, Pergamon Press, London (1957)
5. Revised Activation Handbook for Engineers, ANP Document No. NARF-57-50T. Convair, Ft. Worth, Texas
6. M. Grotenhuis, "Lecture Notes on Reactor Shielding," ANL-6000 (March 1959)

BLANK

APPENDIX F. PROCESS SYSTEM

A. DEFINITION AND REQUIREMENTS

The process system comprises all of the fluid (liquid and gas) systems which are required to support the safe operation of the reactor heat source. Figure F-1 is a piping and instrumentation and heat balance diagram for the S4PR. The primary functional requirements of the process system are as follows:

- 1) Dissipation of power generated by the reactor
- 2) Control of the process fluid chemistry
- 3) Sensing and control of the process fluid and process equipment operating conditions.

B. BACKGROUND

1. General

During FYS 61-65 the concept, known as the SNAP 4 compact nuclear power system, has evolved from a study of basic fuel technology;¹ power conversion systems;² coolant characteristics;² concept applications;^{3,4} and the more detailed aspects of reactor design.⁵ As a result of this effort, the process system has been developed, based on the direct Rankine cycle, using boiling water coolant² to capitalize on the plant ground rules of greater reliability, safety, compactness, flexibility, and reduced costs associated with this type of power conversion system.

2. Process System Design Objectives

On the basis of the plant ground rules mentioned above, specific design objectives have been developed for the S4PR process system. Some of these objectives are as follows:

- 1) Hermetically sealed equipment
- 2) Continuous coolant purification and continuous recombination of radiolytically decomposed coolant
- 3) Maintenance free operation for at least one year
- 4) Reduced pipe sizes with somewhat higher than conventional process fluid velocities and pressure drops

- 5) Equipment support features which allow piping arrangements with a minimum of expansion loops, thus reducing the lengths of pipe runs
- 6) Compact, high-speed, turbine driven process pumps
- 7) A maximum of regenerative heat exchange consistent with an optimization of reactor performance
- 8) Compact heat exchange equipment based on high surface-area-to-volume ratio heat exchanger designs
- 9) Compact valve designs using hydraulic actuation to maximize the available power and actuating speeds for a minimum valve envelope size.

C. SUBSYSTEM DESCRIPTIONS

1. Recirculation Subsystem

The recirculation subsystem (depicted in Figure F-1) includes the process loop from the reactor vessel outlet, through the steam separator, recirculation pump, and back through the reactor vessel. The S4PR heat balance, Figure F-2, indicates that, at the design point reactor power of 12 Mwt and at a reactor inlet coolant temperature of 538°F (32°F subcooling at a pressure of 1225 psia), 410,000 lb/hr reactor flow is required. However, as further process system trade-offs are investigated and the optimum amount of process system regenerative feedwater heating is evaluated, the reactor inlet water

6531-5449

Figure F-1. Recirculation Subsystem

7-2-63

Figure F-2. System Heat Balance Diagram

7564-0325

temperature and the recirculation system flow rate will vary slightly. The recirculation ratio (reactor flow rate to steam flow rate) of 10:1 and hence, the average reactor effluent quality of 10% have been fixed. The selection of a 10:1 recirculation ratio is based on the desirability of maintaining both a high burnout heat flux and a low recirculation pump horsepower requirement.³

The steam separator requirements were established by process system design and plant arrangement considerations and were included in a preliminary steam separator specification recently issued to potential suppliers. Both horizontal and vertical configurations have been proposed to meet the specified requirements. Most of the designs proposed have included two distinct stages of mechanical steam-water separation employing centrifugal and impingement type elements. Other separator designs include a single stage of mechanical steam-water separation preceded by an initial coarse separation by gravity. A recent study of steam separator designs (see Appendix F, Section C-11-g) confirms theory in that the mass velocity of the steam rising toward the mechanical steam-water separator strongly influences the amount of moisture carried into the mechanical element.^{6,7} In addition, the allowable pressure drop across the second stage mechanical separator is limited by the height of the mechanical separator above the maximum transient water level in the vessel. Thus, there is a minimum size of mechanical separator that will separate the water from the steam without drawing water from the storage volume in the bottom of the separator vessel.

The steam separator also serves to remove particulate matter (crud) from the steam supply since the crud falls out with the water as moisture is removed in the first and second stages of the separator. The crud concentration in the steam, discharged from the steam separator vessel, will be reduced to 10^{-3} to 10^{-5} of that in the water discharged from the separator. The very low crud activity in the steam discharged from boiling water reactors, and hence the low contamination level in the associated power turbine has been reported previously.^{8,9} This fact, together with the reduced N^{16} gamma activity of the separated steam, due to the low density and the short N^{16} half-life, ensure that shielding requirements for steam handling equipment will be minimized. In addition, most unexpected maintenance problems can probably be resolved without completely shutting down the reactor provided that the primary (reactor) radiation dose levels are sufficiently low.

The minimum water storage volume in the steam separator vessel will be specified to preclude the recirculation pump from running dry during transient operating conditions. Design of the separator vessel should also consider the velocity of the water in the storage volume to preclude crud precipitation; failure to consider this situation could result in increased dose rates during maintenance operations in the vicinity of the separator vessel. However, during reactor operation the dose rate outside the separator vessel due to the coolant N^{16} gamma activity is significantly greater than that which would result if all of the crud produced by the system during one year of operation were to be mixed with the process fluid at the beginning of operation, circulated through the reactor for one year, and then be deposited in the steam separator vessel.

Saturated water flows at a rate of 366,950 lb/hr from the steam separator vessel toward the suction port of the recirculation pump. To ensure stable pump operation and to preclude damage from cavitation, this stream must be subcooled. Subcooling is accomplished by directing the feedwater pump discharge flow into the recirculation stream between the steam separator and the recirculation pump suction. The mixing of these two streams will provide about 30°F of subcooling. Considerably less subcooling will result if feedwater heating is provided. The 30°F subcooling corresponds to approximately 250 psi of available net positive suction head, more than adequate to preclude pump cavitation.

The head developed by the recirculation pump is 45 psi. A detailed study of pump and pump driver design features was performed (see Section G, Turbo Pumps). The results of this study indicate that significant savings in weight and size can be realized if high speed, steam turbine-driven pumps are employed rather than more conventional electric motor driven units.

Measurement of the rate of coolant flowing from the recirculation pump is provided by a primary flow element. Flow signals are used for control, indication, and safety circuit purposes. Based on a study of the many available primary flow elements, an impact-type flow tube will provide the accuracy desired, with a low permanent pressure loss.¹⁰ Additional advantages of the flow tube are that it is compact and requires a minimum of upstream and downstream straight pipe runs.

2. Steam Subsystem

The steam subsystem (represented schematically in Figure F-3) includes the process lines from the steam separator vessel to the condenser and to the auxiliary turbines. A parametric study, Appendix F, Section G, involving the auxiliary turbine inlet and outlet steam conditions as the independent variables and cycle efficiency, reactor power, and various system parameters as the dependent variables) indicates that higher cycle efficiencies are possible when low-pressure steam, typical of turbine extraction steam, which will be available in the final SNAP 4 System, is used to drive the auxiliary turbines. Based on this study, the design steam flow to the recirculation pump turbine is 2600 lb/hr, and the design steam flow to the feedwater-condensate pump turbine is about 6100 lb/hr. The remaining steam flow, 32,300 lb/hr at design conditions, is throttled by the flow control valve upstream of the condenser. The condenser operates at a pressure of 25 psia. The selection of the 25 psia condition is based on a previously documented, parametric analysis³ which examined the trade-off in the loss of cycle efficiency with higher condensing temperatures versus the advantages gained in decreased condenser weight and volume. At about 240°F (the saturation temperature corresponding to 25 psia), plant efficiency had decreased only slightly from the peak efficiency while the condenser volume had been significantly reduced from the volume required at the pressure corresponding to peak efficiency (Figure F-4).

Information received from heat exchanger suppliers in response to a preliminary condenser specification indicates that the condenser size and weight can vary widely. This wide variation is typical of the two approaches to the design

6531-5450

Figure F-3. Steam Subsystem

5-31-63

7564-0249

Figure F-4. Effect of Condenser Conditions on Cycle Efficiency

of heat exchangers that have been reflected in many of the preliminary vendor responses. One design approach reflects the more conventional industrial practice of utilizing 3/4- to 1-in. diameter tubing with correspondingly low shell and tube-side pressure losses. The second design approach reflects aircraft and missile industry heat exchanger design practice for compact lightweight units. Typically, tube diameters as small as 1/8 in. are employed. These designs result in increased shell and tube-side velocities and pressure losses; these are accompanied, however, by improved film heat transfer coefficients as well as more surface area per unit volume of heat exchanger. The condenser volume, corresponding to the typical process design, was about 70 ft³, whereas the volume corresponding to the compact design was about 20 ft³.

In parallel with the main steam line indicated in the previous sketch, there is also a steam dump line. This line provides the means for the rapid attenuation of a pressure surge arising from a system malfunction. At a specified pressure setting, the steam dump line valve opens to discharge steam

directly to the condenser. Thus, the steam dump line represents one of three methods provided to attenuate excessive system pressure rise transients.

These are:

- a) Pressure control circuits of the plant control system
- b) Steam dump valve
- c) Pressure relief valve.

Methods b and c will only function if the pressure surge is not adequately attenuated by a and b, respectively. The discharge of steam via the dump line to the condenser will be closely scrutinized to avoid attainment of supersonic discharge velocities, potentially damaging to condenser internals. Appropriate steam distribution pipes and baffling, typical of those used in naval nuclear propulsion plant condenser steam dump lines, may be required. This problem will be investigated further.

Since the S4PR reactor is cooled and partially moderated by boiling water, the steam void density in the core is a strong factor in determining control drum reactivity requirements. Since system pressure transients are reflected back to the reactor through the system piping, the steam void density in the reactor can vary and create fluctuating reactivity inputs. As the steam surge volume between the steam line control valve and the reactor increases, the influence of the transients on the reactor is diminished due to the attenuation of the pressure transient. It is planned to provide steam surge vessels connected to the main steam line to add to the steam surge volume existing within the steam separator vessel. By suitably valving the steam surge tanks and by varying the water level within the steam separator vessel, the influence of steam surge volume on the feedback effects of pressure transients will be studied. These studies will permit determination of the total steam volume required to assure reactor stability and will permit verification of requirements predicted by analog computer simulation studies.

3. Feedwater and Condensate Subsystem

The feedwater and condensate subsystem (depicted in Figure F-5) includes the process lines from the discharge port of the condenser hotwell through the feedwater-condensate pump first stage, the condensate cleanup equipment (i. e., regenerative and nonregenerative heat exchangers, deionizer,

Figure F-5. Feedwater and Condensate Subsystem

6531-5451

guard filter), through the second regenerative heat exchanger, and the remaining stages of the feedwater-condensate pump.

The condenser hotwell serves as a water storage volume within the system. There are presently three locations within the system where water storage capacity is provided. They are the condenser hotwell, the steam separator vessel, and the reactor vessel. In the event that the reactor water inventory must be controlled within a very narrow band to preclude large-power level variations (due to the reactivity effects of a variable water density), then only the steam separator vessel and condenser hotwell water volumes will vary significantly. Thus, during transients, there will be a transfer of part of the system water inventory from the steam separator vessel to the condenser hotwell on a load demand transient, or from the condenser hotwell to the steam separator vessel on a reduction in load demand. The process system control loops will affect a redistribution of the water inventory between the two volumes as the transient is attenuated if large changes in the water levels have occurred.

The condensate pump (the first stage of the multistage, turbine-driven, feedwater-condensate pump) will receive 46,000 lb/hr of condensate at a pressure of 16 psia. Subcooling of the condensate, to provide adequate net positive suction head for the condensate pump stage, is accomplished by cooling of the condensate as it passes through the first regenerative heat exchanger. The condensate pump stage will provide a discharge pressure of 125 psia to supply the pressure drop through the coolant purification equipment and to meet the feedwater pump net positive suction head requirements. Consideration will also be given to the amount of heat added regeneratively to the process fluid between the condensate pump and the feedwater pump as a means of optimizing the second-stage inlet conditions.

The condensate pump discharges through the nonregenerative heat exchanger which reduces the temperature of the coolant to 130°F. This temperature is established by resin suppliers as the maximum permissible temperature for reliable, extended deionizer performance. At temperatures above 130°F, resin decomposition occurs, adversely affecting the ion exchange capacity of the bed. A more comprehensive discussion of resin bed performance is contained in Appendix F, Section E.

The condensate cleanup equipment consists of a mixed-bed deionizer and a guard filter installed downstream of the deionizer. The purpose of the deionizer is to exchange metal and nonmetal ionic impurities with the hydrogen and hydroxyl ions of the deionizer resin. The resin bed also serves as a system filter to assist in the removal of "crud." Pressurized water reactor experience indicates that use of the resin bed as a filter does not adversely affect ion exchange capacity. The guard filter precludes gross contamination of the process system in the event that the resin retention element in the deionizer fails. Additional discussion of the water treatment facilities provided is presented in Section E. Following the guard filter, the process fluid passes through the shell side of the first regenerative heat exchanger and the shell side of the second regenerative heat exchanger. The process fluid temperature is increased to 240.3°F during these two heat exchanger passes. At this temperature, there is 36.7 psi of NPSH available at the suction port of the feedwater pump. The condensate and feedwater pumps are mounted on a common shaft together with the steam turbine driver. The three components are supported on water-lubricated bearings and the complete unit is hermetically sealed. The feedwater pump raises the

pressure of the coolant to a pressure of 1220 psia, required to inject the fluid into the recirculation stream.

On the tube side of the second regenerative heat exchanger, a stream of process water from the recirculation system (0.5% of the design reactor flow rate) is cooled from 568 to 240°F. This stream is mixed with the condensate flow, at the inlet to the first regenerative heat exchanger (tube side), and then is purified together with the condensate stream.

4. Valve Operating Subsystem

A recent study (Appendix F, Section C-11) substantiates the position (expressed in Reference 5) that a hydraulic valve operating system, using the process fluid as the working medium, possesses numerous advantages that are desirable in the S4PR system. This study compares the following types of valve operator-controllers:

- a) Electric
- b) Pneumatic
- c) Hydraulic
- d) Electropneumatic
- e) Electrohydraulic.

The following factors were considered for each of the above valve operator-controllers:

- a) Cost
- b) Weight
- c) Size
- d) Performance
- e) Availability
- f) Accessory equipment requirements
- g) Startup, shutdown requirements
- h) Emergency positioning
- i) Sealing.

Although a piping diagram was not prepared for the entire valve operating subsystem, it will be similar to Figure F-6.

Figure F-6. Valve Operating Subsystem

Maximum pressure differential for valve operation is available if 1200 psia water is provided to the servo valve and the servo valve low pressure port discharges to the condenser. Based on maximizing the hydraulic power to the process valve operator, the pressure to the process valve operator would be about $2/3$ of the servo valve supply pressure, or 800 psia. Minor hydraulic losses in the piping were neglected in the calculation of the pressure levels cited above.

Servo valve piston position and hence, the process water flow to the main process valve operator, will be controlled by process flow, and process valve piston position signals. Use of a process fluid operated valve permits the process valve and its associated servo valve to be hermetically sealed conveniently. A number of piston operated, totally sealed, on-off valves are currently being used in the naval pressurized water reactor program.¹¹ Many valve suppliers have offered packed gland, water operated process control valves. Therefore, no problems are anticipated in the procurement of hydraulically operated, hermetically sealed control valves. A survey of servo-valve vendors was also conducted to determine whether they anticipate problems in the application of existing servo valve designs to a water environment. Vendor responses indicate that these problems should be less stringent than several of the missile fuel and refrigerant applications for which servo valves have previously been designed, and have been found to operate reliably.

The addition of a small pressurized accumulator and a small positive displacement pump to the valve operating system will ensure the availability of a high-pressure source of water in the event of a loss of the main source of valve operating pressure (from the recirculation loop or feedwater pump discharge).

A brief investigation of the valve stem force requirements indicates that, even for a high valve plug differential pressure, the operating piston need not be very large to provide the required operating force. For example, with a 4-in. shutoff valve, the largest on-off valve that is to be used, the average process fluid flow rates need not exceed 15 gpm for 1 sec to achieve closure times which are limited only by the stresses imposed upon the valve structure. For control valve actuation, even smaller flow rates (~ 10 gpm) are required, since the maximum stroking speeds do not exceed about 1 in./sec, and since these valves are smaller.

The valve characteristics and associated process requirements presented above are preliminary estimates that require substantiation by analysis.

5. Decay Heat Removal Subsystem

The decay heat removal subsystem (shown in Figure F-7) comprises a process fluid loop from the outlet nozzle of the reactor vessel, through a heat exchanger and back to the inlet nozzle of the reactor vessel.

6531-5453

Figure F-7. Decay Heat Removal Subsystem

This system removes the residual (afterglow) energy generated within the core following a reactor shutdown (see Table F-I).

TABLE F-I
INSTANTANEOUS POWER (AT TIME, t) AFTER SHUTDOWN*

t	Power (kwt)	t	Power (kwt)
0.1 sec	480	1 hr	125
1 sec	460	10 hr	67
30 sec	342	24 hr	54
1 min	308	36 hr	48
5 min	216	48 hr	45
10 min	183	72 hr	40
30 min	142		

*Reactor shutdown from a power level of 12 Mwt. Core contains fission products accumulated after 12 Mwt-years of operation.

The decay heat removal subsystem has been sized to dissipate the decay power corresponding to the end-of-core-life condition. This subsystem will not normally be in use, since the decay heat can be absorbed by vaporization of reactor coolant which can be condensed in the main condenser. In the main condenser, the heat will be transferred to the cooling tower cooling water loop, and then to the ambient air. However, in the event of component failure, rendering coolant pumping impossible, the decay heat removal subsystem must be instantaneously available.

The driving head for the flow of coolant in the decay heat removal loop results from the difference in density of the steam rising to the decay heat exchanger and the condensate which returns to the reactor inlet. The driving head can be increased by maximizing the differential density and increasing the elevation between the decay heat removal heat exchanger and the reactor.

A study was undertaken to make the decay heat removal heat exchanger more compact. Instead of placing the decay heat loop between the reactor nozzles, it was installed in the steam separator. In this design, the decay heat loop was completely sealed with its own circulating water supply (Figure F-8).

6531-5454

Figure F-8. Decay Heat Removal Subsystem
Installed in Steam Separator

One of the advantages of this particular design is that the radioactive process fluid can be retained within the natural boundaries of the process system. The possibility of including the decay heat exchanger within the reactor vessel above the core was also considered. However, as the design of the reactor core and vessel became more firm, it was apparent that sufficient space would not be available within the reactor vessel. An increase in the vessel size to accommodate the decay heat exchanger would have added excessively to the reactor weight.

Preliminary design of this loop indicates that approximately 40 ft of 2-in. diameter pressure tubing, arranged in a serpentine coil, and located at least 3 ft above the reactor nozzles, will adequately handle the heat loads anticipated.

6. Pressure Relief Subsystem

The pressure relief subsystem consists of the pressure relief valves and piping. These components serve primarily to protect the process system vessels from excessive pressure. The pressure relief valves in the high-pressure

part of the process system will discharge to the main condenser. Thus, all radioactive fluid relieved from the process system can be retained within the fluid boundaries. In the unlikely event that the condenser pressure would finally rise to the condenser relief valve setting, the fluid would be discharged out of the plant to an external quench tank. This external tank would also be designed to preclude a release of the process fluid to the environment.

The set pressure and capacity of the relief valves were not established. The pressure relief piping must also be designed to ensure that dynamic fluid loads resulting from relief valve operation can be accommodated. Adequate allowance in the valve orifice area must be provided to allow for back pressure effects. Thus, considerable analysis remains before the pressure relief parameters can be specified.

7. Poison Injection Subsystem

This subsystem will consist of a small, pressurized tank containing an aqueous solution of a neutron absorbing material which may be injected into the reactor vessel as required. This subsystem provides an absolute means of quenching the nuclear reaction in the unlikely event that the reflector control drums become inoperable. The volume of poison solution required is dependent primarily upon the concentration of the poison required to effect shutdown, the dilution of the poison solution when it is injected, and the nuclear cross section of the poison selected.

Detailed analysis of nuclear poison injection requirements and subsystem design have not been performed. However, the ionic nature of the chemical compound has been specified to ensure the ease and completeness of removal of poison from the coolant by demineralization.

8. Recombiner Subsystem

The recombiner subsystem consists of the equipment and piping required to draw gases from the condenser and to effect the recombination of the hydrogen and oxygen generated by radiolytic decomposition of the process water in the reactor. Figure F-9 presents two possible configurations. The specific design of the recombiner system for S4PR depends upon whether all of the plant equipment is hermetically sealed or whether air in-leakage is possible. In the latter case, it will be necessary to remove this air to prevent the buildup of

Figure 9. Recombiner Subsystem

6551-5455

noncondensables in the condenser, and System I would be employed. System II represents the recombiner system proposed for a hermetically sealed plant. Since there would be no air in-leakage, a continuous, nonvented recombiner system could be used. For a more detailed discussion of the water chemistry and recombiner design, see Section Appendix F, Section E.

9. Coolant Makeup Subsystem

The coolant makeup subsystem consists of the piping, tanks, and equipment necessary to provide pure makeup water to the plant, to collect drainage from the plant and plant containment vessels, and to process the plant drainage

for return to the plant. The major factor in determining subsystem requirements is the plant leakage rate. Even with the S4PR hermetically sealed, the need to take periodic samples for coolant chemical analysis results in a small but well-defined makeup requirement. In addition, experience with pressurized water reactor plants indicates that the primary system relief valves are one of the largest sources of plant leakage. Following a very few cycles of operation of these valves, their leak rate can increase significantly. Design of the S4PR pressure relief subsystem is expected to overcome this large source of plant leakage by discharging the high-pressure relief valves to the condenser. However, the condenser relief valve will discharge to a tank which is not directly tied to the process system. Therefore, compensation for the leakage from this valve may be required.

The quality of the water available for makeup is another factor which could have a strong influence on the design of the coolant makeup system. The more contaminated the raw water is, the more the equipment necessary to process it, before injection into the process system. For a truly hermetically sealed system with only minor losses due to sampling, the makeup system may simply be a small pure water storage tank and a low capacity, low-head injection pump. The makeup water could be procured as process quality water and thus delete the need for additional water treatment facilities.

Detailed design of the makeup subsystem must await the selection of process equipment and the establishment of sampling requirements. Thus, it is clear that in this area only preliminary and qualitative judgments can be made until after much of the plant equipment has been specified in sufficient detail to permit procurement.

10. Shield Water Subsystem

The shield water subsystem is provided to maintain both the temperature and purity of the water within desired limits. It includes a recirculating pump, heat exchanger and piping between the shield water tank surrounding the reactor, and the coolant makeup subsystem. This interconnection provides a direct means of purifying the shield water. Besides limiting accessibility to equipment in the vicinity of the shield water tank, radioactivity in the water would interfere with the reactor maintenance and refueling operations. The design of this system is still conceptual; i. e., a more specific definition of radiation tolerance levels in

the vicinity of the plant, as well as of maintenance and refueling requirements, must be established before the design can be made more firm.

11. System Studies

A number of intensive basic process system studies were conducted to develop a substantial technology as part of the basis for an overall power plant definition. The following studies were completed.

a. Literature Search

A literature search was undertaken to collect information from operating water-cooled reactor plants. Specifically, data were sought in the following areas:

- 1) Materials of construction
- 2) Water chemistry conditions
- 3) Radiolysis
- 4) Erosion
- 5) Purification system design
- 6) Chemical poison system design
- 7) Water makeup system design.

Despite a paucity of operating experimental data and despite discrepancies between references in the literature, a large amount of information was gathered. One of the most interesting points uncovered was the lack of evidence to support the selection of high pH coolant conditions for the S4PR. Table F-II summarizes the information collected.

b. Design Pressures and Temperatures for S4PR Components

The basis for the selection of system and component design pressures and temperatures for other reactor plants was reviewed in an attempt to establish a consistent basis for the S4PR. Information from the ASME Boiler and Pressure Vessel Code, the Tentative Structural Design Basis for Reactor Pressure Vessels, and Directly Associated Components (Pressurized Water-Cooled Systems),¹² and other general structural guides was also reviewed.¹³ This literature search revealed that a consistent approach to the establishment of design

TABLE F-2
SUMMARY OF WATER-COOLED REACTOR OPERATING CONDITIONS

Reactor	Type	Core Characteristics			Materials				pH	Reactor Water Chemistry Conditions				Chemical Poison	Reactor Water Purification System			Condensate Purification System	
		Power Density (kw/liter)	Average Core Coolant Temperature (°F)	Average Core Coolant Pressure (psia)	Fuel Cladding	Vessel Cladding	Condenser or Steam Generator	Other Components		Sp. Cond. mmho	TDS (ppm)	Radiolytic Rate liters or (STP/Mwh)	Hydrogen Addition (cc/kg)		Purification Ratio (a)	Filters (Upstream of Deionizer)	Deionizers	Filters	Deionizers
1	APPR (SM-1)	PWR	440	1206	304L				7			0 ^(b)	(s)					N.A. ^(c)	N.A. ^(c)
2	Shipping port	PWR	537	2000	Zry-2	304	304	304	10 ± 0.5	(d)	0.003	0 ^(b)	25-50	H ₃ BO ₃ ^(e)	0.003	None	MB ^(g)	N.A. ^(c)	N.A. ^(c)
3	Yankee	PWR	58	515	2000	348	304		7.5	2	0.3	0 ^(b)	25-45	H ₃ BO ₃ ^(f)	0.002	None	MB ^(g)	N.A. ^(c)	N.A. ^(c)
4	N. S. Savannah	PWR	69	508	1750	304	304	SS ^(h)	(t)			0 ^(b)	20		0.0012	None	Yes	N.A. ^(c)	N.A. ^(c)
5	PM-1	PWR	463	1300	347 ⁽ⁱ⁾	347	Inconel	316				0 ^(b)							
6	Indian Point	PWR	500	1500	304 ^(j)	SS ^(h)	SS ^(h)	Zry, Hf	7 ± 0.5 ^(k)		<1 ^(k)	0 ^(b)	40 ^(k)	H ₃ BO ₃ ^(f)		5	C + MB ⁽¹⁾	N.A. ^(c)	N.A. ^(c)
7																			
8	BORAX IV	BWR	419	300	Al-Ni							32	(u)						
9	EBWR	BWR	62.5	489	Zry-2	304	S. C. ^(o)		7	1 ^(k)		25.5	(u)	H ₃ BO ₃ ^(e)	0.005 ⁽ⁿ⁾	Yes	M. B. ^(g)	2-5	none
10	Vallecitos	BWR	50,100	545	1000	304L	304	304L ^(p)	7.0 ± 0.2	<1	1-12	14 [*]	No			Yes	M. B. ^(g)		Duolite ^(q) S-10
11	SL-1 (ALPR)	BWR	17.5	420	415	AL-Ni	304		6.5-7		<2		(u)	Yes ^(e)		Yes	C + MB ⁽¹⁾	Yes	No
12	Dresden	BWR	27	546	1017	Zry-2	SS ^(h)	S. C. ^(o)	7.2 ± 0.2	2.0		21	No	H ₃ BO ₃ ^(e)		No	M. B. ^(g)	None	M. B. ^(g)
13	S4ER	BWR	Classi- fied	Classi- fied	Classi- fied	304	304	304	(d)		<0.005 ^(k)	2.9 ^(k)	25-60		.05 ^(k)	No	M. B. ^(g)	None	None
14																			
15	BORAX V	Super- heat	90 ^(r)	486 ^(r)	600	304	304		7 ^(k)					H ₃ BO ₃ ^(e)		Yes	M. B. ^(g)	Yes	M. B. ^(g)
16	BONUS	Super- heat	32.9 ^(r)	534 ^(r)	900	Zry-2	304	CS & SS ^(v)	7 ^(k)					Na ₂ B ₈ O ₁₃ ^(e) .4H ₂ O	0.002	5	M. B. ^(g)	5	M. B. ^(g)
17	Path- finder	Super- heat	87 ^(r)	489 ^(r)	615	Zry-2							No						

(a) Weight ratio of purification side stream flow-rate of primary coolant flowrate.
 (b) Net radiolytic rate in PWR's is zero.
 (c) Not applicable in PWR systems.
 (d) Consistent with LiOH and pH specifications.
 (e) Emergency shutdown.
 (f) Required for clean, cold shutdown.
 (g) Mixed bed of cation and anion resins.
 (h) Type of stainless not designated.
 (i) Modified, 0.05% Co Max, 0.15% cot Ta max.
 (j) 304 rods in Zry boxes
 (k) Design value.

(l) Cation plus mixed bed.
 (m) Standard construction.
 (n) Based on an estimate recirculation rate of 2×10^6 lbs/hr.
 (o) Standard construction.
 (p) Dual cycle steam generator.
 (q) Duolite S-10 used for O₂ adsorption.
 (r) Boiler section of core.
 (s) Use of hydrogen is inferred.
 (t) Neutral pH inferred
 (u) Effective on an experimental basis.
 (v) Carbon steel and stainless steel.

Conditions did not exist. Therefore, preliminary S4PR design pressures and temperatures were selected to provide about the same percentage of margin between operating, and design conditions as had been used in the design of other (operating) reactor plants. As detailed operational requirements are analyzed, some of the selected component design conditions, summarized in Table F-III may require revision.

c. Sampling Requirements

A preliminary sample system specification was prepared. The following areas were analyzed and described:

- 1) Sample points-location and number
- 2) Purpose and type of samples
- 3) Laboratory analyses required
- 4) Process analyses
- 5) System design and layout.

This investigation revealed that quality control requirements are extremely stringent for a very pure coolant. The following sampling points were selected:

- 1) Inlet and outlet of recombiner
- 2) Inlets and outlets of deionizer and filter
- 3) Steam condenser hotwell
- 4) Bottom of steam separator
- 5) Steam separator steam outlet.

The recombiner samples will allow a determination of the efficiency of the recombination process. The purification system samples are necessary to evaluate deionizer resin bed and filter performance. The hotwell and steam separator bottom samples will be useful for checking the possibility of crud settling out in these two locations.

Moisture carry-over in the steam to the condenser can be determined by sampling the steam separator steam outlet.

TABLE F-III

S4PR OPERATING AND DESIGN PRESSURES AND TEMPERATURES

Component	Operating		Design	
	Pressure (psia)	Temperature (° F)	Pressure (psia)	Temperature (° F)
Reactor	1245	570	1400	600
Steam Separator	1210	568	1400	600
Condenser				
Shell	25	240	100	330
Tube	50	180	150	330
Recombiner	23	500	1400	750
First Regen.				
Heat Exch.				
Shell	75	227	250	330
Tube	23	236	100	330
Second Regen.				
Heat Exch.				
Shell	70	243	250	330
Tube	1210	568	1400	600
Deionizer	120	130	250	330
Filter	100	130	250	330

Laboratory analysis of these samples will employ the following methods:

- 1) Colorimetric analysis for determining iron, nickel, chromium, and chloride in the low parts per billion range
- 2) Millipore filtering and "evaporating to dryness" for suspended solids
- 3) Diffusion of hydrogen through palladium or the Blacet-Leighton analysis can be used to measure dissolved hydrogen.

In addition, continuous process analysis is planned for gaseous hydrogen and oxygen concentrations as well as for process fluid electrical conductivity.

The sampling subsystem design will include the following features:

- 1) A single cabinet to localize all of the sampling taps
- 2) Valving of the sample lines to preclude "blowing down" the process system
- 3) Instrumentation will be provided to ensure safe and careful sample handling to avoid altering the composition of the stream during the sampling process.

d. Steam Flow Control

A brief investigation was undertaken to determine suitable methods for controlling the rate of steam flow between the steam separator and the condenser. All valve vendors contacted recommended a single flow control valve of essentially the same type, size, and material of construction. They indicated that throttling of high-pressure saturated steam is a common practice and no particular difficulties are encountered. Thermodynamically, the expansion through the flow control valve should result in a small degree of superheat on the downstream side of the valve. However, departure from the ideal throttling process is appreciable due to the valve configuration and to the high downstream velocity; hence, superheat in the steam to the condenser is highly unlikely.

The flow rangeability of many control valves is about 10 to 1. In order to get the particular flow-lift characteristic, and the flow rangeability desired, a smaller valve installed in parallel with the main flow control valve may be required. The need for this additional valve will be determined when the desired flow rangeability is specified.

e. Pump Arrangement and Maximum Flow Capability

On the basis of typical process design information, design-flow pressure drops for the recirculation and feedwater control valves were established at one-half of the associated system pressure drop.¹⁴ Based on this assumption, an analysis was made to determine the increase in the flow rate that could be achieved by opening the subject control valves fully. The maximum possible flow increase at a 10:1 recirculation ratio was 5%. Another calculation was made to determine the control valve design-flow pressure drop that was required to provide an assumed maximum flow of 120% of design when the subject valves were fully opened. The required pressure drops were at least twice as large as those specified initially based on 1/2 of the associated system pressure drop.

It is evident that a considerable increase in pumping power would be required to provide for the excess flow capability above design flow. It has always been recognized that speed control of the system pumps would allow the subject throttling valves to be deleted, thereby reducing the system resistance to $2/3$ that required by throttle control. Therefore, a study of various pump drivers was made to determine the best driver for the S4PR. This study and the results obtained are described in Appendix F, Section G.

f. Pipe Line Velocities

A survey was conducted to determine acceptable ranges of fluid velocity for process streams. Typical ranges for various industrial applications were found to be:

Pump Suction	4 to 7 ft/sec
Boiler Feed	8 to 15 ft/sec
Saturated Steam	100 to 200 ft/sec

In many cases in operating pressurized water reactor plants, liquid velocities exceed 30 ft/sec. Normally, in process piping, economic considerations dictate acceptable piping pressure drops and thus, the related velocities. However, other considerations such as erosion at high velocity and the settling out of particulate matter (crud), and fouling at low velocities must also be considered. The fluid velocity ranges indicated above were used initially to size the process piping for the S4PR. As details of the design have been resolved, pipeline velocities have approached the upper limit of the indicated range. Where practicable the pipe size was reduced to develop a more compact equipment arrangement.

g. Steam Separation

In an effort to develop an understanding of typical steam separator performance and of the physical relationships governing steam-water separation, a study of the available literature was made. The information assembled fell into two categories: (1) theoretical gravity and centrifugal separation analyses, and (2) test data for gravity, centrifugal, and impingement separation devices. The data from gravity separation tests were correlated to relate the effect of

steam surface release rate (lb/hr-ft^2) and height above the steam-release surface to the amount of liquid entrainment for various operating pressures from 600 psi to 1200 psi. Although there were discrepancies between the various references,^{6,7} a nominal limiting steam surface release rate was selected; the corresponding separator vessel diameter was calculated to be about 24 in. (The range in vessel diameter based on the range of steam surface release rates experienced during testing, extended from 20 to 59 in. in diameter.)

Types of mechanical moisture separators required to remove the liquid entrained in the steam were reviewed. The available test results for mechanical moisture removal devices were incomplete. Although mass flow rates and resultant outlet qualities could be related for the various test pressures, experimental values of inlet moisture level were not cited. Assuming identical inlet moisture conditions for all tests of the moisture removal devices, the wire mesh type of moisture separator exhibited the best performance when compared with centrifugal and impingement types.

Further study will be necessary to develop the compact steam separator required in the SNAP 4 system. Steam separator design for the S4PR, therefore, must be more thoroughly investigated before a specific configuration can be established.

h. Pump Flow Control

A very brief study of flow control of motor driven pumps was undertaken as part of the overall study to select pump, pump driver and flow control equipment characteristics. Direct throttle, bypass, and a combination of direct throttle and bypass methods were examined.¹⁵ Although the use of a direct throttle valve increases pumping requirements about 30%, it provides an acceptable range of control. The bypass flow control scheme yielded a limited range of control unless a pump with a special head-flow characteristic could be procured. At this time, it appears that a significant saving in weight, space, and pumping power requirements can be realized with the use of high-speed, turbine-driven pumps. A discussion of the advantages of this type of pump and driver is presented in Appendix F, Section G.

i. Thermal Insulation for Equipment and Piping

A study of the thermal insulation requirements for the S4PR was recently performed. The study was based on the following assumptions:

- 1) The ambient temperature outside the insulation is 115°F. (This temperature was selected based on the environmental requirements for process instrumentation. At 115°F steady-state, most instruments can function effectively.)
- 2) The maximum acceptable insulation surface temperature lies in the range of 130 to 160°F. (The selection of this temperature range is based on safety requirements for maintenance personnel.)

Process fluid temperatures were subdivided into two levels for which the insulation thickness and corresponding insulation surface temperatures were calculated. These results are given in Table F-IV.

TABLE F-IV
PROCESS FLUID TEMPERATURES

Approximate Fluid Temperature (°F)	Insulation Thickness (in.)	Maximum Surface Temperature of Insulation (°F)
570	2	160
570	3	145
230-270	1	140
230-270	2	130

Therefore, about 2 in. of insulation is recommended for fluid temperatures above 500°F, and 1 in. of insulation for temperatures below this level. There is also piping within the plant operating in the range of 130 to 160°F. It is not expected that insulation will be used on this piping. However, a protective coating may be desirable. The need for such a coating has not been firmly established at this time.

There are a number of types of insulation that could be used for the S4PR, e.g., molded asbestos, glass wool, etc. However, consideration of the abuse that the insulation may be subjected to during maintenance led to the selection of a molded insulation. In areas where molded insulation may be difficult to apply, insulation tapes are available for simple application.

Asbestos insulating materials are known to contain chlorides, which in the presence of oxygen, moisture, and piping or vessel stress might attack

ainless steel components.¹⁶ Hence, a survey of insulating materials was conducted to determine how this problem could be overcome. Claims for "Unibestos" insulation indicate that this material chemically binds the chlorides and prevents their leaching out of the insulation. Since the nature of chloride stress corrosion is not well understood, prudent design recommends that simple tests of potentially acceptable insulating materials be performed to avoid chloride leaching. As a further precaution, a protective waterproof barrier can be applied to the pipe or vessel to prevent the access of leached materials to the pipe surface. These methods will be used in the selection of a specific insulation for S4PR.

j. Valve Operating Subsystem

A study of acceptable valve operating and controlling modes was made to determine which mode would prove most advantageous for the S4PR. As indicated in the preceding discussion, various factors were compared to assist in the evaluation of the various modes. This comparison is presented in Table F-V. It shows that process water actuated, servo valve controlled main valves offer compactness, high rates of response, and inherent reliability, with a minimum need of developmental work. It is for these reasons that the hydraulically operated, servo controlled process valves have been selected for the S4PR.

TABLE F-V

VALVE OPERATING SYSTEM COMPARISON

	ELECTRICAL	HYDRAULIC	PNEUMATIC	ELECTROHYDRAULIC	ELECTROPNEUMATIC
COST	Highest cost for valve, motor, gearbox.	Lowest cost for valve (including piston operator).	Low valve cost (including diaphragm operator) but high accessory cost.	About the same cost as electrical.	Cost is somewhat higher than a pneumatic valve.
WEIGHT	Operating motor and gearbox give rise to a heavy valve assembly.	Lowest total valve and operator weight.	Valves are low weight, but accessory weight is high.	Somewhat lower weight than electric but still heavy due to motor, pump and auxiliary equipment.	Same as pneumatic
SIZE	Large size for valves 2 in. and above. Sizes for valves 2 in. and below nearly same as electrohydraulic.	Smallest valve and operator size due to compact operator.	Large valve sizes due to low pressure operation. Large accessory sizes also.	Large valve and operator sizes due to pump motor and auxiliary equipment requirements.	Same as pneumatic
PERFORMANCE	Slow unless motor horsepower is high with corresponding weight increase.	Fast, accurate positioning response.	Usually slower than hydraulic due to lower operating pressure with typical diaphragm sizes.	Same as hydraulic.	Faster response than pneumatic.
AVAILABILITY	Readily available.	Available — however, use of servo valve in water environment is still under investigation.	Most readily available.	Available	Readily available
ACCESSORY EQUIPMENT	Controller	Small hydraulic servo valve, backup pump or accumulator.	Air compressor dryer, accumulator tank, controller.	None	Same as pneumatic
STARTUP, SHUTDOWN REQUIREMENTS	Small auxiliary power requirements or manual through the gearbox.	Very small auxiliary power requirements for servo valve (<1 watt) or a manual hydraulic pump or prepressurized accumulator.	Largest power supply to run compressor, unless a prepressurized accumulator is available. Manual override is also possible.	Small auxiliary power requirements or a manual override.	Same as pneumatic
EMERGENCY POSITIONING (LOSS OF POWER)	Fail-as-is, unless clutch, springs or emergency power is available.	Fail open or fail closed positioning is readily achieved. Fail-as-is position is also possible.	Same as hydraulic	Same as hydraulic	Same as hydraulic
SEALING	Motor gearbox seal and process fluid seals required normally. Alternate water lubricated gear design would require a motor seal.	No seals required if integral cylinder operator is used.	A single process fluid seal is required on the valve stem.	No seals required on an integral cylinder valve, but seals required on pump.	Same as pneumatic
MISCELLANEOUS	Easiest to install since no connecting pipe or tubing runs.	Least experience information available.	Maximum experience but very cold site environment has caused freezing of water vapor in lines.	Due to motor, pump, small control valves, etc., probably the highest maintenance level compared to other modes.	Same as pneumatic

D. HEAT REJECTION SYSTEM

1. Performance Requirements

The final heat rejection system will provide the heat sink necessary to operate the S4PR system; the heat rejection requirements consist of:

- a) Condensation of the steam entering the condenser,
- b) Cooling of the process water in the nonregenerative heat exchanger to the temperature required at the demineralizer inlet, and
- c) Miscellaneous heat loads at such locations as nuclear instruments and auxiliary heat exchangers.

Items a) and b) are the largest heat loads. Not all of the heat loads mentioned in item c) are fully established, but all are expected to be small. In all probability, cooling water required for these minor duties will be obtained from the systems provided to handle items a) and b).

The condenser heat rejection system of the S4PR is designed to condense $\sim 41,000$ -lb/hr of steam at 25 psia and 240°F . The corresponding thermal duty is ~ 40 million Btu/hr. The nonregenerative heat exchanger is designed to cool $\sim 45,550$ lb/hr of condensate from 143 to 130°F ; the corresponding duty is $\sim 686,000$ Btu/hr. Since the operating temperatures in this heat exchanger are considerably lower than those of the condenser, it requires a separate coolant loop and heat exchanger.

The S4PR heat rejection system is designed for an ambient temperature of 100°F . Since the SNAP 4 power system can serve many applications, a preliminary design of the heat rejection system for an ambient of 35°F has also been developed. The various heat rejection system components will be designed so that a minimum number of different designs are required to suit the various SNAP 4 applications. For example, the airblast heat exchanger will be designed in a modular fashion so that additional modules can be added or deleted as the heat load or final heat rejection temperature increases or decreases.

The S4PR condenser will be designed on the basis of a normal inlet steam pressure of 25 psia. There will not be a turbine in the system. It is contemplated that for some applications of the SNAP 4 system, the turbine will exhaust into the steam condenser at 10 psia. Although operation of the S4PR

at this low pressure is not anticipated, the general design of the condenser will not be different for the lower pressure. The controlling operating condition for the condenser design pressure and temperature is probably the buildup in pressure when steam is bypassed around the turbine at full system power, coincident with a rapid closure of the main steam-flow control valve.

The design of the S4PR heat rejection system is based on a heat load of about 12 Mw. This heat load corresponds to a potential power production in a SNAP 4 system of about 2 Mwe. Other applications at lower capacity are anticipated. Partial load tests are to be conducted with the S4PR to evaluate the performance of the controls and equipment at other than the design loads.

2. Suitable Methods

To assist in the selection of a suitable heat rejection system for the S4PR, a study was made to examine the various methods that could be used. The heat rejection systems that have been considered are the following (Figures F-10 through -14).

7-2-63

7564-0328

Figure F-10. Closed Circuit, Intermediate Cooling Loop with Final Heat Rejection from an Air-Blast Heat Exchanger to the Air

The concept of Figure F-10 has many advantages and is well suited to a large number of SNAP 4 applications. The steam is condensed in the shell side of the surface condenser by the intermediate-loop fluid flowing in the condenser tubes. Depending on the application, the intermediate fluid could be water, ethylene glycol, or other fluids. If the application is for environmental temperatures above 32° F,

water will be used; if the ambient temperatures are lower, an ethylene glycol or methanol solution will be substituted. The fluid is pumped through the tubes of both the condenser and an air-cooled heat exchanger. A fan directs the ambient air across the finned surface of the heat exchanger tubes. This concept may be adopted for use in the S4PR system, if it is intended that the S4PR will serve as a prototype for a number of potential SNAP 4 applications.

7-2-63

7564-0326

Figure F-11. Direct Air-Cooled Condenser and Heat Exchanger

In the concept of Figure F-11, steam is condensed directly in an air-cooled heat exchanger, thus eliminating the entire intermediate loop. The advantages of reduced cost, weight, and fan power consumption are offset by the disadvantage of eliminating a second containment boundary for the primary fluid, and by being incapable of reliable operation under freezing ($<32^{\circ}\text{F}$) conditions.

7-2-63

7564-0327

Figure F-12. Closed Circuit, Intermediate Cooling Loop with Final Heat Rejection from a Cooling Tower to the Air

In the Figure F-12 concept, steam is condensed in the shell side of a surface condenser. The tube-side water is pumped through the condenser tubes and connecting pipes to a closed heat exchanger coil which is located within the cooling tower. Thus, the condenser cooling water never comes into contact with the cooling tower water which is sprayed over the cooling coil. The requirements for cooling tower makeup water, water treatment, and the large volume, and heavy weight of the cooling tower itself pose serious disadvantages for this system.

7-2-63

7564-0329

Figure F-13. Open Circuit, Intermediate Fluid, Cooling Tower Loop

In this concept (Figure F-13), steam is condensed in the shell side of the surface condenser and the tube-side, intermediate fluid in turn gives its heat up by direct contact with the ambient air in a cooling tower. This is probably the least expensive method but has the largest number of disadvantages. Among these are large water makeup requirements, no secondary containment of the primary fluid, and excessive fouling of the heat transfer surfaces.

7-2-63

7564-0330

Figure F-14. Intermediate Coolant, Natural Circulation, Heat Rejection Loop

In the concept of Figure F-14, steam is condensed either in the shell or tube side of the condenser and a fluid, such as Freon 12, is vaporized on the opposite side. The Freon is then condensed in a separate, natural circulation, air-cooled heat exchanger, and flows back to the steam condenser by gravity. This concept was not analyzed.

3. Control of Condenser Pressure

There are several ways to obtain temperature control of the intermediate-loop water which in turn controls the condenser temperature and pressure. These are:

- a) Control of the water flow by means of a throttle valve in the intermediate loop.
- b) Use of an automatic steam temperature controller to control the water bypass flow around the air-cooled heat exchanger.
- c) Use of an automatic steam temperature controller to control the air flow by operating dampers in the air ducts.
- d) Use of an automatic steam temperature controller to position a louver in the exit duct to recirculate a portion of the hot exhaust air.

- e) Control of the pitch of a variable pitch fan with a steam temperature controller.
- f) Use of multiple fans for step-wise control.
- g) Control of the air flow by using a steam temperature controller to change the speed of a variable-speed drive motor.
- h) Control of the air flow with a steam temperature controller by positioning a vane at the inlet to the fan.
- i) A combination of the above.

The selection of the best control scheme is dependent on several factors:

- a) Power consumption
- b) Degree of control required
- c) Initial cost
- d) Size and weight of system.

For the selected intermediate cooling loop, air heat rejection system, the available temperature difference between the ambient, and the condensing temperatures must be allocated between the condenser and air-cooled heat exchanger. A relationship between the available temperature difference, the required heat transfer surface, and power consumption requirement has been developed. The power requirement is the sum of that consumed by the intermediate loop coolant pump and by the air fan motors. The control scheme is an important consideration since it will also affect the power consumption during normal operation and to a large degree, during partial load operation. Fan speed control or the use of multiple fans leads to the lowest power consumption at partial load. Fans can also be purchased with vane controls which will result in a saving in power since the head requirement decreases at low loads. Figure 15 shows fan performance vs load for a fan motor with vane control. If first cost is the prime consideration, flow dampers usually are less expensive, but the fan power requirement is greater than that for speed control,

since the head requirement increases slightly at low loads. Figure 16 illustrates the use of dampers in controlling air flow rate.

Placing the condenser pressure (and temperature) control on the air-cooled heat exchanger requires consideration of the response time delays in the heat rejection system. If quick response becomes a necessity, a throttle control

Figure F-15. Fan Performance vs Load for Various Inlet Vane Settings 6531-5456

Figure F-16. Fan Performance vs Load for Various Damper Settings 6531-5457

valve in the intermediate loop can be utilized. This, of course, adds extra pressure drop during normal operation and increases power consumption.

A final control scheme has not been chosen for the S4PR. Tentatively, it is contemplated that multiple fans with damper control will be used. The anticipated power consumption is discussed in the following section.

4. Component Design Features

a. Condenser

(1) Thermal Design

The condenser is designed to condense 41,000 lb/hr of steam at 25 psia (240°F). The coolant is water flowing in a closed intermediate loop. A design temperature of 180°F into and 220°F out of the condenser has been specified. Based on this temperature rise, the log mean temperature difference is 36.4°F and the surface area requirement, on the basis of an overall heat transfer coefficient of 400, is 2730 ft². The overall heat transfer coefficient of 400 corresponds to a typical process industry surface condenser design. An overall fouling factor of 0.001 (°F-ft²-hr/Btu) is also included.

(2) Mechanical Design

The unit has been quoted by two vendors as a shell and tube unit using U-tube construction with the water flowing in the tubes. The required surface can be fit into a 42-in. OD shell, ~13-ft long. For a compact surface condenser design, the overall heat transfer coefficient is higher while the tube packing fraction and tube sizes result in a significantly reduced condenser volume. The final configuration would be dependent on module layouts. Tubes would be welded to the tube sheet. The unit is designed for 100-psig pressure and 330°F, providing adequate margin for the maximum plausible pressure surge. The unit would have been built to Section VIII of the ASME code and the applicable Nuclear Code cases.

(3) Materials of Construction

Consideration is being given to an all-aluminum unit. Various factors such as corrosion rate, erosion rate, and the joining of the aluminum unit to stainless-steel piping must be studied further before a decision can be made.^{17,18} Cost, weight, and volume are all less with aluminum construction than for a comparable unit of stainless steel, providing considerable incentive for further study.

b. Air-Cooled Heat Exchanger (for prototype simulation)

(1) Thermal Requirements

The air-cooled heat exchanger cools 995,000 lb/hr of water from 220 to 180°F to provide the heat sink for the condenser and subcooler, and cools 68,000 lb/hr of water from 130 to 120°F to provide the heat sink for the non-regenerative heat exchanger. For the S4PR, the design is based on an air temperature of 100°F. The air temperature rise in the air-cooled heat exchanger for the condenser heat load has been established at 80°F resulting in an air flow of 526,000 ft³/min. The air temperature rise for the nonregenerative heat exchanger load has been established at 20°F resulting in an air flow of 34,500 ft³/min. Based on a typical finned-tube heat exchanger design, it is estimated that 53,400 ft² of finned surface will be required to transfer the condenser load heat and 3860 ft² to transfer the nonregenerative load heat. A fouling factor of 0.001 (°F-ft²-hr/Btu) has been included.

(2) Mechanical Design

The actual configuration of this unit is still under study. Compactness, low weight, and low cost are given prime consideration. The air will be forced through the air-cooled heat exchanger by a fan driven by an electric motor. The fan selection is specified on a minimum overall efficiency of 75% as an objective, since a large power load is associated with this fan. The heat exchanger will be of aluminum construction to reduce weight and to provide low metal thermal resistance.

c. Power Consumption

Two separate cooling water loops will be provided, one serving the steam condenser and the other serving the nonregenerative heat exchanger. For the condenser loop, the 40°F temperature rise results in a flow of 260 gpm. Using a loop pressure drop of 25 psi, the power consumption is estimated at 30 kw. The 25 psi pressure drop is an estimate, and is subject to final design of the heat exchanger and loop. It is estimated that an 8-in. pipeline is required in the condenser loop. The fan power requirement was estimated on the basis of a pressure loss of 1.3 in. of water. Further analyses are required to determine the configuration of the airblast heat exchanger and the fan power

requirement. Depending on the installation details, ducting may be provided for the air entering and leaving the exchanger. This will impose an additional load on the fan. Based on a fan efficiency of 75%, and a fan motor efficiency of 90%, the power consumption is estimated at 104 kw. In the nonregenerative heat exchanger loop, the 10°F water temperature rise requires a flow of 140 gpm. Again, using a loop pressure drop of 25 psi, the power consumption is estimated at 2.7 kw. The fan power requirement (based on a pressure loss of 1.3 in. of water) is estimated at 10 kw. The total power consumed in this system is therefore, 146.7 kw.

E. WATER CHEMISTRY

The prime purpose for maintaining high water purity is to keep the fuel element fouling problem in hand. If this need is met, in all probability the other requirements for high-purity water, such as reducing the activity level of the coolant to minimize shielding will automatically be satisfied. The maximum allowable deposit on SNAP 4 elements [based on 12 Mw(th) operation] has been estimated to be 0.5 mil. To limit deposition to this amount, it has been estimated that the water purity should be approximately 10 ppb. Although water of this contaminant level is very high-purity water, it is an attainable goal as evidenced by the fact that other water-cooled reactors have achieved this level. Accordingly, the following specifications for SNAP 4 process water have been selected for design purposes.

Oxygen	0.4 ppm (max)
pH	7.0 ± 2
Crud level	10 ppb
Conductivity	10 megohm-cm
Additives	None

The following discussion of the major water chemistry factors is given in support of this specification.

1. Corrosion

Corrosion attack of the 300 series stainless steels is almost wholly centered on the iron component. The intergranular attack, stress and fatigue attack, differential aeration, galvanic, and crevice corrosion mechanisms all destroy by removing iron from the metal. Sensitization (the precipitation of chromium from the austenitic solution with the available carbon) by whatever cause removes the protective chromium and leaves the iron subject to attack. Thus, the principal corrosion mechanism primarily involves the dissolution of iron.

a. Hydrogen and the Mechanism of Corrosion

For iron in contact with water, the first event to occur is the formation of a very thin film of hydrogen on the metal. This film opposes further corrosive tendencies by isolating the iron from the solution. In order for corrosion to proceed, the hydrogen must be removed. This can be accomplished

in two ways:

- 1) Hydrogen combines with the oxygen present in the solution to form water, or
- 2) It may escape as bubbles of hydrogen gas.

In general the rate of destruction of this film is determined by the effective concentration of oxygen dissolved in the water next to the metal. The typical reactions are:

If both reactions (2) and (3) proceed, the iron will continue to be oxidized and thereby dissolved. The familiar "overpotential" for hydrogen (polarization by electrically neutral monatomic hydrogen) has been explained as the unwillingness of reactions (2) and (3) to proceed. The slowness of reaction (2) could possibly be due to the chain of reactions in the formation of water, i. e., $1/2 \text{H}_2 + 1/2 \text{O}_2 \longrightarrow \text{OH}^-$ and $\text{OH}^- + 1/2 \text{H}_2 \longrightarrow \text{H}_2\text{O}$. It has been noted that in acid solutions, reaction (3) is controlling as evidenced by the abundance of hydrogen gas evolution. Whereas in neutral and alkaline solutions, the amount of gas evolved is very small compared to the hydrogen oxidation reaction (2).

Hydroxyl ions present will combine with the ferrous ions. Oxygen may either oxidize the atomic hydrogen or oxidize the ferrous to ferric ion. In any case, the next film after hydrogen to appear will probably be an oxide or hydroxide of iron.

Generally, if a process involves two or more reactions, the rate is usually determined by the slowest reaction. Reaction (1) has been shown to be very fast by the increase in ferrous ion concentration having little or no effect on the rate of corrosion. Thus, attack is stifled by limiting completion of (2) and (3). Reaction (2) is best controlled by removing oxygen from the solution and thereby disallowing the depolarization of the beneficial hydrogen film. It is quite definite that the rate of diffusion of dissolved oxygen to the metallic surfaces determines the rate of corrosion by (2). Reaction (3) should be suppressed by the supply of radiolytic hydrogen gas in the system.

b. The Role of Oxygen

Oxygen in low concentrations exhibits both adverse and inhibiting action. When its function is for removing hydrogen from the system, it has been found¹⁹ that the corrosion rate is strongly influenced by the rate of depolarization at the cathodic area. This in turn is determined by the rate at which dissolved oxygen reaches the metal surface. In the mechanism describing the electrochemical action (without applied emf), the cathode is large and the anode quite small. For example, the oxide film on the metal becomes the cathode and the anode may be a small break in the oxide film exposing bare metal. Current is maintained by oxygen access to the cathodic surface where it reacts according to: $O + H_2O + 2 e \rightleftharpoons 2(OH^-)$ and by the oxidation of iron to the ferrous and ferric ionic states. Then, ferrous and ferric hydroxides are precipitated as ionic reactions occur in solution. Thus, electrochemical action ultimately brings Fe and O_2 into combination; it differs from direct oxidation in that iron goes into solution at one location, oxygen is taken up at a second, and the hydroxide or oxide is formed at a third location. This mechanism allows the solid corrosion product to be formed at a site remote from the point of attack and, therefore, cannot create a film to stifle the action. This makes the action much more dangerous than direct oxidation. This mechanism requires the presence of an electrolytic solution. This discussion emphasizes the need for water of a very high purity and the value of adequate quality control.

Argonne²⁰ found steady-state corrosion rate for the 300 series stainless steels to be on the order of $5 \text{ mg/dm}^2\text{-mo}$ at an oxygen concentration of $2 \text{ ml/l } H_2O$. A substantial difference in corrosive attack was not observed for water initially containing $2.5 \text{ ml } O_2/\text{l } H_2O$ and with 100 to $200 \text{ ml } H_2/\text{l } H_2O$ compared to water with the hydrogen only. It is believed that the hydrogen suppressed attack here.²⁰ Degassed water, containing 0.01 to $0.1 \text{ ml } O_2/\text{l } H_2O$, was found to be very corrosive. This was explained as possibly being due to the low concentration of oxygen being a source of hydroxyl ion. Thus it appears that too much oxygen or absence of oxygen are both deleterious while a low quantity of oxygen may actually inhibit corrosion. This is born out by boiling water reactor experience^{21,13} which showed that corrosion in the presence of water containing stoichiometric ratios of hydrogen and oxygen was less than anticipated.

c. Estimate of Corrosion Rate

Corrosion rates are well substantiated, from a design standpoint, for systems operating with 500 to 600°F water. No significant difference in corrosion rates were found in this 100 degree range.²² Considering only the effects of chemical reaction kinetics and water purity, there is indication that the corrosion rate should be considerably less in the cooler portions of the system, namely, the steam condenser.

KAPL²³ found fusion welding of Type 304 SS >1/8-in. thick produced some degree of sensitization. A complete anneal could be obtained by heating above 1800°F long enough to dissolve all carbides and then quenching rapidly. The dangerous region, that most prone to carbide precipitation, lies between 800 and 1500°F. Type 347 SS appeared to have much greater stability at the 900°F level. Specification of this type of stainless steel may be justified if stabilization seems necessary during welding. At any rate, an extra-low-carbon Type 304 SS is undoubtedly warranted. Any carbide precipitation will leave the material in a condition subject to intergranular corrosion.

In conclusion, a conservative corrosion rate for the S4PR would appear to be 10 mg/dm²/mo. This rate applied to all exposed surfaces corresponds to the loss of about 5 to 6 lb of metal per year, which would be virtually all iron. Assuming an intermediate oxidation state for the iron, about 8 to 10 lb of corrosion products would result. Soluble iron (ionic) would be on the order of 0.2 to 0.3% of the total iron corroded. There would be essentially no increase in the oxidation of iron by hydrogen ion. Since magnetite and ferric oxide are virtually insoluble, their dissolution would not be enhanced.

d. Aluminum

For some applications of SNAP 4, an aluminum condenser might be proposed because of the potential weight and cost savings. For 240°F service, the estimated corrosion rate of aluminum is <5 mg/dm²-mo. However, erosion has been shown to be significant^{24,25} unless special design precautions are taken. Also, since impurities are expected to be more of a problem when aluminum is used in preference to stainless steel alone, the increased burden placed upon the purification system needs more and careful evaluation.

2. Selection of pH Conditions

Neither high pH nor neutral pH conditions can be defended unequivocally. None of the boiling water reactors employes high pH conditions. On the other hand, Shippingport and the naval reactors have operated at a pH of 9.5 to 10.5 with extremely good success.^{26,27} There are data showing that corrosion is lessened slightly, filterability of crud is improved,²⁸ and the yield of crud is decreased,^{29,30} by operating at a high pH. The use of boric acid for poison control has precluded high pH conditions in the other pressurized water reactors. One would surmise that high pH has never been used in boiling water reactors because of the complicating factors it would entail. At neutral conditions, a less elaborate deionizing system would be required, employing a single bed of H-OH base resins in contrast to two beds, one of which would undoubtedly use Li-OH base resins. Furthermore, less flow control equipment would be needed and pH measuring devices could be eliminated. There are also several minor reasons for operating a neutral condition; these are unsubstantiated and somewhat speculative problem areas. First, there are intuitive arguments that any additive should be avoided in a boiling system. Since the mechanism of fouling in boiling water systems is not well understood, any additive could well enhance fouling rather than prevent it. Secondly, high pH conditions might increase the chances of localized caustic cracking.

For SNAP 4, neutral pH conditions have been selected for reasons of simplicity; the purification system is much less complex. However, it is admitted that neutral conditions may not be optimum, but are surely close enough to optimum that there is little question that they will prove satisfactory. This is substantiated by the fact that other BWR's successfully operate at neutral conditions. The S4PR purification system will be so equipped that both neutral and high pH conditions can be maintained; tests will confirm the more desirable coolant condition.

3. Coolant Radiolysis and Hydrogen Suppression

The data on the radiolytic decomposition of coolant from BORAX IV, EBWR, Vallecitos and Dresden have been studied to provide an estimate of the decomposition rate for SNAP 4.^{31,32,33,34} For the same conditions of temperature and pressure, the decomposition rate in the S4PR is expected to be 1/4 that of the other reactors, because of hydride moderation. (Approximately 1% rather

than 4% of the fission energy will be absorbed in the water.)³⁵ Also, the higher pressure of SNAP 4 (1200 psia) will further reduce the rate of decomposition. When all factors are considered, the estimated decomposition rate for the S4PR, when operating at 12 Mw(th), is about 5 lb of water per day. This quantity could be reduced to $\sim 1\text{-}1/2$ lb per day if about 60 cc of hydrogen per kilogram of condensate were added to the feedwater to suppress decomposition.

The advantages of adding hydrogen to the coolant are offset by the added complexity of the process system. It is necessary that the hydrogen be removed along with the other noncondensables from the condenser. It then must be reintroduced into the feedwater to minimize use of the gas. Furthermore, if there is leakage either into or out of the system, there is an added hazard to safety. Calculations were made to define more closely the explosion hazard if hydrogen addition of 60 cc/kg of condensate were employed.

Although the estimated inventory of hydrogen was only 1.05 ft^3 (STP) and could release ~ 305 Btu, insufficient to cause serious damage to the system, the possibility of an explosion should be precluded because of possible damage to the catalyst (the platinum could flake off), possible damage to pressure sensitive instruments, and the initiation of secondary effects. Also, from a psychological point of view, "popping" internally could be disconcerting. Therefore, it is considered essential to prevent even minor explosions. Consequently, for those applications of SNAP 4 where zero leakage cannot be assured, it appears wise to forego the advantages of hydrogen suppression. An exception would be the S4PR itself where, for experimental purposes, provision for hydrogen addition will be made to gather knowledge of the degree of suppression effected with specified quantities of hydrogen.

4. Catalytic Recombination

Recombination of the radiolytic gases in all applications of SNAP 4 will be accomplished by passing the stream of noncondensable gases from the condenser through a catalytic recombiner. For the case where hydrogen is employed for radiolytic suppression, an estimate was made of the recombiner size using the method of Garber and Peebles.³⁶ These calculations indicated that a platinum catalyst bed 12-in. long by 3 in. in diameter would be adequate. For the case without hydrogen addition, the diffusion controlling mechanism within the recombiner is difficult to predict because the hydrogen-oxygen ratio is

precisely the stoichiometric ratio and the circumstances do not lend themselves to reliable prediction. It is probable, however, that the bed size would not differ much from the 12-in. by 3-in. dimension of the aforementioned case. In any event, final verification of recombiner requirements can be obtained via a small-scale laboratory test.

A hazard frequently encountered in recombiner operation is the possibility of producing an explosive mixture of hydrogen and oxygen which can be ignited by the hot catalyst. By diluting to more than 90% steam (a natural consequence of operating the condenser at 25 psi), the mixture is far from explosive composition. Therefore, the recombiner and condenser system should be provided with some interlocking device which would allow the recombiner to receive the noncondensable gaseous mixture only if the condenser temperature is sufficiently high to ensure adequate dilution with steam. When such conditions are not being met, the gases could be allowed to accumulate in the condenser. Such conditions are envisioned to be of short duration, occurring most frequently during startup and shutdown operations.

5. The Potential Problem of Fouling

An extensive study was made of fuel element fouling in other water-cooled reactors to assess the severity of the problem as it might arise in the S4PR. It was concluded that taking the necessary action to keep the fouling problem in hand exerts the greatest demands for high water purity. In all probability, the other demands for water purity will be less stringent. The literature dealing with water chemistry is not sufficiently quantitative to allow a firm estimate of the degree of fouling in the SNAP 4 reactor; however, it is possible to establish tentative maximum permissible limits on impurity concentration and to assess the probability of achieving such purity levels.

The fuel element thermal design for the S4PR core allows a scale heat transfer coefficient of $10,000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$. With an average thermal flux of $430,000 \text{ Btu/hr-ft}^2$, the temperature drop through the deposited scale would be 43°F . Considering the thermal conductivity of the scale to be $0.39 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$,³⁷ an average scale thickness of about 0.5 mils is allowed. Based on a density of 2.6 gm/cm^3 ,³⁷ the weight of scale can be calculated to be $\sim 0.6 \text{ lb/yr}$.

A perspective of the conditions that must be imposed upon the S4PR to limit deposition to 0.6 lb/yr can be gained by considering deposition to be a function of the crud concentration of the water entering the core, the amount of water that will pass through the core in a year's time, and the fraction of ions, colloids, or particles depositing during a single pass through the core. For a steady-state recirculation rate of 410,000 lb/hr, 3.5×10^9 lb of water will pass through the core in one year's time. Accordingly, the limiting concentration and depositing fraction may be expressed as:

$$cf = \frac{0.6}{3.5 \times 10^9} = 1.7 \times 10^{-10}$$

where

c = crud concentration in lb/lb of water

f = fraction of crud that deposits in a single pass through the core.

To evaluate the prospects of the S4PR operation being within the above limit, it is necessary to make estimates of $\underline{c}$ and $\underline{f}$; this can be done by referring to the data of loop experiments and reactor experience. In some Van de Graaff irradiation experiments at Bettis,^{38,39,40,41} the fraction of crud deposited in a single pass, on Zircaloy specimens, averaged 0.177 for conditions of 4.4 ft/sec, and 450°F. The deposition on the stainless-steel "window" in the Yankee development experiment was 0.005 under conditions of 8 ft/sec, 600°F, and 1 ppm O_2 .²⁸ The wide variation makes it extremely difficult to predict the deposition fraction in S4PR. An estimate of 0.01 to 0.05 appears reasonable based upon the similarities and differences of S4PR conditions as compared to the two Van de Graaff experiments.

In a similar fashion, an estimate of crud concentration in S4PR can be made. The concentration can be calculated from the corrosion product release rate and the effectiveness of the purification system for removing crud. The real effectiveness is dependent upon how strongly the core acts in competition with the purification system as a collector of impurities, or in other words $\underline{f}$. Since our estimate of $\underline{f}$ is rather tenuous, it would be better to estimate $\underline{c}$ independently of $\underline{f}$.

One method of estimating crud concentration independently of $\underline{f}$ is to use data from operating water-cooled reactors. In Shippingport, the average crud concentration was 3 ppb, with occasional crud bursts raising it to 60 ppb.²⁶ Data from EBWR operations are very skimpy, but a value of 60 ppb has been reported.³¹ The S4PR should be very much cleaner than the EBWR and will be operating at a lower oxygen concentration; therefore, the S4PR should approach Shippingport's conditions of purity. An estimate of 5 to 10 ppb seems appropriate.

From the estimates of concentration and fraction deposited, the product of $\underline{c}$ and $\underline{f}$ is of the range of 0.5×10^{-10} to 5×10^{-10} , which correspond to film thicknesses of 0.15 and 1.5 mils, respectively. Adding support to the contention that fouling will not be a limitation in S4PR are the data on thicknesses observed in the Shippingport reactor, namely, 0.22 mil.⁴² Thus, it appears that crud deposition can be kept within tolerable limits; however, it remains an unproven matter until sufficient evidence is accumulated, either from continued study of experimental testing or ultimately from results of S4PR operation.

6. Water Purification System

The SNAP 4 water purification subsystem has been given much study, and, as a result, several significant changes and improvements are now embodied in the design. Much that is pertinent to the design of the S4PR purification subsystem can be borrowed from pressurized water, and boiling water reactor practice. Consequently, data from these systems plus data from experimental water loop operations have been studied thoroughly to abstract that which is pertinent to SNAP 4. The real problem in water-cooled reactor purification systems is to purify high-purity water, making it more pure. For SNAP 4, the goal is to maintain water purity at the same level as has been achieved at Shippingport, namely, ≤ 10 ppb.³⁰ From a survey of the literature, this value is better than that attained in boiling water experience, but with appropriate choice of materials and proper design of the SNAP 4 system such a purity level should be achievable.

For the SNAP 4 system, two major choices regarding size and location of the purification system exist. First, purification can be restricted to a reactor coolant side stream loop. If this system were chosen, it would have operated at higher pressure and would be required to process a stream of

~5% of the core feedwater rate. (Based on a corrosion rate of $10 \text{ mg/dm}^2\text{-mo}$ and a steady-state water impurity concentration of 10 ppb, the purification rate should be the aforementioned value of 5%.) The alternative choice is the location of the purification system in the condensate return line so that the condensate flow is purified. In addition, a smaller stream, currently taken as 0.5% of core feedwater rate, and drawn from the recirculation loop, would have to be combined into the purification system feed stream to take care of reactor water cleanup. This system has the advantage of operating at low pressure, and in addition isolates the major source of impurities (preventing them from reaching the core), specifically, erosion and corrosion products from the turbine and the condenser. The added incentive of approaching current boiling water reactor practice is also met (present-day practice calls for full flow condensate "polishing"). However, for SNAP 4, where relatively high condensing pressures are employed (10 to 25 psia), the corresponding high condensate temperature requires a regenerative heat exchange loop in the condensate stream. Obviously, the added complexity plus wasted heat have to be balanced against the advantages of condensate polishing. Thorough evaluation of this point was not yet complete, but it would appear that the advantages of total condensate treatment are worth the additional complexity and heat loss.

Water-cooled reactor technology has, without question, proved that mixed bed deionization is a highly satisfactory method of purification. Study of the performance of these systems shows that a mixed bed deionizer should perform adequately for SNAP 4. It appears that a filter installed before the resin bed would not materially reduce the quantity of impurities because the impurity level would be so low that it would be difficult to effect a further significant improvement. Furthermore, small particle removal is usually attendant with high pressure drop across the filtering medium. However, for the S4PR, provisions are to be made for inclusion of a prefilter so that in the event early operations warrant it, the effects of filtration can be tested and studied. A guard filter downstream (or built into the deionizer assembly) is definitely warranted to preclude contamination of the system with resin fines.

The mixed bed deionizer will be operated at the high flow rate of 50 gal/min-ft^2 as has been developed in present-day condensate polishing systems. Data from the literature indicate that such units can routinely produce effluent having 10 ppb impurity level.⁴³ The point that needs to be proven for

SNAP 4 is that these systems can sustain 12 months of on-stream operation without backwashing or regeneration. Current on-stream times, for condensate polishing systems associated with reactors which are composed of mild and stainless-steel components and use zircaloy cladding, are of the order of a few weeks. However, insurmountable reasons are not foreseen to preclude development of long-term on-stream systems.

Calculations were made to establish the size and exchange capacity of the deionizer. Bases for the estimate were: (a) one year's operation at the estimated corrosion rate of $10 \text{ mg/dm}^2\text{-mo}$; and (b) the total amount of corrosion products would be released into the coolant, and would be completely soluble (ionized). Of course, the low solubility of iron oxide and hydroxide in the high-temperature water causes these two assumptions to be considerably conservative. Nevertheless, it seems unwise to estimate a percentage of soluble matter from the meager data available. The consequences of reducing the resin bed size and thus permitting the unrestricted circulation of ionic and colloidal material may be very serious. Solubilities may be quite unlike those predicted in ordinary chemical systems due to the influence of the ionizing radiation. Application of the above-mentioned average corrosion rate to the entire S4PR surface area results in 5.8 lb of dissolved metal being introduced into the system in one year. This amount of metal is equal to 94.1 g equivalent weights of ferrous ion, requiring a resin volume of $\sim 6 \text{ ft}^3$ based upon the resin manufacturer's recommendation of equivalents per unit volume of column for the specific resin.^{44,45} Internal resin bed dimensions will be $\sim 20\text{-in.}$ -diameter by 3-ft-long. The mixed bed deionizer will utilize cation and anion resins in a one-to-one equivalent ratio. This ratio was selected based on universal practice. Although the capacity is based on the requirement for cation removal, a check shows the flow loading to be within the acceptable limit of 50 gal/min-ft^2 .

As has already been mentioned, plans call for operation at neutral pH. An H-OH based, mixed resin system will automatically maintain this condition. If high pH tests are called for, it will be necessary only to replace the resins with Li-OH based resins.

F. WASTE GAS HANDLING

1. In-Leakage of Air

Some preliminary study has been given to the air in-leakage problem. The advantages of a hermetically sealed system immediately come to the fore and are very pronounced. The complexity and size of any waste gas system is directly proportioned to the quantity of gas in-leakage. Furthermore, in-leakage precludes the use of hydrogen to suppress radiolytic decomposition since it creates an explosive hazard. Analyses, although only preliminary in this regard, indicate a truly unattended compact plant requires zero leakage, or, in other words, hermetic-sealing.

The introduction of air into the process system during shutdown and means for its expulsion during startup have also been studied. A possible solution consists of maintaining the process water just at its normal boiling temperature so that internal pressure will be essentially atmospheric; such a condition will eliminate in-leakage because no pressure gradient would exist. A better solution would be a leak-tight system where no in-leakage would result under any condition. In the final analysis, the complexity of the waste gas system depends upon the sophistication required of the particular SNAP 4 application under consideration and to what degree leakage will be minimized.

2. Gaseous Fission Product Activity

An estimate was made of the gaseous fission products that could be released to the atmosphere in the event of a fuel cladding rupture. For this estimate, the following conditions were assumed.

- a) One fuel element ruptures at the end of one year of steady-state operation at 12 Mwt.
- b) The rupture is assumed to be a slit in the cladding 4 in. long by 1 mil wide. (Slightly more than 0.8 mil is great enough to relieve all radial stress.)
- c) The only portion of the fuel affected is considered to be the exposed hydride to a depth of 0.3 mil. (Evaluation of bare hydride corrosion tests indicate that corrosion penetration was not greater than 0.3 mil. Furthermore, the oxide film that formed was stable and was highly impervious to hydrogen release; it therefore should be equally impervious to krypton, xenon, and iodine release.)

- d) The gases would be released to the atmosphere after a 24-hr decay time and then gradually over the next 24-hr period
- e) After a cladding failure, the reactor would be shut down so that subsequent release would be negligible.

For the case where 400,000 cfm of dilution air were available (the fan-driven air of the heat rejection system) the effluent gaseous activity would be just under 10^{-10} $\mu\text{c/cc}$, the maximum permissible concentration by literal interpretation of the National Bureau of Standards Handbook 69.⁴⁶ For an Antarctic location, where a limit of 4×10^{-14} $\mu\text{c/cc}$ has been imposed, tolerance would be greatly exceeded. Consequently for the latter location, the merits of an essentially leak-tight system restricting the gas to a relatively small volume practical for storage, is immediately apparent.

G. COMPACT, HIGH-SPEED TURBO-PUMPS

Various system parameters and component characteristics have been considered in the selection of pumps and pump drivers for the S4PR. As a result of this study, summarized below, high speed, hermetically sealed, turbine-driven pumps are recommended for the S4PR.

1 Shaft Seal Leakage

Several methods of leakage control were considered; the most effective of these requires the use of face seals. As a result of this study, it was found^{47,50} that face seals

- a) Require some inspection and maintenance during operation
- b) Are erratic in their behavior even under ideal test conditions, and on test the leakage may be greater than claimed by the manufacturer
- c) Require careful handling and assembly
- d) Cannot be shipped fully assembled in the pump
- e) When failed, are equivalent to a water line break
- f) Operate at a speed limited to 3600 rpm or less, to attain reasonable life

Since the study could not uncover any extenuating circumstances that would mitigate these disadvantages for mechanical seals, a hermetically-sealed pump system was selected for S4PR. The higher capital cost of the hermetically sealed, canned motor pumps is offset by lower maintenance costs, as demonstrated by the operating history of the two types of pumps and the elimination of auxiliary equipment such as seal cooling water heat exchangers. In addition, if mechanically sealed pumps were used, the flow rate of the high-pressure feed pump must be increased to provide cooling water for the face seals.

2. Pump Drivers

Various types of pump drivers were considered in the study. Of these, only canned AC motors operating at 60 cps and direct-drive turbines proved consistent with the selection of a hermetically sealed pumping system. Sixty-cycle canned-motor pumps are commercially available. Direct-drive turbopumps, while not commercially available, can be designed and fabricated within the current state of technology.^{48,49}

3. Flow Control

Three methods of pump flow control were investigated in this study. These include bypass control, throttle control, and speed control. It was found that bypass control was best adapted to fixed speed axial flow pumps, while throttle control was best adapted to fixed speed radial flow pumps. However, speed control, where possible, yielded the lowest pump power requirement both at the design and off-design operating conditions. These results are summarized below:

- a) The pump flow, using bypass control, with one valve in the process stream and another in the bypass stream is equal to the design flow flow plus overload flow for all conditions of operation. Therefore, the pump load remains constant for all operating conditions. This is true whether the primary resistance in the pump circuit is frictional resistance or is due to the process head requirements.
- b) Throttle control of a pump circuit consisting mainly of frictional resistance such as the recirculation loop, requires the valve to consume from 30 to 50% of the total circuit pressure drop to achieve a minimum meaningful range of control. Therefore, at the design conditions, throttle control of such a system will add 30 to 50% of

the total pump power.¹⁴ At an off-design condition, say 20% of design flow, the pump power is still about 60% of the design value. While the percentage increase in additional power at design condition is somewhat less for a pump for which the head is dictated by process pressure conditions, as in the high-pressure feedpump, the off-design power ratio remains as above.

- c) In the recirculation loop, consisting mainly of frictional resistance, the pump and circuit characteristics are matched. If pump speed is varied, no additional valve resistance is required for control. As a first approximation the power ratio for two operating points, in such a system, varies as the cube of the flow ratio. The power required at 20% of design flow is approximately 1% of the design value. The pump under these conditions operates along a single specific speed line, and to a first approximation along a constant efficiency line. The study also indicated that speed control is possible for the feedwater pump circuit, where process pressure levels dictate the primary head increase in the pump. The feedwater pump under these conditions would operate at a variable specific speed and variable efficiency. The power required at 20% flow is about 30% of the design value as compared to 60% of the design value for throttle control and 100% of design value for bypass control.

It was also found during the study that, while speed control for canned-motor pumps is feasible with the addition of auxiliary equipment, the additional complexity, cost, and loss of reliability are incompatible with the objectives of the S4PR. Turbo-pump speed must be controlled by a turbine governing device even if throttle or bypass flow control is used on the pumps. Therefore, speed control of the turbo-pumps should be the only method of control to be given serious consideration.

4. Cycle Analysis

Since the ultimate goal of the S4PR experiment is to prove the feasibility of a reactor system that is to serve as the basis of a compact nuclear power plant, several turbo-pump cycles have been investigated and compared to a 60-cycle canned-motor pump power plant cycle. Figure F-17 presents the cycle efficiency and reactor power as a function of feedpump turbine exhaust pressure

Figure F-17. Cycle Efficiency vs Feedpump Turbine Exhaust Pressure

for various constant feedpump turbine inlet pressures, while Figure F-18 presents the reactor inlet temperature and the reactor flow rate for these conditions. All of these arrangements include regenerative feedwater heating wherever possible. Improvement in cycle efficiency can be reflected in terms of fuel cycle cost reductions and the associated permissible increase in capital cost of the plant. These data are presented in Figure F-19, for the maximum efficiency envelope in Figure F-20.

A series of nonregenerative systems were also considered. In all of these cases the reactor inlet temperature is constant and equal to the value for the canned-motor case. Figure F-20 presents the efficiency, reactor power, and flow rate for these cases as a function of recirculation pump turbine inlet pressure for constant values of feedpump turbine inlet pressure. Figure F-21 presents the economic data for these cases.

It was found during the study that by the proper selection of pump design parameters, that identical turbo-pump units, with minor modifications, could be used for both 1- and 2-Mwe power plants. However, the same could not be said of 60-cycle canned-motor pumps. Furthermore, startup, independent of a source of auxiliary power, may be feasible with a turbo-pump system but not with a 60-cycle canned-motor pump system.

Figure F-18. Reactor Coolant Flow and Inlet Temperature vs Feedpump Turbine Exhaust Pressure

5-31-63
 Figure F-19. Cost Savings Due to Increased Cycle Efficiency
 7564-0246

5-31-63

RECIRCULATION PUMP TURBINE INLET PRESSURE (psia)

7564-0247 a

Figure F-20. Efficiency, Reactor Power and Flow vs Recirculation Pump Turbine Inlet Pressure for Various F.W. Pump Turbine Inlet Pressures (exhaust press = 10 psia for both turbines)
F.W.P.T.I.P. = F.W. Pump Turbine Inlet Pressure

5-31-63

7564-0248

Figure F-21. Cost Comparisons for Nonregenerative Turbopump Systems (10 psia exhaust on all turbines) Based on Ref. Canned Pump System

APPENDIX F REFERENCES

1. NAA-SR-3470, "Nuclear Fission Reactors as Compact Energy Sources," (January 12, 1959) Secret Restricted Data
2. NAA-SR-5358, "Selection of a Nuclear Power Plant Concept for Underwater Applications," (September 1, 1960) Secret Restricted Data
3. AI-3471, "Feasibility Study for Compact Mobile Nuclear Power Plants," (February 15, 1959) Secret Restricted Data
4. NAA-SR-5405, "Preliminary Design of a Compact Nuclear Power Plant for Underwater Application," (July 15, 1960) Secret Restricted Data
5. NAA-SR-MEMO-7307, "SNAP 4 Experimental Reactor," (May 7, 1962) Secret Restricted Data
6. J. F. Wilson and R. J. Grenda, ACNP-6105, "Removal of Entrained Moisture from Steam Using Natural Separation and Mechanical Dryers," Allis-Chalmers, (April 3, 1961)
7. CEND-127, "Interim Report - Development of Steam Separation Devices," Combustion Engineering, (April 1961)
8. C. B. Zitek "Highlights of Operating Experience During Past Year (1962) at Dresden Nuclear Power Station," (Paper presented at 25th Annual Meeting of the American Power Conference, March 26-28, 1963)
9. C. R. Breden, ANL-6562, "Boiling Water Reactor Technology, Status of the Art Reporter," Volume II, Water Chemistry and Corrosion, Argonne National Laboratory, (February 1963)
10. D. E. Collins, "Measurement of Primary Water Flow in a Nuclear Power Plant," Nuclear Engineering and Science Conference, (March 17-21, 1958)
11. "The Shippingport Pressurized Water Reactor," Addison-Wesley Publishing Co., Inc., 1958
12. "Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components," (Pressurized Water Cooled Systems), PB 151987, (December 1, 1958)
13. "ASTM Standards and Recommended Practices for Metallic Components of Water-Cooled Nuclear Power Reactors," Preprint Paper No. 96, 1962 Nuclear Congress, New York, (June 4-7, 1962)
14. D. M. Considine, Process Instruments and Controls Handbook, McGraw-Hill Book Co., Inc., 1957
15. "Pump Selection and Application," Hicks, McGraw-Hill Book Co. Inc., 1957

6. ASME Publication, "Testing and Initial Operation of the N. S. Savannah," Paper No. 62-WA-310
17. W. A. Pollock, ASME Publication, Paper No. 62-WA-303, "Seven Years of Experience with Aluminum Condenser and Heat Exchanger Tubes"
18. J. M. Decker, ASME Publication, Paper No. 62-WA-312, "Experience with Aluminum Condenser Tubes at St. Clair Power Plant"
19. F. W. Speller, Corrosion, Causes and Prevention, (3rd ed., New York, McGraw-Hill Book Co., 1951)
20. C. Wohlberg and F. W. Kleimola, "Factors Which Affect Formation and Deposition of Transport Corrosion Products in High Temperature Recirculating Water Loops," ANL-5195 (Rev.) (December 1953)
21. C. R. Breden, "Boiling Water Reactor Technology, Status of the Art Report", Vol. II, Water Chemistry and Corrosion, ANL-6562, (February 1963), pp 59, 69
22. "Nuclear Metallurgy," New York, American Institute of Mining and Metallurgical Engineers, 1956
23. W. D. Valovage and E. E. Baldwin, "Materials Manual," KAPL-A-ME-1 (January 1, 1954)
24. J. M. Decker, "Experience with Aluminum Condenser Tubes at St. Clair Power Plant," ASME Paper No. 62-WA-312 Winter Annual Meeting, ASME, New York (November 25-30, 1962)
25. A. Bayard Sission, "Central Station Uses of Aluminum Alloys for Demineralized Water Storage and Condenser Tubes," ASME Paper No. 62-WA-304, Winter Annual Meeting, ASME, New York (November 25-30, 1962)
26. C. M. Dunn and J. A. Martucci, "Operational Chemistry Summary of the Shippingport Reactor Coolant," December 1957 to August 1958, "Bettis Technical Review, One Year of Operational Experience at Shippingport," WAPD-BT-12 (April 1959)
27. I. H. Wilinsky, Paul Cohen and J. H. Seamon, "Coolant Technology of a Pressurized Water Nuclear Plant," Chem. Engr. Prog., 52, pp 388-393 (September 1956)
28. C. C. Thomas and H. W. Lacock, "Corrosion Product Transport and Deposition Under Ionizing Radiation," YAE-93 (December 1958)
29. T. Rockwell, III and P. Cohen, "Pressurized Water Reactor (PWR) Water Chemistry," Proc. Intern. Conf. Peaceful Uses of Atomic Energy, Geneva, 1956, 9, p 423, United Nations, New York (1956)
30. P. Cohen, "Shippingport Primary Coolant Technology," Bettis Technical Review, One Year of Operating Experience at Shippingport, WAPD-BT-12 (April 1959), pp 93-94

31. C. R. Breden, I. Charak, and R. H. Leyse, "Water Chemistry and Fuel Element Scale in EBWR," ANL-6136 (November 1960)
32. V. M. Kolba, "EBWR Test Reports," ANL-6229 (November 1960)
33. F. J. Brutschy, H. S. Dreyer, and W. L. Pearl, "Vallecitos Boiling Reactor Coolant Technology," Ind. and Eng. Chem., 51, p 1262, (1959)
34. C. P. Gavin, et al, "Water Decomposition in a Direct-Cycle Boiling Water Reactor," Ind. and Eng. Chem., 51, p 1265 (1959)
35. G. K. Whitham and R. R. Smith, "Water Chemistry in a Direct Cycle Boiling Reactor," Proceeding of the Second International Conference on the Peaceful Uses of Atomic Energy, Geneva, September 1959, Vol. 7, p 436, (New York, United Nations, 1959)
36. H. J. Garber and F. N. Peebles, "The Combination of Hydrogen and Oxygen in Platinum Catalyzed Flow Reactors," ORNL-1796, (October 26, 1954)
37. R. F. S. Robertson, "Chalk River Experience in Crud Deposition Problems," AECL-1328, (August 15, 1961) pp 11-15
38. G. P. Simon, B. G. Schultz and Y. Solomon, "Van de Graaff Study: Deposition of Corrosion Products Under Irradiation," Bettis Technical Review, "Reactor Chemistry and Plant Materials," WAPD-BT-3 (August 1957) pp 39-52
39. G. P. Simon, B. G. Schultz, and Y. Solomon, "Van de Graaff Study: Second Interim Report on the Deposition of Corrosion Products under Irradiation," Bettis Technical Review, "Reactor Chemistry and Plant Materials," WAPD-BT-7 (March 1958), pp 43-51
40. G. P. Simon, "Van de Graaff Study: Third Interim Report on the Deposition of Corrosion Products Under Irradiation," Bettis Technical Review, "Reactor Chemistry and Plant Materials," WAPD-BT-11 (December 1958), pp 18-28
41. G. P. Simon, "Van de Graaff Study: Fourth Interim Report on the Deposition of Corrosion Products," Bettis Technical Review, "Reactor Chemistry and Plant Materials," WAPD-BT-16 (December 1959), pp 134-146
42. R. D. Oldenkamp, C. F. Paulson and Y. Solomon, "Evaluation of Crud Deposits on PWR Blanket Bundles," WAPD-TM-261 (January 1961)
43. A. B. Sisson, R. C. Reid and H. W. Frazer, "Operating Experience with High Flow Rate Condensate Polishing Equipment at Dresden," Proceedings of the American Power Conference, Vol. 23, 600-610 (23rd Annual Meeting, Chicago, March 21, 22, 23, 1961) (Chicago, Illinois Institute of Technology 1961)
44. Robert Kunin, "Ion Exchange Resins," (Ind. ed., New York, John Wiley and Sons, 1959) p 89
45. Rohn & Haas Co., Nuclear Grade Amberlite Ion Exchange Resins - Technical Notes, (Philadelphia, Pa.) (1958)

46. "Maximum Permissible Body Burdens and Maximum Permissible Concentration of Radionuclides in Air and in Water for Occupational Exposure," National Bureau of Standards Handbook 69, (June 5, 1959)
47. DP-660, "Leakage of Water from Pump Mechanical Seals," E. I. du Pont de Nemours and Co., (November 1961)
48. A. D. Newman, "Water Lubricated Bearings for Marine Use," Permatrada Research Center, England
49. W. J. Vitellozzi, E. E. S. Report C-3229C, U.S. Naval Engr. Exp. Sta., "Report of an Investigation on Water and Steam Lubricated Bearings," (November 1949)
50. P. A. Scott, HW-76646, "PRTR Mechanical Seal Pump Operating Experience," (March 1963)

BLANK

APPENDIX G. PLANT CONTROL SYSTEM AND INSTRUMENTATION

The objectives applied in the design of the control system for the SNAP 4 power plant are compactness, light weight, long life, high reliability, and automatic or semiautomatic operation. Complexity of control components and subsystems is held to a level which is dictated only by the safety and operational needs of the plant. This control simplicity lends itself advantageously to programmed plant startup and shutdown and eventually to a fully automatic, self-diagnostic control system. Successful application of this control philosophy to the design of the system requires detailed consideration of the process constraints imposed on the system by plant performance requirements.

A. CONTROL SYSTEM FUNCTIONAL DESCRIPTION

The specific design objectives for S4PR plant control system are:

- 1) Automatic operation and load following capability in the normal power range
- 2) Semiautomatic operational capability at all power levels
- 3) Maintenance of system temperatures, pressures, flows, and neutron flux levels within design and safety limits at all times.

The S4PR control system requires attendance during operation. Startup and shutdown operations will be manual. Other manual operations will be: setting the plant output power level, diagnosing system behavior, and correcting operational irregularities. Automatic control is provided for normal operation about a desired power level. Scram and setback system functions are automatically controlled and have manual override capability.

The plant control system consists of a number of subsystems or single control loops, some of which are mutually coupled. Each loop operates by comparing a reference signal to a signal which is derived from the controlled variable. The difference between the actual and reference values of the variable generates an error signal which actuates a controller to reduce the error to zero. In some cases, in addition to proportional control, it is desirable to have an appropriate combination of rate and integral control. The rate of change of the error can be employed to enhance stability, while inclusion of the error

the-integral causes the steady-state error to be zero.

5-1-63

7564-0187A

Figure G-1. Schematic Diagram of S4PR Control System

The individual control subsystems are described in the following and are schematically illustrated in Figure G-1.

1. Neutron Flux Control

The neutron flux control circuit adjusts the reactor flux level, and hence reactor power, to match the plant load demand which for the S4PR is the steam flow rate. The power plant output is compared to the flux power level and the resultant error signal causes the control drum actuators to drive the drums in a direction to cancel the error. The neutron flux control functions in conjunction with the pressure control as described in the following section.

2. Pressure Control

The pressure control loop compares the pressure level within the steam separator to the design pressure level and the resultant error signal causes the control drum actuators to position the drums to cancel the error. The pressure control loop provides control over fast transients and functions in conjunction with the neutron flux control system to adjust for long-term reactor power level variations. Both of these loops utilize the same signal comparator circuitry after appropriate signal conditioning. The combined error signal from these two loops is filtered through a "dead band" (i.e., only signals large enough to lie outside a predetermined range are utilized) to permit maximum use of the inherent load following capability of the reactor and to eliminate a heavy-duty cycle on the drum drive actuators. Rapid high-pressure transients are relieved by dumping steam directly to the condenser.

3. Recirculation Flow Control

The recirculation flow control loop compares the main steam flow with the recirculation flow rate. Any deviation from a 10:1 ratio of recirculation mass flow to steam mass flow causes a change in the position of the throttle valve on the drive turbine of the recirculation pump, thus changing the speed of the pump and adjusting the flow ratio to the desired value. By maintaining a constant recirculation ratio, the void fraction within the reactor core is automatically fixed as long as the coolant inlet temperature is held constant.

4. Feedwater Flow Control

The feedwater flow control loop compares the feedwater flow with the steam flow from the steam separator. Resultant error signals modify the

position of the steam inlet valve to the drive turbine of the feedwater-condensate pump. This changes the speed of the pump and adjusts the feedwater rate. Feedwater flow control is utilized to assure essentially constant coolant inlet temperature to the reactor. The feedwater control functions in conjunction with steam separator liquid level control described in the next section.

5. Steam Separator Liquid Level Control

Accumulated (time integrated) errors, between the rates at which water is removed from the recirculation loop as vapor and returned as feedwater, are regulated by the steam separator liquid level controller. This circuit compares the actual liquid level within the steam separator to the desired level; the resultant error signal is used to trim the feedwater flow rate to maintain the required recirculation process loop inventory.

6. Condenser Heat Removal Control

The condenser heat removal control loop compares the actual condensing pressure to the design condensing pressure and the resultant error signal causes a change in the tube-side coolant flow rate. The coolant flow rate may be regulated either by changing the setting of a coolant flow control valve or changing the speed of the coolant pump. Under certain environmental conditions additional control features may be attractive, such as regulation of the condenser coolant temperature as well as the flow rate. Design of the ultimate heat rejection control scheme is a function of the environmental conditions appropriate to the plant.

B. COMPONENT DESCRIPTION OF THE PROCESS CONTROL MONITORING AND DIAGNOSTIC INSTRUMENTATION SUBSYSTEMS

The functional objectives of the instrument components are to provide accurate, reliable automatic and manual process control information and for operator diagnosis of the plant and component performance. Specific design objectives for these instruments are: high reliability, compactness, low weight, ease of operation and installation, low cost, long life, modular design to facilitate interchangeability and standardization, and removal of accessibility requirements for calibration, reset, etc. for detectors and other inaccessible components.

Separate instruments and circuitry are provided for control and safety information in order to isolate the plant safety circuits from any influence originating from a control component malfunction.

Electrical instrument sensor and control signals have been selected rather than pneumatic because of the following factors:

- 1) Greater ease of installation and maintenance
- 2) No transmission or component clogging problems (due to dust or moisture)
- 3) The signal supply sources (electric power supplies or voltage regulators) weigh less, are easily installed and maintained, cost less, are more reliable (no moving parts), and require less power than their pneumatic counterparts (air compressor, dryer, and accessories).

1. Pressure Measurement and Transmission

One standard type of transducer is used for all pressure transmission (gage, differential, and absolute) including differential pressure head, level, and flow measurements. A variable reluctance pressure transducer was selected for all pressure transmission equipment because of its low weight and high reliability. In addition, these transducers have a long life expectancy, require only control room located adjustments, and all types — gage, absolute, and differential — are available.

Each pressure transducer weighs approximately 1 lb, not including process instrument connections, wiring, and wiring connections. The approximate cost of each transducer is \$600.

2. Flow Elements

Flow-tube type elements may be used for all flow measurements except for extremely low flows. Flow tubes offer the advantages of relatively high accuracy, low permanent head loss (and hence, low power consumption), and shorter straight run piping requirements because of the compactness of the element.

Extremely low flows are most reliably measured by means of precision small bore orifice elements which are integrated with the variable reluctance,

differential pressure transducers. The differential pressure produced by the flow through either the orifice or flow-tube flow elements is sensed and transmitted via the differential, variable reluctance pressure transducer. Pressure transducers used for the measurement of steam or other condensable vapor flows are equipped with condensate chambers in both the high and low pressure connecting lines between the flow elements and the transducers. The condensate chambers assure equal liquid level heads in the low and high pressure connecting lines.

The length of the flow-tube is approximately 2 pipe diameters and the weight is about twice that of an equivalent length of pipe. The price of the flow tube varies proportionately from \$750 for a 2-in.-diameter tube to \$1500 for an 8-in. tube. This price does not include the cost of calibration testing.

Control system studies of the desirability of digital, rather than analog, sensor output signals may recommend the digital mode. In that event the turbine-type flowmeter, e.g., the "Pottermeter," would be selected for measurement of process stream flows. These meters are rugged, extremely compact, and accurate to $\pm 1/4\%$ over an 8-fold flow range, as well as being serviceable over a 12-fold range of flow. The cost of a turbine flowmeter and its output signal conditioning and display devices is not significantly greater than that of the flow-tube and the associated differential pressure transducer and signal conditioning equipment.

3. Level Elements

All liquid levels in the S4PR system are detected by variable reluctance, differential pressure transducers of the type previously described. Vessels, which contain vapor that is condensable at the existing ambient temperature, are provided with a condensate chamber installed at the top (vapor phase) vessel level connection. Output from the differential pressure transducer is proportional to the difference between the constant condensate leg (high pressure) reference head and the variable liquid level (low pressure) head existing in the vessel.

4. Temperature Elements

All temperature sensing elements utilize stainless steel sheathed, MgO insulated, chromel-constantan thermocouples. This type of element was selected

both for its corrosion resistance and relatively high signal output. Thermocouples for the measurement of fluid temperatures are protected by immersion wells, constructed of the same material as the vessel or piping into which it is installed.

The price of each thermocouple is about \$10 and the cost of sheathed leads from the junction is approximately \$70. The weight of protected thermocouples is about 2-1/2 lb each and for nonprotected couples is less than 8 oz.

5. Control Room Receivers and Power Supplies for the Detectors and Transducers

A carrier-demodulator, designed for operation with 95 to 120 volts of 60 cycles ac at a power of 5 watts, operates with the variable reluctance transducer. The carrier-demodulator provides an output of 0 to 5 volts dc to the transducer receiver. The weight of each carrier-demodulator is approximately 1 lb and the cost is \$250 each.

Recording receivers are all electronic, miniature 4-in. strip chart types with 6- by 6-in. face dimensions. Their approximate weight is 250 lb each and their cost is about \$500 each for single pen recorders; additional pens cost approximately \$300 each.

Indicating receivers are voltmeters with 1000 ohms per volt, $\pm 1-1/2\%$ accuracy, 4-1/2-in. scale length and an approximate weight per meter of 1-1/2 lb. The cost of a voltmeter is approximately \$75.

6. Radiation Monitoring Subsystem

The radiation monitoring subsystem is designed to protect plant personnel and equipment from excessive radiation and to provide information required for the health physics services (Figure II-20).

Standard health physics dosimeters, film badges, scalers, hand and foot counters, and portable survey meters are utilized. In addition to these, the following types of radiation monitors are used:

- a) Area radiation monitors
- b) Stack monitor
- c) Process fluid monitors

The area, stack and process fluid radiation monitors are coupled to alarms and the monitored data are recorded in the control room. The area monitors are additionally provided with local visual and audible alarms to warn personnel in the immediate area to evacuate when necessary. This system will conform with Code of Federal Regulation, Title 10, Parts 20 and 70.

a. Area Radiation Monitors

The area monitoring subsystem has ten local detectors, each of which is provided with a local and control room indicator and an individual alarm. A common power supply and the recorders are located in the control room. The ten detectors are located in the following areas:

- 1) Reactor Module
- 2) Process System Module
- 3) Change Room
- 4) Coolant Purification Subsystem Area
- 5) Mechanical Equipment Room
- 6) Coolant Sampling Room
- 7) Heat Rejection Cooling Tower Area
- 8) Control Room
- 9) and 10) Spares.

All station indicators and local detectors of the same activity range are interchangeable. Each detector is equipped with a calibration check source, a weather-proof housing and a 2-1/2-in. diameter, sealed local meter. The individual detectors are detachable from their mountings for portable monitoring within the immediate area. Any one detector signal may be recorded by switching the output of its ratemeter to a multipen recorder. (This recorder is also used to record the output of the fluid activity monitors). A common annunciator panel window is provided for the ten-station area monitoring system. When an

ff-normal" condition is indicated by this annunciator window, the high radiation area (or areas) is located by means of the built-in alarm light system provided with each station indicator. Manual reset of each alarm is also provided at its station indicator.

b. Stack Monitor

The stack monitor is designed to monitor continuously the activity of the particulate and gaseous stack effluent. A sample of the stack effluent is drawn off by an isokinetic nozzle (located at some distance up the stack) and transported to the monitoring system housing. The sample passes through a continuously-moving filter tape which removes particulate impurities from the gas. The activity of the particles retained by the filter tape is monitored by a beta-gamma, G-M detector assembly, and a transistor log ratemeter which has an adjustable high-alarm contact. The particulate-free gas enters a gas sampler which surrounds a large area beta-gamma, G-M detector. The activity of the gas is measured and indicated by a transistorized log ratemeter also equipped with an adjustable high-alarm contact. The gas then passes through the pumping system, the manual regulating valve and flowmeter, and then is discharged into the stack. The activity of both the particulate and gaseous material are recorded. Excessive radiation levels actuate the control room alarms. Check source, low flow, and filter feed control room alarms are also provided. Pumping system and check source operation from the control room is also provided.

c. Process Fluid Monitoring Equipment

The process monitoring equipment is designed to monitor and record the activity of samples from the plant streams, and the activities of the reactor coolant and the water entering the demineralizer and leaving the main purification filter.

The reactor coolant activity is measured by means of a continuous water sample taken from the recirculation loop between the steam separator and the feedwater entry point. The presence of abnormal levels of fission-product activity in the reactor coolant is a direct indication of a fuel-element failure or deterioration. The recirculation loop is monitored by a fission product water monitoring system which employs the technique of continuous isolation of fission-product iodine by ion-exchange resins and gamma-energy discrimination.

TABLE G-I
RADIATION MONITORING SYSTEMS WEIGHT
AND COST BREAKDOWN CHART

Qty	Description	Weight (Each)	Weight Total	Cost (Each)	Cost Total	* Location
10	Area Detector (G-M Tube)	4	40	\$ 425	\$4250	L
1	Control and Power Supply Unit	22	22	675	675	CR
2	5 Unit Indicator with Chassis	6	12	875	1750	CR
Area Monitoring Subsystem Totals			74		\$6675	
1	Stack Monitoring	1300	1300	\$5800	\$5800	L
1	Fission Products Sampler	550	550	3000	3000	L
1	Detector (Gamma Scintillation)	11	11	500	500	L
1	Linear Ratemeter and Dual Power Supply	31	31	1400	1400	CR
1	Plug-in Panel	5	5	25	25	CR
Fission Products Monitoring Subsystem Totals			597	\$4925	\$4925	
2	Liquid Sampler	120	240	\$ 959	\$1180	L
2	Gamma Scintillation Detector	5	10	475	950	L
2	Transistor Ratemeter and Power Supply	40	80	490	980	CR
2	Check Source and Mechanism	10	20	80	160	L
Fluid Monitor Totals			350	\$1640	\$3280	

*Location: L - Local, CR - Located in Control Room.

NOTE: The radiation monitoring system recorder weight and cost information is included in the control room listing.

The main component of the fission product monitor is a water sampler which contains two ion-exchange resin columns for the separation of cation and anion impurities, a filter, flow and pressure controls, check sources, shielding, and sample taps. The activity is detected by a gamma scintillation detector assembly which provides the signal input to a (control room located) linear ratemeter with a plug-in spectrometer input and a dual power supply. This activity may be remotely recorded in the control room by use of a portable or spare recorder. Low flow and high activity alarms are also provided in the control room.

Fluid monitors located at the purification loop filter outlet and at the demineralizer inlet measure gamma radiation by use of scintillation detectors located in stainless-steel wells inserted in the fluid stream. Maximum permissible temperature and pressure of this fluid are 130°F and 150 psia. A calibration check source and an output tap for a recorder are provided.

The reactor coolant activity monitor and the filter inlet and outlet monitors are required for the S4PR system, typical of experimental reactor installations.

Table G-I summarizes the weight and cost data for the radiation monitoring equipment.

7. Nuclear Instrumentation Subsystem

The nuclear instrumentation subsystem provides reactor neutron flux level and period information to the plant protective subsystem, the plant control subsystem, and the control room. The nuclear instrumentation is designed to provide the reliability, accuracy, and sensitivity required for startup, power operation, manual and automatic control of the plant, and to adequately safeguard the plant and personnel against excessive reactor power.

The nuclear subsystem monitors the reactor flux through seven detector channels which cover a total range of at least 10 decades of power in three overlapping ranges. An overlap of at least one decade is provided between successive ranges. Two channels each are provided for the source and intermediate ranges and three for the power range.

The detection system employs fission chambers for the startup range, compensated ion-chambers for the intermediate range, and uncompensated ion-chambers for the power range. All the detectors are housed in waterproof containers located in the shield water below the reactor. The detectors are all standard manufactured types of proven reliability. The detectors are extremely rugged in construction, complying with specifications MIL-S-901 for shock and MIL-STD-167 (Type 1) for vibration.

TABLE G-II
NUCLEAR INSTRUMENTATION WEIGHT AND COST CHART

DETECTORS						
Quantity	Description	Weight (ea) (lb)	Weight (lot) (lb)	Cost (ea)	Cost (lot)	Location
2	Fission Chambers (source range)	1-3/4	3-1/2	\$1250	\$2500	Reactor Area
2	Comp. ion Chamber (intermediate range)	5-3/8	10-3/4	\$2000	\$4000	Reactor Area
3	Uncompensated ion Chambers (power range)	2-1/2	5	\$1250	\$2500	Reactor Area
Detector Totals			19-1/4		\$9000	

NUCLEAR SIGNAL CONDITIONING EQUIPMENT				
Quantity	Description	Weight (lb)	Cost	Location
1	Complete System	600	\$40,000	Control Room

EXTENDED RANGE CHANNEL				
Quantity	Description	Weight (lb)	Cost	Location
1	Micromicro- ammeter	30	\$1000	Control Room

CRITICALITY NUCLEAR INSTRUMENTS				
Quantity	Description	Weight (lb)	Cost	Location
1	Complete System	400	\$5000	Control Room

Nuclear instrumentation weight and cost totals:
Weight - 1150 lb; Cost - \$55,000.

NOTE: Cost and weight information for the nuclear instrumentation system is included in the control room equipment listing.

Fission chambers for the source range detectors were selected because they can withstand power flux levels without requiring means for mechanical withdrawal or the necessity of providing remotely actuated shielding. The approximate dimensions of these fission chambers are 11-7/8 in. long by 2-1/16 in. in diameter.

Two gamma-compensated ionization chambers are used to detect the intermediate range flux level. The thermal neutron sensitivity of these detectors is approximately 4×10^{-14} amperes/neutron/cm²/sec, while the uncompensated gamma sensitivity of these chambers is approximately 3×10^{-11} amperes/R/hour. Maximum dimensions are 3-3/16 in. diameter by 23-13/16 in. long.

The three power-range nuclear channels utilize uncompensated ionization chambers. These ionization chambers are designed to detect neutrons of thermal energy in the flux range from 2.5×10^3 to 2.5×10^{10} neutrons/cm²/sec. The maximum diameter of the detectors is 3-1/16 in. and the maximum length is 13-7/8 in.

The seven detectors receive their operating power from and deliver their outputs to the nuclear signal conditioning and power supply equipment. In addition, the output of one of the intermediate range detectors may be manually switched from the nuclear signal conditioning equipment to an alternate extended range channel (4A) for use during initial reactor shakedown tests and for special postcritical tests. The extended range flux indication is provided by a micro-microammeter with a range switch capable of 20 range changes. The incoming signal information is conditioned and transmitted to the flux recording instruments, to the plant control subsystem, and to the plant protective subsystem. The nuclear signal conditioning and power supply equipment is all highly reliable and is of standard, qualified manufacture. The equipment utilizes pull-out modular construction and solid-state components throughout to ensure highest reliability, facilitate maintenance and repair, and reduce size and weight. Self-checking circuitry assures proper functioning of all channel amplifiers and is monitored by means of malfunction-indicating signals. All seven nuclear detectors are supplied by their own individual power supplies. Table G-II summarizes weight and cost information for the nuclear instrumentation.

The source range signal conditioning equipment is identical for both startup channels. It contains a countrate and period chassis. The countrate and period chassis receives the signal from the source range fission chamber through a preamplifier. This chassis contains a linear pulse amplifier which has two outputs. One of the outputs connects to a scaler (the scaler is only for use during the approach to criticality). The other output is connected to an amplitude discriminator (to eliminate gamma and noise pulses), a log countrate amplifier, and a differentiator and amplifier to indicate the reactor period in seconds, and bistable trip circuits. These activate a period-scram circuit, two annunciator circuits, and actuate minimum flux interlock or source interlock (see the plant protective system description).

All "trips" in the nuclear system are bistable types, and are high-speed transistorized, plug-in card-mounted, and interchangeable. The outputs have discrete voltage levels which are delivered to the logic amplifier inputs. Coupled with each trip is an associated double-pole double-throw relay. These relays operate in approximately 10 ms and are used for interlock, bypass, and annunciation functions.

Each of the two intermediate range signal conditioning channels contains a Log N and period chassis. This chassis contains a logarithmic amplifier to convert current from the compensated ionization chamber to a signal proportional to the logarithm of the reactor power level; it also contains a differentiator and amplifier (to compute the reactor period), and bistable trip circuits to activate the nuclear protective and annunciator circuits.

Each of the three power range channels contains an amplifier which receives the current signal from the uncompensated ionization chamber and converts it to a signal proportional to reactor power. Bi-stable trip circuits to activate the plant protective and annunciator circuits are also provided.

Recording instrumentation for the nuclear system consists of:

- a) A log countrate recorder with a manual switch to enable the operator to record either source Channel I or Channel II
- b) An intermediate range log N recorder which may be manually switched to record either Channel III or IV

- c) A power level recorder capable of being switched manually to record any one of the three power range channels. An output is provided for recording the extended range, Channel IVA on a spare or portable recorder (a specific recorder for this purpose is, however, not provided). The recorders are all standard manufacture, electronic types with proven high reliability.

8. Plant Protective Subsystems Description

The plant protective system is designed to protect the reactor and the entire plant system effectively from damage or to limit such damage to a minimum during all phases of plant operation (startup, shutdown, and operating phases). This protection is accomplished by means of suitable startup interlocks, alarms, automatic reactor setback or scram, activation of the decay heat removal loop, and/or poison injection into the core. Design of the plant protective system is based upon virtually complete separation of the protective and plant control systems except for the use of common nuclear detectors, their amplifiers, and the control drum actuators.

Reactor scram is accomplished by the scram logic system which receives inputs from the opening of any contact in the nonnuclear scram bus and by the period or powerflux scram signals from the nuclear instrumentation system. The logic system in turn deenergizes the windings of the control drum clutches and simultaneously switches the plant control from automatic to manual operation. Upon release of the control drum clutches, the spring-loaded control drums rotate to the "poison-in" position (i. e., the least reactive configuration). The setback safeguard functions in a manner similar to the scram action, except that the setback logic circuit actuates all the drum control drives simultaneously to drive toward the "poison-in" position, thus reducing reactor power rapidly. The power reduction continues until manually stopped by the operator or until a preset power reduction is accomplished. Any setback will also switch control of the plant from automatic to manual.

The logic and power supply chassis includes eight scram output logic circuits and four setback input logic circuits. Silicon-controlled rectifiers are used as solid-state switches and are wired in pairs in a load sharing manner with each clutch switch pair controlling a single clutch. The scram logic circuit inputs are:

- 1) External trip
- 2) Bistable period trip (Channel I)
- 3) Bistable period trip (Channel II)
- 4) Bistable period trip (Channel III)
- 5) Bistable period trip (Channel IV)
- 6) Bistable power trip (Channel V)
- 7) Bistable power trip (Channel VI)
- 8) Bistable power trip (Channel VII)

Inputs 6, 7, and 8, above, are convertible from "OR" trip mode to "2 out of 3" trip mode by means of a manual switch. Pilot light indication of the mode in use is presented to the operator at all times. Activation of any one of inputs 1, 2, 3, 4, or 5 and 6, 7, or 8 (when in "OR" mode) will initiate a scram. Activation of any two of inputs 6, 7, and 8 will cause a scram when the "2 out of 3" mode is in use.

The setback logic circuit is identical with the scram logic circuit with the exception that only four inputs are required (Channel 1, 2, 3, and the 4 period inputs are deleted) and the solid-state switches, controlled by the logic output, are not used to switch off the clutch power supplies. Instead, a 12-pole relay is used to actuate the control drum drives to drive all control drums simultaneously toward their "poison-in" position.

Conditions which will initiate the external (nonnuclear scrams) are as follows:

- a) High core temperature
- b) High reactor outlet steam pressure
- c) Low reactor inlet pressure
- d) Low separator water level
- e) Low flow to condenser
- f) Low reactor shield water tank level
- g) Low reactor inlet flow
- h) Manual scram

A period over-ride circuit bypasses the source and intermediate range fast period scram trips when the power output of the next higher channel is greater than 5%; the period scram provision is restored when the power decreases below 5%. All scram actions, except loss of shield water tank level, will also activate the decay heat removal loop. In the event that the decay heat loop is inoperative, the decay heat is removed by means of the main coolant loop heat rejection system. Key-operated scram and setback bypasses and startup interlocks are provided for reactor startup. In addition to normal scram action, reactor scram by means of poison injection is initiated by confirmed signals of extremely high-core temperature or of the failure of all control drums to rotate to the full "poison-in" position. Table G-III summarizes the cost and weight of conventional safety circuit amplification equipment.

TABLE G-III
SCRAM AND SETBACK LOGIC AMPLIFIERS

Quantity	Description	Weight (lb)	Cost	Location
1	Scram logic amplifier with power supply	50	\$3000	Control room
1	Setback logic amplifier with power supply	40	\$2000	Control room
	Systems Totals	90	\$5000	

9. Control Room Description

A compact layout arrangement for the console and the instrumentation panels within the control room is depicted by the "control room arrangement" drawing (Figure G-2). The control console is located between the facility instrument panel and the main plant instrument panel with the console operator facing the latter. The instrument panel section, containing the nuclear and plant protective amplifiers and the radiation monitoring instruments, is located to the right of the console operator.

A separate console, rather than a chassis with an integral console-instrument panel(s) (or racks) was selected to allow one operator to be able to survey a greater width of panel and a larger number of instruments than is possible with an integral console-type of arrangement.

7-3-63

Figure G-2. Control Room Arrangement

7564-0338

The console and instrument panels are of strong, lightweight, heliarc-welded aluminum construction. The front instrument mounting panels are 1/8in.-thick aluminum and the side and back panels are removable polyvinyl plastic for both the console and the instrument panels.

The approximate dimensions of the console are 5-1/2 ft wide by 3 ft deep (includes a 1-ft console shelf) by 42 in. high (low silhouette portion is 42 in. high - the vertical section on the operator's left is approximately 7-1/2 ft high).

The instrument section beyond the console and immediately in front of the console operator is approximately 8 ft in width by 7-1/2 ft high. This section together with the console contains all the instruments and controls that the operator must normally observe for operating the plant after the power-producing range has been reached.

The instrument section on the console operator's right is approximately 5-1/2 ft in width by 7-1/2 ft high. This section contains the radiation monitoring instrumentation and the nuclear and plant protective signal conditioning instruments. In addition, it contains all of the source and intermediate range nuclear indicators and recorders that are not located on the console.

The criticality instrument section is approximately 2 ft wide by 7-1/2 ft high. This section is portable and may be located close to the console during the criticality testing stage and removed after completion of the test.

The facility instrument panel is located directly behind the console operator. This panel is composed of two sections each approximately 4 ft wide by 7-1/2 ft high. Each section is mounted on casters and provided with flexible extension output and input connections. This allows either or both sections to be moved forward approximately 3 ft to provide rear access for servicing.

The annunciators are located across the top of each instrument section and directly (as practical) above indicators, recorders, switches, or other instruments associated with the annunciated variable.

Estimates of control room equipment cost and weight are presented in Table G-IV.

TABLE G-IV
CONTROL EQUIPMENT COST AND WEIGHT

Quantity	Description	Structure		Instruments	
		Weight (lb)	Cost	Weight (lb)	Cost
1	Console	350	\$ 2,500	400	\$14,000
1	8-ft main instr. section	500	3,000	1000	16,000
1	5-1/2-ft nuclear radiation monitoring and plant prot. section	350	2,000	870*	50,830*
1	Criticality section	150	900	400*	5,000*
1	8-ft facility section	530	3,000	1000	10,000
	Total	1880	\$11,400	3670	\$95,830

*These weights and costs (or parts thereof) are also given in the nuclear radiation monitoring and plant protection system description sections.

C. ANALYSIS OF SYSTEM DYNAMICS AND STABILITY

Analog computer studies, aimed at simulating the operational behavior of the SNAP 4 system, were conducted utilizing a linearized mathematical model. In the formulation of the model, the reactor was described by the Fleck¹ integral representation, modified to reflect forced circulation of coolant, whereas the process system components were represented by differential expressions describing the laws of conservation of mass, energy, and momentum.

The development of the analog representation was carried out by considering the S4PR system to be two intertwining process loops, as indicated in Figure G-1.

- 1) Recirculation loop consists of the reactor, steam separator, recirculation pump, and the interconnecting pipelines.
- 2) Feedwater loop consists of the steam volume in the separator, condenser, hotwell, condensate and feedwater pumps, and the pipelines joining these components.

The following control circuits were incorporated into the analog representation (as shown in Figure G-1): the neutron flux, pressure and load demand signals to the reactor control actuators, the flow balance in the recirculation loop to control the speed of the pump-drive turbine, and the flow balance in the feedwater loop to control the speed of the condensate feedwater pump-drive turbine.

A number of types of perturbing signals, over a wide range of frequency and amplitude, were introduced and fifteen output signals, monitoring the primary operating parameters, were recorded to assess the stability of the system. The perturbations studied included the following:

Reactivity inputs - Step changes of 20, 50, 75, and 100¢
Ramps of 5 and 10¢/sec
Sinusoidal oscillations of ± 4 Mw at
frequencies between 1 and 5 cycles/sec

Reductions in Inlet Coolant Flow - Ramp decreases from the
full flow conditions at rates between
 $1/2$ and 4 ft/sec²

Coolant Pressure Variations - Sinusoidal changes of ± 10 , 25,
and 32 psi at frequencies between $1/2$
and 6 cycles/sec
Ramp decreases of 4 and 8 psi/sec

(All of the above were conducted with and without the
reflector drum control circuit effective.)

Dead-Band Study - Sinusoidal variations from the full power
condition of steam flow valve setting (load
change simulation) with dead-band amplitudes
between 0 and ± 2 Mw and frequencies from
0.01 to 1.0 cycles/sec

7-3-63

7564-0339

Figure G-3. Reactivity Step of \$1.00

These studies of control circuit dead-band width were made to evaluate the effect of various degrees of insensitivity in the control drum positioning actuator on reactor and plant stability when perturbations in load are imposed upon the plant.

The effects of reactivity perturbations are most clearly illustrated by referring to Figure G-3 which is a graph of the major variations recorded during the most severe of the perturbations imposed on the system, viz, the step input of \$1.00 of reactivity. Figure G-3 presents the operational behavior of the reactor for both the controlled and uncontrolled cases. With drum control operable, the system was able to compensate for the step increase in reactivity within about 2 sec and each of the parameters returned essentially to their initial, steady-state values in that time interval. Without control, larger variations in the parameters were observed, but again within about 2 sec, reactor conditions had stabilized but in this case, at higher values of power, fuel temperature and void fraction. It is clear that the increase in steady-state void fraction and fuel temperature provided sufficient negative reactivity to compensate completely for the initial perturbation.

The reactivity ramp inputs, with drum control, caused no detectable changes in reactor parameters other than the variation in control drum position, which compensated for the input. Without drum control, all reactor operating parameters increased including the void fraction which continued to increase until the negative reactivity effect terminated the nuclear reaction; the reactor is subcritical when devoid of coolant.

The reactivity oscillations imposed on the reactor caused only very slight compensatory movement of the control drums, due to the high damping inherent in the reactor; the fuel time constant is appreciably longer than all others. Without drum control, oscillation of reactor parameters was observed at the lower frequencies; however, the system remained completely stable and again appeared highly damped.

Reductions in coolant flow to the reactor were imposed to study the effects of loss-of-coolant flow incidents. Figure G-4 summarizes the data from the most severe of these incidents studied and illustrates the behavior of the reactor both for the controlled and uncontrolled conditions. With drum control, reactivity, reactor power and fuel temperature remain very nearly constant

7-3-63

Figure G-4. Reactor Core Inlet Velocity Ramp 4 ft/sec-sec

7564-0340

Until sufficient coolant loss had accumulated to quench the reaction. Without control, these parameters decreased at a rate nearly proportional to the rate of coolant loss until the reactor again was shut down. In no case was oscillatory behavior observed.

The study of coolant pressure variations failed to disclose effects as severe as any of those described above. The reactor and system exhibited complete stability in all the cases investigated.

The load-following ability of the plant was investigated in the study of dead-band effects and by means of a step increase in power demand from 80 to 100% of full power. In the former, as the width of the control circuit dead-band was increased, the frequency required to break through the dead-band increased proportionately, indicating that even with appreciable dead-band, the plant is inherently capable of following low frequency load changes. The effect of a step increase in power demand is summarized in Figure G-5 which illustrates the rapidity with which the plant is able to adjust to the demand. Steady-state conditions are attained within 2 to 3 sec and there is no indication of divergent oscillatory behavior.

During the analog computer studies described, the limitations of the model employed became apparent. The most serious of these proved to be the use of the loop technique of formulation. These loops were so described analytically that all feedback paths, from the process system to the reactor, were restricted to the nonreactor control system. With inherent perfection of the process control system assumed, system perturbations could only have been studied realistically via perturbations of the control system itself. This was not done. Furthermore, since purely thermodynamic representation was employed for the functions of the process equipment, all loops were essentially resistive in nature (i.e., the capacitive effects, compressibility of steam, and steam-water mixtures were entirely neglected). For these reasons, the model studied permitted a realistic evaluation of reactor stability and performance, but not of the process loops.

A general shortcoming, common to all segments of the model, was recognized in the linearization of all effects about the full power operating point values of the pertinent parameters. The latter criticism of the model assumes importance during the uncontrolled transients which were sustained until reactor

7-3-63

7564-0341

Figure G-5. System Natural Response
to a 20% Step in Power Demand

power had decreased to very low values. Thus, the model was recognized as being unsuitable for the study of system behavior during startup or at very low power operating conditions. Despite its defects, the model was fully capable of confirming the stability of the reactor itself. However, to permit the accurate description of reactor and system interactions, a new analog model representation was initiated as soon as the deficiencies of the old were detected.

The new model employs a nodal technique of functional representation (the loop technique was abandoned) and fully incorporates capacitive effects. This model was completed in June 1963, and checkout of each node, either an entire process component or a portion of a multifunctional components, was underway, e.g., the models of steam separator, condenser, and reactor were wired and checked out. Combination of the various nodes and complete system model studies was to be conducted; it would require the simultaneous use of four PACE computer consoles.

Initially the improved analog model will be used again to study stability and load-following capability in the range of 50 to 120% of full reactor power. In this range, the linearized parameter representation is accurate to within 5% of the actual values. Further study is required to permit definition of the most satisfactory method of attaining accurate simulation at power levels below 50%. This is necessary since mechanization of the model will require more coupled computer consoles and as the physical complexity of the computer arrangement increases, the difficulty of checking out and subsequently of satisfactory operation mounts significantly.

Digital computer studies,² applying transfer function models to the study of system kinetics, were used to analyze reactor stability during startup and at low power levels. The accident analyses (reported in Appendix H, Safety) were performed using the digital computer model. These studies have been directed only to the investigation of reactor behavior. Incorporation of process system feedback loops will undoubtedly complicate and make more difficult the programming of the digital computer model.

A potentially suitable solution to the problem of simulating plant behavior over the entire range of power operation has been proposed. It consists of operating simultaneously both digital and analog computers, coupled together so

that the reactor is described by the digital model while the process system loops are simulated by the analog model. Studies of this type have been performed by the Autonetics Division of Rockwell International Corporation and the technique is available for application to the SNAP 4 system.

D. ANALYSIS OF REFLECTOR (CONTROL) DRUM DYNAMICS

A theoretical analysis³ of the dynamics of the control drum, its actuator, gear train, and scram spring has been performed. The kinetic and kinematic behavior of the following types of components were examined:

- 1) Synchronous servo motor actuator
- 2) Digital (stepper) servo motor actuator
- 3) Spur gear train
- 4) Planetary gear train
- 5) Scram spring and snubber (dashpot).

The analytical comparison of stepper and synchronous motors together with their gear trains (of both types) disclosed that either type of actuator could be applied with equal rigor to the SNAP 4 reactor control task. This conclusion follows directly from the large inertia and damping introduced by the gear train, so that the stepper motor performance closely approximates that of the synchronous motor.

The analysis of drum scram dynamics was accomplished initially by manual computation³ and more rigorously via an IBM, Fortran Program.⁴ Not only were the performance requirements and design characteristics of the scram spring analyzed, but those of the drum snubber were also delineated. In this study, the increase in frictional effects in the drum bearings with time, due to potential corrosion or crud deposition, was considered in developing the requirements for scram spring preload.

The results of these studies have been used to define an acceptable control actuator arrangement and to draft a preliminary specification both for the actuator components and the control circuits required external to the reactor.

APPENDIX G REFERENCES

1. J. A. Fleck, Jr., "The Dynamic Behavior of Boiling Water Reactors," J. Nuclear Energy, Part A, 11, p 114 (1960)
2. R. L. Johnson, "Core Transfer Function Models for SNAP System Kinetics," NAA-SR-8440 (April 29, 1963)
3. R. L. Hall, "Preliminary Design, Reflector Actuator," NAA-SR-TDR-7723 (November 1, 1962)
4. J. H. Pruitt, "DREDS, Dynamics Reflector Drum Scram," AMTD-197 (April 1963)

BLANK

APPENDIX H. SAFETY

Safety considerations for the SNAP 4 system center around the nuclear reactor and the possibility of a nuclear incident accompanied by the release of hazardous nuclear radiations. Although consideration is also given to the non-nuclear part of the system, including the pressure vessel, piping, power plant, operating procedures, etc., these items are generally considered to be part of a standing technology in which acceptable practices have been established. Conversely, nuclear reactors are generally viewed as new, potentially dangerous devices which deserve special attention. Detailed consideration of the SNAP 4 reactor, both as a part of the system and as a separate entity, leads to the conclusion that this reactor is safe and inherently stable. In particular, it appears to be conceptually impossible to suffer a serious nuclear radiation incident with the SNAP 4.

A. REACTOR CHARACTERISTICS

1. Reactivity Coefficients

The safety characteristics of a nuclear reactor are closely associated with the coefficients of reactivity of the system. These were summarized for the SNAP 4 reactor in Table B-II, Coefficients of Reactivity. The reactor is characterized by several large negative coefficients of reactivity associated with changes in temperature and operating conditions. A prompt negative fuel temperature coefficient of reactivity of about $-1.3 \times 10^{-5}/^{\circ}\text{F}$ results from axial fuel expansion, moderator (water) expulsion due to radial fuel rod expansion, the spectral shift in thermal neutron population and the Doppler broadening of resonance absorption. This large negative prompt temperature coefficient of reactivity tends to compensate strongly for all reactivity increases.

In addition to temperature changes, the reactor is sensitive to changes in the condition of the coolant. The formation of steam voids in the core has a particularly strong negative reactivity effect. The change in coolant density from the cold shutdown condition to the hot operating condition, including both the effects of water expansion and the formation of steam voids, is accompanied by a decrease of about 5% in reactivity. The steam void coefficient of reactivity is about $-1.65 \times 10^{-3}/\%$ steam fraction change at the operating condition. The water expansion which accompanies coolant temperature changes also introduces a reactivity effect.

The total result, at the 12 Mwt operating condition, is a power coefficient of reactivity of about $-0.0045/\text{Mwt}$. Part of this coefficient is prompt, being associated with the fuel temperature increase, and part is essentially delayed by the characteristic time required to conduct heat through the fuel rod to the coolant, about 0.1 sec. The effect of this coefficient is to resist power changes or fluctuations.

The reactivity of the system is further influenced by the entrance and exit conditions of the coolant in that the grid plates, which support the fuel rods, expand and contract with temperature variations. The coefficients associated with the grid plate temperatures are approximately $-0.13 \times 10^{-5}/^{\circ}\text{F}$.

System pressure changes result directly in steam void changes, which affect the reactivity according to the above relationship. An increase in pressure compresses the boiling voids, and produces a positive reactivity effect. This is the only positive reactivity coefficient associated with the system.

A further complication associated with a boiling water reactor is introduced by the presence of an interface between the subcooled and the boiling regions of the coolant in the core of the reactor. Fluctuations in the position of this interface serve to introduce reactivity fluctuations, primarily because of variations in steam void position and magnitude. Analytical treatment of this problem requires the simultaneous detailed treatment of heat transfer, hydrodynamics, and neutron physics for the system.

The brief presentation above of the various reactivity feedback effects serves to introduce the complexity of the system. This complex relationship between reactivity feedbacks has, at times, resulted in unstable modes of operation. Most of the coefficients presented above are common to boiling water reactors, which have been known to exhibit instabilities. In the SNAP 4 system however, it is found that the forced flow condition serves to damp severely the interplay between void and pressure reactivity effects and results in an inherently stable system. This is fundamentally different from the situation which exists in natural-convection-cooled boiling-water reactors. A summary of the analog and digital computer studies which have led to these conclusions is presented in the following sections.

2. Fuel Properties

The hydrided uranium - zirconium alloy which serves as the fuel-moderator in the SNAP 4 reactor has several properties which enhance the safe operating

characteristics of the system. The large heat capacity, around 0.14 Btu/lb-°F, appreciably dampens power fluctuation effects, and allows the fuel to survive modest power excursions. However, the most significant fuel property for safety considerations is associated with the evolution of hydrogen at high temperatures. This mechanism, whereby moderator is irrevocably lost if fuel temperatures exceed some critical temperature, provides a permanent shutdown mechanism for any kind of postulated serious excursion. In the event that the moderator is lost in this manner, the system necessarily becomes subcritical, and the fuel is left in such a state that no critical configuration can be attained.

As fuel temperature increases, the hydrogen dissociation pressure increases. The hydrogen is effectively contained within the cladding until a temperature is attained where the dissociation pressure exceeds the strength of the fuel cladding. At this point the cladding bursts, hydrogen is released in the gaseous state, and the system becomes highly subcritical. Figure H-1 presents the temperature at which this cladding burst takes place for the two types of stainless steel under consideration. For the current design a fuel temperature of 1840°F results in the cladding burst. Energy considerations indicate that about 60 Mw

5-16-63

Figure H-1. Cladding Burst Conditions

7564-0231

are required to reach this condition. An additional 160 Mw, most of which must necessarily be supplied by decay heat since the system is subcritical, are absorbed until all of the hydrogen is evolved. This hydrogen evolution occurs at essentially constant temperature since at about 2000°F the characteristic diffusion time for hydrogen across the rod is on the order of 0.5 sec. A further release of 120 Mw of energy will result in a fuel temperature of 3300°F and the initiation of meltdown of the core. The fuel which melts down, given this 340-Mw excursion, contains no moderator and has insufficient fissionable material to assume a critical configuration. The probability of an event of this magnitude is very small, as will be demonstrated in the next sections. The result, however, of even this severe case is the attainment of a safe subcritical configuration. The primary hazards associated with this event involve the release of fission product gases when the cladding bursts and the possibility of a hydrogen fire as the 940 moles of hydrogen contained within the 1-ft³ core are released.

The above example is extreme. The safety valve of hydrogen release is most clearly demonstrated by the case where an excursion just in excess of 60 Mw occurs and is terminated, permanently and safely, by a small hydrogen release. However, even an excursion of this magnitude is highly unlikely.

In the succeeding sections, the behavior of the reactor is described under normal operating conditions and under postulated accident conditions. Conditions under which the inherent stability and feedback effects are sufficient to establish and maintain a safe operating system are presented. Accident conditions which result in permanent shutdown by hydrogen release are discussed. The vulnerability of the system to such accidents is indicated.

B. OPERATIONAL BEHAVIOR

1. System Response at Power

Analog simulation studies of the SNAP 4 system have been carried out on a linearized model which incorporates all of the reactivity feedback effects discussed in the previous section. The reactor is represented by two nodes, representing the subcooled and the boiling regions, respectively. First-order heat-transfer time lags have been included. The system external to the reactor, including the pumps, steam separator, condenser, etc., is also represented.

Studies have been conducted in which the system, initially operating at 12 Mwt, was subjected to steps, ramps, and oscillations in reactivity.

Pressure loss ramps and velocity loss ramps of the reactor inlet coolant have also been studied. In all cases, calculations were accomplished with and without the operation of the control drums to study the compensating characteristics of the reactor without external control, and to observe the proposed control scheme.

Figures H-2 through -7 present a few examples of these studies. Step changes in reactivity of \$0.50 and \$1.00, an inlet velocity ramp of -2 ft/sec^2 , and a 3.5 cps oscillation are presented. Reactor power, fuel temperature, steam fraction, and reactivity variation were recorded. For the cases with control drum feedback, the control drum response is also represented. The stable characteristics of the system during power operation are clearly portrayed. Even a \$1.00 step in reactivity, in the absence of drum control, creates only a minor perturbation. The step is accompanied by a power surge which is quickly terminated by the strong negative reactivity coefficients. The fuel temperature rises only about 200°F . This is the most severe perturbation observed. The other cases cause even smaller variations in system parameters.

2. Reactor Stability

In addition to analog studies of system response, studies of reactor stability have been made utilizing transfer function techniques. A distributed parameter transfer function, in which detailed consideration of the spatial heat transfer and fluid flow problems is included, has been derived. Calculations have been accomplished numerically on the IBM 7094. Typical results are presented in Figures H-8, -9, and -10, which are Nyquist diagrams for the SNAP 4 reactor at operating powers of 0.5, 8.0, and 13.0 Mwt, respectively.

The curves present input frequency variations from $\omega = 0$ to $\omega = +\infty$, which lie at the origin. These plots have been examined for stability in the customary manner by drawing the image of the curve in the upper plane and closing the contour in a clockwise fashion. None of the (-1) points (marked X on the plots) were encircled in a clockwise fashion. All of the figures shown correspond, therefore, to unconditionally stable response.

Figure H-8 is typical of the frequency response at a low power and a low flow rate, such as might be encountered during startup. The power is 450 Btu/sec, or about $1/2 \text{ Mw}$, with a flow of 10 lb of water/sec. Even at this low power and flow, the core is extremely stable.

7-2-63

7564-0343

Figure H-2. \$0.50 Reactivity Step-Controlled

Figure H-3. \$1.00 Reactivity Step — Controlled

7-2-63

7564-0345

Figure H-4. \$1.00 Reactivity Step-Uncontrolled

7-2-63

7564-0346

Figure H-5. Velocity Ramp-Controlled

7-2-63

7564-0347

Figure H-6. Velocity Ramp-Uncontrolled

7-2-63

7564-0348

Figure H-7. Power Oscillation

7-2-63 7564-0349
Figure H-8. Reactor Stability-
Nyquist Diagram, Q = 1.12

7-2-63 7564-0350
Figure H-9. Reactor Stability-
Nyquist Diagram, Q = 2.154

P=12500.0 Q=3.58

7-2-63 7564-0351
Figure H-10. Reactor Stability-
Nyquist Diagram, Q = 3.58

Figures H-9 and H-10 are frequency responses typical of operational conditions. Figure H-9 is for a power of about 8 Mw, Figure H-10 for about 13 Mw. In both cases the coolant flow rate through the core is 110 lb/sec. The Nyquist plots again indicate a very stable system.

C. ACCIDENT CONDITIONS

1. Startup

In addition to analog studies and transfer function calculations, direct numerical calculations of the reactor kinetic response have been made using a digital computer program. This last technique is necessary to carry out calculations of modes of operation where the non-linear behavior of the reactor is important. Such is the case during startup when power levels change by many decades, reactivity variations are significantly large, temperature variations are large enough to warrant consideration of changes in properties of materials, and time spans are such as to require inclusion of various time-dependent feedback effects. A number of possible accident conditions have been studied; a few examples are presented in Figures H-11 through -14.

Figure H-11 presents the results of a reactivity insertion rate of $2\text{¢}/\text{sec}$ up to an accumulated maximum of 1.00 starting from zero power. The system is assumed to be pressurized and flow established. $2\text{¢}/\text{sec}$ corresponds to the rate of reactivity insertion which accompanies simultaneous insertion of all the control drums. This is not the normal startup condition, but rather represents a possible accident which could occur only if a number of the system safety precautions fail simultaneously. The results show that the system is quite safe. The rapid reactivity insertion results in a power surge which reaches approximately 3 Mw before it is "turned over" by the feedback from fuel and coolant temperature changes. The effect on the fuel is to increase the temperature from room temperature to about 240°F , which together with a comparable increase in coolant temperature, provides sufficient reactivity compensation to make the reactor subcritical.

Figure H-12 shows the same rate of reactivity insertion but with no termination of reactivity insertion until all drums are fully in. This accident would be predicated upon the same failures as the previous case, with the addition of no corrective control drum action, either by automatic scram or normal control, over a period of many minutes. Even in this case the excursion is mild. Initially

5-16-63

Figure H-11. Startup Accident - 2¢/sec to \$1.00

7564-0233

5-16-63

7564-0234

Figure H-12. Startup Accident - 2¢/sec to \$12.00

5-16-63

7564-0235

Figure H-13. Startup Accident - \$2.00/sec to \$1.00

5-16-63

Figure H-14. Startup Accident - \$2.00/sec to \$12.00

7564-0236

the power rises, turns over at about 4 Mw, and levels out at about 230 sec as the coolant temperature reaches the boiling condition. Boiling ensues, resulting in an over-compensation of reactivity and a subsequent dip in power level. Because of the continuing reactivity insertion, the power level would now start to rise if the incident were continued. This accident condition is sufficiently mild that it could be plausibly proposed as a normal startup procedure, although it certainly is not being proposed seriously as such here.

Figure H-13 describes the result of an insertion rate of \$2/sec continued to a reactivity insertion of \$1. This could occur only as a consequence of some catastrophic event that rammed a drum fully-in within 1/2 sec, or jarred in several of the drums simultaneously. The result, however, is not very much different from the 2¢/sec insertion to \$1. The power peaks more quickly, but is still turned over at about 4 Mw by the temperature coefficients of reactivity, and the reactor becomes subcritical. Again the fuel temperature rises to about 240°F.

Figure H-14 presents a case where \$12 in reactivity is inserted at \$2/sec. This would require the simultaneous insertion of all the drums in about 6 sec, an extremely improbable event. In this case the power pulses quickly to about 200 Mw, then drops and remains above 10 Mw for almost 15 sec. During this time the fuel temperature rises to about 2000°F, the cladding bursts, and some hydrogen is evolved from the hydride fuel. The initial power pulse is terminated by the prompt coefficients. The reactivity is subsequently reduced by the temperature rise of the fuel and coolant and by the loss of hydrogen. The power levels here are sufficiently large that boiling begins after about 10 sec, further negating the reactivity insertion. The net result of this very severe reactivity input is that the reactor experiences a power excursion, some hydrogen is evolved from the fuel, and the excursion is terminated with the reactor in a subcritical, safely contained state.

2. Loss of Coolant

The only potentially serious accident that can be postulated for the SNAP 4 system involves the loss of coolant. Losing the coolant does not cause reactivity insertion nor does it in any other way create a criticality incident. On the contrary, without the water in core, the reactor is safely subcritical. The problem arises from the heat generated by the decay of fission products following high-power operation. In normal operation, a natural convection loop is provided

For removal of decay heat following power operation. In the event that a catastrophic accident would prevent maintaining coolant in the core, it would be possible, theoretically, for the decay heat to result in a core meltdown. Actually, all coolant pipes have been located well above the top of the core, and the entire recirculation loop is totally immersed in water in order to make this accident a virtual physical impossibility.

Figure H-15 shows the sequence of events that follows a loss of coolant after extended operation at a number of thermal power levels. This figure was prepared with the assumption that no heat transfer mechanism was available to remove the decay heat. During the initial heating period, heat generation causes increasing fuel temperature until, at about 1840°F, the fuel element cladding bursts and hydrogen is evolved. The time required to reach this state varies from a few minutes to several hours, depending upon the pre-accident operating power. Hydrogen continues to evolve, at about 2000°F, until it has been removed. This requires from 20-min to several hours. Subsequently, fuel temperatures increase until the melting temperature is reached. In the case where the reactor has been operating at 10 Mwt, the whole process requires about 40-min.

The assumption of no heat transfer is, of course, highly conservative and in reality, longer times to melt are required. In the case of 1 Mwt operation before coolant loss, heat conduction will suffice to remove the decay heat without a core meltdown. With the availability of any kind of a coolant, or other favorable heat transfer path within the core, the decay heat following high-power operation may also be removed without fuel melting.

3. Pressure Loss

A rupture in the system which produces a loss of pressure, but not total loss of coolant, is less serious than the case cited above. With loss of pressure, the coolant within the core flashes into steam, creating voids which rapidly produce a subcritical state. After shutdown, the presence of coolant allows the natural convection decay heat removal loop to function, thus preventing a core meltdown.

Rupture of the system does not create a serious nuclear incident. An intermingling of shield water and reactor coolant is not dangerous since both are primarily water. The radioactivity of the core coolant is only nominal and short-lived. In addition, it should be noted that a rupture of the system, near the core in particular, is highly unlikely.

2-16-63

7564-0230

Figure H-15. Loss-of-Coolant Accident