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GAS-COOLED THERMAL REACTOR PROGRAM

Semiannual Technical Progress Report for the Period
April 1–September 30, 1983

December 1983

Work Performed Under Contract No. AC03-80ET34034

General Electric Company
Sunnyvale, California

Technical Information Center
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ADVANCED REACTOR SYSTEMS DEPARTMENT • GENERAL ELECTRIC COMPANY
SUNNYVALE, CALIFORNIA 94086

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TABLE OF CONTENTS

	<u>Page</u>
ABSTRACT	xiii
1.0 SUMMARY	
1.1 Program Management (WBS 01)	1-1
1.2 Application Definition and Assessments (WBS 03)	1-2
1.3 Plant Technology (WBS 13)	1-3
1.4 HTGR-SC/C Project Management (WBS 30)	1-5
1.5 HTGR-SC/C Plant Integration (WBS 31)	1-6
1.6 Modular Reactor System (WBS 41)	1-6
2.0 INTRODUCTION	2-1
3.0 PROGRAM MANAGEMENT (WBS 01)	
3.1 Advanced Concept Plans	3-1
4.0 APPLICATION DEFINITION AND ASSESSMENTS (WBS 03)	
4.1 Opportunities Assessment	4-1
5.0 PLANT TECHNOLOGY (WBS 13)	
5.1 Coolant Transport Behavior	5.1-1
5.2 High Temperature Structural Engineering	5.2-1
5.3 Materials Application Engineering	5.3-1
6.0 HTGR-SC/C PROJECT MANAGEMENT (WBS 30)	
6.1 Lead Plant Systems Engineering	6-1
7.0 HTGR-SC/C PLANT INTEGRATION (WBS 41)	
7.1 Plant Simulation	7-1
8.0 MODULAR REACTOR SYSTEM (WBS 41)	
8.1 Modular Reactor Design	8-1

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LIST OF FIGURES

		<u>Page</u>
2-1	HTGR Program Summary Level Work Breakdown Structure FY83	2-3
3-1	Evaluation Phase Logic HTGR Advanced Systems Concept	3-6
4.1-1	Projected Available Energy Demand for HTGR	4.1-14
4.1-2	Nuclear Power Split for MRS-COREFORMER	4.1-15
4.1-3	HTGR Energy Cost vs Plant Size	4.1-16
4.1-4	HTGR Energy Costs vs Time	4.1-17
4.1-5	HTGR Energy Costs vs Time	4.1-18
4.1-6	HTGR Market Penetrations vs Time	4.1-19
4.1-7	Present Worth of Accumulated Cost Benefits of HTGR	4.1-20
4.1-8	MRS-C/R Syngas Market Potential	4.1-21
5.1-1	Schematic Diagram of HTGR Primary Loop	5.1-17
5.1-2	HTGRIT Program Organization	5.1-18
5.1-3	Typical Scale Formation on Alloy 800 after Extended Exposure to Typical Reactor Levels of Impurities	5.1-19
5.2-1	Example Model Structure: Three Bar Plane Frame	5.2-84
5.2-2	Elastic Stress Patterns in the Structure as Carburization Proceeds	5.2-85
5.2-3	Structural Model Analysis as Function of Time	5.2-86
5.2-4	Illustration of Proposed Code Method for Carburized Component Design	5.2-87
5.2-5	Stress Distribution on Cool Down in Reformer Tube (Schematic)	5.2-88
5.2-6	K_I for an External Radial Edge Crack in a Tube Subjected to a Uniform Internal Pressure	5.2-89
5.2-7	Crack Growth Rate Data for Unaged Inconel 617	5.2-90
5.2-8	Allowable Stress Range vs. Initial Flaw Depth	5.2-91

LIST OF FIGURES

	<u>Page</u>
5.2-9 Geometry of the Cracked Section of the Pipe. The Cross Section Contains Two Identical and Symmetrically Situated Cracks	5.2-92
5.2-10 Design Decision Process	5.2-93
5.2-11 Accident Preventions Diagram-A: Reformer	5.2-94
5.2-12 Accident Source Diagram (A51)-Reformer Tube Blockage	5.2-95
5.2-13 Accident progression Diagram (AP51)-Plant Shutdown with Reformer Tube Failure	5.2-96
5.2-14 Accident Progression Diagram (AP52)-Plant Operations with Reformer Tube Failure	5.2-97
5.2-15 Accident Source Diagram (A53)-Catalyst Loading Error	5.2-97
5.2-16 Accident Source Diagram (A54)-Defect in Reformer Tube	5.2-98
5.2-17 Example Decision Matrix	5.2-99
5.3-1 Inconel 617 Fatigue Design Curves-Predicted Average Values (in Fahrenheit Increments)	5.3-25
5.3-2 Inconel 617 Fatigue Design Curves-Predicted Average Values (in Centigrade Increments)	5.3-26
5.3-3 Straight Line Fit to 1000°F Inconel 617 Fatigue Data	5.3-27
5.3-4 Inconel 617 Fatigue Design Curves-1000°F Data	5.3-28
5.3-5 Straight Line Fit to 1300°F Inconel 617 Fatigue Data	5.3-30
5.3-6 Optimized Straight Line Fit to 1300°F Inconel 617 Fatigue Data	5.3-31
5.3-7 Inconel 617 Fatigue Design Curves-1300°F Data	5.3-32
5.3-8 Straight Line Fit to 1382°F Inconel 617 Fatigue Data	5.3-34
5.3-9 Optimized Straight Line Fit to 1382°F Inconel 617 Fatigue Data	5.3-35
5.3-10 Inconel 617 Fatigue Design Curves-1382°F Data	5.3-36
5.3-11 Straight Line Fit to 1400°F Inconel 617 Fatigue Data	5.3-38
5.3-12 Optimized Straight Line Fit to 1400°F Inconel 617 Fatigue Data	5.3-39
5.3-13 Inconel 617 Fatigue Design Curves-1400°F Data	5.3-40
5.3-14 Straight Line Fit to 1500°F Inconel 617 Fatigue Data	5.3-42

LIST OF FIGURES

		<u>Page</u>
5.3-15	Optimized Straight Line Fit to 1500°F Inconel 617 Fatigue Data	5.3-43
5.3-16	Inconel 617 Fatigue Design Curves-1500°F Data	5.3-44
5.3-17	Straight Line Fit to 1562°F Inconel 617 Fatigue Data	5.3-46
5.3-18	Optimized Straight Line Fit to 1562°F Inconel 617 Fatigue Data	5.3-47
5.3-19	Inconel 617 Fatigue Design Curves-1562°F Data	5.3-48
5.3-20	Straight Line Fit to 1600°F Inconel 617 Fatigue Data	5.3-50
5.3-21	Inconel 617 Fatigue Design Curves-1600°F Data	5.3-51
5.3-22	Straight Line Fit to 1700°F Inconel 617 Fatigue Data	5.3-53
5.3-23	Optimized Straight Line Fit to 1700°F Inconel 617 Fatigue Data	5.3-54
5.3-24	Inconel 617 Fatigue Design Curves-1700°F Data	5.3-55
5.3-25	Straight Line Fit to 1742°F Inconel 617 Fatigue Data	5.3-57
5.3-26	Optimized Straight Line Fit to 1742°F Inconel 617 Fatigue Data	5.3-58
5.3-27	Inconel 617 Fatigue Design Curves-1742°F Data	5.3-59
6.1-1	Requirements Documentation Logic	6.1-4
7.1-1	Block Diagram of HTGR SC/C Lead Plant Engineering Simulation	7-12
7.1-2	HTGR Simulation Code Modular Code Structure	7-13
7.1-3	Control System Model Diagram	7-14
7.1-4	Top Level Data Flow Diagram	7-15
7.1-5	Key System Parameters	7-16
7.1-6	Feedwater/Turbine Flows	7-17
7.1-7	Valve Positions	7-18
7.1-8	Steam/Feedwater Temperatures	7-19
7.1-9	Feedwater Heater Flows	7-20
7.1-10	Pressures	7-21
7.1-11	Steam/Feedwater Temps	7-22

LIST OF FIGURES

		<u>Page</u>
8.1-1	Schematic of MRS Simulation Model Data Flow to Modules	8.1-69
8.1-2	Simplified Representation of Reformer Dynamic Analysis Model	8.1-70
8.1-3	Helium and Process Gas Temperature-Loop Model Case 2L	8.1-71
8.1-4	MRS Configuration Dynamic Analysis Model Parameters	8.1-72
8.1-5	Helium and Process Gas Temperature-Loop Model Case 3L	8.1-73
8.1-6	Helium Temperatures MRS Loop Model	8.1-74
8.1-7	Reformer Process Gas Inlet Runback Conditions	8.1-75
8.1-8	Process Gas Temperatures MRS Loop Model	8.1-76
8.1-9	Helium Temperatures MRS Loop Model Cases 1L & 4L	8.1-77
8.1-10	Temperature Difference Across Reformer Tube	8.1-78
8.1-11	Reformer Tube Temperature MRS Loop Model Case 2L	8.1-79
8.1-12	Reformer Tube Temperature Differences at 0.6 Meters	8.1-80
8.1-13	Reformer Tube Temperature Difference at 5.6 Meters	8.1-81
8.1-14	Reformer Pigtail Temperatures MRS Loop Model Case 2L	8.1-82
8.1-15	Reformer Tube Temperatures MRS Loop Model Case 3L	8.1-83
8.1-16	Reformer Tube Temperature Difference at 0.6 Meters MRS Loop Model Case 3L	8.1-84
8.1-17	Reformer Tube Temperature Difference at 5.6 Meters MRS Loop Model Case 3L	8.1-85
8.1-18	Reformer Pigtail Temperatures MRS Loop Model Case 3L	8.1-86
8.1-19	Thermal Profile of Upper Tubesheet Region Heatup Transient (2a) at Five Hours (°F)	8.1-87
8.1-20	Process Gas Temperature Profile	8.1-88
8.1-21	Effect of Natural Circulation Flow on Peak Temperatures for Transient C-2a	8.1-89
8.1-22	Reformer Process Gas Plenum Reconfiguration	8.1-90
8.1-23	Example of Process Gas Temperatures when Incorporating A Recuperator	8.1-91

LIST OF FIGURES

		<u>Page</u>
8.1-24	Schematic of Isolating Individual Reformer Tubes to Mitigate the Effect of a Ruptured Process Gas Line	8.1-92
8.1-25	Comparison of Fatigue Results with Allowable Cycles and Previous Results for Lower Tubesheet	8.1-93
8.1-26	HTGR High Temperature Process Heat Advanced Systems Concepts Commercialization Program Major Milestones	8.1-94
8.1-27	Target Schedule for the HTGR Lead Plant	8.1-95

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LIST OF TABLES

		<u>Page</u>
4.1-1	HTGR-Systems Definitions	4.1-12
4.1-2	Market Penetration Factors	4.1-13
5.1-1	HTGRIT Program Models	5.1-15
5.1-2	Maximum Number of Each Loop Model Permitted in Loop Definition	5.1-16
5.2-1	Dynamic Fracture Toughness Values for Inconel 617 After Carburization in He-1% CH ₄	5.2-69
5.2-2	Line of Defense Description, SAD #A, LOD #1	5.2-70
5.2-3	Line of Defense Description, SAD #A, LOD #2	5.2-71
5.2-4	Line of Defense Description, SAD #A, LOD #3	5.2-72
5.2-5	Line of Defense Description, SAD #A, LOD #4	5.2-73
5.2-6	Line of Defense Description, SAD #A, LOD #5	5.2-74
5.2-7	Line of Defense Description, SAD #A51, LOD #1	5.2-75
5.2-8	Line of Defense Description, SAD #A51, LOD #2	5.2-76
5.2-9	Line of Defense Description, SAD #A51, LOD #3	5.2-77
5.2-10	Line of Defense Description, SAD #A51, LOD #4	5.2-78
5.2-11	Line of Defense Description, SAD #A51, LOD #4	5.2-79
5.2-12	Line of Defense Description, SAD #AP51, LOD #1	5.2-80
5.2-13	Line of Defense Description, SAD #AP51, LOD #2	5.2-81
5.2-14	Line of Defense Description, SAD #AP52, LOD #1	5.2-82
5.2-15	Line of Defense Description, SAD #AP52, LOD #2	5.2-83
8.1-1	Safety Measures	8.1-44
8.1-2	Representative Plant Transients	8.1-45
8.1-3	Summary of MRS-R Umbrella Service Loadings Events	8.1-48
8.1-4	Conditions for Duty Cycle Analysis	8.1-49
8.1-5	Summary of Results HTGR Thermal Analysis	8.1-50
8.1-6	Summary of Results HTGR Thermal Analysis Lower Tubesheet Temperature and Thermal Gradient Reduction Studies	8.1-51

LIST OF TABLES

		<u>Page</u>
8.1-7	Reformer Tube Primary Membrane Stress Analysis Results	8.1-52
8.1-8	Transition Weld Fatigue Evaluation, Event C-2A	8.1-53
8.1-9	Lower Tubesheet Sizing Results - Minimum Thickness to Satisfy Primary Loading	8.1-54
8.1-10	Lower Tubesheet Support Cone Analysis	8.1-55
8.1-11	Comparison of Thermal Results for Lower Tubesheet	8.1-56
8.1-12	Development Plan Main Activities	8.1-57
8.1-13	Design Cost Estimates	8.1-59
8.1-14	Development Cost Estimates	8.1-61
8.1-15	Estimated Costs by Participants	8.1-66

ABSTRACT

This report provides descriptions and results of the technical effort during the second half of FY83 on the Gas-Cooled Thermal Reactor Program. The FY83 work is organized according to the Work Breakdown Structure (WBS) for the National HTGR Program with activities in seven of these WBS tasks. The work on Integration and Management (WBS 01) includes the preparation of the Advanced Systems Concept Evaluation Plan and the Advanced System Technology Development Plan in addition to the program management activities. The evaluation plan provides the planning necessary to complete the advanced systems evaluation phase of the overall program. The concept evaluation phase also provides the basis for identifying the technology development program. The technology development plan is predicated on the advanced systems studied to date. The plan provides the overall strategy and R&D requirements to bring the advanced concept (mainly the modular steam cycle and reformer plants) to the point of initiating deployment of the commercial plants.

An assessment of the HTGR opportunities from the year 2000 through 2045 was the principal activity on the Market Definition Task (WBS 03). The study was performed as a demonstration of the market analysis modeling approach to determine the penetration and cost/benefits of the various HTGR concepts in various process heat applications. The study looked at large and small steam cycle plants and at reforming plants that provide distributed syngas and, in some cases, local steam. Due to the brevity of the study, the results are very preliminary.

Within the Plant Technology (WBS 13) task, there were activities to develop analytical methods for investigation of Coolant Transport Behavior and to define methods and criteria for High Temperature Structural Engineering design. Coding of the coolant transport behavior simulation computer program (HTGRIT) has been completed and the program is now capable of operating as a whole. However, in generating selected models, certain data required by these models are not presently available so "typical" values were inserted at the time of model coding. The effort on the high tempera-

ture structural engineering focused on (1) the application of existing pressure vessel codes to the design of gas reactor structures operating at temperatures up to 950°C, (2) a unified method for dealing with gas-metal interactions in a reactor or test circuit and the consequent structural phenomena, (3) design guidance to the materials test program and consolidation of existing test data for Inconel 617, (4) work on reformer tube fracture due to aging embrittlement and/or carburization, and (5) completion of a probabilistic framework for the determination of structural design margins. The air data base for Inconel 617 was compiled and analyzed, and average curves (i.e., best fit to experimental data) and design curves incorporating the standard 2-20 reduction on total strain range and cycles-to-failure were prepared.

The activities in support of the HTGR-SC/C Lead Plant (WBS 30 and 31) were the participation in the Lead Plant System Engineering (LPSE) effort and the plant simulation task. GE continued to provide a full time participant in the LPSE effort. The LPSE has completed the functional analysis of the four goals of the "Integrated Approach" and has prepared a preliminary Overall Plant Design Specification which implements the "Integrated Approach." The simulation task has progressed through an initial planning phase, definition of requirements, code design, development and testing of software, analysis of checkout benchmark transients and preparation of a preliminary code description/user's guide document. Selected benchmark transients have been completed which represent a broad range of plant operating conditions. The simulation code is identified as "GCRS", (Gas Cooled Reactor Simulation). The code is operational and runs interactively on a Vax 11-750 computer.

The efforts on the Advanced HTGR systems was performed under the Modular Reactor Systems (MRS) (WBS 41) to study the potential for multiple small reactors to provide lower costs, improved safety, and higher availability than the large monolithic core reactors. The existing FY82 MRS process heat plant concept studies were updated to reflect the results of recently completed preconceptual design and optimizations and trade-off studies. The principal activities on the MRS were: (1) provide the technical lead and overall coordination of the study; (2) perform a safety and licensing assessment of the concept; (3) prepare the design approach

document and plant heat balances; (4) develop a duty cycle for the reformer plant; (5) evaluate the structural adequacy of the reformer; (6) further define the control room design and operational requirements; (7) provide input on the preceeding topics for the Design and Development Plan; and (8) prepare the Integrated Design and Cost Summary with input from the other study participants.

1.0 SUMMARY

1.1 Program Management (WBS 01)

1.1.1 Advanced Concept Plans

During this period, two plans on the Advanced HTGR Program were completed; these plans are: (1) "High Temperature Gas-Cooled Reactor Program Advanced Systems Concept Evaluation Plan," and (2) "HTGR Advanced Systems Concepts Design and Development Plan." The HTGR Advanced Systems Concepts addressed by these plans consist of a modular HTGR for reforming (MRS-R) and a modular HTGR reactor system steam cycle/cogeneration MRS-SC/C plant. The MRS-R is an open-ended system which supplies synthesis gas wherever needed, and the MRS-SC/C supplies steam for industrial process heat and electricity.

The strategy selected to implement these plans is to design a common nuclear heat source (NHS) applicable to either the reformer or steam generation system. To accomplish this, the NHS is designed as the lower vessel system containing the reactor, circulator, and control elements. The lower NHS vessel assembly is designed to mate with an upper reformer vessel assembly or an upper steam generator vessel assembly.

The upper reformer vessel assembly system consists of a steam methane reformer tube bundle in series and preceeding a steam generator tube bundle housed in a steel vessel. The steam generator upper vessel assembly contains only a helical coil steam generator. This strategy for a common vessel NHS has been selected to achieve a minimum cost versatile development program.

The concept evaluation plan written during this period updates and supercedes the previous plan written by Gas Cooled Reactor Associates (GCRA 82-009), dated April 1982. The purpose of the concept evaluation plan is to outline a general plan to complete the study work necessary to further define the economic incentives and to complete the preconceptual design of

the attractive advance systems concepts in preparation for initiation of the Project Definition Phase.

The Advanced Systems Concept Evaluation Plan consists of the evaluation phase logic, an applications assessment summary, definition of the current advanced systems, future study plans, scenarios for commercialization of the advanced concepts, and plans for completing application and market studies.

The Design and Technology Development Program Plan defines the R&D requirements currently deemed necessary by the participants to support the design, procurement, and construction of a Modular Reactor System Reformer Demonstration Plant and a Modular Reactor System Steam Cycle/Cogeneration Demonstration Plant.

1.2 Applications Definition and Assessments (WBS 03)

1.2.1 Opportunities Assessment

This study was performed to provide a demonstration of the results that could be obtained through application of a Market Analysis modeling approach. This approach was used to determine the penetration and cost benefit of the HTGR for various applications. Due to the brevity of the study, the results are very preliminary. In the Years 2000 to 2045 the syngas market will expand more rapidly than the steam market as more hydrogen and syngas will be needed for chemical processes. Since syngas can be delivered long distances and distributed through a network system, large reformer plants will be built to minimize costs and plant sites will be relatively easy to locate. Subsequently, the nuclear reformer system will penetrate about 70% of the syngas market and will realize a present worth saving of about 19 billion dollars.

For the steam cycle/cogeneration systems, the need to locate the nuclear plant close to the steam users and be compatible with the market size will grossly limit the steam market penetration to about 33% for the modular system and 7.5% for the monolithic system. The corresponding cost benefits are about 12 billion dollars and 7 billion dollars, respectively.

The co-reformer system that provides both syngas and local steam will penetrate 31% of the steam market and up to 70% of the syngas market. The cost saving for the co-reformer is about 29 billion dollars.

The present worth of the cost savings for the HTGR program, therefore, is on the order of 30 billion dollars.

1.3 Plant Technology (WBS13)

1.3.1 Coolant Transport Behavior

Coding of the coolant transport behavior simulation computer program (HTGRIT) has been completed and the program is now capable of operating as a whole. However, in generating selected models, certain data required by these models are not presently available so "typical" values were inserted at the time of model coding. These models must now be tested against available experimental data to "extract" the required values. In addition, extensive testing will now be required to check the adequacy of the implemented models against existing data to see if modifications are required. Upon completion of the above tests, the program will be fully operational.

1.3.2 High Temperature Structural Engineering

The program of work established under the Joint Project Work Statement (PWS No. R1) with the Federal Republic of Germany (FRG) was continued in the second half of FY83. The detailed objectives were slightly modified as one result of a meeting between GE staff and KFA/Int̄eratom staff in Germany during April 1983 and, thus, the specific topics studies in the last six months have been:

- (1) The application of existing pressure vessel codes to the design of gas reactor structures operating at temperatures up to 950°C,
- (2) A unified method for dealing with gas metal interactions in a reactor or test circuit and the consequent structural phenomena,

- (3) Design guidance to the materials test program and consolidation of existing test data for Inconel 617,
- (4) Further work on reformer tube fracture due to aging embrittlement and/or carburization, and
- (5) Completion of a probabilistic framework for the determination of structural design margins.

A report on the applicability of existing pressure vessel codes to the design of gas reactor components was compiled to consolidate the views of engineers in the U.S. and the FRG on the limitations of existing criteria and to provide a succinct, although general, description for program management purposes of essential work to define gas reactor suitable criteria.

The exact manner by which the interaction of containment gases in helium, with each other, and the helium-exposed structures will be defined remains the subject of study, as does the manner in which structural effects of either carburization or decarburization are dealt with. However, it is necessary to consider possible design approaches and one method has been developed. In addition, the work on the structural effects of gas metal interactions has illuminated some presumable, second order effects associated with material dilation which could, with some configurations such as helically wound heat exchangers, become of importance.

Work on the topic of carburization remains speculative in nature because of an absence of certain essential physical data. In addition, the examination of such published data has revealed manipulation of this data such that some aspects of behavior are obscured by the manipulation.

Work has continued on reformer tube fracture, since it is likely to be the case that no distinction could be made, using present non-destructive testing techniques, between flaw indications and inter-crystalline carbide precipitates. In these circumstances, a possible engineering approach is to diminish permissible stresses such that for a minimum credible flaw size, leak-before-break will always result.

The probabilistic framework for determining structural design margins reflects the fact that there is no precedent to guide the stipulation of design margins. Whether or not a quantitative probabilistic approach can be finally adopted, the heuristic value of the approach is undoubted.

1.3.3 Materials Application Engineering

An effort was undertaken to compile the air data base for Inconel 617, as a precursor to the preparation of design curves and design allowables and to determine priority data needs. A preliminary analysis was then undertaken to develop Inconel 617 fatigue design curves using the available air data for continuous cycling fatigue. The analyzed data is presented in the form of average curves (i.e., best fit to experimental data) and design curves incorporating the standard 2-20 reduction on total strain range and cycles-to-failure.

1.4 HTGR-SC/C Project Management (WBS 30)

1.4.1 Lead Plant Systems Engineering

GE-ARSD continued to provide a full time participant in the Lead Plant Systems Engineering effort. The LPSE has continued to provide leadership in the application of systems engineering techniques to the HTGR Lead Project. During this period, the LPSE has completed the functional analysis of the four goals of the "Integrated Approach"; i.e.,

- Maintain Plant Operation
- Maintain Plant Protection
- Maintain Radionuclide Containment
- Maintain Emergency Preparedness

A second major accomplishment has been the issue of a preliminary Overall Plant Design Specification which implements the "Integrated Approach." This task has proven to be a difficult one due to the undeveloped nature of the "Integrated Approach". Substantial LPSE effort therefore, has been directed toward development of the "Integrated Approach."

1.5 HTGR-SC/C Plant Integration (WBS 31)

1.5.1 Plant Simulation

The simulation task for the HTGR-SC/C lead plant has progressed through an initial planning phase, definition of requirements, code design, development and testing of software, analysis of checkout benchmark transients and preparation of a preliminary code description/user's guide document. Selected benchmark transients have been completed which represent a broad range of plant operating conditions. The simulation code is identified as "GCRS", (Gas Cooled Reactor Simulation). The code is operational and runs interactively on a Vax 11-750 computer. The code is in the development stage.

1.6 Modular Reactor System (WBS 41)

1.6.1 Modular Reactor Design

During this period, the safety and licensing assessment focused on the development of a comprehensive safety assurance approach to assure the MRS safety and licensability. This approach—consists of the following elements:

- a. Safety Assurance Diagrams (SADS)
- b. Probabilistic Risk Assessment (PRA)
- c. Investment Risk and Operational Safety Assessment
- d. Design Basis Accident Definition and Safety Classification of Systems
- e. Safety Design Optimization
- f. Safety Test Planning

Work completed in FY83 consisted of initiating development of SADS and the two elements of the safety assurance approach for the reformer component.

The technique of Safety Assurance Diagrams provides a qualitative tool to identify and evaluate the effectiveness of safety assurance provisions prior to and during operations. This technique requires the identification and consideration of such parameters as:

- ° Inhabited areas and a broad definition of the function within these areas,
- ° Sources of hazardous material, type, location, level of hazard,
- ° Potential leak paths from source to inhabited areas, and
- ° Potential failure causes and modes of the hazardous sources.

For each of these causes, these types of SAD's were developed: Accident Prevention Diagrams, Accident Source Diagrams, and Accident Progression Diagrams. Further, description sheets are prepared for the identified lines-of-defense (LOD's). The LOD's are the inherent and engineered safety features that prevent or minimize the potential of an off-normal situation from progressing into a serious accident.

The evaluation of the structural adequacy of the vertical in-line (VIL) 950°C direct cycle reforming plant concept included defining the structural requirements and the plant duty cycle, the selecting of representative umbrella transients, establishing the primary circuit response under these conditions, determining the sub-component thermal response, and performing the necessary stress analysis and evaluating it against a criteria (in this case the high temperature Code Case N-47). For this evaluation it was also necessary, in certain cases, to define code "allowables," especially where Inconel 617 material was used in the design.

The analytical efforts were focused on structurally critical areas of the concept with an evaluation in sufficient detail to define potential problems that could invalidate the design. It should be noted that this analysis was scoping in nature and due to limited resources did not include an evaluation of all structurally related design concerns.

The Service Loadings (level A, B, C & D) used for these evaluations included the umbrella upset and emergency transient events, which highlight the severe service imposed upon the process components in the vertical in-line reformer concept. The events of special interest are:

1. Isolation of reformer with primary helium circulation and with decay heat removal by the main cooling system (Transient B-17a).
2. Isolation of reformer without primary helium circulation and with decay heat removal by the main cooling system (Transient B-17b).
3. Isolation and blowdown of steam generator concurrent with reformer isolation with natural circulation core cooling with decay heat removal by vessel cooling system (Transient C-2a).
4. Process gas line rupture (Transient C-7).

In order to determine the effect of these transients on the design concept, a thermal stress analysis was performed. This analysis was performed using the general finite difference heat transfer code SINDA. A two dimensional thermal model was developed of the reformer sub-components located above the reformer tubes at the tubesheet. This model provided both initial steady temperature distributions and temperature histories at the stress model element centers for the transients evaluation.

The reformer thermal analysis identified several problem areas of which the following were analyzed:

1. Upper and lower tubesheets experience large center to rim radial temperature gradients under emergency natural circulation core heat up events.
2. Flanges and nozzles experience large thermal gradients even during normal operation.
3. Reformer tubes and lower tubesheet are subjected to long duration soak periods at high temperatures under natural circulation pressurized core heat up events.

The FY82 reference design specified Inconel 617 for both the lower tubesheet and the reformer tubes. However, it was later determined that the lower tubesheet size was beyond state-of-the-art fabrication methods for Inconel 617. Therefore, this resulted in the selection of Incoloy 800H as a tubesheet material, thus requiring incorporation of a transition weld.

Based on a primary membrane and fatigue evaluation of the tubes, there appears to be adequate design margin. However, the transition weld was determined to be very marginal because of the fatigue damage caused by the thermal cycling. Several possible improvements are evident but will require additional analysis. First, the transition weld should be placed in the protection of insulation at the bottom of the tubesheet because of lower through-wall gradients expected and the cooler temperatures of the lower tubesheet. And secondly, the method of estimating the fatigue can be improved by removing some of the conservatism.

The reformer tubesheets were found to be satisfactory, with only minor adjustments, for primary loading when using Incoloy 800H reference material. However, the fatigue evaluation of both tubesheets showed they had inadequate margin or exceeded the design creep-fatigue criteria. The unsatisfactory fatigue evaluation is principally a result of large center to rim temperature differences ($\sim 390^{\circ}\text{F}$). It appeared that a redesign of the FY82 configuration was warranted. Design changes and methods investigated include reducing the tubesheet rim thickness and restraints, increasing the insulation on the exterior of the vessel at the reformer support flange, and better modeling of the natural circulation on the process gas side of the reformer tube. Initial assessment of these design features indicates that many of the tubesheet and support thermal gradients are significantly reduced.

However, further improvements are needed for the lower tubesheet and a more detailed analyses are required to resolve the design adequacy of this component in the vertical in-line concept.

The control room design and operational requirements were further defined for a phased construction approach where some modules of the plant complex are assumed to be operating and others are under construction.

A control system conceptually designed which uses microprocessor-based signal conditioning, data acquisition multiplexers, and microprocessor-based multiple controllers communicating via fiber-optic links with redundant system controllers to provide the appropriate isolation of operating

units from each other and from those under construction. The use of such a state-of-the-art control system would provide the high-speed data acquisition and control necessary for multiple reactor operation. This control system configuration will allow operation of from one to eight reactor modules from a single control complex while coordinating balance of plant operations.

2.0 Introduction

This report is the Gas-Cooled Thermal Reactor Program Semiannual Report for the period from April 1, 1983 to September 30, 1983. The work was performed under the Department of Energy (DOE) Contract DE-AC03-890ET34034, which has a contract term starting October 1, 1980. This contract is a continuation of work initiated under DOE Contract DE-AC02-77ET35202, titled Gas Cooled International Cooperative Program and which had a contract term from October 1, 1976 through September 30, 1979.

This report is organized according to the Work Breakdown Structure (WBS) used on the High Temperature Gas-Cooled Reactor (HTGR) Program. The overall WBS structure for the HTGR Program is shown in Figure 2-1, and those areas covered by this report are shown in heavy border.

The objectives of the program are:

- (1) to determine the suitability and accessibility of HTGR's to various synfuels and process heat applications;
- (2) to evaluate and provide design definition of reactor plant concepts, systems, and components that are critical to high temperature reactor operation and/or necessary for the various synfuel and process heat applications;
- (3) to determine the design methods and criteria, the design data needs, technology development requirements for the reactor and plant systems and components that are critical to synfuel and process heat applications;
- (4) to investigate the major technical and licensing issues affecting the nuclear heat source design; and
- (5) to coordinate with and maintain cognizance of other U.S. and foreign programs related to HTGR's and nuclear process heat applications.

. A summary of the key activities is provided in Section 1 and the details of the work performed and the results obtained are provided for each WBS task starting with Section 3.0.

FIGURE 2.1 HTGR Program Summary Level Work Breakdown Structure FY83

3.0 PROGRAM MANAGEMENT (WBS 01)

3.1 Advanced Concepts Plans

3.1.1 General

During this period, two plans were completed for the Advanced HTGR Concepts. The titles of these plans are: (1) "High Temperature Gas-Cooled Reactor Program Advanced Systems Concept Evaluation Plan," and (2) "HTGR Advanced Systems Concepts Design and Technology Development Plan." The HTGR Advanced Systems Concepts addressed by these plans consist of an HTGR Modular Reactor system steam methane reformer (MRS-R) and an HTGR modular reactor system steam cycle cogeneration (MRS-SC/C) plant. The MRS-R is an open-ended system which supplies synthesis gas wherever needed, and the MRS-SC/C supplies steam for industrial process heat and electricity.

The strategy of these plans is to design a common nuclear heat source (NHS) applicable to either the reformer or steam generation system. To accomplish this, the NHS is designed as the lower vessel system containing the reactor, circulator, and control elements. The lower NHS vessel assembly is designed to mate with an upper reformer vessel assembly or an upper steam generator vessel assembly.

The reformer upper vessel assembly system consists of a steam methane reformer tube bundle in series and preceeding a steam generator tube bundle. The steam generator upper vessel assembly contains only a helical coil steam generator. This strategy for a common vessel NHS has been selected to achieve a minimum cost versatile development program.

The concept evaluation plan written during this period updates and supercedes the previous plan written by Gas Cooled Reactor Associates (GCRA 82-009) dated April 1982. The purpose of the concept evaluation plan is to outline a general plan to complete the study work necessary to further define the economic incentives and to complete the preconceptual design of the Advanced Systems Concepts in preparation to entering the Project Definition Phase.

The Design and Technology Development Program Plan defines the R&D requirements currently identified by the Project participants as necessary to support the design, procurement, and construction of a Modular Reactor System Reformer Demonstration Plant and a Modular Reactor System Steam Cycle/Cogeneration Demonstration Plant. In the plan, the priority technical issues are identified, design data needs to resolve the technical issues are described, and the development activities is enumerated.

3.1.2 Advanced Systems Concepts Evaluation Plan

The Advanced Systems Concept Evaluation Plan is part of the HTGR Program currently being undertaken by the Department of Energy (DOE), utilities, process heat user groups, and industry, to realize the national benefits of the High Temperature Gas Cooled Reactor (HTGR) as the nuclear heat source for industrial process heat, steam and electricity. This HTGR Program is a continuation of related research, development, design, and demonstration work which has been supported by DOE, utilities, and reactor manufacturers since the late 1950's.

The Evaluation Phase of the advanced system program has been in effect for about three years. Although several attractive applications have been identified at this juncture of the study, additional study effort is needed to further quantify national benefits and incentives, (including plant performance and developmental needs) and to specify in greater detail the process heat system requirements. To meet general process heat requirements, the evaluation studies to date have indicated that the HTGR modular reactor system is competitive. Thus the advanced program is now being planned around the modular reactor systems reformer (MRS-R) and modular reactor system steam cycle/cogeneration (MRS-SC/C) configurations.

The specific goals of the Evaluation Phase are to:

- o Complete the application and market studies and show the incentives for the advanced systems serving the process heat market.

- o Define the requirements for the advanced systems on the process heat market characteristics.
- o Complete the preconceptual design and capital cost estimate of the advanced systems concepts.

The scope of work described by this Evaluation Plan addresses both the application, market, and commercial plant studies of the candidate advanced systems. The general scope of work and the logic formulated to conclude the Evaluation Phase are shown by Figure 3-1. The logic involves further assessment of the MRS-SC/C and MRS-R prismatic in-line systems. In addition, a study of the MRS pebble bed reactor is included to assure full coverage of system options. These studies are intended to complete the preconceptual design, resolve outstanding technical issues where possible, and to effect cost reductions commensurate with defined requirements. Parallel to this, the application studies will provide updated functional requirements and the application assessment report will identify the commercial incentives and requirements necessary to evaluate and rank the candidate systems.

Evaluation of the modular reactor systems shown on the logic diagram will culminate in a system evaluation. These systems will be judged and ranked. The evaluation will consider the state-of-the-art technology relative to the design, the ability of the system to meet the application and market requirements, and also permit a judgment of the incentives in relation to the size of the potential market. This latter consideration will determine the national benefits relative to the cost of system development.

In conjunction with the preconceptual design and application studies, the results of the on-going technology transfer and existing technology program will be factored into the commercial plant studies. In the other direction, technical issues, design data needs, and needed technology development will be identified for the systems under study, and these requirements will be applied to update the technology development program requirements.

At the completion of the Evaluation Phase Study, a decision will be rendered on whether or not to proceed to the Project Definition Phase. The criteria to judge progression to the Project Definition Phase are the following:

1. The economic incentives have been identified.
2. The advanced systems have the timely potential of meeting the application requirements.
3. There is user/industry interest as operator/owners of the system.
4. The Demonstration Plant, as supported by the technology program can be designed to meet the system requirements in the identified time frame.

3.1.3 Advanced Systems Concepts Design and Development Plane

Based on the current conceptual design status of two Modular Reactor Systems (MRS), both using a common design High Temperature Gas-Cooled Reactor (HTGR), the Design and Technology Development Plan presents the priority technical issues, which require design solutions and technical areas that require research and development before final design of the two systems can be completed. One system is the MRS-Reformer which can be used to convert methane to syngas or to support chemical processes. The other system is the MRS-Steam Cycle/Cogeneration which can be direct-coupled to supply steam for a chemical process. Both systems can be used as cogenerators by using a portion of the reactor heat to generate electricity.

Priority issues amenable to design solutions include structure, components, reactor support systems, steam systems and plant arrangements. Other issues requiring research and development programs are mainly materials characteristics at high temperatures (above 900C), performance of new types of catalyst and effects of impurities in the reactor coolant on

materials and components. Safety and licensing issues based on a benign (walk-away-safe) reactor need to be proven both by design and by demonstration.

The design plan includes the design of the reactor fuel, reactor core and reactor systems common to both MRS-Reformer and Steam Cycle plants. This also includes the design of the lower and upper reactor vessels, components, support systems and the balance-of-plant (BOP). Design data needed to complete the final design of both MRS plants include data on materials, catalyst, coolant chemistry and safety issues which must be developed.

This development plan presents estimated costs and schedules for obtaining the needed data not currently available.

Figure 3-1

EVALUATION PHASE LOGIC
FTGR ADVANCED SYSTEMS CONCEPT

4.0 APPLICATIONS DEFINITIONS AND ASSESSMENTS (WBS 03)

4.1 Opportunities Assessment

4.1.1 Trial Application of HTGR Market Analysis Methodology

The high temperature gas cooled reactor is conceived as a vital nuclear option for the future supply of process heat to industrial users to conserve the depleting fossil fuels. The use of HTGR process heat seems promising, and the on-going efforts in the HTGR development focus mainly on the design of the HTGR systems. However, a successful deployment of the HTGR depends on market requirements for which the HTGR products can be delivered efficiently and the economics relative to competing systems. An initial study was performed to assess the pertinent characteristics of the HTGR systems. A calculation model was developed to determine HTGR system cost benefits and future demand. Since this study is more a demonstration of the technique than a rigorous analysis, the results should be considered very preliminary.

Five steps are required to perform the analysis: (1) estimate future process heat market, (2) define HTGR systems, (3) estimate nuclear-fossil plant cost differentials, (4) assess HTGR market penetration, and (5) determine HTGR benefits. Fiscal Year 1983 GCRA Economic Ground Rules and the relevant plant configurations and economics for the HTGR systems reported by GCRA, Bechtel, GA, GE and other sources, were used in the analysis.

4.1.2 Process Heat Energy Market

HTGR systems can be used to supply process steam and nuclear heat for steam reforming to industrial users. Projections for industrial energy markets had been reported by Data Resource Inc. (Ref. 4.1-1), the Energy Information Administration (Ref. 4.1-2 and 4.1-3), General Energy Associates (Ref. 4.1-4 & 4.1-5) and GE (Ref. 4.1-6). For this study, the projections for process steam demand were adopted directly from the steam market

projection in Ref. 4.1-6. For the hydrogen/syngas market, however, the syngas market demand was conservatively estimated as a fraction (about 20%) of the available demand projected in Reference 4.1-6 for reforming needs in direct heating, shale oil, coal conversion and methanol/ammonia production. The projected energy demands for process steam and nuclear heat to the year 2045 are presented in Figure 4.1-1.

There are considerable uncertainties in future energy projection, but the trend is clear. There is an established demand for process steam. The steam market likely will expand about 1.6% per year into the next century, as shown in Figure 4.1-1. The projection for steam-reforming is more difficult as the future course of synfuel development in coal, oil shale, and methanol are less certain. But a conservative estimate for the syngas demand, as used in this study, still reveals a considerable hydrogen/syngas market (1.2 quads/year) at the turn of the century. The demand for syngas, however, will accelerate rapidly in the next century as more syngas will be needed to process heavier (hydrogen deficient) oils and as the demand for methanol increases.

4.1.3 HTGR System Definitions

The basic designs of the HTGR systems for this study are summarized in table 4.1-1. System descriptions and plant economics for the HTGR systems are documented in reports prepared by GCRA (Ref. 4.1-7 & 4.1-8), Bechtel (Ref. 4.1-9). The modular co-reformer system (MRS-C/R) is a relatively new concept. For the economic comparison with other systems, it was necessary to define the nuclear power split between the steam-reforming and steam generation for the co-reformer system to formulate the plant configuration.

Two methods were used to define the co-reformer plant configuration. For the oil refinery application, the amount of steam and syngas required to process oil were estimated. United States hydrogen consumption for oil refining is expected to increase from today's average of 120 cubic feet per barrel to 400 cubic feet per barrel by 2000, 700 cubic feet per barrel by 2020 and 1,500 cubic feet per barrel by 2040 (Ref. 4.1-11

through 4.1-14). No comparable trend is evident for steam usage, which will most likely remain near current levels. Currently steam generation accounts for approximately one third of an oil refinery's energy consumption or about 260 thousand BTU's per barrel of crude oil processed (Ref. 4.1-15). The energy required to produce the above hydrogen requirements has been calculated and related to refinery steam energy requirements by calculating a steam/hydrogen energy ratio. The steam-to-hydrogen energy ratio will decrease rapidly, as shown in Figure 4.1-2, when more hydrogen will be needed to process heavier oils. In a market study to estimate the available steam and syngas market for the HTGR, the energy ratio for the steam market and the syngas market will also drop rapidly, as shown in Figure 4.1-2. Assuming that the MRS-C/R is designed for a mature market in the Year 2035, about 30 years after its commercialization, the steam-to-syngas energy ratio for the MRS-C/R design will be about 1.5/1 and the corresponding reformer power split will be about 36%.

Coal and natural gas will be the prime sources of fossil fuel for process steam generation and steam-reforming, respectively. The coal-fired plants will be centralized similar to the nuclear plants such that the process steam will be piped to the local users. The economics of the large coal-fired plants, small fluidized bed coal boilers, and natural gas-fired systems are reported in Reference 4.1-8.

4.1.4 HTGR Energy Costs

Energy cost rather than product cost was the basis for this assessment study. Figure 4.1-3 shows the relative energy cost of the nuclear systems and the fossil alternatives for different size markets in the Year 2005. The steam cost of the nuclear and fossil systems for large, intermediate and small markets were derived directly from Reference 4.1-8 while energy cost for TCP, reformer and co-reformer systems were calculated based on cost inputs in Reference 4.1-9.

For the steam market, energy cost for the nuclear systems (except the TCP) is generally below \$4 per MBTU as compared to about \$7 per MBTU for the coal-fired plant. However, as the market size decreases, the

energy cost of the nuclear system will increase rapidly. For the syngas market, energy cost for nuclear steam reforming is about \$9 MBTU as compared to about \$13 per MBTU for the natural gas system.

The energy cost of the nuclear and fossil systems projected to the Year 2045 for the 2000 Mwt, 1000 Mwt, and 250 Mwt size markets are presented in Figures 4.1-3 and 4.1-4, respectively. Using the cost estimates for the year 2005 as the base, the projected energy cost for the nuclear and fossil systems were calculated based on the 1983 GCRA economic groundrules of zero escalation for capital cost and O&M cost, 1.5% for coal and natural gas, 0.5% for busbar electricity, 1.0% for non-firm electricity and 1.1% for nuclear fuel. The cost differential between the nuclear plants and the fossil systems will be used for the cost benefit calculation.

Although the thermochemical pipeline (TCP) appears to have a competitive advantage over natural gas, it does not appear to be competitive with small atmospheric fluidized bed combustors (AFB) in the initial period of HTGR process heat commercialization. If and/or when it is competitive depends on the escalation of coal prices, the costs of AFB's in small sizes, the impact of emission controls on coal plants, and the ability to serve nearby users from small central coal-fired stations in a manner that they would be served by a methanator substation. Because of the large uncertainty in the TCP competitiveness relative to coal, it was not included in this demonstration phase.

It is assumed that energy cost escalation for the HTGR systems will be small. The escalation in nuclear fuel cost will be offset by the increase of electricity revenue due to the escalated electricity price. But, the energy cost for the MRS-C/R will even decrease. As the price of natural gas will escalate faster than nuclear fuel, the higher energy credit from steam-reforming will reduce the steam cost.

As the energy cost of the nuclear options will be competitive with their fossil alternatives, the date for the commercialization of the nuclear systems will be determined mainly by the time required to develop

the systems. An early commercialization of a system will be beneficial for a better return of the investment. It was assumed in this study that the nuclear systems will be commercialized independently in the Year 1995, 1998, 2003, and 2005 for the monolithic HTGR-SC/C (mono-SC/C), MRS-SC/C, MRS-C/R, and MRS-R, respectively.

The deployment of the mono-SC/C will be limited to the large steam market as it is required to absorb the large steam output of the plant. The modular approach in the MRS-SC/C design will make it easier for the system to service the small size steam market. However, the steam cost of the small MRS-SC/C system may not be competitive with the small coal-fired plant until the Year 2020. Subsequently, the MRS-SC/C may not be able to penetrate a large share of the steam market right after the commercialization of the system.

4.1.5 HTGR Market Penetration

A systematic approach was used to estimate market penetration for the HTGR systems. Table 4.1-2 defines the market penetration factors used in the calculation. The value of the market factors, usually between zero and unity depending on market conditions, were either assigned directly from existing data or calculated based on available information. For the 1977 process steam market, for instance, 90% of steam users required a steam temperature of 500°F or lower. Therefore, the technical factor for the HTGR steam cycle/cogeneration plant of 0.9 has been used.

For the steam market, the national steam distribution pattern was assumed to be the average of the steam distribution in three regions: Public Service Electric and Gas in New Jersey, Commonwealth Edison in Illinois, and Gulf State in Texas-Louisiana. The steam market for these regions are reported in Reference 4.1-16. Process steam distribution was allowed for up to ten miles from the central plant. Total market share for steam (or syngas) which can be delivered to energy users (F_M) was estimated for each concept. The market share for the large, intermediate and small markets were further broken down for the distribution factor (F_D).

The safety factor, F_S , used to estimate the potential user sites according to the NRC population guidelines, were based on an ORNL study as reported in Reference 4.1-17. It was also assumed that the retirement and retrofit of existing fossil systems will take place linearly over 30 years after the commercialization of a nuclear system. Then, the mature market will be 100% available for competition and market penetration. The market penetration, F , for a nuclear system is, subsequently, a product of all the factors considered:

$$F = F_T \cdot F_E \cdot F_S \cdot F_{EV} \cdot F_{RE} \cdot F_{IM} \cdot F_M \cdot F_D \cdot F_{PM}$$

Figure 4.1-6 shows the overall market penetration for the HTGR systems relative to their fossil alternatives. The ability of the nuclear systems to penetrate the energy markets can be categorized into three levels reflecting that market penetration depends to a large extent on flexibility in plant location and the size of the market. For instance, the mono-SC/C can penetrate only 7.5% of the steam market. The monolithic system will be limited by its large steam output as required by PURPA and the requirement of being located within five miles of its steam users.

The modular approach in the MRS-SC/C design will allow access of the small nuclear plants to the small steam markets. Subsequently, market penetration for the MRS-SC/C will increase to about 33%.

The basic characteristics of the MRS-C/R are similar to the MRS-SC/C. The low energy cost of the system and the ability to serve even smaller steam markets, as a larger portion of the nuclear heat is used for steam reforming, will increase the steam market share for the MRS-C/R. However, this is offset by the pipeline system required to deliver the syngas. The estimated steam market penetration for the MRS-C/R is about 31%.

The advantage of the nuclear reformer system is the possibility to decouple the nuclear plant from the energy market. As syngas can be delivered through a pipeline network system, market size and market

location will not directly affect the nuclear plant design. The MRS-R can serve about 70% of the syngas market.

4.1.6 HTGR Cost Benefits

From the projections of energy demands, differential energy costs and market penetrations, future demand for HTGR and the associated cost benefits, then, can be calculated.

The accumulated cost benefits for the HTGR systems relative to their fossil alternatives are shown in Figure 4.1-7. The accumulated cost savings to the Year 2045 are presented in present worth of billions of 1983 dollars.

The accumulated cost saving for the mono-SC/C will increase gradually to about 7 billion dollars. This is close to the maximum as future gain from market expansion and differential cost increase will be small and will not add up after being adjusted to present worth. As compared to the monolithic plant, the modular steam cycle/cogeneration system will save more, particularly after the Year 2030. The saving will reach a maximum of about 12 billion dollars.

As the hydrogen/syngas market expands in the next century, the cost benefit of the MRS-R system will increase and exceed that of the MRS-SC/C in the Year 2025. The accumulated cost saving for the nuclear reformer system to the Year 2045 is about 19 billion dollars. However, the maximum saving will be higher as the syngas market will still be expanding vigorously and the cost differential will still be favorable.

As shown in Figure 4.1-7, the cost saving from MRS-C/R can exceed that of the steam/cogeneration system soon after its commercialization, despite a later deployment of the system. The co-reformer system will achieve a saving of about 29 billion dollars to the Year 2045 (164 trillion dollars in inflated 2045 dollars). If the reformer power split is increased after the Year 2025, as shown in Figure 4.1-8, to maintain the supply of syngas to a larger share of the syngas market, the cost saving

will be even higher. The cost saving from the MRS-C/R will exceed the sum of the savings from the modular reformer system and steam/cogeneration system.

4.1.7 Results

As discussed in Section 4.1-1, this study was performed mainly as a trial application to demonstrate the methodology. Although the best readily available input was used, there were also many assumptions that had to be made. The preliminary results of this study are summarized as follows:

- o The present worth of the cost saving for the HTGR application is on the order of 30 billion dollars.
- o The syngas market will expand rapidly relative to the process steam market.
- o The MRS-SC/C system can penetrate 33% of the steam market. The mono-SC/C system can penetrate 7.5% of the market. This results in a 70% higher benefit for the modular system.
- o The cost saving for the MRS-R system will exceed that for the MRS-SC/C system in the year 2025. The nuclear-reformer system can penetrate about 70% of the syngas market.
- o The cost saving for the MRS-C/R system is about 29 billion dollars. This is about equal to the sum of the saving from the MRS-SC/C and MRS-R systems. The MRS-C/R system can penetrate about 31% of the steam market and 70% of the syngas market.

4.1.8 Future Work

More work is required to refine the analysis so that more accurate results will be available for program development. In future studies, work should focus on improvement of the data base used in the study. Besides cost, the benefit analysis should include an estimation of the amount of fossil fuels that can be conserved and the associated environmental impacts of coalmining, oil drilling, etc. A sensitivity analysis also should be included to assess the impacts due to changes in energy demand, commercialization date, cost differentials, and market penetration potentials on the results of the study. Finally, the return on investment for the HTGR systems should be assessed and compared.

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TABLE 4.1-1 HTGR-SYSTEMS DEFINITIONS

	<u>Mono-</u> <u>SC/C</u>	<u>MRS-</u> <u>SC/C</u>	<u>MRS-R</u>	<u>MRS-</u> <u>TCP</u>	<u>MRS-</u> <u>C/R</u>
Core Thermal Rating, MWt	2240	300	250	250	270
Reactor Outlet Temp., °C	689	688	950	950	850
Reactor Inlet Temp., °C	319	283	425	425	350
System Pressure, psia	1025	725	725	725	600
Reformer Power Split, %	-	-	56	56	36
Steam Output, MWt	1852	217	-	-	136
Chemical Energy, MWt	-	-	142	149	98
Net Power Output, MWe	231	35	30	22	14

TABLE 4.1-2 MARKET PENETRATION FACTORS

<u>VARIABLE</u>	<u>SYMBOL</u>	<u>DESCRIPTION</u>
Technical	F_T	That fraction of the Total Demand through which the HTGR system meets the product Technical requirements.
Economic	F_E	The HTGR system has the lowest cost.
Safety	F_S	The HTGR systems meets safety and licensing requirements.
Environmental	F_{EV}	The HTGR system meets environmental requirements
Resource	F_{RE}	The necessary resources are available.
Implementation	F_{IM}	The HTGR system meets the requirements for commercial implementation,
Market	F_M	The market is available for HTGR penetration.
Distribution	F_D	The customer density within the HTGR system's service area is sufficient to warrant a system of it's size.
Potential Market	F_{PM}	Which represents customer plants that are new or retrofit since HTGR commercialization.

Figure 4.1-1 - PROJECTED AVAILABLE ENERGY DEMAND FOR HTGR

Figure 4.1-2 Nuclear Power Split for MRS-COREFORMER

Figure 4.1-3 - HTGR ENERGY COST vs PLANT SIZE

Figure 4.1-4 - HTGR ENERGY COSTS vs TIME

Figure 4.1-5 - HTGR ENERGY COSTS vs TIME

Figure 4.1-6 - HTGR MARKET PENETRATION vs TIME

Figure 4.1-7 - PRESENT WORTH OF ACCUMULATED COST BENEFITS OF HTGR

Figure 4.1-8 - MRS-C/R Syngas Market Potential

5.0 PLANT TECHNOLOGY (WBS 13)

5.1 Coolant Transport Behavior

Loop component materials are vulnerable to chemical reaction by the gaseous impurities present in the coolant stream with a resultant loss in physical properties. Component lifetimes will be a complex function of the local environment to which each component is subjected during operation and the resultant interaction with the coolant system as a whole with time. Because the reaction kinetics of a mixture of gaseous impurities are extremely complex and can potentially lead to a dynamic transport system between the core and loop components, the corrosion of component materials must be analyzed on a system-wide base to obtain a balanced set of system requirements for component design and to set test conditions to be studied to qualify component materials. See Reference 5.1-1 for more details. To meet the above system-wide analysis requirement, a computer program (HTGRIT) was designed to simulate the complex gaseous impurity reactions encountered throughout the entire coolant system and to take into account the dynamic transport effects between the core and primary loop regions. The program will serve as a major tool along with conventional corrosion tests to insure adequate component design and materials qualification.

5.1.1 HTGRIT Computer Program

Coding of the High Temperature Gas Reactor Impurity Transport computer program (HTGRIT) has been completed and the program is now capable of operating as a whole. However, in generating selected models, certain data required by these models are not presently available so that "typical" values were inserted at the time of model coding. These models must now be tested against available experimental data to "extract" the required values. In addition, extensive testing will now be required to check the adequacy of the implemented models against existing data to see if modifications are required. Upon completion of the above tests, the program will be fully operational.

The following sections provide a summary description of the HTGRIT program and the models implemented within the program.

5.1.1.1 Reference HTGR System

The HTGR primary loop reference system selected for modelling is shown in Figure 5.1-1. This system consists of a core support structure, insulated pipe (hot duct), reformer, steam generator, purification system, helium circulator and uninsulated piping (bare pipe). With this system definition, simulation of any HTGR primary loop design should be possible.

5.1.1.2 Program Organization

Program organization is structured to provide flexibility of operation. Figure 5.1-2 shows the basic program organization. The main driver program performs several functions. These are (1) control input/output, (2) initialize appropriate models per loop definition and, (3) control loop calculations. Each loop model is assigned a number which is used to specify the loop circuit configuration (loop definition). The main driver program calls the individual loop models in sequence as defined by the loop definition. The component models set (1) the local physical conditions such as temperature, pressure, flow, geometry, etc. and (2) integrate the axial change in impurity levels over the coolant flow path.

The main processes that must be considered in modeling the impurity reactions and resultant material physical changes are:

1. Impurity transport to reacting surface.
2. Non-equilibrium (flowing system) impurity oxidation/carburization potentials at the reacting surface and thermodynamic stable phases (metal oxides/carbides).

3. Kinetics of impurity decomposition and reaction.
4. Scale growth/morphology.
5. Subsurface diffusion/precipitation phenomena (carbide precipitation/decomposition, oxidation).

These processes are implemented in the program Transport, Potential, Kinetics, Scale Growth and Diffusion Models. These models are called by the individual component models as required. This concept permits the same models to be used by the many different component models, thus reducing redundancy. The gas exit conditions from one loop model provides the inlet conditions to the next loop model.

5.1.2 Program Models

The requisite program models identified for inclusion in the HTGRIT program are shown in Table 5.1-1. The maximum number of each loop model that may be specified in the program loop definition input is shown in Table 5.1-2. The models as implemented in the program are summarized below.

5.1.2.1 Impurity Sources

The Steady State Impurity Source (S.S.I.S.) Model permits specification of the steady state impurity levels for the helium stream at the start of the defined loop. As such, the S.S.I.S. Model must be specified as the first component in the program loop definition. Both the S.S.I.S. Model and the Core Support Structures Model cannot be specified in the loop definition. Instead, sufficient impurities are added to or removed from the helium stream on each pass around the loop to return them to the specified steady state values. The model tracks the weight of individual impurities added or removed with time to maintain the specified impurity levels. The non-steady State Impurity Source Model is used to inject an impurity or impurities at a specified location along the loop. The

injection rate is specified as a weight per unit time and the impurities that may be specified are H_2O , CO , CO_2 , H_2 and CH_4 . The injection may be specified as continuous, intermittent or single event. This is accomplished in the program by specifying the loop cycle at which injection is to start, the loop cycle at which injection is to stop and, if intermittent, the number of loop cycles between injection events.

5.1.2.2 Core Support Structures and Insulated Pipe (Hot Duct)

The Core/Support Structures Model is based on the Graphite Oxidation Program (GOP) developed at BNW (Ref. 5.1-2). The particular models that were used from the GOP program are those modelling the core geometry and the incore thermal and radiolytic reaction rates. These models were re-coded to fit within the framework of the HTGRIT program. The GOP graphite thermal reaction model was also used for the Insulated Pipe Model. The Insulated Pipe Model only considers the graphite liner reactions. If subsequent calculations indicate a need, the Insulated Pipe Model could be expanded to include reaction at the containment pipe surface.

Both the Core Support Structures Model and Insulated Pipe Model break the units up into axial segments or nodes and the change in impurity levels are integrated along the flow path using a numerical integration technique. During calculations, an average coolant pressure is used for the Core Support Structures Model and an average temperature and pressure for the coolant in the Insulated Pipe Model.

5.1.2.3 Steam Generator/Reformer

Because these components are of similar geometry, the same tube and shell model was implemented to handle both components. The model considers both parallel and crossflow to the tubes (coolant flow to shell side). The HTGRIT program performs no thermal calculations itself so that appropriate input data is required for the flow option selected. For crossflow, this consists of the number of baffles, temperature/pressure drop on each crossflow pass, average film drop temperature at the tube

surfaces on each pass and selected bundle geometry information. For crossflow, one calculation using average values is performed for a tube in each axial baffle segment. These values are then applied to the remaining tubes for calculating impurity changes, etc. One calculation is also performed for the baffle/shell surface for each axial baffle segment. These surfaces are considered to be at the average bulk gas stream temperature.

For parallel flow, the input consists of a normalized axial temperature, pressure and film drop temperature profile across the unit as well as selected bundle information. To accommodate the varying operating conditions (temperature, pressure, etc.) along the flow path, the model breaks the unit up into axial segments or nodes and the rate of change in impurity levels are integrated along the flow path using a numerical integration technique similar to the Core Support and Insulated Pipe Models. Again the shell inner surface is considered to be at the bulk gas temperature at each node interface.

The model implemented permits injection of one or more impurities at a node boundary (parallel flow) or baffle boundary (crossflow) within the unit to simulate a tube failure or defect. Similar to the non-Steady State Impurity Source Model, injection may be continuous, intermittent or single event and is specified as a weight per unit time. The impurities that may be specified for the Steam Generator are H_2O and H_2 while H_2O , CO , CO_2 , H_2 and CH_4 may be specified for the Reformer. To simulate this injection at some intermediate location in the unit, the model breaks the unit into two sections using the leak location as the break point. Using the inlet conditions, the impurity level changes are calculated over the first section. Then using the exit compositions from the first section and the injected values as the inlet values for the second section, the impurity level changes are calculated over the second section. This model permits considerable versatility.

5.1.2.4 Uninsulated Pipe (Bare) and Input Pipe

The Uninsulated Pipe Model consists simply of a module to integrate the impurity change over the length of the pipe if the helium temperature is sufficiently high for reactions to occur. If temperatures are too low for appreciable reactions to occur, the Inert Pipe Section Model may be used. For the Inert Pipe Section Model, the program accounts for any temperature and pressure drops to the coolant that may have been specified but performs no reaction calculations.

5.1.2.5 Helium Circulator

A pipe length of varying cross-sectional area is used to model the Helium Circulator. This approach was used to permit specification of representative flow conditions through the unit. The total inner surface area of the pipe section being set equal to that of the Helium Circulator unit being simulated. The normalized temperature and pressure profile through the unit must be specified as part of the data input. Similar to the previous component models, the change in impurity levels is integrated over the flow path. In addition, the model permits specification of an impurity injection at the inlet or exit of the model to simulate leakage of a seal. Here again the injection may be continuous, intermittent or single event. Only in-leakage of water may be specified.

5.1.2.6 Purification System

The purification system is broken into two loop models. The first model is used to specify the purification stream exit location. The input consists of the exit stream flow rate and the temperature at which the flow is measured. In addition, the removal efficiency for each impurity is specified. The second model is used to specify the purification stream re-entry location to the primary coolant stream. No additional input is required for this second model.

5.1.2.7 Kinetic Processes

In developing the models to simulate the gaseous impurity reactions and any physical changes to the reacting surface/subsurface, certain assumptions have been made. These are:

1. Thermodynamic equilibrium is not achieved between impurity species in passing around the loop.
2. All reactions are surface catalyzed.
3. The only reactions that occur are those in which only one reactant gaseous molecule participates. Solid compounds which require more than one molecule of gas for their formation are built up by diffusion or combination of simple chemisorbed complexes.

The kinetic models implemented in the program are summarized below.

5.1.2.7.1 Impurity Transport to Reactant Surface

The rate of impurity transport to the reactant surface must be considered. The rate of reaction at a surface may be expressed in terms of the rate of movement of gas to the surface or in terms of reaction at the surface. These two options can be expressed as

$$r = k_m a_m (C_b - C_s)$$

$$r = k C_s n$$

where

- a_m = reaction surface area
- r = reaction rate
- k_m = mass transfer coefficient
- k = surface reaction coefficient
- C_b = concentration of reactant in bulk stream
- C_s = concentration of reactant at surface
- n = order of reaction

If the reaction rate at the surface is sufficiently energetic that $k \gg k_m a_m$ then C will approach zero and

$$r = k_m a_m C_b ,$$

i.e., the reaction rate will be controlled by movement to the reactant surface. If, however, $k \ll k_m a_m$ then C_s will approach C_b and

$$r = k C_b^n ,$$

i.e., the kinetics of the surface reaction will control the reaction rate. With respect to our present system, the first case could occur for fresh surfaces where, for example, chromium is readily available to react. Because of the low gaseous impurity levels, the amount of chromium that can react will be controlled by the movement of the gaseous impurity from bulk stream to the surface. The second case could occur if a surface scale is formed which significantly retards the surface rate so that the rate of movement of gaseous reactant exceeds the rate of movement of reactant from the interior of the material to the reactant surface. The present program uses a somewhat similar approach to account for mass transfer effects in calculating reactant concentrations at the reacting surface. Looking at a mass balance at the reacting surface, one may write:

reactant reacted at surface = quantity of reactant reaching surface due to bulk motion of gas + quantity of gas reaching surface due to diffusion superimposed on the bulk motion rate.

The above equation cannot be solved directly. The HTGRIT solves the above equation by an iterative procedure. The reaction rates of the gaseous impurities are formulated in terms of the surface concentration of the reactants. To start the calculation, the impurity surface reaction is calculated using the bulk reactant concentration. Using this value, the above equation may be solved for the unknown surface concentrations. This value for the surface concentration is used to calculate a new value for the impurity surface reaction and so on until convergence is achieved. If the calculated value for the surface concentration is zero or less, then that means the reaction is limited by the transfer rate of reactant to the surface. The reaction rate can then be calculated from the above equation setting the surface concentration to zero. The mass transfer coefficient, diffusivities etc., which are required to evaluate the above equation for the different species are determined using standard procedures (Ref. 5.1-3 and 5.1-4).

5.1.2.7.2 Chemical Potentials/Reaction Rates

Since equilibrium is not established between the different impurity species, each reacting species (H_2O , CO , CO_2 and CH_4) must be considered for its contribution to the oxidizing or carburizing potential. Each carburizing/oxidizing species arrives at the reactant surface at some rate. Of the molecules that arrive at the surface, only a certain portion of each species will be catalyzed. The number catalyzed will be directly proportional to the number of "sites" available to catalyze that particular species and the number of sites already occupied. The actual oxidizing/

carburizing potentials established at the surfaces will then be dictated by the surface fluxes of the individual species and their decomposition rates with respect to each other. A particular compound can form if this potential is sufficiently high for its formation to be thermodynamically possible.

Based upon this framework, the models implemented in the program operate as follows. The surface catalyzed reactions considered by the program are:

Two sets of rate expressions are used to calculate the decomposition rates of these species at the reactant surface. One set is used if the reactant surface is metallic; the other if the reactant surface is scale. These rate expressions are used in conjunction with each other to obtain the resultant surface potential. This potential is then used to calculate whether oxidation/decarburization or carburization can occur. The actual rates of reaction is handled by the Scale Morphology or Diffusion Models.

5.1.2.7.3 Scale Growth/Subsurface Diffusion and Precipitation

The HTGRIT program is presently programmed only to handle Alloy 800. Figure 5.1-3 shows schematic representation of a typical scale observed to form on Alloy 800 after exposure to typical reactor levels of oxidizing impurities (Ref. 5.1-5). Under these oxidizing conditions as shown in the Figure, the scale formed is primarily composed of Cr_2O_3 . In Alloy 800, chromium is the major oxidizable constituent. The scale is

observed to grow at the oxide/gas interface indicating that chromium ions diffuse faster through the oxide than the oxygen ion. Manganese is observed to be enriched at the surface of the oxide indicating that manganese diffuses more readily in the alloy and the oxide than chromium. Titanium is observed at the oxide metal interface primarily associated with the grain boundaries (primary transport path to metal/oxide interface). In addition, titanium is also observed at the scale surface above the grain boundaries. The titanium then is observed to diffuse laterally along the oxide surface region. Islands of aluminum and silicon oxides are observed at the metal/oxide interface indicating that aluminum and silicon ions do not readily diffuse through the chromium oxide. At higher temperatures and times, the manganese present at the surface of the oxide may form a spinel. Under certain oxidizing conditions subsurface oxidation may also occur as well as carburization.

If the oxide scale formed is not protective, then the scale morphology can become even more complex. Gas can diffuse into or through the scale. As it diffuses through the scale it is reduced in oxidant and the carbon activity increase such that carbides become stable. Under carburizing conditions, the reverse can occur, i.e., oxide can form under the carbide layer.

For non-protective oxide scale, various degrees of carbide formation can occur. A carbide free zone is observed below the metal/scale interface. This is due to the depletion of chromium which has been reduced to a sufficient level by formation of the oxide that carbides are no longer stable. Below this zone, carbide precipitation is observed. Under purely carburizing conditions, of course, only carburization/carbide precipitation will occur to Alloy 800.

Because of the complex interactions that can occur in the scale and subsurface, finite difference type models were developed for the HTGRIT program to simulate these reactions. The Bongartz, Lupton and Schuster (Ref. 5.1-6) model was selected to predict the carbon diffusion and carbide precipitation/decomposition in the metal. This model permits diffusion

diffusion in the metal and the simultaneous chemical reaction with one or more metallic components. The diffusion of carbon is calculated using Fick's second law. For the present model, the diffusion coefficient D is assumed to be constant and that diffusion is only occurring in one dimension, x ; Fick's equation then becomes

$$D \frac{d^2c}{dx^2} + D \frac{z}{x} \frac{dc}{dx} = \frac{dc}{dt}$$

where

c = diffusant concentration

t = time

and Z determines the geometry of the calculation:

0 plane geometry
for $Z = 1$ cylindrical geometry
2 spherical geometry

Converting the above equation to finite difference form and assuming a two dimensional grid of space/time points, the equation can be solved as a boundary value problem. Exactly the same scheme is used to model subsurface oxidation.

For scale formation, a numerical scheme similar to that outlined in Crank (Ref. 5.1-7) for movement of a boundary was developed. In addition to considering the diffusion of chromium in the oxide, the model presently also considers manganese, aluminum and silicon. It is presently uncertain whether this complexity is required. The prime reason for considering these materials is because of their potential for altering the basic diffusion rate of chromium to and in the oxide. For example, the aluminum and silicon can impede movement of the chromium through the interface while manganese may alter the diffusion rate near the oxide/gas interface. It is known from experimentation that CO does not form a

protective film due to its simultaneous oxidation/carburization. This is modelled presently by assuming that as the oxide forms it forms with porosity which permits gas to diffuse through the oxide to the metal interface. When both carburization and oxidation can occur, the chromium is portioned between in the ratio of the carbon to oxygen flux.

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- 5.1.6 K. Bongartz, D. F. Lupton and H. Schuster, "A Model to Predict Carburization Profiles in High Temperature Alloys", AIME Metallurgical Transactions, Vol. 11A, November 1980.

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Table 5.1-1

HTGRIT Program Models

- I. Impurity Sources
 - 1. Steady State
 - 2. Non-steady State
- II. Component
 - 1. Core Support Structures
 - 2. Steam Generator/Reformer
 - 3. Pipe
 - A. Insulated (Hot Duct)
 - B. Uninsulated (Bare)
 - C. Inert
 - 4. He Circulator
 - 5. Purification
 - A. Exit Stream (Impurity Removal)
 - B. Re-entry Stream
- III. Kinetic Processes
 - 1. Impurity Transport to Surface
 - 2. Impurity Surface Chemical Potential
 - 3. Impurity Decomposition/Reaction
 - 4. Scale Growth
 - 5. Subsurface Diffusion/Precipitation (or Decomposition)

Table 5.1-2

Maximum Number of Each Loop
Model Permitted in Loop Definition

<u>Loop Model</u>	<u>Number Permitted</u>
I. Impurity Source	
A. Steady State	1
B. Non-Steady	4
II. Component	
A. Core/Support Structures	1
B. Reformer	1
C. Steam Generator	1
D. Piping	
1. Insulated (Hot Duct)	1
2. Uninsulated (Base)	6
3. Inert	6
E. He Circulator	1
II Purification System	
A. Exit Stream Location	1
B. Re-entry Stream Location	1

FIGURE 5.1-1 SCHEMATIC DIAGRAM OF HTGR PRIMARY LOOP

83-443-01

Figure 5.1-2 HTGRIT PROGRAM ORGANIZATION

83-443-02

Figure 5.1-3 TYPICAL SCALE FORMATION ON ALLOY 800 AFTER EXTENDED EXPOSURE TO TYPICAL REACTOR LEVELS OF IMPURITIES

5.2 Structural Analysis Engineering

5.2.1 Introduction

The work accomplished during the second half of FY83 falls into five somewhat independent categories. These are:

1. A review of the application of existing pressure vessel codes to gas reactor components operating at temperatures to 950°C. The object of this work was to define the limitations of existing codes and to offer a general appreciation of necessary developments (Section 5.2.2).
2. Definition of a method by which to continue the load, temperature and gas-metal interaction influences on the behavior of gas reactor components. This work was necessary in order to provide a background to open questions on the description of environmentally induced material property changes and induced stresses (Section 5.2.3).
3. Design guidance to the materials test program and compilation of a basic test data base for Inconel 617. Work was undertaken to provide a physical test guide to data needed for further work on environmental effects and mechanical material properties (Section 5.2.4).
4. Reformer fracture studies. Studies were undertaken to illuminate possible categorization and magnitudes of stresses to assure leak-before-break flaw behavior (Section 5.2.5).
5. Structural design margin probabilistic decision framework. This work has been undertaken to provide some logical, and ultimately quantitative, background to the definition of design margins, since a satisfactory basis from operating equipment is not available (Section 5.2.6).

5.2.2 Pressure Vessel Design Codes: Their Applicability to HTGR Components Above 800°C

5.2.2.1 Pressure Vessel Code Policies

All pressure vessel codes are either based on a policy of "design by rules" or a policy of "design by analysis." Compliance with the rules is sufficient for safe design with codes of the "design by rules" type. A formal structural evaluation of critical vessel regions and loadings, for comparison with structural criteria, is the basis of safety in "design by analysis" codes.

The regulations and requirements to be observed in "design by rules" concern geometrical arrangements, dimensions, loading conditions and materials. Estimation of the necessary wall thickness is obtained from elementary calculations which relate a permitted stress at temperature, the principal vessel dimensions and the pressure loading. The regulations and requirements represent a synthesis of satisfactory vessel design and operation. For this reason, they may not be applied to vessels intended for operation at temperatures and in geometrical forms under load conditions which differ from those from which the rules were made.

Codes based on a policy of "design by analysis" enable vessel design to proceed for complex loading conditions or geometric arrangements or both. In such codes, specific criteria which define the permissible stress, strain or displacement are stated for both elastic and inelastic behavior. Specification of a criterion for either stress, strain or displacement uniquely specifies the remaining two quantities for the case of elastic vessel response. This cannot be so for inelastic behavior, and stress, strain and sometimes displacement, each require separate criteria. The structural response to load and temperature for comparison with criterion may be obtained from experiment but is usually the result of calculation.

5.2.2.2 Limits of Code Applicability

The principal pressure vessel codes in use in the U.S. and the Federal Republic of Germany (FRG) are the following:

- FRG - AD - Instructions (Ref. 5.2-1)
- FRG - TRD - Regulations (Ref. 5.2-2)
- FRG - KTA - Rules, Primary Circuit Components of Light Water Reactors, 3201 (Ref. 5.2-3)
- USA - ASME Code, Section III (Ref. 5.2-4)
- USA - ASME Code, Section VIII (Ref. 5.2-5)
- USA - ASME Code, Case N47-17 (1592-17) Class 1 Components in Elevated Temperature Service, Section III, Division 1 (Ref. 5.2-6)

The AD-Instructions, ASME Code, Section VIII and TRD-Regulations are based on the policy of "design by rules." They are intended to govern the design and construction of conventional boilers and pressure vessels. The design restrictions imposed by such codes in conjunction with the need for a more specific evaluation of safety margin have resulted in the evolution of "design by analysis" codes for nuclear quality pressure vessels and reactor structures. In the FRG, the KTA-Rules based substantially on ASME Code, Section VIII are in force. The design of components to 800°C is governed by ASME Code Case N-47 in the US, since the applicability of ASME Code, Section III is limited to temperatures to about 400°C as are the KTA-Rules.

The limits of applicability of these codes may be summarized as follows:

- ° AD-Instructions, TRD - Regulations and ASME Code, Section VIII provide means by which to calculate vessel wall thicknesses for pressure loading, at temperature, for the required service life. The allowable stresses are specified based on both time independent and time dependent materials behavior. Time dependent allowable stresses are related to both a creep rate and total

service life creep strain to offer protection against failure by creep rupture, but other elevated temperature structural phenomenon such as creep-fatigue interaction, creep ratcheting and creep caused enhancement of geometric imperfections are not explicitly covered.

- ° KTA-Rules and the ASME Code, Section III permit only an evaluation of structural response to ensure freedom from time dependent failure modes because of the specified and limited temperature range at which they apply. They require first an evaluation of the structural response to primary loads which could cause bursting or unacceptable deformation, followed by an evaluation of the magnitude and frequency of primary and secondary (self limiting) stresses for comparison with fatigue criteria.
- ° While stress or strain due to external forces, the self-weight, and pressure sustained by a component are covered similarly to the procedures of References 5.2-1 and 5.2-2, elastically calculated stresses, due to all types of loading, for the fatigue evaluation are permitted to vary over a range of magnitude equal to twice the yield stress (3 Sm-criterion). Additionally, criterion must be met to ensure that plastic deformation sustained from strain cycling in the permitted range does not impair integrity or function ("shakedown"). Loads from other than conditions of normal operation are classified by frequency of occurrence and successively greater primary stress limits are permitted as the loads become less probable.
- ° ASME Code, Case N-47 includes criteria for "design by analysis" in the time dependent failure temperature range. These criteria were developed through a number of preceding code cases from the fundamental elevated temperature considerations of ASME Code, Section VIII. The limit of elastic (linear) approximation to non-linear creep behavior is defined in ASME Code, Case N-47 by restricting to about the 0.2% yield stress, any cycle composed of elastically calculated load and deformation (thermal) stresses.

If this restriction cannot be observed, inelastic analysis is required to ensure that stress, and cyclic and cumulative strains, meet separate inelastic criteria. A definition of secondary stress in the sense of the ASME-Code(s) is not applicable in such circumstances and stresses are instead defined as load or deformation controlled. The upper temperature limit to the use of a material is governed by creep rupture behavior for which a satisfactory definition requires data which permits well described scatter bands.

5.2.2.3 Structural Design Considerations for HTGR Components

The design of components for the high temperature regions of an HTGR plant requires consideration of both time independent and time dependent material properties, the latter characterized by irrecoverable strain deformation which increase with time. By a process of selection through a formal description of material behavior, and the imposition of geometric and fabrication limits in an elevated temperature code, conditions may be arranged such that the list of possible failure modes is restricted. Criteria can then be devised to ensure a remote probability of failure in any mode. For pressure vessel structures the failure mode list should include

- creep rupture due to long term loading
- fatigue failure
- fast fracture (failure due to unstable crack growth)
- excessive strain due to incremental deformation (cyclic strain accumulation due to creep or plasticity or both)
- loss of function due to excessive deformation
- loss of stability due to short term loading (elastic or elastic-plastic buckling)
- loss of stability due to long term loading (creep enhancement of initial geometric imperfection)
- environmentally caused material failure (carburization, corrosion, irradiation, etc.)

Quantitative criteria, observance of which will avoid failure in seven of these nine modes are stated in ASME Code, Case N-47. Failures due to either ductile fracture or creep rupture are avoided by the definition of load controlled stress limited. Loss of stability due to both short and long term loading is avoided by stipulations such that working loads are substantially less than divergence loads. Calculation of the cumulative strain for comparison with permitted values is the policy used to avoid failure due to creep-fatigue interaction, excessive strain accumulation from incremental deformation and loss of function because of large deformation. These criteria may indeed be suitable for gas reactor components operating at very high temperatures, but a number of aspects associated with these criteria require critical investigation.

The effects of service environments on the behavior of materials and the failure modes of components are not satisfactorily covered in any of the available design codes.

5.2.2.4 Aspects of an HTGR Pressure Vessel Design Code

An HTGR pressure vessel design code may very well be based on the general policies of the ASME Code, Case N-47. A general level of security should be obtained by placing limits on the magnitude of load controlled stress and strain and a specific security by further stress and strain limits for the combination of load and displacement controlled strains. The security necessary for prolonged gas reactor operation may also require fracture mechanics based criteria, environmental effects criteria and, until sufficient experience accrues, periodic testing of material test coupons and component sections which have been exposed to reactor operation.

For the drafting of a design code, the following must be met:

1. Material data sufficient to permit a satisfactory calculation of the force distribution in a component must be available.

2. Material data to permit evaluation of force distribution in terms of failure modes is required.
3. Design margins which offer protection against approximations and uncertainties must be determined for criteria of stress, strain, load and time.
4. The nature and extent of periodic component inspection and possible testing to ensure structural reliability must be considered.

The data required for the materials of HTGR components to permit calculation of force distribution and force effects in the application temperature range include:

- ° Physical properties data, such as
 - density
 - thermal conductivity
 - specific heat
 - thermal expansion coefficient
 - modulus of elasticity
 - Poisson's ratio.
- ° Data for constitutive equations (strain-time laws) to permit inelastic analysis such as:
 - form of the equations
 - constants and coefficients
 - environmental effects.

The data required to assess the significance of force distribution and force effects to stress, strain and displacement for the various failure modes in the application temperature range must include:

- Impact Strength
- Yield Strength
- Maximum Tensile Strength
- Fracture Elongation and Reduction of Area
- Creep Strain Rates

- Creep Rupture Elongation
- Fatigue Parameters.

The structural engineering problems of the sodium-cooled reactor and the high temperature behavior of low alloy steel and austenitic stainless steel have been major influences in the drafting of ASME Code, Case N-47. The use of nickel based alloys for components in a gas reactor environment at temperatures above 800°C however, presents somewhat different engineering problems and materials behavior. Thus a number of questions arise for which resolution must be found within the general ASME Code, Case N-47 policy of "design by analysis" and the description of stress as load controlled or deformation controlled.

A failure mode entitled "environmentally caused material failure" has been introduced. The design measures to avoid this failure mode include:

- selection of materials
- thickness allowance
- surface protection
- consideration of the changes in material properties.

Use of any one or combination of these measures required clarification of the following questions:

- Which environmental parameters should be used to describe the process of material property changes which lead to environmentally caused failure or accelerated failure in other modes?
- How should material property alterations and environmentally structural effects be specified and presented?
- What should be the basis for placing limits on environmental effects?

- In what form should limits to permissible environmental effects be embodied in design criteria?

A decision to adopt the ASME Code, Case N-47 policy for the limitation of primary stresses in gas reactor components operating at temperatures above 800°C requires resolution of numerous questions which include the following:

- With what strain rate are the time independent strength properties to be determined?
- Which cross-section factor K is sufficient for restriction of the primary bending stress in time dependent materials behavior?
- Is the stated equation for accumulation of creep damage appropriate for the various operating conditions of a gas reactor?
- Is it necessary to specify specific stress limits for welds which differ from those applicable in all regions away from welds?

Similarly, a number of questions arise when considering the ASME Code, Case N-47 philosophy with respect to the accumulation of component material damage due to creep-fatigue:

- Which strain rate and hold time should be used in determining cyclic strength properties?
- Is the criterion for evaluation of creep-fatigue damage also suitable for HTGR components?
- What significance have the strain limits criteria and are the allowable values acceptable?

- Does it remain an acceptable procedure to dispense with an evaluation of creep-fatigue damage against regions specific criteria for weldments?

To provide a sound and thorough basis for the evaluation of creep-fatigue damage using inelastic calculations, resolution of the following types of questions must be achieved:

- What manner of tests are practicable and necessary for determining the constants and coefficients of constitutive equations?
- In what form should the qualification and verification of computation methods for inelastic analysis be cast?

The definition of criteria to avoid the failure mode of unstable crack growth at temperatures below 800°C because of material changes encountered at temperatures above 800°C will be achieved only by answers to the following questions:

- What techniques and constants should be used to characterize toughness alterations and crack growth properties?
- In view of inspection difficulties, especially of weldments, should fast fracture criteria be formed in terms of fracture mechanics techniques or a reduction in load and deformation controlled stress and strain criteria, in combination with certain geometric criteria?

There have been technical arguments to the effect that ASME Code, Case N-47 incorporates criteria for creep enhancement of geometric imperfections which are too conservative for load to compensate for lack of margin on time. This situation requires answers to the following type of question.

- Should there be criteria and margins for time in addition to, or supplementing, direct criteria and margins for load?

The intention of a design code is to offer criteria such that for the factors under design control, the probability of failure is remote. Uncertainties in defining criteria and uncertainties and approximations in the evaluation of a design to meet these same criteria, require the definition of design margins. The factors and effects covered by ASME Code, Case N-47 design margins are not well defined. In addition there is little useful background of operating vessel experience above 800°C by which to justify these design margins. Thus the following questions arise:

- How should a design specification and an HTGR structural code be formally linked from the viewpoint of acceptable failure rates?
- To define specific and quantitative meaning to criteria which embody design margins what is the best approach and what theoretical and physical data base development are required?
- Which components must be tested to validate criteria and design margins?
- What possibilities for testing are practicable?
- How are the results of physical testing to be used in comparison with design calculations to verify that satisfactory criteria and design margins have been provided?
- Which components when in service require periodic inspection to provide final code verification and information for improvements?
- Which in-service inspection possibilities are practicable and how is the inspection information to be used to verify or modify criteria and margins?

5.2.3 Design of Carburizing Gas Reactor Structures

5.2.3.1 Summary

The objective of the work described in this section is to define methods, procedures and criteria which will permit the satisfactory design of very high temperature gas reactor structures. The two factors which distinguish the behavior of such structures from other nuclear structures are those of temperature and environment, which cannot easily be separated.

5.2.3.2 Structural Model

The configuration chosen for the structural model is the three-member pin jointed plane frame illustrated in Figure 5.2-1. This model is sufficiently simple that significant problems and difficulties are not obscured by detail. The model is however representative of the complexity of a nuclear reactor structure since it is redundant. One characteristic of a redundant structure significant to this analysis is that the internal force distribution cannot be ascertained solely from consideration of equilibrium. Also, each member of the model may fail in any one of several failure modes such as ductile rupture, brittle fracture, creep rupture or creep buckling. Because the structure is pin jointed, the three members directly sustain only axial forces (no shear, no bending) so that direct use might be made of conventional material test data obtained from the uni-axial loading of cylindrical test specimens.

5.2.3.3 Model Description

The model comprises three cylindrical bars or tubes, AB, CB and DB. These members are connected by frictionless pin joints to a rigid structure at A, C & D and are mutually joined by another frictionless pin at B. The structure is exposed to a carburizing gas atmosphere throughout the design lifetime and is subject to a load P which may vary in both magnitude and duration. We specify a displacement at point B of vital significance to safety.

5.2.3.4 Carbon Uptake, Members AB, CB, and DB

The procedure for establishing the carbon uptake in the members AB, CB and DB requires the following sequence:

- a) The exact composition of the gas mixture flowing toward AB is defined as are all significant gas flow parameters.
- b) The equations governing the reactions within the gas mixture and the reactions between the gas mixture and the surface of member AB are assembled.
- c) The necessary test data are assembled to define the reaction rates for the three conditions of: 1) a surface unsaturated with respect to carbon, 2) the stable condition in which the intrinsic reaction rate applies, and 3) the conditions at which the reaction rate is governed by diffusion.
- d) The preceding description of the gas-metal interaction process is used to establish the surface carbon flux in a time interval sufficiently short that no sensible alteration occurs in the nature of the gas-metal interaction defined for the interval.
- e) Standard diffusion theory, modified to account for both granular and intergranular carbide precipitation is used to determine the carbon profile increment for the defined interval.
- f) The altered gas composition flowing toward member CB is the basis of a repetition of steps a) through e) for member CB in the selected interval, as is the further altered gas mixture flowing toward DB, the basis for the same steps for that member.

A logic diagram for these steps is provided in Figure 5.2-3.

5.2.3.5 Distribution of Forces

The form of structure shown on Figure 5.2-1 permits the most direct of the possible approaches to the solution of the force distribution within a structure. At a time closely following the commencement of service, it is possible to easily write down the following conditions:

- g) Conditions of internal equilibrium
- h) Conditions of external equilibrium
- i) Conditions for displacement compatibility of the three members at points A, B, C and D.

For elastic action only of the structure, it is a straightforward task to define the unique stress-strain relationships which satisfy conditions g) to i) and have thus obtained the force distribution within the structure. However, at the temperatures at which creep is a factor and at which carburization takes place, both the conditions of internal equilibrium and of displacement compatibility alter with time.

5.2.3.6 Redistribution of Forces Due to Creep

It is assumed that the rate of change of strain with time may be written as $\dot{\epsilon} = K\sigma^n$ or some other power function. The elastic stress-strain relationships which had satisfied conditions g) through i) must now be modified to incorporate time-dependent effects. For each member, the total strain at any time t is written

$$\epsilon = \epsilon_{\text{elastic}} + \epsilon_{\text{creep}}.$$

The service period is divided into a number of intervals t , such that the internal creep strain in any of the members is small. The internal force distribution at the beginning of a time interval t may then be used to

compute ϵ_{creep} for that period. The conditions of internal equilibrium and displacement compatibility are then redefined for the calculations of the succeeding interval.

Since no assumptions of small displacement or pseudo-elastic behavior have been made, the described method of analysis permits an accurate knowledge of force redistribution due to creep and only required that t be selected such as ϵ_{creep} in the interval t is small.

5.2.3.7 Carburation Effects on Creep Induced Force Redistribution

The effect of carbon uptake on the members of the structure depicted in Figure 5.2-1 is to alter both the physical and mechanical properties of these members. One physical effect is caused by the formation of carbides of a lower density than that of the parent material. A dilation of the material of the members ensues. By consideration of two initially identical cubes, one of which has subsequently dilated due to carburization, the other which occupies the same volume solely due to an expansion caused ΔT , one may deduce a convenient temperature analog for carburization dilation.

Also stipulated is a unique creep strain rate for each level of carburization $c\%$ in the form, $\dot{\epsilon}_{c\%} = k_{c\%} \sigma^{n_{c\%}}$ and a damage law for each level of carburization,

$$\int \frac{\dot{\epsilon}_{c\%}}{\epsilon_{c\%}} dt = 1.0$$

or some other appropriate damage factor; in the latter equality $d_{c\%}$ is the creep strain to rupture.

Now consider the state of strain in the structure as it carburizes. The factors contributing to a definition of the creep and elastic strains at any time t are as follows:

1. The external forces.
2. The internal forces due to any initial lack of fit or to unequal temperature expansion of the members.
3. Internal forces in the members resulting from the carbon concentration profile.
4. Internal forces due to constraint among the members of the free-body axial extension of the members due to carburization.

See Figure 5.2-2 for an illustration of these cumulative effects.

For the purpose of brevity of explanation the situation in which an established carbon gradient alters its profile without the further addition of carbon is ignored.

The steps necessary for the evaluation of the load distribution within the structure throughout the service life are now stated.

- 1) The service is to be divided into discrete intervals. The carbon profile for the first interval is ascertained by the methods described in Section 5.2.3.4 of this paper. By direct means or by use of the temperature analog devised earlier, the elastic stresses due to self-constraint of a member are then determined using the variation of material properties according to the carbon gradient.
- 2) For the same interval, using carburized material properties, the elastic stress distribution is determined from carburization-induced member axial extension constraint by other members, lack of fit, unequal thermal expansion, and the external loading, invoking the three conditions noted in the Section 5.2.3.5 on Distribution of Forces.

- 3) The average elastic stress in the period of interval is employed to calculate the creep strain produced in each member, the relaxation of carburization and constraint stresses and the redistribution of the externally imposed forces.
- 4) The same three steps are undertaken for the succeeding time interval for which the initial conditions would now include carbon gradient definition from the preceding step, cumulative creep strain, residual elastic strain and an altered geometry.

The state of strain and the internal force distribution, determined by the preceding steps for any time during the operation of the structure, may be used to examine the possibility of structural failure. Also the damage law defined earlier may be exercised as damage accumulates to follow the radial crack development in each member. Appropriate periodic adjustments to the stiffness definition for each member would form the basis for a description of total structure strength/stiffness degradation and a fail-safe evaluation.

5.2.3.8 Evaluation of Failure

Again, for brevity of description, such obvious failure modes as elastic buckling, compressive load induced creep deformation (so called "creep buckling") and others are excluded, and attention is continued to the following failure modes which directly involve rupture of a structural member:

- Ductile Rupture from short-term loading
- Creep Rupture from long-term loading
- Creep-Fatigue Failure leading to gross rupture
- Brittle Fracture.

Many of the candidate alloys for use in very high temperature gas reactor structures display a marked preference for the formation of intergranular carbides when exposed to carburizing environments. It has been noticed that in standard tensile tests failure occurs because of cracking through the intergranular carbide layer, a critical carbide between the grains of the parent metal. The permissible depth of critical carburization is stipulated by the engineer and is regarded as an expendable depth, analogous to the well known corrosion allowance.

It is suggested that the procedure for evaluation of structural integrity should take the following form for the failure modes listed in the preceding. A diagram indicating the essential steps is provided on Figure 5.2-4.

To prevent ductile rupture, a member is to be sized using the standard criteria for primary general membrane stress, S , and for local membrane stress (not a factor for the model selected) of $1.5S$. To the so established dimensions is added an amount, at the discretion of the designer, which is reserved for the critically carburized zone. The values used include examination of both thermochemical and mechanical stability of such barriers.

5.2.3.9 Conclusions

The principal conclusion is that the phenomenon of carburization affects not only local material properties but also the global distribution and redistribution of forces within a structure. It is also concluded that a practicable design procedure can be evolved for carburizing structures and one such procedure has been outlined.

5.2.4 Design Guidance to the Material Test Program

5.2.4.1 Summary

The most promising material for use in reformer tubes and other critical components of the reforming application gas reactor in the near

term is Inconel 617. A decision to coordinate US/FRG structural developments around this material was made in April 1983. This decision does not eliminate the need for screening tests or other promising alloys, but since screening tests can only indicate the desirability of pursuing further test and development activities, by definition there is an insufficient data base for structural development purposes on the advanced alloys.

A study was made in 1983 of the derived data on Inconel 617 for structural methods development, which is reported in this Section. An exercise to establish which of the required data exists in some form, to make clear for planning purposes data to be obtained has resulted in the compilation of an Inconel 617 data base represented in Section 5.3.

5.2.4.2 Structural Analysis, Failure Modes and Basic Data

Personnel working on this task in the U.S. and the FRG have agreed on the following list of failure modes against which code criteria will offer a low probability of occurrence.

- 1) Ductile fracture or excessive deformation from short term loads
- 2) Creep rupture from long term loads
- 3) Creep-fatigue due to cyclic loads
- 4) Loss of integrity (buckling) due to short term loads
- 5) Loss of function due to excessive deformation
- 6) Loss of integrity (buckling) due to excessive creep deformation
- 7) Brittle fracture from short term loads
- 8) Creep Ratcheting

9) Environmentally Induced Failure

For the case where there are no environmental effects, (Mode 9, Environmentally Induced Failure, is excluded), the load distribution within a component with temperature, loading and time, and the corresponding failure modes can be approximately ascertained using the following information (uni-axial):

- (a) Short term tensile and compressive load displacement curves as a function of temperature and strain rate,
- (b) Short term shear displacement curves as a function of temperature and strain rate,
- (c) Thermal expansion coefficient as a function of temperature curves,
- (d) Conductivity as a function of temperature curves,
- (e) Emissivity as a function of temperature curves,
- (f) Tensile and Compressive stress and time to rupture as a function of temperature curves,
- (g) Strain dependence on tensile or compressive stress curves as a function of temperature and time,
- (h) High-cycle stress and cycles to-failure data,
- (i) Low-cycle stress and cycles-to-failure data,
- (j) Critical stress-intensity factors as a function of temperature (specific test not defined but tests which will allow determination of K_{IC}), plane stress, and plane strain, and
- (k) Critical stress-intensity factors as a function of time (same remarks).

Environmental effects, whether oxidizing or reducing or both, can, for the purpose of structural criteria evaluation, be regarded as uptake or loss of carbon. To place limits on the permissible uptake or loss of carbon, the following data is needed (this data will also place a perspective on past test results):

- l) The dilation of material as a function of temperature and homogeneous carbon uptake,
- m) Material carbon content (bulk quantity and gradient) as a function of surface flux and time,
- n) Short-term tensile and compressive load displacement curves as a function of temperature and strain rate for various homogeneous carbon contents,
- o) Thermal expansion coefficient as a function of temperature and various homogeneous carbon contents,
- p) Tensile and compressive stresses and time-to-rupture as a function of temperature for various homogeneous carbon contents,
- q) Strain dependence on tensile stress and compressive stress as a function of homogeneous carbon content, temperature and time,
- r) Critical stress intensity factors as a function of carbon content and temperature,
- s) Short-term tensile load displacement curves as a function of grain size and temperature for specification material,
- t) Time to rupture data as a function of stress, temperature and grain size for the specification material,

- u) Strain dependence on tensile and compressive stress as a function of temperature time and grain size for the specification material, and
- v) Critical stress intensity factors as a function of grain size and temperature for the specification material.

The sequence to be followed is as follows:

The problem of multi-axial stress states and the required test data will be investigated subsequent to a satisfactory evolution of methods and procedures for handling uni-axial data and will be postponed until FY1985.

5.2.4.3 Desired Data, Inconel 617

5.2.4.3.1 Short-term Tensile and Compressive Load-Displacement Curves

This series of tests is required to enable short-term primary allowables to be set.

Test Series 1

The curve above has been drawn for one grain size. The tests are to be repeated for a minimum of two other grain sizes and at temperatures at 50°C intervals in the range 20°C-600°C and at 20°C intervals in the range 600°C-1000°C.

Test Series 2

It is necessary to also obtain the short-term load displacement curves for several levels of carbon. The tests are to be repeated for the selected structural grain size at (0.02, 0.04, 0.06, 0.10, 0.20, 0.30)% homogeneous carbon content for a number of specified strain rates and at temperature intervals of 50°C in the range 20°C-600°C and 20°C intervals in the range 600°C to 1000°C.

5.2.4.3.2 Creep-rupture Data

This series of tests is required to enable long-term primary and secondary stress level proposals to be examined.

Test Series 3

These tests are required for a duration to at least 5000 hours. Data points sufficient to permit a reasonably satisfactory extrapolation are required at 50°C intervals in the range 600°C to 800°C and at 25°C intervals in the range 800°C to 1000°C. The tests must cover the range of grain sizes stipulated for Test Series 2.

Test Series 4

For the selected grain size, the effect of carbon content on creep rupture strength is to be obtained from time-to-rupture data obtained in a stagnant helium atmosphere of homogeneous carbon contents of (0.02, 0.04, 0.10, 0.20, 0.30)% at the intervals in the temperature range specified in Test Series 3.

5.2.4.3.3 Creep Stress-Strain Rate Dependence

This series of tests is required to begin formulation of the constitutive equations, as another basis for setting primary and secondary creep stress allowables and to provide essential environmental influence data.

Test Series 5

These tests are required for a duration (5000 hours) to establish the dependence of strain and strain rate on temperature and stress. They are to cover three grain sizes for a range of stress to be defined and at temperature intervals of 50°C from 600°C to 800°C and at 25°C intervals for the range 800°C to 1000°C.

Test Series 6

For the selected grain size, the effect of carbon content on stress-strain and strain rate is to be obtained in a stagnant helium atmosphere at homogeneous carbon contents of (0.02, 0.04, 0.10, 0.20, 0.30)% at the intervals stipulated for the temperature ranges stated under Test Series 5.

5.2.4.3.4 Fracture-Mechanics Data (Stress Intensity), Temperature Dependence

This data is needed to enable design methods, design features and code criteria to be formed to deal with poor fracture toughness characteristics. Charpy data already indicates a severe problem. It is intended to first attempt quantify the problem through linear elastic fracture mechanics.

Test Series 7

Data points sufficient to enable the variation of K_{1C} with temperature are to be obtained for a range of pre-test exposure times and temperatures which will be defined by the structure/design staff. This data is to be compiled for three grain sizes.

Test Series 8

The same K_{1C} data as a function of temperature is to be obtained, for the selected grain size, at homogeneous carbon contents of 0.02, 0.04, 0.10, 0.20, 0.30. The pre-test exposure times and temperatures will be defined by the structures/design staff.

5.2.4.3.5 Short-term Shear Displacement Curves

This data is needed to derive the shear constants and is the vital complement to the data obtained in the short-term tensile and compressive load displacement tests.

Test Series 8

These tests are to be performed for the selected grain size at homogeneous carbon levels of (0.02, 0.04, 0.06, 0.10, 0.20, 0.30)% for the specified strain rates and at temperature intervals of 50°C in the range 20°C-600°C and 20°C intervals in the range 600°C to 1000°C.

5.2.4.3.6 Fracture Mechanics Data, Time Dependent

This data is required to ensure that the true minimum value of fracture toughness coefficient with time has been obtained.

The linear elastic fracture mechanics coefficient K_{1C} is to be obtained as this coefficient varies with exposure time for the selected grain size and for the exposure elevated temperature or temperatures which have been shown to result in the poorest fracture toughness properties in the range 20°C to 600°C. The effect of carbon at (0.10, 0.20 and 0.30)% homogeneously distributed is to be obtained for the same conditions.

5.2.4.3.7 High Cycle Fatigue Data

This data is required for development of high cycle fatigue criteria and as a guide to the specification of low cycle fatigue tests.

Test Series 13

Conventional σ - N data is to be obtained at frequencies to the point that creep because of the test duration has become significant. Data is required at 50°C intervals in the range 20°C-600°C and at 25°C intervals in the range 600°C to 1000°C. Test data are also to be obtained for the same conditions at homogeneous carbon contents of (0.04, 0.10, 0.20, 0.30)%. These tests are all to utilize material of the selected grain size.

5.2.4.3.8 Low Cycle Fatigue Data

This data is required to develop criteria for creep-fatigue interaction. Since an adequate basis for creep and environmental structural degradation has not been developed, an exact definition of this work remains to be developed in 1984.

Test Series 14

An explanatory series of tests to be performed at 50°C intervals in the range 600°C to 1000°C at values of hold stress equal to 0.25, 0.5 and 0.75 times the long-term creep stress allowable developed from previous data. The cyclic stress amplitude, frequency and duration remain to be specified.

These tests are to be repeated at homogeneous carbon contents of (0.04, 0.10, 0.20, 0.30)% and all tests must utilize material of the selected grain size.

5.2.4.3.9 Carburization Dilation Data

This data is needed to assist in understanding the affect of dilation on all carburized component properties but especially on creep properties at low values of applied stress or strain.

Test Series 9

The dilation of material (volumetric growth) at homogeneous levels of carbon is to be obtained at 50°C intervals in the temperature range 20°C to 600°C and 25°C intervals in the temperature range 600°C to 1000°C. The tests will be based on the selected grain size and at carbon levels of (0.02, 0.04, 0.06, 0.10, 0.20 and 0.30)%.

5.2.4.3.10 Carbon Gradient Data

This data is needed to obtain the basic carbon diffusion constants.

Test Series 11

The carbon profile within cylindrical specimens as a function of surface carbon concentration is to be determined for temperature and time. Carburizing temperatures and concentrations are not here specified but are to be sufficient that the laws governing carbon migration may be deduced.

5.2.5 Secondary Stress Limits For Reformer Tubes

5.2.5.1 Introduction

The structural design criteria for design of components generally involve separate limits for primary and secondary stresses. The stress limits are derived from applicable theories for materials with considerable ductility. Thus, primary stresses are to be limited to some fraction of the limit or collapse loads. Secondary stresses do not affect the limit load of the structure. In a similar fashion, the secondary stresses are limited so that after a few cycles the structure will "shake-down", that is, the combined primary plus secondary stress values will be within the elastic regime. When these concepts are extended to high operating temperatures where creep is present, the secondary stress limits involve slightly different considerations. Under these conditions, secondary stresses could cause ratcheting arising from creep leading to an accumulation of strain in the structure even though shake-down would be indicated in the absence of creep. The limits for secondary stresses in this instance would be based on either an elastic analysis which assures freedom from ratcheting or a complete inelastic analysis that would demonstrate bounded strains in the structure which are within allowable limits.

The above considerations are only true in structures which have a high degree of ductility. In the case of reformer tube applications, severe degradation in fracture properties of high nickel alloys (Inconel 617 and Incoloy 800) following thermal aging at 850°C has been reported in literature (Refs. 5.2-7, 5.2-8). While such degradation raises a number of issues which need to be addressed, the present data are far from clear in indicating that a design based on these materials is infeasible. The degradation in fracture properties has come to light in view of the lower Charpy impact energy values at room temperatures - in the range of 10 Joules in some cases contrast to values in excess of 200 Joules for unaged material. However, the elongation in room temperature tensile tests are quite significant, on the order of 10% . One conclusion that could be

drawn is that the arrest fracture toughness for this material could be expected to be low although the critical stress intensity or fracture toughness for crack initiation and slow crack growth could be significant. It is also conceivable that the very low impact values are peculiar to this heat and somewhat better properties could be expected by optimizing the thermo-mechanical treatments and composition.

In addition, it should be recognized that the low impact properties are generally prevalent at low temperatures and, at temperatures higher than 400°C, the material appears to have adequate ductility. There is little doubt that the current data base is not adequate to answer all the questions, particularly as to the underlying metallurgical and microstructural reasons that result in the poorer impact properties. Grain boundary precipitation of carbides has been cited as the most probable mechanism for the observed effect. In view of the fact that carburization of the material could also result from contamination from either the reactor gas coolant or even from the process gas side would imply that, even if the material properties could be improved, specific design alternatives should be explored so that feasible designs could be evolved for materials with limited ductility. In the present investigation, one specific aspect of this problem, that related to secondary stresses, is examined.

5.2.5.2 Design Approach

In evaluating a possible design approach for materials with limited ductility, it will be noted that the primary stresses are kept extremely low. To ensure high levels of reliability, it is necessary to keep primary stresses low enough that the possibility of rupture at normal operation arising from primary stresses should be negligible. This might mean a somewhat lower allowable primary stress value than currently used with existing high temperature design criteria. No further discussion on this aspect will be given here as the primary stresses are kept low in the reformer tube specifically to protect against creep rupture, and these stresses are judged to be so low as not to give rise to any fracture related concern.

The secondary stresses including those arising from elastic follow-up are harder to bound within specific limits. In general, these stresses arise due to temperature transients or existing normal temperature gradients. In this investigation, no attempt has been made as to whether these could be kept within the bounds necessary to avoid fracture, but only to derive such bounds. Comparisons and further evaluations will be carried out at a later date.

In trying to come up with a design methodology for fracture, it is also necessary to examine the consequences of rupture. Assuming a once through cycle, a rupture would result in mixing of the primary and secondary side. Provided the secondary (process gas) side pressure is higher and the rupture is of either a pin-hole or tight intergranular nature, sufficient time would be available to detect the leak and carry out the necessary shutdown/ replacement operations. If the fracture is sudden or the crack length is large or if the crack geometry results in a severing of the tube, the results are harder to quantify. Therefore, the design process would involve two steps to protect against fracture:

- a. Ensure freedom from rupture under anticipated load cycles based on fracture considerations employing high levels of design margin considering both load controlled and deformation controlled stresses.
- b. Ensure freedom from catastrophic crack propagation that could result in pipe break or large axial cracking before an initial leak type rupture by a combination of fracture methodology, operating conditions and laboratory experiments.

The latter objective of course relates to the concept of leak before break. This objective primarily takes care of the small probability that there, in fact, might be a rupture. The primary objective would, however, be to build sufficient design margins to avoid the possibility of rupture. The following discussion therefore pertains to the applicable limits to assure of such a high degree of design conservatism. It should be

recognized that application of fracture methodology in design criteria for high temperature components is still in a stage of development. Further evaluation along the lines indicated here might be necessary.

5.2.5.3 Stress System in the Reformer Tube

A simple model of the reformer tube thermal system is shown in Figure 5.2-5. The outside of the tube is exposed to the reactor coolant gas and the inside to the process gas. It will be assumed that after the initial start up, the secondary thermal stresses are completely relaxed at operating temperature. On cooldown, the outside which was at a higher temperature, would be subject to a tensile residual stress of $E \cdot 0.5T$, while the inside radius would be subject to a compressive stress. The outside is also subject to possible deleterious environmental effects from the reactor coolant gas.

Since the material ductility appears to be the lowest at low temperatures, the loading state on cooldown is more of a concern for crack growth and fracture than the stress state at the high temperature. If the secondary stresses are not relaxed by creep at the operating temperature, there will be a corresponding reduction in the residual stress after cooldown. In the extreme case where none of the secondary stresses are relaxed for instance, there will be no secondary stress on cooldown. Since we are primarily concerned with the fracture properties of the material at the low temperatures the worst case stress distribution corresponds to the case of a completely relaxed state at temperature.

5.2.5.4 Considerations for Limiting Secondary Stresses

In general, where fracture could limit the allowable secondary stress, the fatigue properties of the material could also be expected to be quite low. In high temperature design, the allowable limit on the primary plus secondary stresses could exceed yield, provided there is no ratcheting. In view of the low fracture toughness of the material, it would appear to be inadvisable to incur low cycle fatigue even though there may be no ratchet-

ing. Thus, when the primary plus secondary stress values are in excess of yield, the outer surface could be expected to sustain plastic strain cycles though there is no net diameter increase in the tube. For a material with limited ductility, this would not seem to be appropriate. Thus, for normal operating conditions, that is, for Levels A and B operating conditions as per the ASME Code Case N47, it would be desirable to restrict the primary plus secondary stresses to within the yield strength of the material at low temperatures where the material has limited ductility. This is not necessarily highly restrictive as the yield strength of the material is high at low temperatures. For Level C and D loadings, this limit will be relaxed. The main thrust behind this assessment is that routine plastic strain cycling of the tube surface arising from primary plus secondary stresses should be avoided so that a high level of confidence could be placed in the protection against fracture. The question then becomes one of whether an even lower limit should be imposed to avoid possible fracture.

In trying to assess fracture protection, two possible methodologies could be employed. First, starting with a postulated initial flaw, the growth of this flaw from stress cycling during Levels A and B loadings could be performed over the history of the tube lifetime. The resulting crack length at any time during the history could be compared with the critical crack length for unstable propagation. If the design at no time gives rise to a crack length greater than that required for unstable propagation with some design margins, protection against fracture could be assured. But this methodology involves fracture calculations after the fact, that is after the design has been more or less completed. A second methodology would be to assume that a maximum primary plus secondary stress level S exists during each cycle. Starting with an initial length and over an upper bound number of cycles, the flaw length could be calculated. The final stress intensity with the stress level S and the final flaw length could be assessed for unstable fracture. This provides a means of specifying a bound for the primary plus secondary stress for the designer during design. This is the methodology that has been opted here. Following this

procedure, the steps to be taken for bounding the primary plus secondary stress are:

1. Postulate an initial flaw length, a_0 , consistent with detectability during initial fabrication and assembly.
2. Describe crack growth due to fatigue cycles of primary plus secondary stress level of S .
3. Compute the final stress intensity and compare with allowable for a range of initial flaw lengths and stress levels S .
4. Identify a design space of S and initial flaw length where the fracture protection could be assured after imposing a margin on the fracture toughness of the material.

In carrying out these steps, certain simplifying assumption will be made. These simplifications could be removed in later developments. First it will be assumed that the stress S acts uniformly over the entire thickness of the tube. At first sight, this might seem to be highly restrictive for secondary stresses vary through the thickness going from compression to tension (above the mean primary stress levels) so as to be self-balancing. However, in terms of evaluating the fracture possibilities only those stresses that existed at the location of the crack are important. If the crack depth, as a function of thickness, is so large that the variation of secondary stress needs to be taken into account, there is already substantial cracking. Since the development of margins for Levels A and B loading are to ensure high levels of conservatism in a major component such as the reformer tube, the final crack length in any case will probably be restricted to less than 20% of the thickness. The added complexity of calculating the stress intensity factor with varying stress across the thickness could be implemented following the procedures outlined in Reference 5.2-9, but does not seem to be necessary for this initial evaluation.

A second simplification relates to the assumption of primary plus secondary levels of S which might vary as slow crack growth proceeds. Specifically, if secondary stresses arise as a result of deformation controlled scenarios, cracking which tends to increase the compliance would tend to lower the level of the applied primary plus secondary stress. On the other hand, if the cracking were to initiate only at one isolated location, the elastic restraint of the uncracked tubing could be sufficiently large that no relief might occur because of the cracking.

A third simplification relates to the assumption of no crack growth at the higher temperatures of operation. At present, data for creep crack growth are limited. While it might appear to be a major drawback in this type of methodology, it should also be recognized that, for low levels of creep damage (time fraction damages much less than unity), little if any actual cracking could be found in the structure. In other words, for most of the lifetime involving the secondary creep rate regime, the material does not actually have observable cracks though a life fraction damage could be phenomenologically associated with the structure. To this extent, calculating the crack growth based on fatigue alone may not be unrealistic. Further models that incorporate crack growth under creep conditions - in this case assuming the highest permissible primary creep stress - could be included at a later date if such data were to become available.

A fourth simplification relates to the use of linear elastic fracture mechanics with possible correction for the crack length based on the plastic zone size (Irwin correction). This will be the methodology employed in comparing the final flaw size with the allowable size. If elastic-plastic fracture mechanics were needed, it is felt that the ductility of the material would be sufficiently high that the initial considerations outlined earlier, particularly limiting the primary plus secondary stress to be within the yield, may not be valid. Further developments in this area, specifically in respect to whether elastic-plastic fracture mechanics should be employed, will have to await the development of a larger material data base for fracture toughness of the material, particularly for the section thickness sizes of relevance to design.

5.2.5.5 Secondary Stress Limits

The initial flaw size is assumed to be a_0 . There are several cases of crack geometries for which the stress intensity factors are available. Only one of these, pertaining to the radial flaw of external origin (Fig. 5.2-6) will be used in this development. With the application of a hoop stress S , this configuration appears to be the one with the highest stress intensity except the one with the internal crack. This latter configuration may not be of importance to the specific application being considered as the inner radius temperature is lower than the outer radius for normal operation and for most upset conditions. In turn, the inner radius will be under compression on cooldown. The crack growth law will be assumed to be power function of the form: $C(\Delta K)^n$, where C and n are constants and ΔK is the stress intensity range. Since the primary stress values are low and it is assumed that, at operating temperatures, the secondary stresses are completely relaxed, K could be calculated as the stress intensity range resulting from a stress range from 0 to S . The resulting final flaw length, a_f follows from the initial relation:

$$\frac{da}{dN} = C(\Delta K)^n$$
$$a_f = \int_0^{N_L} C(\Delta K)^n dN + a_0$$

where the integral is to be taken over an upper bound of the expected number of cycles N_L over the lifetime of the component. A more accurate description of the crack growth law will have to take into account a three stage crack growth process. The accelerating growth rate in the third stage could be neglected as this usually happens when the imposed stress intensity levels are approaching the critical stress intensity limit and may not be allowable in the discussions to set design margins for secondary stress. A simplified formulation would involve only the second stage and is in fact more conservative in many instances as some initial overload could develop the crack into the second stage without the component spending a long time in the first stage. Referring to Figure 5.2-6, the stress

intensity would vary with the crack length a/t , where t is the thickness of the tube. For a range of a/t values of the order of 0.2, the increase in fracture toughness could be represented as a quadratic equation of the variable (a/t) . The equation for crack growth can then be integrated with such a quadratic variation. However, considering that K_0 in Figure 5.2-6 represents the stress intensity in an infinite plane sheet with a central crack and K_I/K_0 varies from 1.12 to 1.34 for the range of a/t values of interest, a further simplification could be brought about. This would be to assume that K_I/K_0 is 1.34 independent of the crack length at any given time, so that an upper bound estimate could be obtained. Using only the relation for the second stage, the final crack length a_f and initial crack length a_0 are related to number of cycles N_L by the following relation:

$$N_L = \frac{1/[a_0^{(n/2)-1}] - 1/[a_f^{(n/2)-1}]}{C S^n [(n/2)-1] 1.34^n}$$

Fatigue crack growth curves for Inconel 617 in the aged condition are not available at the present time. In Figure 5.2-7 the fatigue crack growth curves for this material at room temperature are presented for the unaged material. In Table 5.2-1 the fracture toughness values for the carburized material are presented after carburization in an impure He atmosphere. It is highly likely that the crack growth rates will be accelerated in the aged material compared to the unaged material. For the present, in the absence of aged material data for crack growth, the crack growth data for the unaged material will be used to derive the initial design regime. Similarly in the absence of fracture toughness data for aged material, the data on carburized material will be used as a lower bound guideline for the fracture toughness of aged material. The same methodology could be later adopted once the crack growth data for the aged material become available.

The procedure for computing the design space was implemented using a computer algorithm which had an initial input of flaw size and total expected number of cycles. The crack growth data represented in Figure 5.2-7 was fitted to the polynomial expression:

$$\frac{da}{dN} = 1.88 \times 10^{-12} (\Delta K)^{5.398}$$

where da/dN is the growth rate in m/cycle and ΔK is the stress intensity range expressed in $\text{MPa}\cdot\text{m}^{0.5}$. An initial guess for the maximum allowable stress intensity was made, and the final crack length at the end of the expected service cycles was then computed. The end of life applied stress intensity was then computed for this stress level and the final crack length, and compared with the allowable critical stress intensity factor.

Fracture toughness data for aged material are not available at the present time. For conservatism, the properties of highly carburized material given in Table 5.2-1 were used. The lowest level of fracture toughness given in Table 5.2-1 was chosen for calculational purposes. If the applied stress intensity exceeded the allowable, another iteration was performed with a reduced stress level. The iterations were repeated so that for the given number of cycles and initial starting flaw length, the stress intensity at the end of lifetime was less than the minimum value in Table 5.2-1. In Figure 5.2-8 a plot of the allowable stress is shown as a function of the maximum initial flaw size for a maximum number of 1000 and 500 expected cycles during the lifetime. This plot could therefore be utilized to set the allowable primary plus secondary stress level in the tube. It should be recognized that this is only a preliminary investigation and that crack growth data for aged material are not at present available and could be higher. If safety margins are to be included they could be prescribed at three different levels, by assuming an initial flaw size greater than the maximum size expected after nondestructive testing, by reducing the allowable stress by some factor, or by reducing the value of the allowable fracture toughness during the calculation procedure. The results of evaluations with a safety factor of two on fracture toughness are also presented in Figure 5.2-8. Even though the allowable values

appear go be quite large in comparison with the yield strength of the material at room temperature, preliminary calculations assuming that the crack growth is accelerated by a factor of 100 compared to the unaged material resulted in allowable stress values that were a factor of two lower, as indicated in the Figure 5.2-8.

5.2.5.6 Conclusions

A methodology for prescribing the allowable primary plus secondary stress limit for a material with limited ductility has been identified. The lack of applicable properties data, specifically for crack growth for aged material precludes a complete assessment at this time though the methodology is equally applicable once the data become available. In assessing the allowable stress limits with unaged crack growth properties and carburized material fracture toughness data, it is clear that the allowable primary plus secondary stress could be in excess of the yield strength of the material at the low temperatures. That is, in spite of the low fracture toughness of the material, primary plus secondary stress values limited to the yield strength of the material for Level A and B operation should nominally be conservative with respect to fracture considerations as the initial flaw lengths are generally tractable at the 10 mil level. The methodology does not account for possible crack growth at temperature, as it is assumed that the residual stresses are initially relaxed completely by creep. If crack growth rates for the aged material are some 100 times greater, preliminary calculations indicate that the allowable stress range just about equals the yield strength of this material with the same reduced room temperature fracture toughness levels. More detailed calculations would have to be performed to ascertain such limits.

A comparison of the allowable temperature differentials could be made assuming that the primary plus secondary stresses are limited to the yield strength. For yield strength levels around 35Kpsi over the low temperature region and a Young's Modulus of 30×10^6 psi, an upper bound for thermal expansion of 16×10^{-6} in/in/°C, neglecting the primary stresses, the

allowable temperature range would be on the order of 150°C. It would thus appear that through the wall secondary stresses could be accommodated in the reformer tube. Typically, the through-the-wall temperature differential during normal operation is at present calculated to be only on the order of 40°C.

In summary, a methodology to define allowable primary plus secondary stresses for material with limited low temperature ductility has been formulated. An important data need here would be crack growth rates in aged material at the low temperatures. Provided the slow crack growth characteristics of the aged material does not differ significantly from the unaged material, a feasible design regime appears to exist to protect against secondary stress induced fracture.

5.2.5.7 Methodology for Analytical Evaluations

5.2.5.7.1 Introduction

The stability of crack growth in pipes has received considerable attention in literature. Essentially, if slow crack growth leading to initial rupture or a limited unstable crack propagation in the radial direction without an attendant circumferential instability or extensive axial propagation could be demonstrated, the concept of leak before break could be assured in practice. The initial breach, provided it could be identified quickly, will give sufficient time for the operator to isolate specific components and carry out the necessary discharge operations and ensure maintainability.

In the reformer tube, the primary stresses are principally due to internal pressure. The reactor coolant gas pressure is lower than the process gas pressure so that a break will not result in contamination of the process gas side. In order to design the reformer tube for extended lifetime application and avoid creep rupture, the primary stresses are kept quite low. It is unlikely that axial flaw propagation or complete circumferential rupture could occur as a result of the low levels of internal pressure. Primary attention has therefore been given to other modes of

loading which could introduce significantly higher stresses in the tube. These include possible axial tensile loads or bending loads during postulated off-normal events and through the wall secondary hoop stresses arising from temperature variations across the tube thickness. For each of these types of loading, one could evaluate the consequences using either a linear elastic approach or an elastic-plastic material behavior. In the present investigation, only the methodology to be used relative to these approaches has been outlined. Detailed analyses with specific material property data will be carried out later as the property data in many cases are not available at this time.

The specific methodology to be applied in the final evaluations will depend upon the material behavior. There is concern that Inconel 617, one of the candidate materials for reformer tube applications, could suffer considerable loss of ductility after aging at high temperatures. In addition, the potential for carburization from the reactor coolant side environment with possible attendant loss of ductility is now being evaluated. These considerations will have to be factored into the final choice of the type of methodology chosen for leak before break assessment.

In the investigation outlined here, two principal cases are considered: one employing the linear elastic approach and another which assumes fully plastic failure in the region of the crack. Some of the more recent literature for these situations has been cited to develop the methodology to be used. The case of elastic-plastic failure in the region of the crack itself has not been addressed and further investigations are needed to assess if bounding criteria could be developed using the two extreme fracture behaviors (elastic and fully plastic in the cracked region) for this intermediate case.

5.2.5.7.2 Conditions for Slow Crack Growth: Linear Elastic Approach

The conditions for slow or quasi-static crack growth can be ascertained using the expression:

$$\frac{dK}{dc} < 0$$

where K is the applied stress intensity and c is the crack length. Obviously this approach has a number of shortcomings.

First, the formulation is based on linear elastic fracture mechanics. The basic formulation could be extended to the elastic-plastic case formulation using the J integral approach but would require further modifications and complex analyses.

Second, the formulation assumes that the critical stress intensity for fracture is a constant ($=K_C$) and is an inherent material characteristic. This is not quite the case as the fracture toughness of a propagating crack could differ and, in most instances, the "arrest fracture toughness" is lower than the fracture toughness required for initiation. If data were available, the right hand side would be expressed as a differential evaluated from crack growth resistance curves.

Notwithstanding the above reservations, it is useful to look at leak before break in terms of the above formulation. Primary attention will be focused on a radial surface flow and considerations relative to its possible growth as an extended axial or circumferential crack. Growth into the thickness as a through-the-wall radial crack will also be considered. This latter situation, if the crack does not propagate immediately thereafter in the circumferential or axial directions, would indicate leak before break.

5.2.5.7.3 Radial Propagation: Effect of Hoop Stress

The general formulation of the stability problem should address a stress intensity factor K of the form:

$$K = F(a) a^{0.5} \phi(P)$$

where $F(a)$ is a function of crack length generally expressed as a dimensionless number in terms of the thickness, and $\phi(P)$ is the loading state. Differentiating,

$$\frac{dK}{da} = \phi(P) \cdot \left[\frac{dF(a)}{da} a^{0.5} + \frac{1}{2a^{0.5}} F(a) \right]$$

For stability, $\frac{dK}{da} < 0$

$$\text{or} \quad \frac{dF(a)}{da} a^{0.5} + \frac{1}{2a^{0.5}} F(a) < 0$$

Case (1) - Internal radial crack, internal pressure P : The applied K increases monotonically with increasing crack length. For a typical thin tube, e.g., $R_1/R_2 > 0.8$, the crack will always be unstable.

Case (2) - External radial crack, internal pressure P : Once again, crack propagation will be unstable based on the LEFM approach as K increases with increasing crack length.

Both the above formulations consider only the case of internal pressure loading. It is therefore necessary to make sure that the applied K from internal pressure loading does not exceed the allowable fracture critical stress intensity and is, in fact, limited to a small fraction of the allowable. This is generally called for in the design. The more important cases of loading arise from secondary or deformation controlled stresses. The analysis of crack stability is somewhat more complex and requires the availability of the Green's function for the problem. Though no detailed analysis will be given here, the general procedures and the underlying

assumptions could be illustrated. For the case of steady operation followed by a shutdown, a thermal stress (tensile on the outside and compressive on the inside) will exist. The applied stress intensity would be higher than that indicated by the primary stress alone and could exceed the critical stress intensity for propagation. However, as cracking proceeds radially, it is moving into zones of lower stress. In other words, the loading function $\phi(P)$ tends to drive K down thereby lowering the possibility of further propagation. The differential of K has to be constructed as:

$$\frac{dK}{da} = \phi(P) \frac{d}{da} [F(a) a^{0.5}] + F(a) a^{0.5} \cdot \frac{d\phi(P)}{da}$$

If K is available as a function of $\phi(P)$, which in this case includes the secondary stresses, dK/da can be evaluated.

An assumption made here is that the deformation pattern controlling the secondary stresses do not redistribute as the crack propagates. This assumption is in fact valid in many instances. For instances in a tube with radial temperature gradients, the propagation of a radial thumb-nail flow into a through-the-wall crack will not necessarily affect the compatibility restraints significantly unless the crack length is long compared to the characteristic length l of the tube given by approximately $2(Rt)^{0.5}$, where R is the radius and t is the thickness of the tube. For small lengths, the restraint from the uncracked portion of the tube will continue to impose the compatibility restrictions, so that conservatively one could consider the deformation controlled stresses as a primary loading system. Of course, the compatibility effects will be negated in considering long axial cracks.

While the details of the analysis have to be worked out, the methodology is clear. After deriving the secondary plus primary stress distribution for the worst case, the Green's function for describing K should be developed. The $\phi(P)$ term could then be evaluated to see if unstable propagation occurs. Since the material properties are at the lowest in terms of fracture during shutdown, this probably represents the worst case. At operating temperatures in excess of 400°C , available material properties data indicate significant fracture toughness (as implied by the Charpy

impact tests though no fracture toughness values are available). In this case, considerable crack blunting could be expected and in most instances should lead to slow stable crack growth.

The steps to be taken for analysis of leak before break could then be outlined as follows:

- (a) Derive the worst case through the wall stresses from both primary and secondary sources.
- (b) Postulate the initial flaw size
- (c) Compute the possible flaw size due to duty cycles, e.g., shutdowns, postulated upsets, etc.
- (d) Derive the unstable crack length for $dK/da > 0$ for the stress obtained in (a).

In the case a thumb-nail surface flaw, the propensity for both axial and radial propagations need to be taken into account. The limits on primary plus secondary stress during design essentially assume that gross crack propagation does not occur due to expected load cycles. Preliminary analysis using postulated linearly varying hoop residual stress distributions have been performed using the results cited in Reference 5.2-9. In this reference, the stress intensity factors for semi-elliptic flaws on either the internal or external surface of a pressure wall under polynomial loading distribution on the crack surface have been given. By using the appropriate influence coefficients, the applied K values could be calculated. For a linearly varying hoop stress across the thickness enter negligible internal pressure, radial crack growth is stable as it moves into regions of compressive stress. However, if the dynamic fracture toughness is lower, it is desirable to avoid fracture by restricting the applied K values to be lower than the K_{IC} required for propagation.

In evaluating radial versus axial crack propagation, it is seen that when the axial length is twice the radial depth, the maximum stress

intensity occurs at the two extremal axial locations. So the crack would be expected to extend axially first. However, for long semi-elliptical flaws the maximum stress intensity occurs at the maximum depth point. In other words, in this case, the crack would extend radially first. If linear elastic analysis were applicable, it would therefore appear that radial cracking resulting in a leak would almost always occur before significant axial extension would take place. However, the stress variation with thickness could possibly exclude through-the-wall radial cracking. Therefore, in order to assure leak before break, through the wall secondary stress variations have to be limited so that no unstable crack lengths (in the axial direction) result during service. Otherwise, there is no easy means of assuming that a long axial crack will not result before a through-the-wall rupture.

Hoop stress by itself is not expected to result in circumferential crack propagation so the possibility of pipe break need not be considered in this context. Axial tensile loading from possible upsets is the major source of loading to be considered in this case.

If the material behavior warrants the application of linear elastic techniques, once again the axial tensile loads or applied elongation should be limited so that the stress intensity for a through the wall part circumferential crack does not exceed the critical stress intensity factor. This would certainly be the case when external tensile loads are applied as the stress intensity factor increases as the crack propagates. While more detailed calculations are needed for the case of fixed external displacement, crack extension could be stable in this case, depending upon the compliance of the system. However, in view of the engineering complexity associated with defining the compliance precisely, leak before break essentially translates to restricting the allowable axial stresses or displacements to be below those consistent with the expected flaw size and fracture toughness of the material when the material has low ductility.

In summary, if the material behavior could be modeled by linear elastic analysis, it is necessary to keep the imposed loads or extensions below those required for propagation. Specifically, for pipe break

concerns, the axial loads/displacements from upset incidents should be kept well below those required for propagation. As yet, such loading conditions, specifically tensile axial loads or gross bending of the reformer tubes, have not been identified as being realistic for any of the postulated events. The primary need here then is to categorize postulated duty cycle events, including unlikely events, and ascertain if indeed such loading conditions are possible.

5.2.5.7.4 Crack Stability Under Plastic Failure Conditions

The stability of an initial part-through radial crack could also be investigated for leak before break conditions when the linear elastic framework no longer applies and plastic fracture occurs. The case of a radial circumferential crack subject to internal pressure has been investigated in some detail in literature (Refs. 5.2-10 and 5.2-11). Essentially, these results apply to cases of primary loads at specified displacements rather than secondary stresses. In general, compatibility driven secondary stresses arising from thermal gradients in the tube would not appear to lead to complete break before leaking as explained in Section 5.2.5.7.3. Axial propagation of a reformer tube crack does not appear to be influenced by the type of secondary stresses arising from thermal gradients. Therefore, a detailed consideration of leak before break analysis would then involve possible unstable circumferential propagation of the crack rather than axial rip. Axial rip could indeed take place from an initial through the wall axial flaw but has to arise mainly from high internal pressures and not due to secondary stresses. Also axial rip propagation from elastic follow up type loads appear unlikely since a scenario for follow-up effects promoting high hoop stresses has not been found.

The circumferential extension of a through the wall or part through radial crack severing the pipe leading to a break before leak situation, however, remains to be considered. This type of break generally requires tensile axial loads or bending type stresses. Analysis for the stability of crack growth under tensile loads has been carried out in Reference 5.2-9. In practice, the reformer tubes do not see any axial tensile loads

except the low level of tensile stress due to the internal pressure. Since the design allows for free axial expansion, little if any secondary stresses due to (axial) elastic follow-up are expected. There is, however, the possibility of high tensile axial stresses arising as a result of either overpressurization or from dynamic phenomena such as "water-hammer" type loads. It is necessary to investigate the effect of these loads on crack stability.

The initial flaw for investigating this case will be assumed to radial. An axial surface flaw will have little if any tendency to propagate due to axial tensile loads. Thus we are primarily interested in evaluating the possibility of a complete rupture along the circumference due to the tensile load. The model of the initial flaw for this case is shown in Figure 5.2-5. Following the notation contained in Reference 5.2-12, the cross-section is assumed to contain two identical symmetrically situated cracks of depth in wall thickness t with the mean radius of the pipe being R . The following relations then apply in examining whether the propagation will be radial or circumferential for the case of an elastic-perfectly plastic material.

First, it will be assumed that the cracked section is under limit load with flow-stress of σ_0 . This will be the case for an elastic-perfectly plastic material. For a work hardening material, σ_0 will be between the yield and ultimate stress of the material. The limit load P_0 is given by

$$P_0 = 2\pi R t \sigma_0 \left[1 - \frac{2a}{\pi t} \right]$$

The elastic contribution of the total displacement would be

$$\Delta_{EL} = \frac{P_0 L}{2\pi R t E} = \frac{\sigma_0 L}{E} \left[1 - \frac{2a}{\pi t} \right]$$

where L is the total length of the tube and E is the Young's Modulus. A plastic contribution Δ_{pL} should be included which can be approximated as δ_{IC} the critical crack opening displacement. The total displacement Δ_{IN} can be written as

$$\Delta_{IN} = \frac{\sigma_o L}{E} \left[1 - \frac{2a}{\pi t} \right] + \delta_{IC}$$

δ_{IC} is essentially equal to J_{IC} / σ_o where J_{IC} is the critical value of the J integral required for crack propagation. The crack tip opening angle χ is considered to be a constant in this model. For crack growth $d\Delta_{pL}/ds = \chi$, where $ds = da$ for radial crack growth and $ds = d\theta$ for circumferential growth. After substitution and differentiation, the radial instability is:

$$\frac{d\Delta_T}{da} = \chi - \frac{2\sigma_o L\theta}{\pi Et}$$

or $\frac{2\sigma_o L\theta}{\pi Et\chi} > 1$ for radial instability.

Similarly, by differentiating with respect to θ ,

$$\frac{1}{R} \frac{d\Delta_T}{d\theta} = 2 - \frac{2\sigma_o La}{\pi ERt}$$

or $\frac{2\sigma_o La}{\pi ERt\chi} > 1$ for axial instability.

Comparison of the equations for radial and axial instabilities indicates that radial instability is preferred over circumferential instability if $R\theta/a > 1$. This is generally the case for most practical situations. Therefore, the crack will break through radially before it does so circumferentially.

While the above equations have been derived for an internal radial crack, the same derivation applies for the external radial crack as well as the considerations outlined here are principally based on local limit load. A second consideration is whether, after breaking through radially, the crack will also propagate circumferentially. The conditions for complete circumferential rupture are:

$$\frac{2\sigma_0 L}{\pi L R \chi} > 1$$

corresponding to the case of circumferential instability at $a/t = 1$. A second criterion is one where the total strain required to propagate in the radial direction for complete breakthrough is greater than that for complete circumferential growth. As cited in Reference 5.2-12, the corresponding condition is:

$$\frac{2\sigma_0 L}{\pi E R \chi} > \frac{[\pi/2 - \theta]}{[1 - a/t]}$$

Evaluation of these equations indicates that leak before break occurs in most cases, that is, except in cases of (circumferentially) very short or very long cracks. Circumferentially very long cracks, that is, θ almost equal to $\pi/2$, arising from steady crack growth cannot, of course, be permitted in design. While the specific parameters for the reformer tube will have to be included in the evaluation, it appears likely that the most important case is one of a very short crack where $R\theta/a < 1$.

The considerations outlined above are for the case of elastic-perfectly plastic material. The same considerations could be used as a guideline for work hardening material provided the material exhibits sufficient plasticity prior to fracture. This would generally be the case for the reformer tube at operating conditions when the temperature exceeds 400°C. At lower temperatures, there is a probability that the material may not possess

adequate ductility. It will be necessary to perform material property tests and data evaluations to assure that adequate ductility exists to apply the above methodology. In the case where the material has sufficient ductility, the conditions described above could be translated into a simple criterion based on the geometric parameters of the tube and a material property referred to in fracture literature as the tearing modulus. The tearing modulus T_{MAT} is defined as $E\chi/\sigma_0$. With this definition, the circumferential instability condition can be written as

$$\frac{2L}{\pi R} \cdot \frac{1}{T_{MAT}} > 1$$

While T_{MAT} for the reformer tube material is not available at this time, the ratio of L/R for the reformer tubes appear to be too high to satisfy the stability criterion. Essentially this means that once cracking occurs, the stored elastic energy in the length of the tube would "follow-up" to continue the crack growth process and result in tube separation if the length of the tube is long compared to the radius. However, it should be borne in mind that the reformer tube is essentially free to move axially so that there is no physical basis for postulating a significant axial strain elongation in the first place.

5.2.5.7.5 Conclusions

The methodologies for analytically demonstrating leak before break have been examined for a linear elastic and fully plastic fracture scenarios. For the linear elastic case the primary emphasis should be to limit the applied loads such that the applied stress intensity factors do not exceed the critical value. On the fully plastic case, the lengths of the tube are sufficiently long that a circumferential propagation could be expected - depending upon the tearing modulus of the material - due to the large levels of stored elastic energy for the case of a fixed end displacement loading.

In either approach, the possibility of having high axial tensile loads in the reformer tube have not been identified yet. A thorough evaluation of normal, anticipated and unlikely events needs to be carried out to

identify if such loads are plausible before further detailed analyses of leak before break are made.

5.2.6 Probabilistic Framework for the Determination of Structural Design Margin Targets

5.2.6.1 Introduction

Attempts to establish a probabilistic interpretation of structural design margin dates back to World War I. The objective of this early work was to provide a rational approach for the design and evaluation of aircraft castings by relating the design margin to reliability in casting strength. Later work focused on relating the design margin to the probability of structural failure by considering variability in both strength and loading. More recently, the question of establishing structural failure probability targets and defining the corresponding design margins have been investigated.

Despite the extent of the above effort and the reflection of probabilistic ideas in Title 10 of the Code of Federal Regulations and the ASME Code, some important questions remain unresolved. These include such basic questions as the framework within which design margins are to be determined, the meaning of uncertainty, the relationship among various uncertainty factors, and which of these factors can effectively be controlled by R&D, design, or operations. In the absence of clear answers to the above questions, it is highly doubtful that extrapolation of current practices to the high temperature regimes contemplated for the HTGR's will be acceptable.

The objective of this report is to define an approach for answering the above questions. In Section 5.2.6.2, a decision analysis structure is presented as a rational framework for determining design margin targets. The section provides a formulation of the decision analysis problem, discusses the consequences of optimism and conservatism in defined design margin targets, and provide the framework of assessing the value of the R&D information. Section 5.2.6.3 contains recommendations for implementing the defined approach.

5.2.6.2. Proposed Approach

5.2.6.2.1 Overview

The ASME Code provides the following deterministic design format for the design of mechanical components:

$$\phi S \geq C_1 X_{d1} + C_2 X_{d2} + \dots + C_n X_{dn}, \quad (1)$$

where .

ϕ = resistance factor whose value is associated with a particular service condition

S = max. allowable stress for the material of the component

C_n = coefficient to convert load into a particular response (in this case stress)

$X_{d1}, X_{d2}, \dots$ = design loads

Although variations to the above deterministic format are in use, Inequality 1 will be used here as a vehicle to provide an overview of the approach discussed below and the philosophy underlying this approach with no major loss of generality.

Traditionally, a probabilistic analogue to Inequality 1 will be described as follows: Find the probability distributions of both sides of the inequality, then estimate the probability of violating the inequality using probability calculus and the given distributions. This gives the probability of failing the service conditions.

This traditional approach has many drawbacks which are best uncovered by casting Inequality 1 in different probabilistic framework which is closer to the "real world". The design process is a sequence of decisions

to choose among candidate alternatives. This is best illustrated by Figure 5.2-10 which presents an idealized model of the design process.

It is clear from Figure 5.2-10 that:

1. The design process involves a sequence of decisions by different experts to satisfy different design criteria. These design criteria should be compatible with the interests of the design organization and with institutional requirements.
2. The decision process is generally iterative with feedback loops to refine the selected design.
3. Each design decision depends on the information available to the decision maker.

Each decision in the above process can be abstracted in the following form: Given a hypothesis H_0 , say that the design is safe; the designer tests this hypothesis by applying an inequality similar to Inequality 1. If the inequality is satisfied, the designer accepts the hypothesis H_0 that the design is safe; if not, he rejects H_0 and changes the design until the inequality is satisfied. This abstraction casts design decisions in the form similar to hypothesis testing in the theory of statistics. On accepting the hypothesis while in fact it is false, one commits one type of error; on rejecting the hypothesis while in fact it is true, one commits another error. Each error has its probability of occurrence which is different from zero by the mere fact that a deterministic region of acceptance similar to Inequality 1 has been used, while at least one side of the inequality contains random variables. The difference between this abstraction and the traditional probabilistic model described earlier is the introduction of the concept of a hypothesis, H_0 . The probability of accepting the hypothesis while in fact it is false is given by the joint distribution;

$$\begin{aligned} &P(\text{inequality 1 satisfied, } H_0 \text{ false}) \\ &= P(\text{Inequality 1 satisfied}) \cdot \\ &P(H_0 \text{ false} / \text{Inequality 1 satisfied}) \end{aligned} \quad (2)$$

where the second term in the RHS presents the conditional probability that H_0 is false given the evidence that Inequality 1 is satisfied. We are now in a position to address the question of uncertainty.

Consider specifically the case where H_0 is the hypothesis of structural success corresponding to a particular service condition. The first term in Equation 2 reflects uncertainty in Inequality 1 which can be categorized broadly as follows:

1. Uncertainty in material properties under the given service conditions (LHS of Inequality 1).
2. Uncertainty in loading under the given service condition (RHS of Inequality 1).

The second term in Equation 2 reflects the uncertainty in relating a physical phenomenon such as structural failure under the given service condition to the mathematical model of Inequality 1. This third type of uncertainty is termed "Modeling Uncertainty".

Uncertainty in material properties reflects the variability in production processes, e.g., heat treatments and surface finish, and in the operating environment. It also depends on such administrative procedures as quality control and quality assurance. Uncertainty in loading depends on the design complexity and the modeling of load combination. It also depends on designed load limiting features such as rupture disks and safety release valves. Modeling uncertainty depends on the weight of evidence available to support the hypothesis of interest.

The above discussion indicates that the probability of structural failure is not a fixed parameter like physical constant, but a variable which can be controlled by design, production, operation, and the use of design criteria supported by adequate evidence. A reasonable strategy to reduce the probability of failure is to use these controls in such a way that higher objectives such as safety, cost and availability are optimized. This is the essence of the approach proposed in this work.

5.2.6.2.2. Importance of Structural Failure

The term "Structural Failure" does not have a unique meaning. There is failure in the sense of violating Inequality 1, failure in the sense of pipe rupture, or failure in the sense of failing the structural function of isolating two fluids. During the operation of the HTGR reformer, only the last failure is likely to be detectable. A proper characterization of HTGR reformer failure should therefore include the extent of failing the isolation function, the consequences of such a failure, and the probability of the failure. It is appropriate then to talk about, for example, undetectable leaks, detectable leaks with acceptable neutronic, structural, mechanical, and radiological consequences, and larger leaks where the consequences are unacceptable. An optimum HTGR design is expected to include appropriate provisions to reduce the probability as the leak size increases. As discussed below, a blind use of the concept of the design margin could fail this expectation.

Use of a design margin amounts to estimating the expected stress and the expected strength, divide the expected strength by the design margin to determine the maximum allowable stress, and design the component so that the expected stress is less than or equal to the allowable stress. Suppose that we apply this procedure to find the reformer tube thickness. It is clear that the larger the design margin, the thicker the tube will be. This in turn leads to the possibly erroneous conclusion that the design is more "conservative". Yet, a thicker tube has the following disadvantages:

1. It results in larger temperature differences across the wall which could enhance failure.
2. It is stiffer and as such more susceptible to transmitting concentrated loads to weak connections.
3. It is less amenable to producing a leak, which could be detectable and repairable before break, which could be catastrophic.

That the use of a design margin without due regard to other failure-probability control mechanisms could compromise the design reliability is further illustrated by past experience. The American Petroleum Institute (API) conducted a survey of 34 gas-fired ammonia reformer plants operated world wide (Ref. 5.2-9). Sixteen of the plants surveyed reported reformer tube failure with tube lives varying between 2 to 6 years. It is interesting to note the following list of failure causes: one failure occurs because of carryover of solids, two because of material defects, two because of weld cracking, and 11 caused by over-temperatures as a result of temperature control failure. Needless to say, all of these causes are controllable and reversible by techniques other than the use of a more conservative design margin for reformer tube design.

5.2.6.2.3 Relation Between Safety Assurance Alternatives and the Design Margin

The above discussion signals the need to place the questions of design margin in the broader context of the design decision process. In this section, we consider a specific example where alternatives for reducing the probability of structural failure of an HTGR reformer have been identified using the systematic technique of the safety assurance diagrams (SAD). The SAD technique provides a structured procedure to follow the evolution of design, construction, and operation to identify potential accident causes, then follow these causes through the potential accident scenarios. At each juncture in this process, lines of defense (LOD), which are potential or real means of preventing and mitigating the accident, are identified. These LOD's can be design features, administrative procedures, phenomena, analysis, etc. The resulting SAD's provide a road map of how things could go wrong and what options exist to reduce the probability and consequences.

A preliminary analysis for the HTGR MRS-R concept has been conducted and is shown in Figures 5.2-11 through 5.2-16. These figures are supplemented by Tables 5.2-2 through 5.2-15 which contain descriptions of the lines of defense. Figure 5.2-11 presents the accident prevention diagram for the reformer. The diagram is to be read from the top to the bottom. The

diagram is organized in the chronological order of the process of design, fabrication and assembly, pre-operation testing, and operation. By identifying potential deficiencies in these processes and the lines of defense against the deficiencies, a comprehensive list of the important factors which affect the safety performance of the reformer can be identified, evaluated, and documented in a traceable and logical fashion.

The MRS-R project is in the process of completing its preconceptual phase. Safety assurance work in the areas of design, fabrication and assembly, pre-operational testing and operation has been mainly directed towards analytical feasibility studies with minimal effort directed towards the administrative, data collections, and testing activities, which should be contained in the comprehensive program forming LOD 1 through 4 in Figure 5.2-11. Description of these LOD's, their importance, and reference to related work completed or to be scheduled are contained in the LOD description sheets, Tables 5.2-2 through 5.2-5.

The comprehensive safety assurance programs forming LOD 1 through 4 contain such activities as:

1. Continuous independent design reviews as the design evolves.
2. Industry-wide review of major design features. This industry wide review should preferably include reviews from related industries with operational experience such as ammonia reforming.
3. Carefully planned program of proving tests on major hardware items.
4. Carefully planned program of acceptance tests.
5. Careful QA/QC following acceptable standards.
6. Carefully planned program of construction.

7. Carefully planned ISI and post-maintenance check program.
8. Pre-power operational procedures.

The above programs should be scheduled to serve the MRS-R project as it moves from the design phase, to procurement, to fabrication, construction and assembly, to pre-operational testing and finally to operation. Timely institution and completion of these programs provide an inherent built-in level of safety which reduces the frequency and severity of the accident initiators labeled A-1 through A-5, in Figure 5.2-11.

Initiators A1 through A5 cover events caused by failures in the process gas train (A1 through A3), failures on the helium side of the reformer (A4), and failures in the reformer itself (A5). For this example, only the local reformer faults (A5) have been expended in Figure 5.2-11 to more specific failures modes for further analysis. Notice, however, that a potential line of defense has been identified for A1.

Accident source and progression diagrams for local reformer faults are shown in Figure 5.2-12 through 5.2-14. Tables 5.2-2 through 5.2-15 include LOD descriptions for the LOD's identified. These tables are self-explanatory. The point to notice in these tables, however, is that there are many options for improving the probability and consequences of structural failure. How much of each option should be used presents the main question in our probabilistic decision approach. The approach proposed in this work involves the use of SAD's to identify and relate possible options, and then to conduct a trade-off between these options to identify the group of options which provide maximum returns in terms of some objective functions, such as public risk, plant availability, total costs, etc.

5.2.6.2.4 Decision Model for Evaluating R&D

By R&D is meant experiments which produce some information under a specified set of controlled variables. To be specific, suppose one is interested in the expected thickness of carburized layer in a reformer

operating with a given set of conditions. An experiment is conducted and its thickness is measured. Because of variability in the properties of the test specimen, testing controlled and uncontrolled environment, measurement instruments and conditions, and the difference between the tested specimens and the real reformer, there is an uncertainty about the measured thickness and the true expected value. Nevertheless, suppose that the expected thickness is used by two designers who come up with two different designs. Under the hypothesis H_0 that the measured thickness is the same as the true one, an assessment of the cost and safety to the two designs is made. Under the hypothesis H_1 that the measured and the true values are different, different costs and safety assessments are obtained. Suppose further that the experiment has been of such poor quality that a probability of 0.5 is assigned for each of the above hypothesis. The question is to choose between Act A_1 , to select the first design, or Act A_2 , to select the second design, if the cost corresponding to A_1 , A_2 and H_1 and H_2 are as shown in Figure 5.2-17 (arbitrary units).

A reasonable decision criterion for selecting between A_1 and A_2 is to estimate the expected cost and choose the one with the lower value. Using the cost shown in Figure 5.2-17, the expected value is 75 cost units if A_1 is chosen, and 105 cost units if A_2 is chosen. This means that under our state of uncertainty, A_1 , should be selected.

Suppose now that further experiments and the results confirm H_0 so that the probability is 0.9 for H_0 and 0.1 for H_1 . Recalculating the expected value the costs are 55 cost units for A_1 and 29 cost units for A_2 .

Under the given selection rule, therefore, A_2 should be selected and not A_1 . If the previous selection is A_1 and the probability of H_0 turns out to be as in the second experiment, there is an expected loss of $55 - 29 = 26$ cost units. If the added experiment costs less, then, in this case, it will be desirable to conduct this experiment.

The above example provides a simple illustration of the decision model proposed in this work which contains some of the basic elements. The model proposed here is in an adapted version of an R&D selection code developed for LWR's (Ref. 5.2-9). The model contains capability to handle more alternatives and decision criteria based on factors other than the cost, e.g., structural reliability, public risk, and availability.

5.2.6.3 Conclusions and Recommendations

The above discussions analyzed some of the shortcomings of the traditional use of the design margin and probabilistic modeling concepts. An alternative approach which seems more logical and avoids the shortcomings has been discussed. The approach is based on the following steps:

1. Place the questions of design margin target and experimental needs in the broader context of the design decision process. The technique of the Safety Assurance Diagrams is recommended to provide this context. The technique has been demonstrated to provide the following capabilities:
 - failure characterization
 - insight into design margin allocation as a part of overall system design optimization,
 - contributors to uncertainty, its extent as controllability,
 - loading to be considered in design and testing, and
 - insight into experimental design.
2. Adapt the already developed computer code for the optimum selection of LWR R&D projects to identify the optimum design margins and level of experimental work needed for the HTGR applications.

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Table 5.2-1

Dynamic Fracture Toughness Values for Inconel 617
after Carburization in He-1% CH₄

Time (hour)	Carburization Depth (μm)		K _{ICY} (ksi-in ^{1/2})
	Grain Boundary	Maxtix	
10	1635	635	43.4
20	center	1460	37.9
80	center	2604	27.5

LINE OF DEFENSE DESCRIPTION

5.2-70

Table 5.2-3
LINE OF DEFENSE DESCRIPTION

LOD IDENTITY

S.A. DIAGRAM IDENT. NO. A

LOD NO. 2

REV. 0

DATE 9/27/83

S.A. DIAGRAM Accident Prevention-Reformer

LOD DESCRIPTION Institute a comprehensive quality assurance and quality control program
LOD OBJECTIVE to minimize assembly and fabrications deficiencies

Assignment ☒ Absolute ☐ Potential - Dependent upon
☐ Critical ☐ Mgt. decision, ☐ Development
☐ Regular

Type of Defense ☐ Design Feature ☒ Quality Assurance
☐ Physical Phenomenon ☒ Adm. Procedure
☐ Other Construction tests, acceptance tests

LOD IMPORTANCE

A. Effectiveness (to acceptable severity) Ineffective 0 | | | | | | | | | | X 1.0 Very Effective
Level of defense This is a first level of defense which screens out deficiencies which could have adverse consequences on licensing, safety and availability

Relationship to other LOD's This LOD reduces the challenges to other LODs and the frequency of such challenges

B. Strength 0 | | | | | | | | | | X 1.0 Absolute
Resistance against penetration Violable | | | | | | | | | | Barrier
This LOD has not been installed yet due to higher priorities of design. Its expected strength is very high due to the need to meet licensing requirements

Constancy This LOD should be matched with the fabrication and construction effort. Prefabrication and assembly planning is essential. Its effectiveness at and beyond preoperation is reduced.

C. Impact of Penetration 0 | | | | | | | | | | X 1.0 Totally Unacceptable
Accident severity impact No impact | | | | | | | | | | X
This LOD is for accident prevention but has an impact also on preventing and mitigating accident progressions

Summary LOD Importance 0 | | | | | | | | | | X 1.0 85

LOD VERIFICATION

Action for substantiation

☐ Design Confirmation

SDD No. _____

Fault Tree No. _____

☒ Experimental/Analytical

☒ Quality Assurance Procedure

☐ FMEA

☐ Other _____

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☒
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None 0 | | | | | | | | | | 1.0 High

Within current costs and schedule

None

With additional time of _____ mon.

With additional development costs of \$ _____

Firm verification needed by before preoperational testing

Recommendation A must LOD which should be installed before equipment

procurement

Table 5.2-4

LINE OF DEFENSE DESCRIPTION

LOD IDENTITY

S.A. DIAGRAM IDENT. NO. ALOD NO. 3REV. 0DATE 9/27/83

S.A. DIAGRAM

LOD DESCRIPTION Institute a preoperational test program to assure adequate conformance to
 LOD OBJECTIVE safety objectives

Assignment

- ☒ Absolute
☐ Critical
☐ Regular

- ☐ Potential - Dependent upon
☐ Mgt. decision, ☐ Development

Type of Defense

- ☐ Design Feature
☐ Physical Phenomenon
☒ Other Testing

- ☐ Quality Assurance
☐ Adm. Procedure

LOD IMPORTANCE

- A. Effectiveness (to acceptable severity) Ineffective 0 | | | | | | | | | | X | 1.0 Very Effective
 Level of defense This is a second level of defense to screen out deficiencies in design, fabrication and assembly penetrating LOD 1 and 2

Relationship to other LOD's This LOD is a backup of LOD Nos. 1 and 2 to reduce the challenge to other LODs and the frequency of these challenges.

B. Strength

- Resistance against penetration Violable 0 | | | | | | | | | | X | 1.0 Absolute Barrier
This LOD has not been installed yet due to higher priorities of design. Its strength is expected to be very high due to the need to meet licensing requirements

Constancy This program should be matched with the pre-operational schedule to rise to 100% power. Plans should be completed by the time FSAR is issued.

C. Impact of Penetration

- Accident severity impact No impact 0 | | | | | | | | | | X | 1.0 Totally Unacceptable

This LOD is for accident prevention, but also has an impact on preventing and mitigating accident progression

Summary LOD Importance

0 | | | | | | | | | | X | 1.0 0.9

LOD VERIFICATION

Action for substantiation

☐ Design Confirmation

SDD No. _____

Fault Tree No. _____

☒ Experimental/Analytical☐ Quality Assurance Procedure☐ FMEA☐ Other _____

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☐
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None 0 | | | | | | | | | | X | 1.0 High

Within current costs and schedule

None

With additional time of _____ mon.

With additional development costs of \$ _____

Firm verification needed by before rise to 100% power

Recommendation A must LOD which should be installed before rise to 100% power.

LINE OF DEFENSE DESCRIPTION

5.2-73

LINE OF DEFENSE DESCRIPTION

5.2-74

Table 5.2-7
LINE OF DEFENSE DESCRIPTION

<u>LOD IDENTITY</u>		REV. <u>0</u>
S.A. DIAGRAM IDENT. NO. <u>A51</u>	LOD NO. <u>1</u>	DATE <u>9/29/83</u>
S.A. DIAGRAM <u>Accident Source-Reformer Tube Blockage</u>		
<u>LOD DESCRIPTION</u>	Show that increase in reformer efficiency with flow reduction provides an inherent negative feedback mechanism to mitigate the temperature increase.	
<u>LOD OBJECTIVE</u>		

Assignment	☐ Absolute ☐ Critical ☒ Regular	☐ Potential - Dependent upon ☐ Mgt. decision, ☐ Development
Type of Defense	☐ Design Feature ☒ Physical Phenomenon ☐ Other	☐ Quality Assurance ☐ Adm. Procedure

LOD IMPORTANCE

A. Effectiveness (to acceptable severity) Ineffective 0 | | | | | | | | | | X | | | | | | | | | | 1.0 Very Effective
 Level of defense This is a third level of defense which could prove that the reformer is significantly tolerant to sizeable blockages to render the probability of critical blockages as incredible

Relationship to other LOD's This LOD is a backup of LOD's Nos. 1 thru 4 of accident prevention SADS.

B. Strength 0 | | | | | | | | | | X | | | | | | | | | | 1.0 Absolute
 Resistance against penetration Violable | | | | | | | | | | Barrier
 Currently unknown

Constancy May be limited to a range of blockages defining the critical size

C. Impact of Penetration 0 | | | | | | | | | | X | | | | | | | | | | 1.0 Totally
 Accident severity impact No impact | | | | | | | | | | Unacceptable
 The LOD is backed up by the following LODs

Summary LOD Importance 0 | | | | | | | | | | X | | | | | | | | | | 1.0 .5

LOD VERIFICATION

Action for substantiation ☐ Design Confirmation SDD No. _____ Fault Tree No. _____ ☒ Experimental/Analytical	☐ Quality Assurance Procedure ☐ FMEA ☐ Other _____
---	---

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☒
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None 0 | | | | | | | | | | 1.0 High

Within current costs and schedule None

With additional time of _____ mon. 0 | | | | | | | | | | 1.0 Now

With additional development costs of \$ 100K

Firm verification needed by FSAR issuing date

Recommendation _____ conduct the appropriate analysis within the funding level indicates

LINE OF DEFENSE DESCRIPTION

REV. 0

DATE 9/25/83

DATE 9/25/83

<u>LOD DESCRIPTION</u>	Show that the reformer has an adequate thermal margin in accordance with accepted
<u>LOD OBJECTIVE</u>	codes, standards and test data

Type of Defense ☒ Design Feature ☐ Quality Assurance
☐ Physical Phenomenon ☐ Adm. Procedure
☐ Other

A. Effectiveness (to acceptable severity) Ineffective | | | | | | | Effective
Level of defense This is a first line of defense which belongs to design assurance.
It is a specific application of LOD 1 of the accident prevention SAD.

B. Strength 0 10 Absolute
Resistance against penetration Violable | | | | | Barrier
The strength of this IOD is currently unknown but is expected to be very high
to conform with licensing requirements

C. Impact of Penetration
 Accident severity impact
 This LOD is backed up by the following LODs

0 1.0 Totally
 No impact | X | | | | | | | | | | Unacceptable

0 X 1.0 .7

Action for substantiation

☐ Quality Assurance Procedure

☐ FMEA☐ Other☐ Other

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☐
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Within current costs and schedule None 0
 With additional time of _____ min. 0
 With additional development costs of \$ _____ 0 Now
 Firm verification needed by _____

5.2-76

LINE OF DEFENSE DESCRIPTION

REV. 0

LOD NO. 4

DATE 9/29/83

S.A. DIAGRAM Accident Source-Reformer tube blockage

<u>LOD DESCRIPTION</u>	Demonstrate stability of small breaks of undetectable size under the
<u>LOD OBJECTIVE</u>	temperature and chemical environment of the reformer

Assignment

☐ Absolute
☒ Critical
☐ Regular

- ☐ Potential - Dependent upon
- ☐ Mgt. decision, ☐ Development

Type of Defense

☐ Design Feature
☒ Physical Phenomenon
☐ Other

- ☐ Quality Assurance
- ☐ Adm. Procedure

LOD IMPORTANCE

A. Effectiveness (to acceptable severity) Ineffective | | | | | Effective
Level of defense This is a second level of defense which is backed up by the preceding
100's 82

Relationship to other LOD's See above, this LOD also serves accident source diagram (A54) defect in reformer tube

B. Strength

Resistance against penetration Violable Barrier
Currently unknown due to uncertainty on the questions of effect of carburization
brittleness and fatigue.

Constancy Currently unknown

C. Impact of Penetration

Moderate impact due to support by following LODs

Summary LOD Importance

LOD VERIFICATION

Action for substantiation

☐ Design Confirmation

SDD No.

Fault Tree No.

☒ Experimental/Analytical

☐ Quality Assurance Procedure
☐ FMEA
☐ Other

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	not	Needed
a)				☐	☐	☐
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None ⁰ 1.0 High

Within current costs and schedule

With additional time of mon.

With additional development costs of \$

Firm verification needed by

Recommendation Work involved could be very extensive. A tradeoff between this

LOD and other LODs to scope the work needed should be given first priority

Table 5.2-11
LINE OF DEFENSE DESCRIPTION

LOD IDENTITY

S.A. DIAGRAM IDENT. NO. A51 LOD NO. 5

REV. 0

DATE 9/29/83

S.A. DIAGRAM Accident Source - Reformer tube blockage

LOD DESCRIPTION Demonstrate that the use of redundant and diverse detection systems lead to
LOD OBJECTIVE timely detection for orderly shutdown given a reformer tube break.

Assignment ☐ Absolute ☐ Potential - Dependent upon
☒ Critical ☐ Mgt. decision, ☐ Development
☐ Regular

Type of Defense ☒ Design Feature ☐ Quality Assurance
☐ Physical Phenomenon ☐ Adm. Procedure
☐ Other

LOD IMPORTANCE

A. Effectiveness (to acceptable severity) 0 Ineffective | | | | | | | | | | 1.0 Very Effective
Level of defense This is a third level of defense which is backed up by the
preceding LODs

Relationship to other LOD's See above. This LOD also serves accident source diagram (A54) - defect in reformer tube

B. Strength 0 Violable | | | | | | | | | | 1.0 Absolute Barrier
Resistance against penetration Strength of this LOD is currently unknown due to lack of IC& SDDs. Expected strength
is high due to redundancy and diversity (He side & process gas side contamination).

Constancy Possible aging effect in process gas environment.

C. Impact of Penetration 0 No impact | | | | | | | | | | 1.0 Totally Unacceptable
Accident severity impact

Summary LOD Importance 0 | | | | | | | | | | 1.0 7

LOD VERIFICATION

Action for substantiation

☒ Design Confirmation

SDD No. I&C

Fault Tree No.

☒ Experimental/Analytical

☐ Quality Assurance Procedure

☐ FMEA

☐ Other

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☒
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None 0 | | | | | | | | | | 1.0 High

Within current costs and schedule

None

With additional time of mon.

With additional development costs of \$

Firm verification needed by

Recommendation This LOD should be demonstrated when I&C design is available

LINE OF DEFENSE DESCRIPTION

REV. 0

LOD NO. 1

DATE 9/29/83

S.A. DIAGRAM Accident Progression diagram

LOD DESCRIPTION	Show that the small pressure differential between the reformer process.
LOD OBJECTIVE	gas and heating He allows ample time for automatic isolation of the reformer

- ☐ Absolute
- ☐ Critical
- ☒ Regular

- ☐ Potential - Dependent upon
- ☐ Mgt. decision, ☐ Development

☒ Design Feature
☒ Physical Phenomenon
☐ Other

- ☐ Quality Assurance
- ☐ Adm. Procedure

6

A. Effectiveness (to acceptable severity) Ineffective | | | | | Effective
Level of defense This is a fourth level of defense Its effectiveness
is currently unknown

Relationship to other LOD's This LOD is a backup of the following one and is backed up by the preceding ones.

7

Resistance against penetration

Violable | | | | | Barrier

This LOD is sensitive to pressure fluctuations within the control band and the breadth of this band. Faulty isolation could result in pressure shock which should be avoided.

Constancy Unknown

7

Accident severity impact

No impact | | | | | | | | | | Unacceptable

Moderate due to the next LOD

Summary LOD Importance

0 x 1.0 .3

Action for substantiation

☒ Design Confirmation

SPP No. Reformer Isolation Systems

Fault Tree No.

☒ Experimental/Analytical

☐ Quality Assurance Procedure

☒ FMEA☐ Other

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☐
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None⁰ 1.0 High

Within current costs and schedule

None

With additional time of mon.

With additional development costs of \$ 50K

Firm verification needed by FSAR Issuing date

Recommendation Investigation of the advantages and disadvantages of automatic isolation vs. manual isolation at the indicated level to determine the potential effectiveness of this IOD.

LINE OF DEFENSE DESCRIPTION

Related Program Status

Table 5.2-14

LINE OF DEFENSE DESCRIPTION

LOD IDENTITY

S.A. DIAGRAM IDENT. NO. AP52 LOD NO. 1 REV. 0
 DATE 9/29/83
 S.A. DIAGRAM Accident progressions diagram-Plant operations with Reformer tube failure

LOD DESCRIPTION Shows that jet impingement from a failing reformer tube on other tubes does
 LOD OBJECTIVE not cause their failure.

Assignment ☐ Absolute ☐ Potential - Dependent upon
☒ Critical ☐ Mgt. decision, ☐ Development
☐ Regular
 Type of Defense ☒ Design Feature ☐ Quality Assurance
☒ Physical Phenomenon ☐ Adm. Procedure
☐ Other

LOD IMPORTANCE

A. Effectiveness (to acceptable severity) Ineffective 0 1.0 Very Effective
 Level of defense This is a fifth level of defense. Preliminary analysis shows this LOD as highly effective

Relationship to other LOD's This LOD is backed up by LODs up to the fourth level

B. Strength 0 1.0 Absolute
 Resistance against penetration Violable Barrier
 Preliminary analysis shows high resistance due to natural phenomena involved.

Constancy Could deteriorate with aging of reformer tubes

C. Impact of Penetration 0 1.0 Totally
 Accident severity impact No impact Unacceptable
 Serious process gas ingress accident.

Summary LOD Importance 0 1.0 .9

LOD VERIFICATION

Action for substantiation

☒ Design Confirmation

SDD No. Reformer

Fault Tree No.

☒ Experimental/Analytical

☐ Quality Assurance Procedure

☐ FMEA

☐ Other

Related Program Status

Org.	Prog.	Activity	Ref (work plan)	going	ned	Needed
a)				☐	☐	☒
b)				☐	☐	☐
c)				☐	☐	☐
d)				☐	☐	☐
e)				☐	☐	☐
f)				☐	☐	☐
g)				☐	☐	☐
h)				☐	☐	☐

Assessment of current program capability for verification None 0 1.0 High

Within current costs and schedule

None

With additional time of mon.

With additional development costs of \$ 120K

Firm verification needed by FSAR Issuing date

Recommendation A must LOD which should be initiated ASAP to avoid massive tube failure propagation by jet impingement.

LINE OF DEFENSE DESCRIPTION

5.2-83

83-195-01

Figure 5.2.1 EXAMPLE MODEL STRUCTURE: THREE BAR PLANE FRAME

Figure 5.2-2 ELASTIC STRESS PATTERNS IN THE STRUCTURE AS CARBURIZATION PROCEEDS

83-195-03

Figure 5.2-3 STRUCTURAL MODEL ANALYSIS AS FUNCTION OF TIME

INITIAL FLAW = FINAL DEPTH
OF CRITICAL CARBURIZATION

83-146-09

Figure 5.2-4 ILLUSTRATION OF PROPOSED CODE METHOD FOR CARBURIZED COMPONENT DESIGN.

84-077-02

Figure 5.2-5 STRESS DISTRIBUTION ON COOL DOWN
IN REFORMER TUBE (SCHEMATIC)

84-077-03

Figure 5.2-6 K_I FOR AN EXTERNAL RADIAL EDGE CRACK IN A TUBE
SUBJECTED TO A UNIFORM INTERNAL PRESSURE

84-077-01

Figure 5.2-7 CRACK GROWTH RATE DATA FOR UNAGED INCONEL 617

84-077-04

Figure 5.2-8 ALLOWABLE STRESS RANGE VERSUS INITIAL FLAW DEPTH

Figure 5.2-9 GEOMETRY OF THE CRACKED SECTION OF THE PIPE.
THE CROSS SECTION CONTAINS TWO IDENTICAL AND
SYMMETRICALLY SITUATED CRACKS.

Figure 5.2-10 DESIGN DECISION PROCESS

83-513-01

Figure 5.2-11 ACCIDENT PREVENTION DIAGRAM-A: REFORMER

83-513-02

Figure 5.2-12 ACCIDENT SOURCE DIAGRAM (A51) – REFORMER TUBE BLOCKAGE

83-513-03

Figure 5.2-13 ACCIDENT PROGRESSION DIAGRAM (AP51) – PLANT SHUTDOWN WITH REFORMER TUBE FAILURE

83-513-04

Figure 5.2-14 ACCIDENT PROGRESSION DIAGRAM (AP52) – PLANT OPERATION WITH REFORMER TUBE FAILURE

83-513-05

Figure 5.2-15 ACCIDENT SOURCE DIAGRAM (A53) – CATALYST LOADING ERROR

83-513-06

Figure 5.2.16 ACCIDENT SOURCE DIAGRAM (A54) – DEFECT IN REFORMER TUBE

	H0	H1
A1	50	100
A2	10	200

Figure 5.2-17 EXAMPLE DECISION MATRIX

5.3 Materials Application Engineering

5.3.1 Analysis of the Continuous Cycling Fatigue Behavior of Inconel 617

5.3.1.1 Introduction

An effort was undertaken to compile the air data base for Inconel 617 as a precursor to the preparation of design curves and design allowables and to determine priority data needs. The compilation is divided into sections, as follows:

- A Tensile Properties
 - Yield and Tensile Strength
 - Modulus of Elasticity
- B Creep-Rupture Properties
 - Rupture Strength
 - 1% Total Strain
 - Tertiary Creep
- C Physical Properties
 - Mean and Instantaneous Coefficient of Thermal Expansion
 - Thermal Conductivity
- D Fatigue Properties
 - Low and High Cycle Fatigue
 - Creep Fatigue and Damage Envelope

The compiled data is summarized in Tables 5.3-1 to 5.3-4. Table 5.3-1 provides an overall summary, on a heat by heat basis, of the data available for the four property categories. The attempt here has been to establish

how well any given heat has been tested over the range of materials properties needed for allow qualification. Heats which do not contribute significantly to the data base, by criteria stated on the table, are not included in this summary. Tables 5.3-2 to 5.3-4 provide detailed summaries of each heat for which data was available (except Physical Properties, for which only average values from an unknown number of heats are currently available). Accompanying each table is a description of alloy qualification requirements, and an assessment of the data needs. This work provides the basis for continuing efforts to develop data correlations and design allowables, and should provide input to the specification of further testing on this alloy.

A preliminary analysis of the available Inconel 617 continuous cycling fatigue data was undertaken with the primary goal of providing HTGR designers and stress analysts with well documented design curves. Analysis of the effects of various parameters, e.g., heat treatment, specimen geometry, testing technique, etc. was considered outside the scope of this work, and in any event, the amount of data available limits the usefulness of such an assessment. Although the compilation contains data from tests conducted in non-air environments, only air data were utilized in this analysis. The analyzed data is presented in the form of average curves (i.e., best fit to experimental data) and design curves incorporating the standard 2-20 reduction on total strain range and cycles to failure (Ref. 5.3-2).

5.3.1.2 Data Base

The data used for this analysis is a subset of the data base compiled in Reference 5.3-1. Essentially, only the following were used:

- 1) data from fully reversed, push-pull, constant amplitude strain-controlled tests

- 2) data from tests conducted in air and on material in the as-received and aged (in Ar) condition

Data from GE, GA, ORNL, and the FRG were utilized. All these laboratories conducted tests at a strain rate of 4×10^{-3} /sec, using a triangular wave form. Specimen geometries varied, and other effects (e.g., surface finish) have not been assessed in the present study. Japanese, Huntington Alloys, Inc., and Batelle data have not been included in this assessment because i) tabulated data were not available, ii) background material and test information were not available, and/or iii) the tests did not meet the conditions of 1 and 2 above.

5.3.1.3 Analytical Description of Data

Of the many models available in the literature for fitting fatigue data (Ref. 5.3-3 through 5.3-6), a modification of the form originally published by Langer (Ref. 5.3-6) was selected. The reasons for this selection are:

- 1) the Langer equation was used in the development of ASME design fatigue curves, and thus represents an accepted (although not necessarily the "best") method of analysis,
- 2) trial fits to the data demonstrated that most of the data populations could be fit by the equation, with both good descriptive ability and good predictive ability,
- 3) the elastic and plastic components of the total strain range were not available in many cases, thus precluding use of the widely accepted and popular power law equations (Ref. 5.3-3).

A derivation of the modified equation and rationale for the modifications is presented by Booker (Ref. 5.3-7). This reference, and the original work

by Langer (Ref. 5.3-6) are recommended to the reader requiring further information. Pertinent aspects of the analysis technique are summarized below.

The form of the Langer equation used in this study is:

$$\log (\log (N_f)) = B_1 \log (\Delta \epsilon_t - \Delta \epsilon_0) + B_0$$

where

N_f is the number of cycles to total specimen failure,

$\Delta \epsilon_t$ is the total strain range in %, and

B_1 , B_0 , and $\Delta \epsilon_0$ are regression coefficients.

A linear least squares regression was performed on the transformed variables, with $\log (\log (N_f))$ as the dependent variable. Since the available software was not capable of performing a non-linear regression analysis, values of $\Delta \epsilon_0$ were guessed (this allows B_0 and B_1 to be determined by a linear least squares method). These guesses were iteratively modified to obtain a regression with the highest coefficient of determination.

The experimental data was then plotted in the conventional manner [$\log$ (strain range) vs. $\log$ (total cycles) to failure], and the best fit line (as determined above) was drawn through the data. This mean curve was then converted to a design curve(s) by applying a factor of two reduction on the total strain range and a factor of 20 reduction on the cycles to failure. This results in two curves; the lower curve at any value of N_f is the design curve. Typically, the cusp between the two design curves is smoothed to yield one continuous lower bound curve. Currently, however, our software does not have this capability. As a result, tabulated design data will follow the design curves to the point of intersection of the two design

curves, and will be vary slightly non-conservative (in the cusp region) relative to values from a smoothed curve. Also, the curves have not been corrected for mean stress or for elastic Poisson's ratio (Ref. 5.3-8).

5.3.1.4 Results

The results of the study are contained in Section 5.3.2. Figures 5.3-1 and 5.3-2 are two sets of mean curves for all the temperatures. Figure 5.3-1 is for data in increments in °F (e.g., 1300, 1400, etc.), and Figure 5.3-2 is for data in increments in °C (e.g., 750, 850). A curve for the 1400°F data has been drawn on each figure to allow comparisons between the two. These curves are useful for visualizing data trends with temperature, although they do not provide information for specific design use. Following this, the design curves for each temperature are presented (shown as dashed curves), accompanied by log-log scatter plots of the fatigue data with the best-fit curve (solid line) drawn through the data points. The scatter plots can be used as a guide to the range of applicability of the design curves.

Scatter plots of the transformed (e.g., $\log \log N_f$) variables are also presented, both for unoptimized ($\Delta \epsilon_0 = 0$) and optimized regressions. The design curves are developed using the optimized equation (except where noted); the equation (with the regression coefficients) is included for each temperature, along with pertinent comments.

5.3.1.5 Conclusions

A set of equations and preliminary fatigue design curves has been developed for Inconel 617. These curves were developed using accepted ASME practice, and are based on well documented data. The curves are not yet corrected for mean stress and elastic Poisson's ratio effects. The preliminary conclusions that can be drawn from the data are:

1. Additional data in the $>10^5 N_f$ regime is needed at 1600°F, and less critically so, at 1000°F.
2. If the elastic and plastic components of the total strain range can be obtained, a power law fit to the data should be tried.

REFERENCES

- 5.3-1 American Society of Mechanical Engineers, "Criteria for Design of Elevated Temperature Class 1 Components in Section III, Division of the ASME B&PV," New York, May 1976.
- 5.3-2 S.S. Manson, "'Fatigue' A Complex Subject - Some Simple Approximations," Exp. Mech. 5(7):193-226 (July 1965).
- 5.3-3 M.K. Booker et al., "Mechanical Property Correlations for 2½Cr-1Mo Steel in Support of Nuclear Reactor Systems Design," Int. J. Pressure Vessel Piping 5:181-205 (1977). Also ORNL/TM-5329 (June 1976).
- 5.3-4 R.C. Rice and C.E. Jaske, "Consolidated Presentation of Fatigue Data for Design Applications," Society of Automotive Engineers, Detroit, 1974.
- 5.3-5 B.F. Langer, "Design of Pressure Vessels for Low-Cycle Fatigue," J. Basic Eng. 84:389-402 (Sept. 1962).
- 5.3-6 J.P. Strizak et al., "The Influence of Temperature, Environment, and Thermal Aging on the Continuous Cycle Fatigue Behavior of Hastelloy X & Inconel 617," ORNL/TM-8130, April 1982.
- 5.3-7 M.K. Booker et al., "Analysis of the Continuous Cycling Behavior of 2½Cr-1Mo Steel," ORNL/TM-5593, Dec. 1979.

Table 5.3-1 INCONEL 617 AIR DATA SUMMARY

Heat Number	Product Form (19.1 mm)	Supplier	Test Laboratory	Average Grain Size (ASTM No.)	Tensile ¹			Creep- ² Rupture		Physical Properties		Fatigue				
					σ_y	UTS	E	S_r	$S_{1\%}$	α_1	α_m	Thermal Conductivity	LCF ($<10^6$ Cycles)	HCF ($>10^6$ Cycles)	C-F	D
XX00A1USL	3/4 in (19.1mm) HR ROUND	HAI	HAI	2	8	8		--	--							
	0.062 in (1.57mm) CR SHEET	HAI	HAI		7	7		--	--							
XX00A4USL	1 in (25.4mm) HR ROUND	HAI	HAI	1.5	*	*		2	2***							
XX00A4US	?	HAI	GA	2-3	--	--		4***	--				--	--	--	--
XX00A5USL	5/8 in (15.9mm) HR ROUND	HAI	HAI	2.5	**	**		2	1							
XX00A7US	0.047 in (1.19mm) CR SHEET	HAI	BATTELLE		24†	24†		--	--				10††	13††	--	--
XX01A3US	1/2 in (12.7mm) PLATE	HAI	ORNL	1.5-2	60	60		2	--				59	--	--	--
XX05A4UK	1/2 in (12.7mm) HR ROUND	HAI	HAI	2.5	22	22		2***	1***							
XX07A7UK	5/8 in (15.9mm) HR ROUND	HAI	HAI	3	22	22		3	--							
XX09A1UK	3.15 in OD (80mm) x .59 in (15mm) WALL TUBE	HAI	FRG, KFA; IA	2									14	1	6	6
XX14A6UK	5/8 in (15.9mm) PLATE	HAI	GE	00-0.5	--	--	--	2	--	--	--	--	2	2	--	--
XX19A6UK	7/8 in (22.2mm) BAR	HAI	GA	1.5	--	--	--	--	--	--	--	--	18	5	13	?
XX20A5UK	0.187 in (4.75mm) CR SHEET	HAI	HAI	2	12	12		--	--							
XX26A8UK	0.04 in (1.02mm) CR SHEET	HAI	HAI		21	21		--	--							
?	?	?	FRG	?	9	--										
?	?	?	JAPAN	?									6	--	--	--

Footnotes on next page.

Footnotes for Table 5 3-1

- * 3 data points at $T > 1200$ F.
- ** 2 data points at $T > 1200$ F.
- *** Includes data from discontinued tests.
- + Only six data points are at $T > 1200$ F, and these are all at 1600 F.
- ++ Tests were not done under fully reversed loading conditions, and were all at $T \leq 1200$ F.
- 1 Only heats which significantly contribute to the development of elevated temperature strength curves were included; i.e, at least 4 data points at $T > 1200$ F. The total number of data points for each heat meeting the above criterion is shown.
- 2 Only heats with greater than 10,000 hour "time to rupture" or greater than 5000 hour "time to 1% total strain", at at least two temperatures above 1200 F, were included. The number of data points shown for each heat are only those meeting the above duration criteria. "Time to onset of tertiary" was not included due to questions concerning the meaning and reliability of such measurements.

Table 5 3-2 SUMMARY OF INCONEL 617 YIELD AND TENSILE STRENGTH AIR DATA.
Number of Data Points per Temperature, °F(°C)

Heat Number; Product Form (Laboratory)	Prior Test Exposure Conditions	RT	100	200	300	400	500	550	600	700	800	900	1000	1100	(650) 1200	(750) 1382	(800) 1400	(800) 1472	(850) 1500	(850) 1562	(900) 1600	(950) 1700	(950) 1742	(1000) 1800	(1000) 1832	(1000) 1900	(1000) 2000
XX00A1USL; 0.062" (1.57mm) CR SHEET (HAI)	NONE; AS - SOLUTION TREATED	2											1		1		1				1			1			1
XX00A1USL; 3/4" (19.1mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED	1											1		1		1				1			1			1
XX00A1US(L); 3/4" (19.1mm) HR ROUND (HAI)	AGED UP TO 1K HRS IN AIR AT 1200-1600°F, THEN AIR COOLED	10																									
XX00A2USL; 9/16" (14.3mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED	1																									
XX00A2USL; 3/4" (19.1mm) HR ROUND (HAI)	NONE; AS-SOLUTION TREATED	1																									
XX00A3USL; 1/2" (12.7mm) HR FLAT (HAI)	NONE; AS-SOLUTION TREATED	2																									
XX00A3USL; 1" (25.4mm) HR FLAT (HAI)	NONE; AS-SOLUTION TREATED	2																									
XX00A3USL; 12"x12"xL (304.8mm) FORGED SQR (HAI)	NONE; AS-SOLUTION TREATED	4																									
XX00A3US; 0.575" (14.6mm)x3.875" (98.4mm)xL HR FLAT (HAI)	NONE; AS-SOLUTION TREATED	2																									
	AGED UP TO 24K HRS IN AIR AT 1100 TO 1400°F, THEN AIR COOLED	27																									
XX00A3US; 0.521" (13.2mm)x3.875" (98.4mm)xL CR FLAT - 10% CW (HAI)	AGED UP TO 24K HRS IN AIR AT 1100-1800°F, THEN AIR COOLED	31																									
XX00A4USL; 1" (25.4mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED	2		1		1			1		1		1	1	1	1		1			1						
XX00A4US; 1" (25.4mm) HR ROUND (CA)	AGED ~3K HRS IN AIR AT 1472 & 1652°F	1-2																									
XX00A5USL; 5/8" (15.9mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED	1		1		1			1		1		1	1	1		1				1						
XX00A5USL; 0.032" (0.813mm) CR SHEET (HAI)	NONE; AS - SOLUTION TREATED	1													1		1										
XX00A5US; 5/8" (15.9mm) HR ROUND (CA)†	AGED 30K HRS IN AIR AT 1000 - 1500°F	1																									
XX05A4UK; 1/2" (12.7mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED	4											1		1		1		1							1	1

† Additional data for 100 & 8000-hr. exposure, and also at 1100°F, is available and being requested.

Table 5.3-2 (cont'd) SUMMARY OF INCONEL 617 YIELD AND TENSILE STRENGTH AIR DATA (Continued)
Number of Data Points Per Temperature, °F(°C)

Heat Number; Product Form (Laboratory)	Prior Test Exposure Conditions	RT	100	200	300	400	500	550	600	700	800	900	1000	1100	1200	1300	1382	1400	1472	1500	1562	1600	1700	1742	1800	1832	1900	2000
XX05A7UK; 0.170" (4.32mm) WALL x 1" OD CD TUBE (HAI)	NONE; AS-SOLUTION TREATED	1																										
XX07A7UK; 5/8" (15.9mm) HR ROUND (HAI)	NONE; AS-SOLUTION TREATED	2	1	1	1	1	1		1	1	1	1	1	1	1	1		1		1		1	1		1		1	1
XX18A4UK; 0.190" (4.83mm) CR SHEET	NONE; AS-SOLUTION TREATED	1																										
XX20A5UK; 0.187" (4.75mm) CR SHEET (HAI)	NONE; AS-SOLUTION TREATED	2		1		1			1		1		1		1			1				1			1			1
XX26A7UK; 0.04" (1.02mm) CR SHEET	NONE; AS-SOLUTION TREATED	1	1	1	1	1	1		1	1	1	1	1	1	1	1		1		1		1	1		1		1	1
XX00A7US; 0.047" (1.19mm) CR SHEET (BAFFELLE)	NONE; AS-SOLUTION TREATED	6									6				6							6						
XX14A6UK; 5/8" (15.88mm) PLATE (GE)	NONE; AS-SOLUTION TREATED	3																										
XX14A6UK; 5/8" (15.88mm) PLATE (CA)	?	1																										
XX01A3US; 1/2" (12.47mm) PLATE (ORNL)	NONE; AS-SOLUTION TREATED	3			2			3			2	3	2	3	2	3		2		3		2	3		2			
XX01A3US; 1/2" (12.47mm) PLATE (ORNL)	AGED UP TO 20K HRS IN Ar AT 1000-1600°F, THEN AIR COOLED	12											5			4						4						
?; ? (KFA)	NO AGING	3																										
	AGED TO 1K HRS AT 1562°F AND 1742°F	12																										
?; ? (KFA)	?																3				3			3				

†Additional Data, for 100 & 8000 hr. exposure, and also at 1100°F, is available and is being requested.

TENSILE PROPERTIES

Yield and Tensile Strength

Code Case N47 Data Requirements (Ref 1)

- 1) 5 commercial size heats, minimum, representing the product forms expected in typical service.
- 2) Data at 100°F intervals, from RT to 100°F higher than the maximum intended use temperature (with consideration given to performing tests at 50°F intervals in regions of rapidly changing properties).
- 3) Heats should originate from several different suppliers, if possible, and should represent extremes of the chemistry range, while including heats which are representative of current commercial practice.
- 4) Replication of tests is not specified.

Assessment of Current Status¹

- 1) Essentially complete temperature coverage on four heats,
XX05A4UK; 1/2" HR Round, HAI
XX07A7UK; 5/8" HR Round, HAI
XX26A8UK; 0.04" CR Sheet, HAI
XX01A3US; 1/2" Plate, ORNL
- 2) Partial temperature coverage (to 1600°F only) on two heats,
XX00A4USL; 1" HR Round, HAI
XX00A5USL; 5/8" HR Round, HAI
- 3) Partial temperature coverage (to 2000°F, but not 100°F increments) on 3 heats,
XX00A1USL; 0.062" CR Sheet, HAI
XX00A1USL; 3/4" HR Round, HAI
XX20A5UK; 0.187" CR Sheet, HAT
- 4) Assessment includes only limited FRG & Japanese data, and should be revised when further such data is made available.
- 5) Overall status: Need full temperature coverage on at least one additional heat, which should be a tube, or similar, product form - preferably from a different supplier.

Notes to Table 5.3-2 (cont'd)

Modulus of Elasticity

Code Case N47 Data Requirements (Ref 1)

- 1) No specific quantity requirements, other than full temperature range coverage.

Assessment of Current Status

TBD

Footnote 1 Intended use temperature taken to be RT to 1900°F
(includes transients).

Ref 1: Guidelines for Required Data on New Materials, Elevated
Temperature Nuclear Construction, ASME B&PV Code Case
1592 Supplement.

Table 5.3-3 SUMMARY OF INCONEL 617 CREEP-RUPTURE AIR DATA
Number of Data Points Per Temperature, °F (°C)

Heat Number; Product Form (Laboratory)	Prior Test Exposure Conditions	800	1100	1200	1300	(750) 1382	1400	(800) 1472	1500	(850) 1562	1600	(900) 1652	1700	(950) 1742	1800	(1000) 1832	1900	(1050) 1922	2000	(1100) 2012
XX00A1USL; 0.062 in (1.57mm) CR SHEET (HAI)	NONE; AS - SOLUTION TREATED									2R ¹	2R 3S ²				3S 2T ³					2R 3S 3T
XX00A1USL; 3/4 in (19.1mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED			3R 3S 1T			4R 3S 2T		3R 4S 3T		4R 6S 2T		3R 1S 1T		4R 5S 3T		5R 3S 3T		4R 4S 5T	
XX00A2USL; 9/16 in (14.3mm) HR ROUND† (HAI)	NONE; AS - SOLUTION TREATED								2R		2R 1S				2S 2T	3R				4R 1S 1T
XX00A3USL; 1/2 in (12.7mm) HR FLAT†† (HAI)	NONE; AS-SOLUTION TREATED								4R		4R									4R
XX00A3USL; 12 in (304.8mm)x12 inxL FORGED SQR (HAI)	NONE; AS-SOLUTION TREATED								6R		6R									6R
XX00A4USL; 1 in (25.4mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED		3R 3S 3T	1R 2S 1T			3R 4S 4T				6R 6S 6T				1R 2S 2T	1R 2S 2T				
XX00A4US; ? (GA)	?							1R		2R		1R								
XX00A5USL; 5/8 in (15.9mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED		4R 4S 4T	3R 4S 3T			2R 5S 2T				3R 4S 3T				2R 2S 2T	2R				
XX00A5USL; 0.032 in (0.813mm) CR SHEET (HAI)	NONE; AS - SOLUTION TREATED										1R									

†Dimensions not certain; could also be 3/4 in (19.1mm) HR Round.

††Dimensions not certain; could also be 1 in (25.4mm) HR Flat.

¹Rupture

²S = 1% Total Strain

³T = Tertiary Creep

Table 5.3-3 (cont'd) SUMMARY OF INCONEL 617 CREEP-RUPTURE AIR DATA (Continued)
Number of Data Points Per Temperature, °F (°C)

Heat Number; Product Form (Laboratory)	Prior Test Exposure Conditions	800	1100	1200	1300	(750) 1382	1400	(800) 1472	1500	(850) 1562	1600	(900) 1652	1700	(950) 1742	1800	(1000) 1832	1900	(1050) 1922	2000	(1100) 2012
XX00A9US; ? (GA)										1R										
XX05A4UK; 0.5 in (12.7mm) HR ROUND (HAI)	NONE; AS - SOLUTION TREATED			2R 2S 2T	3R 2S 2T		2R 2S 2T		3R 3S 3T		3R 2S 2T				2R 2S 2T	2R 2S 1T			1R 1S 1T	
X05A7UK; 0.170 in (4.32mm) WALL x 1 in (25.4mm) OD CD TUBE (HAI)	NONE; AS-SOLUTION TREATED						1R 1S 1T				3R 3S 2T				2R 2S 2T	2R 1S 1T				
XX07A7UK; 5/8 in (15.88mm) HR ROUND (HAI)	NONE; AS-SOLUTION TREATED			3R 2S 2T	3R 3S 3T		2R 2S 2T		3R 3S 3T		4R 3S 3T				1R 1S 1T	2R 2S 2T			2R 2S 2T	
XX10A0UK; ? (GA)	?						2R				2R									
XX10A3UK; 0.71 in (18.03mm) WALL x 5.4 in (173.2mm) OD EXTRUDED TUBE (HAI)	NONE; AS-SOLUTION TREATED			1R 1S 1T			1R 1S 1T				2R				1R 1S 1T				1R 1S 1T	
XX18A4UK; 0.190 in (4.83mm) CR SHEET (HAI)	NONE; AS-SOLUTION TREATED										1R									
XX20A5UK; 0.187 in (4.75mm) CR SHEET (HAI)	NONE; AS-SOLUTION TREATED			3R 3S 3T	1R 1S 1T		2R 2S 2T				3R 1S 1T		1S 1T							

Table 5.3-3 (cont'd) SUMMARY OF INCONEL 617 CREEP-RUPTURE AIR DATA (Continued)
Number of Data Points Per Temperature, °F (°C)

Heat Number; Product Form (Laboratory)	Prior Test Exposure Conditions	800	1100	1200	1300	(750) 1382	1400	(800) 1472	1500	(850) 1562	1600	(900) 1652	1700	(950) 1742	1800	(1000) 1832	1900	(1050) 1922	2000	(1100) 2012
XX41A7UK; 0.048 in (1.22mm) CR SHEET (HAI)	NONE; AS-SOLUTION TREATED			1R 1S 1T							*		*		1R 1S 1T					
XX00A7US; 0.047 in (1.19mm) CR SHEET (BATTELLE)	NONE; AS-SOLUTION TREATED	1R		3R 3S							3R 3S									
XX14A6UK; 5/8 in (15.88mm) PLATE (GE)	?					1R 1S				3R 1S				3R 1S				3R 1S		1R 1S
XX01A3US; 1/2 in (12.47mm) PLATE (ORNL)	?		1R 1S 1T	1R 1S 1T			2R 2S 2T			2R 2S 2T	6R 3S 3T									
?; ? (FRG)								1R 9S		6- 26R 29S		9R 18S		10- 23R 11S		15R 20S				
?; ? (IHI/Japan)																27R 4S				
?; ? (IHI; Hitachi/Japan)	?											6R				38R 4S		5R		

*Test in progress.

CREEP-RUPTURE PROPERTIES

Rupture and Creep Strength

Code Case N47 Data Requirements (Ref 1)

- 1) 8 commercial size heats, minimum, representing the product forms expected in typical service.
- 2) Desirably, data at 100°F intervals, from 800°F to 100°F higher than the maximum intended use temperature. It is recognized that experimental difficulties exist at the lower end of the temperature range which may preclude the generation of large amounts of data.
- 3) Additional guidance below assumes a design life of 300,000 hrs (~40 years at ~85% availability). For applications requiring shorter design lives, test data requirements may be reduced in time.
- 3a) Rupture Tests-
Approximately five data points per heat/temperature, extending from less than 10 hours to more than 10K hrs, with 20K hours very desirable.
- 3b) Creep Tests-
A minimum of four data points per heat/temperature. Accurate strain measurements should be made from zero time to beyond the onset of tertiary creep, and should encompass secondary creep rates ranging from ~10 to less than 0.01% per 1000 hours. Some creep data for periods greater than 5000 hours should be available.
- 4) Heats should originate from several different suppliers, if possible, and should represent extremes of the chemistry range, while including heats which are representative of current commercial practice.
- 5) Replication of tests is not specified.

Assessment of Current Status

General Remarks

- 1) No data between 800°F-1100°F
- 2) Fill in tests with available archive material may be helpful in taking better advantage of the existing, but inadequate data base for several of the heats (Note: This approach should be followed for other properties as well--a strategic data acquisition plan, which best utilizes the available archive material, should be developed.)
- 3) The assessment includes only limited FRG & Japanese data and should be revised when further such data is made available.

NOTES TO TABLE 5.3-3 (Cont'd)

A) Rupture

- 1) Essentially complete temperature coverage, above 1100°F, on:

XX00A1USL; 3/4" HR Round, HAI
XX05A4UK; 1/2" HR Round, HAI
XX07A7UK; 5/8" HR Round, HAI

Heats XX00A1USL & XX05A4UK have an insufficient number of data points at most temperatures (i.e., <5); also the duration of the longest test at each temperature is inadequate (i.e., <10K hrs). Test durations for Heat XX07A7UK are <10k hrs at 4 temperatures, but the number of data points at each temperature is inadequate.

- 2) Poorer temperature coverage (i.e., not every 100°F) on:

XX00A4USL; 1" HR Round, HAI
XX00A5USL; 5/8" HR Round, HAI

Each heat has less than 5 data points per temperature; Heat XX00A4USL has 2 data points in excess of 10K hrs, heat XX00A5USL has 4 data points in excess of 10K hrs.

- 3) Overall Status:

Need full temperature, quantity (no. of data points at a given temperature), and duration coverage on at least 5 and possibly 7 heats - including tube, or a similar product form.

B) 1% Total Strain

- 1) Essentially complete temperature coverage, above 1100°F, on:

XX00A1UEL, XX05A4UK, and XX07A7UK

These heats have an insufficient number of data points at most temperatures (i.e., <4). The maximum test duration at most of the temperatures is also inadequate (i.e., <5K hrs).

- 2) Poorer temperature coverage (i.e., not every 100°F) on:

XX00A4USL, XX00A5USL

These heats have an insufficient number of data points at most temperatures. The maximum test duration at most of the temperatures is also inadequate.

- 3) Overall Status: similar to that for rupture.

NOTES TO TABLE 5.3-3 (Cont'd)

C) Tertiary

- 1) Essentially complete temperature coverage, above 1100°F, on:

XX00A1USL, XX05A4UK, XX07A7UK

These heats have an insufficient number of data points at most temperatures (i.e., <4). The maximum test duration at most of the temperatures is inadequate (i.e., <5K hrs).

- 2) Poorer temperature coverage (i.e., not every 100°F) on:

XX00A4USL, XX00A5USL

These heats have an insufficient number of data points at each temperature; however, the maximum duration of many of the tests for XX00A5USL is adequate.

- 3) Overall assessment: similar to that for rupture.

Ref 1: Guidelines for Required Data on New Materials, Elevated Temperature Nuclear Construction, ASME B&PV Code Case 1592 Supplement.

PHYSICAL PROPERTIES

Coefficient of Thermal Expansion, Thermal Conductivity

Code Case N47 Data Requirements (Ref 1)

- 1) No specific quantity requirements, other than full temperature range coverage.

Assessment of Current Status

TBD

Ref 1: Guidelines for Required Data on New Materials, Elevated Temperature Nuclear Construction, ASME B&PV Code Case 1592 Supplement.

Table 5.3-4 SUMMARY OF INCONEL 617 LOW¹ AND HIGH² CYCLE AIR FATIGUE DATA

Number of Data Points Per Temperature, °F (°C)

Heat Number; Product Form (Test Laboratory)	Prior Test Exposure Conditions	RT < T < 1000	(538) 1000	(593) 1100	(649) 1200	(704) 1300	(750) 1382	(760) 1400	(815) 1500	(850) 1562	(871) 1600	(927) 1700	(950) 1742	(982) 1800	(1000) 1832
XX19A6UK; 7/8 In (22.2mm) Bar (GA)	NONE; AS - SOLUTION TREATED					4L ¹ 2H ²		5L	4L 2H			5L 1H			
	CARBURIZED (ACCELERATED CONDITIONS)					5L			4L 2H						
XX01A3US; 1/2 In (12.7mm) PLATE (ORNL)	NONE; AS - SOLUTION TREATED		10L			7L					11L				
	AGED 10K Hrs; Ar		6L			6L					6L				
	Aged 20K Hrs In Ar		3L			6L					4L				
XX14A6UK; 5/8 In (15.88mm) PLATE (GE)	NONE; AS - SOLUTION TREATED									2L 2H					
XX09A1UK; 3.15 In (80mm) OD X 0.59 In (15mm) WALL TUBE (FRG)	?						(4L) ³ (1H)			(6L)			(4L)		
? ; ? (JAPAN)	NONE; AS - SOLUTION TREATED														(6L)
	CARBURIZED														(6L)
	"HE" EXPOSED														(3L)
XX00A7US; 0.047 In (1.194mm) SHEET (BATTELLE)	NONE; AS - SOLUTION TREATED	RT: 3L 7H 800°F: 4L 3H			3L 3H										

FOOTNOTES: 1) LCF < 10⁵ Nf2) HCF > 10⁵ Nf

3) Values in parentheses represent data points indicated on a curve, but for which tabulated data was not available.

FATIGUE PROPERTIES

Low and High Cycle Fatigue

Code Case N47 Data Requirements (Ref 1)

- 1) An absolute minimum of 3 commercial size heats, which account for the poorer behavior which might be expected from variations in product form, orientation, and heat treatment. One of the heats should be a master heat, with all the minimum requirements (below) performed on this heat. The remaining heats should be tested at, for example, every other condition.

- 2) Data should be obtained at a minimum of three or four test temperatures, as defined below:
 - a) at 200°F above the Section III maximum use temperature (800°F + 200°F = 1000°F min. temperature)
 - b) at the maximum use temperature as determined by the interested party, taken to be 1900°F (including transients).
 - c) at an intermediate temperature to a) & b) that shall not differ from a or b by more than 200°F. Depending upon the range defined by a) & b), tests at more than 1 additional intermediate temperature may be necessary to meet the requirements of c).
 Further, tests at less than 200°F increments may be necessary if the fatigue properties are particularly temperature sensitive. If, for adjacent curves, the strain range at a given life exceeds a factor of 1.5, or if the cyclic life for a given strain range exceeds a factor of 50, then an additional fatigue curve should be determined at an intermediate temperature.

- 3) A minimum of 12 (with a preferred maximum of 16) data points per temperature/heat of material. Data points may either be uniformly distributed along the curve, with no intentional duplication, or they may be grouped at several specific strain ranges.

- 4) The requirements for the "cycles to failure" range are not clear, although reference 1 indicates the total life spectrum to be from 10^1 to 10^8 cycles to failure.

Assessment of Current Status

- A) Low Cycle Fatigue ($<10^5 N_f$)
 - 1) None of the heats have sufficient temperature coverage to be considered a "master" heat, although
 XX01A3US, 1/2" Plate, ORNL
 is closest (in terms of quantity). The other heats do not have a sufficient number of data points at any of the required temperatures to be considered even a secondary heat.

NOTES TO TABLE 5.3-4 (Cont'd)

- 2) Overall status: Need data on at least 1 additional heat of tube or similar product form, at all required temperatures, and with the minimum number of data points at each temperature. Fill-in testing on archive material of the heats for which data is presently available may obviate the need for complete testing on an additional heat.
- B) High Cycle Fatigue ($>10^5 N_f$)
- 1) A virtually complete dearth of data.
 - 2) Overall status: Need to obtain full quantity of data per code requirements. This will necessitate testing of archive material in the high cycle regime--to be combined with low cycle data to develop a complete fatigue curve.

Creep-Fatigue and Damage Accumulation

Code Case N47 Data Requirements (Ref 1)

- 1) The number of required heats is not specified, but is presumed to be the same as for fatigue.
- 2) Data should be obtained at the same temperatures as the continuous cycling fatigue tests.
- 3) A minimum of 12 failure data points should be obtained at each temperature, with the failures covering the cyclic life span as evenly as possible, and to as high a cyclic life as possible.
- 4) Hold times and cycle type (e.g., tensile strain hold) should be determined from exploratory tests, at the highest temperature, and selected to maximize the damage to the material.

Assessment of Current Status

- 1) Virtually no data available (only 18 tests from GA, for which tabulated data is available).

Ref 1: Guidelines for Required Data on New Materials, Elevated Temperature Nuclear Construction, ASME B&PV Code Case 1592 Supplement.

5.3.2 Summary of Inconel 617 Fatigue Data

FIGURE 5.3-1 **INCONEL 617 FATIGUE**

PREDICTED AVERAGE VALUES

FIGURE 5.3-2

INCONEL 617 FATIGUE

PREDICTED AVERAGE VALUES

FIGURE 5 3-3

INCONEL 617 FATIGUE**1000 F ORNL AIR TESTS; AS-RECEIVED AND AGED CONDITION**

FIGURE 5.3-4 **INCONEL 617 FATIGUE**

1000 F ORNL AIR TESTS; AS-RECEIVED AND AGED CONDITION

1000°F Data

Unoptimized

$$\log (\log (N_f)) = -.38932 \log (\Delta \epsilon_t) + .51462 \quad (1)$$

$$R^2 = .95708$$

$$\Delta \epsilon_t (\%) = 20.9818 (\log N_f)^{-2.5686} \quad (2)$$

Optimized

Same as unoptimized

Comments

Due, in part, to the lack of data at higher N_f values, the equation has poor extrapolating ability. The equation (1,2) should not be used outside the range of the experimental data. Attempts at optimization yielded physically unreasonable extrapolations; hence, the unoptimized equation was fit to the experimental data. Due to the poor curve behavior, design data in tabular form is not presented.

FIGURE 5.3-5

INCONEL 617 FATIGUE

1300 F GA & ORNL AIR TESTS; AS-RECEIVED & AGED CONDITION

FIGURE 5.3-6 **INCONEL 617 FATIGUE**

1300 F GA & ORNL AIR TESTS; AS-RECEIVED & AGED CONDITION

FIGURE 5.3-7 **INCONEL 617 FATIGUE**

1300 F GA & ORNL AIR TESTS; AS-RECEIVED & AGED CONDITION

1300°F Data

Unoptimized

$$\log (\log (N_f)) = -.43285 (\log \Delta \epsilon_t) + .47945 \quad (1)$$

$$R^2 = .94061$$

Optimized

$$\log (\log (N_f)) = -.29210 \log (\Delta \epsilon_t - .24) + .42864 \quad (2)$$

$$R^2 = .96822$$

$$\Delta \epsilon_t (\%) = 29.3388 (\log N_f)^{-3.4235} + .24 \quad (3)$$

Comments

Optimization visibly improves the data fit (least squares regression of the transformed variables). The equation (2,3) should have reasonable predictive ability, since the curve seems to be approaching an endurance limit.

FIGURE 5.3-8 **INCONEL 617 FATIGUE**

1382 F (750 C) FRG AIR TESTS

FIGURE 5.3-9

INCONEL 617 FATIGUE

1382 F (750 C) FRG AIR TESTS

FIGURE 5.3-10 INCONEL 617 FATIGUE
1382 F (750 C) FRG AIR TESTS

1382°F (750°C) Data

Unoptimized

$$\log (\log (N_f)) = -.49106 (\log \Delta \epsilon_t) + .42813 \quad (1)$$

$$R^2 = .98875$$

Optimized

$$\log (\log (N_f)) = -.33550 \log (\Delta \epsilon_t - .18) + .39001 \quad (2)$$

$$R^2 = .99825$$

$$\Delta \epsilon_t (\%) = 14.5370 (\log N_f)^{-2.9806} + .18 \quad (3)$$

Comments

Optimization visually improves the data fit. Since there is no high cycle ($< 10^5 N_f$) data, and the curve does not appear to have reached an endurance limit, use of the equation beyond the range of experimental data is not recommended.

FIGURE 5.3-11

INCONEL 617 FATIGUE

1400 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-12 **INCONEL 617 FATIGUE**

1400 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-13 **INCONEL 617 FATIGUE**
 1400 F GA AIR TESTS; AS-RECEIVED CONDITION

1400°F Data

Unoptimized

$$\log (\log (N_f)) = -.59952 (\log \Delta \epsilon_t) + .35427 \quad (1)$$

$$R^2 = .94372$$

Optimized

$$\log (\log (N_f)) = -.22865 \log (\Delta \epsilon_t - .24) + .38725 \quad (2)$$

$$R^2 = .98862$$

$$\Delta \epsilon_t (\%) = 49.3897 (\log N_f)^{-4.3735} + .24 \quad (3)$$

Comments

Optimization visibly improves the data fit. The equation (2,3) should have good predictive ability, since the curve seems to have reached an endurance limit.

FIGURE 5.3-14 **INCONEL 617 FATIGUE**

: 1500 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-15 **INCONEL 617 FATIGUE**

1500 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-16 **INCONEL 617 FATIGUE**

1500 F GA AIR TESTS; AS-RECEIVED CONDITION

1500°F Data

Unoptimized

$$\log (\log (N_f)) = -.52364 (\log \Delta \epsilon_t) + .37895 \quad (1)$$

$$R^2 = .93584$$

Optimized

$$\log (\log (N_f)) = -.20811 \log (\Delta \epsilon_t - .196) + .42161 \quad (2)$$

$$R^2 = .99678$$

$$\Delta \epsilon_t (\%) = 106.1451 (\log N_f)^{-4.8052} + .196 \quad (3)$$

Comments

Optimization visibly improves the fit. The equation (2,3) should have good predictive ability, since the curve appears to be approaching an endurance limit.

FIGURE 5.3-17 INCONEL 617 FATIGUE

1562 F (850 C) GE & FRG AIR TESTS; AS-RECEIVED CONDITION

5.3-46
LGLG CYC. TO FAILURE

LOG STRAIN RANGE

FIGURE 5.3-18 **INCONEL 617 FATIGUE**

1562 F (850 C) GE & FRG AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-19 **INCONEL 617 FATIGUE**

1562 F (850 C) GE & FRG AIR TESTS; AS-RECEIVED CONDITION

NOTES TO FIGURES 5.3-17, 5.3-18 and 5.3-19

1562°F (850°C) Data

Unoptimized

$$\log (\log(N_f)) = -.53065 (\log \Delta\epsilon_t) + .39162 \quad (1)$$

$$R^2 = .88822$$

Optimized

$$\log (\log(N_f)) = -.23527 \log (\Delta\epsilon_t - .26) + .36574 \quad (2)$$

$$R^2 = .93402$$

$$\Delta\epsilon_t(\%) = 35.8554 (\log N_f)^{-4.2504} + .26 \quad (3)$$

Comments

Optimization visibly improves the fit, although as evidenced by the R^2 value, the fit is relatively worse for this data set than other temperatures. The equation (2,3) should have reasonable predictive ability, since the curve seems to have reached an endurance limit.

FIGURE 5.3-20 **INCONEL 617 FATIGUE**

1600 F ORNL AIR TESTS; AS-RECEIVED & AGED CONDITION

FIGURE 5.3-21 **INCONEL 617 FATIGUE**

1600 F ORNL AIR TESTS; AS-RECEIVED & AGED CONDITION

1600°F Data

Unoptimized

$$\log (\log(N_f)) = -.27199 \log \Delta\epsilon_t + .43873 \quad (1)$$

$$R^2 = .95030$$

$$\Delta\epsilon_t (\%) = 41.0239 (\log N_f)^{-3.6766} \quad (2)$$

Comments

Due to lack of data at higher N_f values, the equation has poor extrapolating ability. The equation (1,2) should not be used outside the range of the experimental data. Attempts at optimization yielded physically unreasonable extrapolations; hence, the unoptimized equation was fit to the experimental data. Due to the questionable extrapolation, design data in tabular form is not presented.

FIGURE 5.3-22 **INCONEL 617 FATIGUE**
1700 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-23 **INCONEL 617 FATIGUE**

1700 F GA AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-24 **INCONEL 617 FATIGUE**

1700 F GA AIR TESTS; AS-RECEIVED CONDITION

1700°F Data

Unoptimized

$$\log (\log(N_f)) = -.50227 (\log \Delta\epsilon_t) + .33334 \quad (1)$$

$$R^2 = .91848$$

Optimized

$$\log (\log(N_f)) = -.21337 \log (\Delta\epsilon_t - .14) + .40676 \quad (2)$$

$$R^2 = .99011$$

$$\Delta\epsilon_t (\%) = 80.6046 (\log N_f)^{-4.6867} + .14 \quad (3)$$

Comments

Optimization visibly improves the fit. The equation (2,3) has good predictive ability.

FIGURE 5.3-25 **INCONEL 617 FATIGUE**

1742 F (950 C) FRG AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-26 **INCONEL 617 FATIGUE**

1742 F (950 C) FRG AIR TESTS; AS-RECEIVED CONDITION

FIGURE 5.3-27 **INCONEL 617 FATIGUE**

1742 F (950 C) FRG AIR TESTS; AS-RECEIVED CONDITION

NOTES TO FIGURES 5.3-25, 5.3-26 and 5.3-27

1742°F (950°C) Data

Unoptimized

$$\log (\log (N_f)) = -.41319 \log \Delta \epsilon_t + .36531 \quad (1)$$

$$R^2 = .95294$$

Optimized

$$\log (\log (N_f)) = -.18001 \log (\Delta \epsilon_t - .25) + .35995 \quad (2)$$

$$R^2 = .99110$$

$$\Delta \epsilon_t (\%) = 99.9105 (\log N_f)^{-5.5553} + .25 \quad (3)$$

Comments

Optimization visibly improves the fit. The curve seems to be approaching an endurance limit, and the equation should have good predictive ability (within the limitation that only a few data points were used to develop the equation).

6.0 HTGR-SC/C PROJECT MANAGEMENT (WBS 30)

6.1 Lead Plant Systems Engineering

An HTGR-SC/C Lead Plant Systems Engineering (LPSE) group was chartered by DOE in July 1982 to:

1. Prepare the systems engineering related portions of the Project Management Plan, including the configuration management procedure, design interface procedure, documentation and design review procedure, and the HTGR-SC/C Lead Plant Documents listing.
2. Coordinate and approve plant and system level design disclosure documents (OPDS, SDD's, ICD's).
3. Identify and coordinate plant level analyses and trade studies.
4. Plan and coordinate the accomplishment of Baseline Design Reviews.
5. Manage plant and system level requirements through management of the allocation process based on plant level analyses, trade-off studies, and engineering judgment.

The LPSE was formed with representatives from the organizations participating on the HTGR Program, and made considerable progress toward establishing the basis for project execution of the systems engineering functions. GE-ARSD provided a full-time senior project engineer as an assistant manager of the HTGR-SC/C LPSE team. The GE representative's experience and knowledge in systems engineering principles, practices, and methods resulted in valuable contributions to the efforts for formulating a systems engineering approach to the HTGR-SC/C Lead Plant Project.

The purpose of the LPSE team is to build a foundation of procedures, methods, documentation, analyses and reviews that guide the project in a structured manner as it matures from the development stage toward the preliminary design stage. The following is a brief summary of the HTGR Lead Project's LPSE effort to:

- Implement the systems engineering process.
- Implement DOE's "Integrated Approach" to economical, reliable, safe nuclear power production.

LPSE has issued procedures for Configuration Management and Design and Documentation Review. The Plant Documentation Listing has been placed under GCRA control. The Design Interface control procedure has continued to evolve and at this writing is still in draft form.

6.1.2 Plant Level Disclosure Documents

The major effort has been the implementation of the Integrated Approach into the project documentation. LPSE has drafted several versions of the Overall Plant Design Specification and has issued for comment a Revision 0 draft. Response to comments is ongoing and the redraft for project use in 1984 is scheduled for January 30, 1984.

6.1.3 Functional Analysis

The LPSE has completed a functional analysis of the four goals of the "Integrated Approach," i.e., Maintain Plant Operation, Maintain Plant Protection, Maintain Radionuclide Containment and Maintain Emergency Preparedness. Goals 1, 2, and 3 were completed by LPSE while Goal 4 was adapted from work by Baltimore Gas and Electric under DOE direction. These functional trees have been provided to the participants for use in developing the design requirements.

LPSE has met with other participants in DOE's program to implement the "Integrated Approach" in an effort to develop a generic functional analysis for Safe, Economical Nuclear Power. Participants have been LPSE, Baltimore Gas and Electric, University Research Foundation, Sandia, Georgia Tech., TVA, and DOE. This work is ongoing.

6.1.4 Requirements Analyses

The functional analysis formed the basis for requirements analysis which the systems organizations were asked to use as a basis for documentation of systems requirements in the SDDs and SSDDs. The mixed implementation of this concept by the systems (SDDs) has led LPSE into an explicit definition of the concept of requirements management. Figure 6.1-1 illustrates the requirements documentation logic that has been put forward by LPSE. As shown in the figure, the requirements are developed at several levels starting at the plant level and following down through the system, subsystem and components levels. A major discipline being advocated by LPSE is that the proper level of requirements should appear in the respective documentation, i.e., plant level requirements in OPDS-Section 5 and SDD, etc. A second discipline put forward by LPSE is the systematic development of requirements through interfacing systems analyses at several levels.

6.1.5 Plant-Level Analyses and Trade Studies

An effort has been initiated to list the required analyses, trade studies and reports which eventually will comprise the project data base. The notes to Figure 6.1-1 illustrate the initiation of this work. Eventually these reports will form the basis for requirements traceability.

(1) NOTES - SEE NEXT PAGE

FIGURE 6.1-1 REQUIREMENTS DOCUMENTATION LOGIC

NOTES TO FIGURE 6.1-1

HTGR-SC/C LEAD PLANT

ANALYSIS REPORTS

(1) PLANT

HCA-20101 APPLICATION OF THE INTEGRATED APPROACH
HCA-20102 PLANT DESIGN BASIS TRANSIENT ANALYSIS
HCA-20107 PLANT AVAILABILITY ANALYSIS
HCA-20109 PROBABILISTIC RISK ASSESSMENT
HCA-20110 INVESTMENT RISK ASSESSMENT
HCA-20111 PLANT MAINTAINABILITY ASSESSMENT
HCA-20112 PLANT INSERVICE INSPECTION ASSESSMENT
HCA-20114 PLANT CONSTRUCTABILITY/SCHEDULE ASSESSMENT
HCA-20115 PLANT OPERABILITY ASSESSMENT
HCA-20116 PLANT SHIELDING ANALYSIS
HCA-20117 PLANT SECURITY ASSESSMENT
HCA-20118 PLANT COST AND SCHEDULE (INDEPENDENT ANALYSIS)
HCA-20120 PLANT FIRE HAZARD ANALYSIS
HCA- PLANT PERFORMANCE ANALYSIS
HCA- PLANT DECOMMISSIONING ANALYSIS

(2) MULTISYSTEM

HCA-20103 PLANT CONTROL ANALYSIS
HCA-20104 PLANT PROTECTION ANALYSIS
HCA-20105 PLANT SEISMIC ANALYSIS
TBD

(3) SYSTEM

HCA-30113 SYSTEM FABRICABILITY
HCA-30119 ELECTRICAL SYSTEM STABILITY ANALYSIS
TBD

(4) TBD

(5) TBD

(6) TBD

7.0 HTGR-SC/C PLANT INTEGRATION (WBS 31)

7.1 Plant Simulation

7.1.1 Introduction

General Electric has responsibility for the development of a computer simulation of the HTGR-SC/C lead plant. This simulation will be used for a number of engineering and systems analysis tasks, including:

- o Systems Transient Analysis
- o Control Systems Analysis
- o Plant Protective Systems Analysis
- o Plant Control Room Evaluation

Ultimately, the plant simulation software will be utilized and implemented in the operator training simulator.

7.1.2 Requirements

Simulation software requirements were developed and documented. The software requirements are prepared to define the objectives and specific performance requirements of the simulation code from its inception through its development as an engineering analysis tool, including support of Control Room development and finally as software for the plant training simulator. The key software requirements applicable to the current effort are discussed below.

7.1.2.1 Analytical Capabilities

- o Transient Analysis for the structural design and specification of plant components.
- o Evaluation and analysis of plant controls under system transient events.

- o System Transient Analysis as boundary conditions for detailed core/component design assessment.
- o System Transient Analysis to evaluate the performance of alternate component configurations and designs.
- o Evaluation and analyses of plant protection trip settings.
- o Evaluation and analysis of alarm set points and ranges for events where the transient exceeds the normal control range.

7.1.2.2 Transient Capability

Normal Transients

Startup from a PCRV depressurized condition
 Startup with a PCRV pressurized condition
 Shutdown to a PCRV depressurized condition
 Shutdown to a PCRV pressurized condition
 Rapid process steam load increase (3%/minute)
 Rapid process steam load decrease (3%/minute)
 Normal process steam load increase (0.5%/minute)
 Normal process steam load decrease (0.5%/minute)
 Step process steam load decrease (-20% of nominal load)
 Rapid electric load increase (5%/minute for 25-100%)
 Normal electric load increase (0.42%/minute 25%-100% and 50%-100%)
 Rapid electric load decrease (5%/minute for 100%-25%)
 Normal electric load decrease (0.42%/minute 100%-25% and 100%-50%)
 Step electric load increase (+10%)

Upset Transients

Multiple loop shutdown/trip
 Single loop shutdown

Reactor trip from full load
Reactor trip from low power
HP and/or IP/LP turbine trip & load rejection
Process steam load rejection
Sudden reduction of feedwater flow
Steam generator tube leak (1.0 lbm/second)
Control rod insertion
Main loop overcooling (e.g., excess feed flow)
Steam transmission line leak (small)

Emergency Transients

Slow primary system depressurization
Rod withdrawal with core power-to-flow trip
Loop shutdown with helium valve failure
Failure of circulator speed control
Loss of helium flow (failure of non-safety control)
Shutdown of all main loops (false PPS signal, or turbine trip + LOSP¹)
Main steam piping leak (small)
Total loss of feedwater flow²

Faulted transients

Sudden depressurization and shutdown with CACS Rod withdrawal with backup trip on high steam generator inlet helium temperature

7.1.2.3 Performance

- o Real-time computation for man-machine interface evaluations, control room mock-up, operating procedure evaluations, control room design evaluations, and systems transient evaluations of relatively short duration (one hour).

- o Faster than real-time (10X) execution for system transients involving extended lengths of time (hours).
- o Slower than real-time computation for transients that involve complex computations more detailed than normally called for by a systems transient model. (Example: detailed core model.)

-
- 1 Provides boundary conditions as input to specialized analysis of core heatup under natural circulation conditions.
 - 2 Provides boundary conditions for specialized analysis of depressurization dynamic loadings, core response under CACS operation.

7.1.2.4 Accuracy

The accuracy of the simulation must be sufficient to allow use of the simulation for licensing purposes. Validation of the simulation will be required.

7.1.2.5 Software Requirements

Simulation software will be prepared using an established software engineering process. This process involves: 1) software planning, 2) software development, and 3) software validation. The major elements in the development of the software are:

- o System requirements analysis
- o Preliminary software planning
- o Software requirements
- o Software design
- o Software test
- o Software configuration control
- o Software maintenance

7.1.2.6 Language

Fortran-77 is used as the simulation language for all software.

7.1.3 Simulation Software Design

The simulation software has been developed in a structured, modular format. The modular design allows for development of the code in a manner that can be readily organized and efficiently prepared. A modular structure allows development and testing of self-standing portions of the code and then integrating modules for the total system simulation.

Top level functional modules are identified in Figure 7.1-1. The description of these top level modules is as follows:

Interactive Software (Figure 7.1-1)

Transient setup and interactive commands to the simulation are handled with a software module which provides the interface between the user and the simulation. This interactive software is used to select a particular transient, set up run control parameters, and initiate the run.

Output Software (Figure 7.1-1)

The output software in GCRS-1 provides an interface between the simulation software and graphics displays or post-processing routines. In GCRS-1, the output software receives values of selected parameters from the simulation software at regular intervals during the transient, converts the format and writes to output files for post-processing. Output files are set up for post processing by the Honeywell H6000 Versatec plotter and for processing by the General Electric Advanced Reactor Systems Department Simulator Facility. Graphics display software has been written for the GE-ARSD Simulator Facility to generate CRT displays, to access and process the output data files and to display the data on CRT's.

Input Data File (Figure 7.1-1)

The input data required to make a transient analysis is contained in a file which is read by the simulation software during an analysis. This file provides all physical data and thermal hydraulic conditions necessary for the simulation software modules. The file is user provided for a selected initial steady state condition or generated as a restart file from an analysis with the code.

Simulation Software (Figure 7.1-1)

Simulation software is used to calculate plant transient response. It is comprised of software modules which provide the functions described below.

Executive Module: This module controls the execution sequence of the various control system, component, and subsystem modules. It also provides for module interfacing and transfer of boundary condition variables from modules. The executive module sets up the input and output data files and controls reading and writing data files.

Boundary Condition Module: This module is used to read boundary condition variables and analysis control variables at the beginning of a transient. It is also used to specify changes in these variables during a transient in order to control the analysis.

Thermal-Hydraulic System Modules: These modules are used to model the physical phenomena based on first principles techniques of thermodynamics, heat transfer, and fluid flow. These modules evaluate and integrate first order ordinary differential equations to compute the time response of state variables, including pressure, temperature and flow rates. The integration scheme is

a constant time step Euler technique. Figure 7.1-2 illustrates the organization of the top level thermal hydraulic modules. These modules are described below:

HECIRC - The module HECIRC is used to evaluate the primary loop hydraulics, the circulator speed control and the performance of the helium circulator. The module computes flow rate and inlet pressure for the core and the steam generator.

CORE - The core module is used to calculate the thermal response of the core. The helium temperature is calculated with a quasi-static approximation and the temperature distributions in the fuel and moderator are calculated as state variables.

REACT - REACT is used to evaluate the core reactivity including control rod and scram reactivity and feedback reactivity due to temperature effects.

KINET - The module KINET is used to calculate prompt and decay power for the core using point reactor kinetics equations.

STMGEN - The module, STMGEN, models the helical coil once through steam generators. Heat transfer correlations are included for saturated liquid, critical heat flux, and saturated vapor for water on the tube side. The model contains dynamic energy equations for the tubes and water. On the shell side, the helium temperatures are calculated with a quasi-static approximation.

MSHEDR - The main steam header module, MSHEDR, is used to calculate the main steam pressure from conservation of mass and energy equations. This module contains calculations for steam flow rates through control valves in the bypass lines

to the condenser and the the process line inlet. It also calculates vent flow to the atmosphere from the main steam header.

FWFLOW - The feedwater flow module calculates the feedwater flow rate to the steam generator and the feedwater pressures.

FDWHTR - The feedwater heater module, FDWHTR, calculates the thermal response of the feedwater heaters to changes in feedwater flow, steam flow and flow enthaply. The shell side is modeled as a homogeneous condensing volume using conservation of mass and energy.

DEAR - The deaerator module, DEAR, is used to calculate the deaerator pressure from conservation of mass and energy equations for a homogeneous volume.

TURGEN _ The turbine system model, TURGEN, evaluates turbine performance and computes extraction flow rates, pressures and enthalpies.

PROSINLT - The module PROSINLT models the process steam transmission line inlet region. It is used to evaluate the vent flow rate and flow rate through the bypass from the main steam header to the process line. The process flow rate is an input boundary condition. A mass balance and an energy balance are used to calculate the process line inlet conditions.

CONTROL SYSTEM MODULES: These modules simulate the function of proportional/integral/differential controllers and other control components in the plant design. Figure 7.1-3 illustrates the top level control system model used in the simulation. The control system model contains a module for the supervisory control, a module for the valve controllers and a module for the reactor control rod controller. These modules are described below:

SUPCONT - The module SUPCONT evaluates the plant load demand, sets demanded turbine valve positions for manual control and determines set points for the controllers.

VLVCTL - The module VLVCTL contains models for all valve controllers. It is used to compute control valve demand positions and also to compute control valve position based on a valve response model.

CTLRD - The module CTLRD models the control system for the control rods and calls a control rod drive subroutine to calculate transient response and position of the control rods and the scram rods.

7.1.4 Data Flow Structure

Figure 7.1-4 is a top level diagram indicating data flow between the software/hardware modules. Each of the connecting lines represents data transfer between modules. Those shown solid are currently operational. Those shown dashed represent an interactive real-time run mode with data displayed in real-time to the user and with the user modifying the run as it executes. This capability will be implemented in the future.

The present capability includes communication from the user of a transient selection via a case number and user set run control parameters. This information sets up the transient analysis. The simulation software reads input data from data files (user provided) and performs the transient analysis. At regular time intervals, values of selected parameters are transferred to the display software which write this data to an output file. The output file is accessed for post-processing. The figure indicates the use of the GE-ARSD simulator graphics terminal which sends processed data to CRT's for displays or to a plotter for engineering plots.

7.1.5 Benchmark Transients

Several plant transients have been selected to be compared against MTLAP (HTGR plant model developed by GA Technologies). The following benchmark transients have been selected:

- Process load ramp down at 3%/minute from full power, 100% process flow.
- Trip of an independent IP/LP turbine from 50% process flow.
- All turbines trip from low process flow condition.
- All turbines trip from 100% process flow condition.
- Rapid process flow shutoff from 100% process flow.
- Reactor trip from full power 100% process flow.
- Process load ramp up at 3%/min. from 0% process flow.

The results of one transient (process load ramp down at 3%/minute from full power, 100% process flow) are shown in Figures 7.1-5 through 7.1-11 to illustrate the performance of the simulation. Figure 7.1-5 shows the core parameters and mainsteam header pressure during this transient. Reactor power, core flow and feedwater flow all remain constant during this maneuver. Figure 7.1-6 shows the process flow being reduced at 3%/minute, while steam flow to the IP/LP turbine increases. The valve position to the inlet of the IP/LP turbine is shown in Figure 7.1-7 as well as the closing of the letdown valve, which reduces steam flow to Heater No. 6 as feedwater heating begins to increase as a result of increased extraction flows to the LP heaters (Nos. 1, 2, & 3), the deaerator, and Heater No. 5. Figure 7.1-10 provides the pressure changes in the heaters and 7.1-11 illustrates the feedwater heater temperatures.

FIGURE 7.1-1. BLOCK DIAGRAM OF HTGR SC/C
LEAD PLANT ENGINEERING
SIMULATION

FIGURE 7.1-2.

Figure 7.1.3. CONTROL SYSTEM MODEL DIAGRAM

Figure 7.1-4. TOP LEVEL DATA FLOW DIAGRAM

KEY SYSTEM PARAMETERS

FIGURE 7.1-5

FEEDWATER/TURBINE FLOWS

HTGR S/C LEAD PLANT

PROCESS LOAD RAMP DOWN

AT 3% / MINUTE FROM

FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-6

VALUE POSITIONS

PARAMETER		RANGE	LINE TYPE
IPT1 VALVE POSN	2	0- 100	—————
LETDOWN ULV POSN	2	0- 100	
HPT VALVE POSN	2	0- 100	-----
DEB LETDOWN POSN	2	0- 100	- - - - -
EXT SHIFT ULV PSN	2	0- 100	- - - - -

HTGR S/C LEAD PLANT

PROCESS LOAD RAMP DOWN
AT 3% / MINUTE FROM
FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-7

STEAM/FEEDWATER TEMPERATURES

PARAMETER	RANGE	LINE TYPE
STEAM HEADR TEMP	F 0-1250	Solid
PRCS LINE IN TEMP	F 0-1250	Dotted
FINAL FDWTR TEMP	F 0- 500	Dashed

HTGR S/C LEAD PLANT

PROCESS LOAD RAMP DOWN

AT 3% / MINUTE FROM

FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-8

FEEDWATER HEATER FLOWS (Cont.)

HTGR S/C LEAD PLANT

PROCESS LOAD RAMP DOWN

AT 3% / MINUTE FROM

FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-9

PRESSURES

HTGR SC/C LEAD PLANT

PROCESS LOAD RAMP DOWN

AT 3% / MINUTE FROM

FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-10

STEAM/FEEDWATER TEMPS (Cont.)

FTGR SC/C LEAD PLANT

PROCESS LOAD RAMP DOWN

AT 3% / MINUTE FROM

FULL POWER, 100% PROCESS FLOW

FIGURE 7.1-11

8.0 MODULAR REACTOR SYSTEM (WBS 41)

8.1 Modular Reactor Design

8.1.1 Safety Assessment

The main objective of the Safety and Licensing assessment in FY83 was the development of a comprehensive safety approach and a plan to assure the MRS safety and licensability. The MRS safety philosophy and the comprehensive approach developed are discussed in Section 8.1.1.1. Section 8.1.1.2 discusses the design basis events for the reformer.

8.1.1.1 MRS Safety Philosophy

According to Reference 8.1-1, known safety measures can be arranged in rough order of preference as shown in Table 8.1-1. The MRS Safety Philosophy is based on a defense-in-depth approach which puts highest priority on the first two methods of hazard elimination and hazard level limitation. In particular, the use of a relatively small, low-power, and low-power density core limits the level of radioactivity inventory and the maximum fuel temperature under hypothetical accident conditions, which in turn limits the potential radionuclide release. Despite these intrinsic advantages, other safety measures shown in Table 8.1-1 have been used to serve the dual purposes of enhancing the plant safety and reducing plant outages and investment risk.

To implement the above philosophy, work in FY83 was directed to:

1. Develop a comprehensive safety assurance approach.
2. Evaluate the compatibility of the MRS and HTGR Lead Plant safety criteria to make utmost use of the technology and experience developed under the Lead plant.

8.1.1.1.1 Safety Assurance Approach

A comprehensive safety assurance approach has been developed for the MRS. The approach contains the following elements:

1. Safety Assurance Diagrams (SADs).
2. Probabilistic Risk Assessment (PRA).
3. Investment Risk and Operational Safety Assessment.
4. Design Basis Accident Definition and Safety Classification of Systems.
5. Safety Design Optimization.
6. Safety Test Planning.

A description of these elements and the work completed in FY83 is presented below.

8.1.1.1.2 Safety Assurance Diagrams

The MRS concept offers unique inherent safety characteristics as discussed above. The concept also provides novel features such as the use of a steel reactor vessel, non-graphite core support structure, and high temperature steam-methane reforming. Safety analyses to date have confirmed the conceptual feasibility of the MRS-R concept. However, as the MRS-R design moves from its conceptual stage to the preliminary stage, it is imperative to: (1) determine if any significant safety issues have been overlooked that would later lead to costly design changes, (2) identify the safety areas common with the HTGR-SC/C Lead Plant to make utmost use of the advanced stage of design of the lead plant, and (3) lay the ground work for the organization and defense of the MRS-R safety in the environmental and safety analysis reports. Of paramount importance in this regard is to determine how best to demonstrate the claimed inherent safety features by such measures as system testing.

The technique of Safety Assurance Diagrams (SADs) (Ref. 8.1-2) have been used extensively for the Fast Flux Test Facility to accomplish objectives similar to that above. The technique provides a qualitative tool to identify and evaluate the effectiveness of safety assurance provisions prior to and during operation. This technique had been selected for application during FY83. Due to the novel nature of the MRS-R concept, some preparatory work had to be done before the SADs could be developed. Specifically, the safety assurance approach applied in this work contained the following steps:

1. Scoping. This step identifies the following parameters:
 - a. Inhabited areas, such as off-site land, control room and containment (intermittent occupation), and a broad definition of the function of the inhabitants and their occupation pattern.
 - b. Sources of hazardous material, nuclear and otherwise, by type, location, and level of hazard. Examples are reactor core, radwaste system, and reformer (containing carbon monoxide).
 - c. Potential leak paths from the hazard sources to the identified inhabited areas.
 - d. Potential failure causes and modes of the hazardous material sources. These causes should be divided into two categories: (1) internal to the system containing the hazard source, e.g., faulty reformer catalyst, or error in fuel loading, and (2) external to the system, e.g., loss of flow as a cause of radionuclide release or earthquake.
2. Developing the Safety Assurance Diagrams. For each of the causes identified above, three types of SADs are developed:
 - a. Accident Prevention Diagrams: These diagrams are

principally concerned with the inherent engineered safety features and preoperational administrative and technical efforts to identify flaws in design, fabrication, transport, and assembly prior to operation.

- b. Accident Source Diagrams: These diagrams are principally concerned with system response to off-normal occurrences resulting from undiscovered flaws in the accident prevention stage and the inherent and engineered lines of defense that prevent the progression of these flows from threatening the containment of hazardous material sources.
 - c. Accident Progression Diagrams: These diagrams are principally concerned with the progression of threats to the hazardous material sources and the inherent and engineered lines-of-defense that limit the release of the hazardous material to inhabited areas.
3. Documentation. Documentation of the safety assurance analysis involves both the SADs and description sheets for the identified lines-of-defense (LODs). These LODs are inherent and engineered safety features that prevent or have the potential to prevent an off-normal situation from progressing into a serious accident. The information provided in the LOD sheets does not only describe the LOD, but also shows its relation to SDDs, its importance, and how the safety issues involved will be resolved. The LOD description sheets provide a cornerstone in the safety assurance approach and in the defense of the safety of the analyzed concept.

8.1.1.1.3 Safety Design Optimization

In the course of the MRS design and analytical work conducted in FY82 and FY83, many viable alternatives design and operating parameters have been identified. Due to the large number of these alternatives, only some

have been evaluated on a case by case basis. To aid the design process in a more systematic fashion, a computerized procedure was defined to aid in the selection of the optimum alternative which meets safety and licensing constraints and to optimize the plant economic and availability performance. The defined procedure has been tested for other applications and is being planned for application to the MRS concepts in FY84.

8.1.1.1.4 Safety Test Planning

Demonstrating the inherent safety characteristics of the MRS presents challenging test requirements. In contrast to engineered safety systems which can be tested prior to and during operation without the need for simulating the accident environment, inherent safety characteristics such as heat removal by natural convection or timely heat transport by conduction and radiation to limit the maximum core temperature cannot be demonstrated under normal operating conditions or adequately extrapolated from these conditions. A conceptual test plan based on a concept similar to the "approach-to-criticality" procedure has been identified for evaluations to be carried out in FY84 (Ref. 8.1-3). Future activities to identify and evaluate out-of-pile, in-pile, pre-operational, and operational tests for the MRS are also discussed in Reference 8.1-3.

8.1.1.2 Design Basis Event Definition and Safety Classification

Work in this area had the objective of developing a comprehensive set of design basis events to allow the proper use of codes and standards in the design activities. The list of events developed, their frequency and the corresponding service levels are shown in Table 8.1-2.

8.1.2 MRS Structural Assessment

This section summarizes the structural analyses and evaluations completed in FY83 on the MRS-R reformer and its major sub-components. The objective of this task was to provide a scoping evaluation of the structural adequacy of the reformer for the vertical in-line 950°C direct cycle reforming plant concept.

This assessment required the preparation of structural design requirements, the defining of a plant duty cycle, the selection of appropriate umbrella transients, determination of the sub-component thermal response, performing the necessary primary loading and fatigue stress analyses, redesigning components (where necessary) and re-evaluating the component thermal response and stress analyses. The results of this task are summarized below.

8.1.2.1 Plant Duty Cycle/Umbrella Transients Definitions

The evaluation of the structural adequacy of the Vertical In-Line (VIL) 950°C direct cycle reforming plant concept required the performing of various preparatory tasks including the defining of the plant duty cycle and the selecting of representative umbrella transients.

This section briefly describes the type of duty cycle and umbrella transients which were considered in evaluating and analyzing the structural design of the 950°C Direct Cycle MRS-R and its components.

The selected transients are representative of conditions which are sufficiently severe or frequent to be of possible significance to the cyclic behavior of plant components. The transients described herein are meant primarily for use in the component stress analysis and do not necessarily represent actual plant operation. These transients, when used as a base for component structural design, will provide confidence that the component is appropriate for its application over the design life of the plant.

The "best estimate" of the MRS-R representative plant transients for service loadings (Levels A,B,C, and D) are defined and compiled in Table 8.1-2. Selected umbrella upset and emergency transients events were identified for use in the plant concept structural evaluation. These events are descriptively summarized below and in Table 8.1-3 to highlight the severe service seen by the process components in the VIL reformer concept.

8.1.2.1.1 Selected Umbrella Events

A. Upset Events

1. Isolation and blowdown of steam generator (B-11)

This event is characterized by a reactor trip from full power and the steam generator isolated and dumped on the secondary side (either due to a tube leak or secondary system malfunction). Steam flow to the reformer is maintained at a reduced rate, thereby achieving nominal reformer helium outlet temperatures. The circulator continues to operate at 100% speed with an auxiliary steam supply for the reformer and a heat dump. This event is particularly severe on the steam generator components.

2. Deactivation of Reformer Catalyst (B-16)

This condition is one where the reforming catalyst is damaged in a group of tubes such that those tubes basically become a heat exchanger where the average tube wall temperature is expected to increase and approach helium temperatures (assumed for 10% of reformer life). This condition basically overheats the reformer tubes.

3. Isolation of Reformer Process Gas (B-17a & b)

This event is one where the reformer process gas is isolated when operating at power (refueling from either a reformer tube leak or malfunction in the process gas system) followed by a reactor trip. This event assumes the steam generator continues to function to remove the decay heat, either with or without the circulator functioning. With the circulator functioning (transients B-17a) this transient is relatively short (15-30 minutes) with moderate reformer component temperatures. Under natural circulation conditions, the transient is long and results in high reformer component temperatures.

4. Inadvertent Opening of SG Helium Bypass Valve at Power (B-18)

This event is assumed to occur by the opening of the "Steam Generator Helium Bypass Valve," either inadvertently by operator action or by control malfunction when at power. This event is followed by a reactor and circulator trip. This event is particularly severe on the upper pressure vessel, with high skin temperatures and possible overheating of the vessel (depending on time required to take corrective action).

B. Emergency Events

1. Isolation and Blowdown of SG Concurrent with Reformer Isolation (C-2a)

This event is characterized by the isolation and blowdown of the steam generator along with the isolation of the reformer process gas following operation at power. Decay heat is removed by the vessel cooling system with the primary helium under natural circulation. This event is quite severe for all the heat exchanger components (reformer and steam generator) because of the high temperatures seen by the components, which are normally operated and designed for much lower temperature, and due to the long duration of the transient (50 hours).

2. Process Gas Line Rupture (C-7)

This event is assumed to be initiated by the rupture of either an inlet or outlet process gas line to the reformer such that it cannot be isolated. This is then followed by a reactor trip with the process gas pressure assumed to bleed off to near zero in a short time period (5-10 minutes). This transient is severe (limiting design transient in some cases) on most of the reformer subcomponents, i.e., reformer tube, upper and lower tubesheets and process gas plenums.

8.1.2.2 Plant Duty Cycle Simulation

Duty cycle transient events were analyzed using a computer model of the Modular Reactor System and the General Electric Code STAR (Systems Transient Analysis; Reforming High Temperature Reactors). The model developed for these analyses is a simulation of the primary loop including models of the core, the steam generator and the reformer. For the steam generator, the dynamics of the tubes and the water/steam on the tube side are included. For the reformer, the dynamics of the reformer tube are included and heat transfer and chemical kinetics of the reacting process gas on the tube side are included. The balance-of-plant components for feedwater heating and process gas heat transfer were not modeled; conditions for the feedwater and the process gas at steam generator and reformer inlets were input as boundary conditions.

8.1.2.2.1 Simulation Model

The simulation model contains three modules as indicated in Figure 8.1-1 representing the components of the primary loop of the MRS. This figure is a top level causality diagram showing the module interfaces and the data flow between modules. Explicit in the analysis with these modules are several approximations:

1. The core outlet plenum is neglected. As shown in Figure 8.1.2-1, helium flow conditions at the core outlet are used by the reformer module as helium inlet conditions.
2. Pressure drop and heat loss effects in the annular region for helium return flow to the core from the steam generator are neglected. Helium conditions calculated at the steam generator outlet with the module STMGGEN are used as inlet conditions for the core calculation.
3. The helium circulator is not modeled. Helium flow rate is an input which must be specified for the model.
4. Radial heat losses for all components are neglected.

Each of the three modules is described briefly in the following paragraphs.

Core

The core model calculates thermal response of the core modeled as a single channel with radial heat transfer calculated between the helium coolant, the moderator and the fuel. The model includes a single node for the top and bottom reflectors and eight axial nodes in the core active region with axial conduction accounted for in the moderator. The model uses the helium flow rate and the core inlet temperature and pressure as known. The core model calculates the helium temperature distribution and sets up heat balances used to evaluate the fuel, moderator and reflector temperatures.

Reactor power is calculated with a point kinetics model using six groups of delayed neutron precursors and five decay power groups. An axial power distribution is input to the model and used to evaluate the heat generation for each node. Calculations were made for a scram transient. For this transient, the effect of the scram rods was simulated by an input reactivity.

The energy equations in the core active region are based on a two dimensional (horizontal plane) finite element model to evaluate heat conduction geometric factors which describe heat transfer and temperature drops between coolant, fuel and moderator. These factors are used as constants throughout transient solutions. Axial heat transfer is included for the moderator and the top and bottom reflector heat balances.

The dynamics of the helium energy equation are much faster than the dynamics of the fuel and moderator energy equations due to the small thermal capacitance of the coolant, so coolant thermal inertia is neglected. The helium temperature distribution is evaluated with a quasi-static relationship.

The results of a computation with the core model are the helium temperature at the core outlet and values for the derivatives with respect to time of the state variables (nodal temperatures for the fuel and moderator, top reflector temperature, bottom reflector temperature).

Reformer Model

The reformer model evaluates the reformer as a single equivalent tube and pigtail with helium cross flow on the shell side. All gas temperatures, helium, process gas inside the tube and process gas in the pigtail are computed from quasi-static heat balance relationships. The reformer dynamics are a result of transient energy balance equations for the tube temperatures which are state variables. The thermal capacitance of the pigtail is much lower than that of the tube due to the thin wall so the pigtail temperatures were evaluated from steady state heat balances.

Process gas heat transfer and pressure drops in the tubes, chemical kinetics of the reforming reactions and physical properties for the reforming gas mixture were taken from the DSR1 code. The helium side properties and correlations were also taken from the DSR1 code (and updated version DSRDSGN), including equations for the serpentine flow produced with a baffled shell side.

The reformer model is indicated schematically in Figure 8.1-2. There are eight state variables: tube temperatures for the eight axial locations. The pigtail temperatures and gas temperatures at the axial node positions shown were evaluated from quasi-static relationships. The reforming reactions are strong functions of temperature so the process gas temperature heat transfer and chemical kinetics computations were performed for 40 axial nodes. The gas heat balance computations require tube temperatures which were interpolated assuming a linear distribution between the eight axial node points. The tube temperature calculations for each node in turn require known heat flux values which were obtained as a sum for the five gas nodes adjacent to a tube node.

The reformer model requires as input helium temperature and flow rate at the inlet. These were obtained during a systems transient analysis directly from the core model as the core outlet conditions. The reformer model also requires process gas temperature, pressure and composition at the reformer inlet. These were input as boundary conditions. The results of the computation are helium temperature and pressure at the reformer outlet, process gas temperature, pressure and composition at the reformer outlet, pigtail temperatures and tube temperature derivatives with respect to time.

Steam Generator

The steam generator model is a STAR model adapted for use to analyze the MRS by inputting appropriate MRS physical data. The model treats a once through helical coil steam generator with helium on the shell side. The dynamics of the tube and dynamics of the water on the tube side are included. The model is a moving interface model which treats the subcooled and superheated boundaries as state variables.

The model requires helium flow rate, temperature and pressure at the steam generator inlet. In addition, the model requires feedwater temperature and flow rate at the inlet and steam pressure at the outlet. The helium conditions were obtained directly from the reformer model results. Water conditions were input as boundary conditions.

Transient Results

A number of transient analyses were run with the simulation code for a single duty cycle transient--reactor scram from full power. This transient was investigated parametrically to determine the desired runback rates on flows in order to establish an acceptable scenario for a scram. All cases were run with helium flow rate maintained at 100% and with the feedwater flow rate maintained at 100%. In addition, the feedwater temperature was held constant at 500°F for all transients.

Analyses were also made with assumed process gas inlet temperature sag to simulate the effect of the process gas heat exchange equipment transient on the primary loop.

The transient cases which were analyzed are listed in Table 8.1-4. Preliminary analyses run with the reformer as a stand alone model are included as well as the analyses run with the complete loop model consisting of core, reformer and steam generator. It may be desirable to run similar analyses in the future with uncoupled models. Core outlet results obtained by GA could be used as input conditions for dynamic reformer analyses.

Discussion of Results

Figure 8.1-3 presents key system level temperature transients: helium and process gas temperatures at the inlet and outlet of components for Case 2L. See Figure 8.1-4 for locations. The sudden switch from a reforming gas mixture to 100% steam at the initiation of the transient results in rapid steam temperature increases in the reformer for the first 50 seconds of the transient because the endothermic reforming reaction no longer occurs. After 200 records, the helium temperature at the reformer outlet is higher than the reformer inlet helium temperature due to heating of the helium by the process gas (steam in this case) and the reformer tubes. The steam at the reformer inlet is held constant at 1000°F during the transient and is hotter than the helium at the inlet for times greater than 350 seconds. The effect of a steam temperature sag, a more realistic assumption, was investigated with a subsequent run.

The effect of maintaining flow of the reforming gas mixture during the transient is shown in Figure 8.1-5. The temperature of the process gas and of the helium at the reformer outlet decrease throughout the transient. The lower helium temperature at the reformer outlet (compared to Figure 8.1-3) has little effect on the reformer inlet helium temperature conditions on the time scale of this case.

Figure 8.1-6 compares helium temperatures at the reformer inlet and outlet for different conditions of process gas flow. The extreme case is

1L with no process gas flow. The helium is heated during a portion of the transient by the reformer tubes and leaves the reformer higher in temperature than the inlet helium. This effect persists for a long time. The reformer helium inlet and outlet temperatures for the case of no process flow are slowly coming together during the 1000 seconds of the transient shown in Figure 8.1-6.

As noted previously, cases 2L and 3L were run with the process gas at the reformer inlet held constant at 1000°F throughout the transient. After 350 seconds, the helium temperature at the reformer inlet is below 1000°F so the helium is cooling the process gas. In reality, the process gas temperature at the reformer inlet will sag during the transient as the heat rejection required in the process gas plant decreases. An analysis was run with an assumed process gas reformer inlet temperature sag from 1000°F to 600°F.

In addition to forcing the process gas temperature down to simulate the transient effects in the process gas plant, the process flow rate and composition were run back at assumed rates to simulate a scenario for plant runback due to a scram. The conditions for Case 4L are shown in Figures 8.1-7 and 8.1-8.

The process gas temperature at the reformer outlet is shown in Figure 8.1-8. It drops initially as did the results of Case 3L with the reforming gas mixture maintained. The rapid increase and peak near 100 seconds indicates that the flow decrease is the major influence as shown by comparison to the Case 1L (no process flow) results.

Figure 8.1-9 presents a comparison of Case 1L and Case 4L results for the helium temperatures at the reformer outlet. The temperature sag for the reformer inlet process gas condition has little effect on the helium temperature due to the reduced flow after 75 seconds.

Tube temperature results were provided for stress analysis purposes. Two tube locations were selected: 0.6 meters from the top of the reformer

and 5.6 meters from the top (Fig. 8.1-4). Mean tube wall temperatures and tube wall gradients are given. The tube wall gradients are given for the inner and outer tube halves as shown in Figure 8.1-10.

Note that the inner tube wall temperature gradient requires some interpretation. The conditions on the inside of the tube change instantaneously at the beginning of the transient. The inside surface temperature is evaluated from the heat transfer rate to the tube from the process gas. This changes instantaneously and the temperature T_I changes instantaneously also. The situation is indicated in Figure 8.1-10. For a short period, the temperature difference $T_M - T_I$ is an inaccurate representation of the inner tube temperature gradient. The curves show the pretransient value at time = 0 with a dashed line down into the curve obtained during the transient.

Tube temperature results are given in Figures 8.1-11 to 8.1-14 for Case 2L and in Figures 8.1-15 to 8.1-18 for Case 3L.

8.1.2.3 Reformer Thermal Response

The service Loadings (Level A, B, C & D) used for this evaluation included the umbrella upset and emergency transient events described below and as summarized in Section 8.1.2.1:

- 1) Isolation of reformer with primary helium circulation and with decay heat removal by the main cooling system (Transient B-17a).
- 2) Isolation of reformer without primary helium circulation and with decay heat removal by the main cooling system (Transient B-17b).
- 3) Isolation and blowdown of steam generator concurrent with reformer isolation with natural circulation core cooling with decay heat removal by vessel cooling system (Transient C-2a).
- 4) Process gas line rupture (Transient C-7).

In order to determine the effect of these transients on the design concept, a thermal stress analysis was performed. This analysis was performed using the general finite difference heat transfer code SINDA. A two-dimensional thermal model was developed of the reformer sub-components located above the reformer tubes at the tube sheet. This model provided both initial steady temperatures distributions and temperature histories at the stress model element centers for the transients evaluated.

The results of this thermal analysis for the reformer components is summarized in table 8.1-5 below. As can be seen from this table the reformer sub-components experience the severe service of large radial and axial thermal gradients and long soak period at high temperatures in the VIL concept.

In an effort to understand the causes for and to reduce the thermal gradients, a sensitivity study was performed of various configurational parameters; namely the tubesheet rim thickness, hole inserts, and process gas flow variations. The results of this thermal sensitivity evaluation for transient C-2a is shown in Table 8.1-6.

Reduced Rim Width

It can be seen in Table 8.1-6, the results for comparing configurations 1 and 2, that reducing the lower tubesheet diameter reduces the predicted peak radial temperature difference in the lower tubesheet by 40% but hardly affects the peak temperatures achieved by the tubesheet center.

Tubesheet Hole Inserts

Comparing the results for Configuration 2 and 3 in Table 8.1-6 shows that the addition of insulating inserts in the lower tubesheet holes can be expected to reduce the peak temperature in the tubesheet center by about 100°F and the radial temperature difference between the lower tubesheet center and rim by about 15%.

No Reformer Tube Natural Circulation

Eliminating the reformer tube process gas natural circulation results in greatly reduced heat transfer rates from the helium to the lower tubesheet and consequently both the peak tubesheet center temperature and the peak radial temperature difference between it and the rim are correspondingly reduced, as shown by configuration 6 in Table 8.1-6.

Reformer Tube Natural Circulation

The effects of the reformer tube natural circulation model on the thermal parameters (peak component temperatures and thermal gradients) for Transient C-2a are shown in Figures 8.1-19 and 8.1-20. It can be seen in the figures that it will require a considerable reduction in the natural circulation flow, below that predicted by the very conservative natural circulation model used in the evaluation, to appreciably reduce the peak temperature of the lower tubesheet center and the peak radial temperature difference between it and the rim. One half of the predicted natural circulation flow will hardly affect the peak lower tubesheet temperatures or peak radial temperature difference between it and the rim. If the flow were 1/5 the predicted natural circulation flow, the peak temperature would drop by 61°F and the radial temperature difference by 10%. If the flow were 1/10 the predicted natural circulation flow, the peak temperature would drop by 160°F and the radial temperature difference by 20%.

A great deal of uncertainty exists as to the effectiveness of natural circulation within the reformer tubes in transferring heat from the helium to the respective tubesheets. This, however, is a critical parameter since the conservative natural circulation models used thus far (i.e., those that tend to enhance the flow) predict that over 90% of the heat transfer to the lower tubesheet during reformer isolation events is produced by natural circulation flow within the reformer tubes. A similar but far less severe situation exists for the upper tubesheet. The uncertainty as to the effective natural circulation flow within the reformer and return tubes is due to the complex geometry of the system being modeled. The thermal evaluations made to date assumed that the buoyancy forces produced by temperature differences within the process gas are opposed only by frictional losses and that no mass or heat communion exists between the upward

and downward flow within the tubes. It also does not account for the presence of the catalyst within the reformer tube below the lower tubesheet insulation. If these things are considered, they will tend to reduce the effective natural circulation flow within the reformer and return tubes, creating a less severe thermal environment for the tubesheets during reformer isolation events. However, studies of the effects of reducing natural circulation flow within the tubes indicate that the reduced flows will have to be reduced by an order of magnitude before substantial improvement in the predicted thermal environment for the tubesheets occurs. In order to upgrade the analytical natural circulation model and still remain conservative, it will be necessary to study the phenomena in detail using specialized natural circulation computer codes for that purpose. Two such codes have recently been developed under the sponsorship of DOE, COMMIX-1P and TEMPEST. Whether such upgrading of the natural circulation model is sufficient to greatly alter the predicted thermal environment for the tubesheets during reformer isolation transients remains to be seen.

Thermal Evaluation Conclusion

1. The thermal evaluation of the HTGR modular concept predicts high lower tubesheet (center) temperatures and radial temperature differences for reformer isolation events which occur concurrent with shutdown of the helium circulators.
2. Most of the heat transfer to the lower tubesheet center is predicted to occur via natural circulation of the process gas in the reformer tubes.
3. If the natural circulation mentioned in Item 2 above can be greatly reduced (by nearly an order of magnitude or more), then the peak temperatures and radial temperature differences within the lower tubesheet can be greatly reduced (by up to 100°F and 15% , respectively).
4. If the diameter of the lower tubesheet is reduced, then the peak radial temperature difference within the lower tubesheet can be reduced by up to 40%.

5. Thermal insulating inserts placed within the tubesheet holes can reduce the peak lower tubesheet temperature by up to 100°F and the radial temperature difference by up to 15%.
6. The combined effects of the above (e.g., reduced tubesheet diameter, hole inserts and upgraded natural circulation model) could reduce the peak tubesheet center temperature by up to 150°F (i.e., <1570°F) and the radial temperature difference between the center and tubesheet rim by up to 55% (i.e., less than 170°F difference).

8.1.2.4 MRS-R Structural Analysis

The MRS-R structural analysis efforts were focused on the identification of areas critical to the structural integrity of the reformer plant concept, in sufficient detail to define potential problems that could invalidate the design, and to evolve the design toward a configuration that can meet the structural requirements. Using the results from the thermal analysis, described earlier, simplified stress analyses were initiated in several areas. These areas included the reformer tubes, the upper tubesheet, the lower tubesheet, and the vessel tubesheet support flange.

8.1.2.4.1 Reformer Tubes

The reformer tubes are attached to the lower tubesheet by butt welds to bosses machined on the bottom of the tubesheet. The upper few inches of the reformer tubes are made of Incoloy 800H material for compatibility to the tubesheet while the lower section is of Inconel 617. The tube transition joint weld should be flush ground or otherwise smooth surfaced and positioned within the insulating material of the tubesheet for protection from thermal transients. The tube is laterally supported by flow baffles along its length. Besides loads exerted by pressure and seismic motions, the tubes are expected to sustain thermal loads which consist of radial gradients with axial thermal discontinuities introduced by counter cross flow on both sides of each baffle. The analysis presented here does not include the latter loading, but concentrates on primary membrane, external pressure stability, fatigue caused by cycles of pressure, and the average differential thermal expansion loading for the transition weld.

Primary Membrane Evaluation

The results of the primary loading evaluation are shown in Table 8.1-7. The transient cases considered were divided into discrete segments of loading in a conservative manner. The allowable stresses were determined for the durations and temperature from appropriate limits in ASME Code Case N-47 and compared to the calculated stresses. As noted, all segments pass the criteria with significant margin (minimum of 19% margin). The loading considered was due to differential pressures and results in primary membrane loading. Bending stresses, produced by such loadings as lateral seismic motions and thermal expansion of the bundle, remain to be determined and applied.

Since significant times are spent under external pressure loading the severity of possible instabilities was investigated by performing a modified time independent buckling calculation. The results of this analysis show that the reformer tube is very stable even under the loss of process gas pressure event C-7 and is not likely to be a problem.

Fatigue Evaluation

The propensity toward fatigue damage was examined for a cycle composed of pressure differential extremes. The extremes were identified as +330 psid (positive internal) and -672 psid for transients B-17a and C-7, respectively. This results in a cycle with an 1002 psi pressure range with a maximum temperature of 1735°F. The range of through-wall thermal gradient has not been evaluated for these transients, however, analysis of a severe scram transient has indicated a range of 102°F. Using this value, it was estimated with hold time consideration that the damage fraction for four (4) occurrence of this cycle would be 1.3×10^{-3} , which is certainly a negligible amount.

It is expected that fatigue damage from a combination of pressure and through-wall thermal gradient is not a problem. This analysis does not include loading due to vibrations, seismic and other causes, but it appears there is significant margin available for their accommodation when they are defined.

Transition Weld

The transition joint weld in the tubes between the 800H material at the top and the 617 tubes was reviewed for effect of thermal cycling fatigue. During event C-2a, the maximum tube temperature reached is close to 1800°F. Assuming the temperature range is between 1800°F and room temperature and using the simplified equations of NB36, it was found that for four (4) cycles of C-2a a damage fraction of 0.6 would be encountered in the 617 material and approximately 0.7 in 800H. (These results are summarized in Table 8.1-8.) This is high in view of the fact that there are many other transients that cause tube temperatures in excess of 1700°F. Essentially, it will be required to take 12 cycles of this nature since there are 12 periods where the reformer is cold during the 20 yr. reformer life.

The significant loading for the transition joint is the thermal cycle with the pressure cycle providing a minor part in the fatigue damage, consequently, improvements in life expectancy should be addressed to the thermal problem. Several improvements are possible. The transition joint should be placed in the protection of the insulation on the bottom of the tubesheet for two reasons. First, through-wall thermal gradients swings will be less because of protection from direct contact with hot helium. Second the upper tube in this region will run cooler because of heat loss to the cooler tubesheet. The tubesheet peak temperatures are typically estimated to be 200 to 400°F less than peak temperatures in the upper exposed tube region. If only a drop in peak transition joint temperature of 100°F can be achieved this would reduce fatigue damage by one third.

The method of estimating fatigue damage can also be improved to eliminate some of the conservatism. For example, the three components of load considered (differential expansion, through-wall gradient, and pressure) are assumed to superimpose at the peak temperature when, in fact, proper time phasing of the loading could significantly reduce the strain range.

It is clear that the transition weld region will demand careful future consideration to insure satisfactory reformer performance. The design should include a flush ground profile (the assumed profile for the above analysis), since the consequences of an as-welded configuration are a 30% larger stress and significantly lower number of allowable cycles.

8.1.2.4.2 Upper Tubesheet Analysis

The FY82 reference configuration showed an upper tubesheet thickness of six inches. Because of revised design conditions, it was necessary to re-evaluate this thickness. Simplified thermal fatigue stress analysis of the tubesheet under the heat up transient (C-2a) was also evaluated.

Tubesheet Thickness

Previously, sizing of the upper tubesheet was based on the limiting case of temperature and pressure differential during the normal operating condition. An additional transient was identified (C-7) that provides a new limiting case. In this emergency event, the outlet process gas line ruptures causing increased flow with increasing pressure drop through the system. Under these conditions, a maximum pressure difference across the tubesheet of 200 psi is assumed to exist (rupture disc limits pressure differential) with an estimated duration for the life of the component of two hours.

The analysis was performed using the procedures of the ASME Code Section III, Division I Appendices (A-8000). The minimum thickness was derived to be 6.8 inches for the assumed emergency condition and 5.0 inches for Normal operation.

From this evaluation, it is clear that the assumed Emergency condition is limiting and that the thickness should be increased to seven inches.

Tubesheet Fatigue Evaluation

Because of the large thermal differences between the center perforated portion of the tubesheet and the outer ring noted in the thermal analysis

result, the upper tubesheet was evaluated as a possible problem area. The radial gradient from center to rim is a maximum of about 360°F at about 5 hours into transient C-2a. The temperature profile at this time is shown in Figure 8.1-21. It can be seen that the central perforated region is fairly uniform in temperature. Near the edge, there is a sharp gradient and the rim essentially operates at a significantly lower uniform temperature.

Such regions where abrupt changes in geometry, material, or loading occur are known as discontinuity areas and the stresses associated with them are known as discontinuity stresses. The discontinuity stresses are required to satisfy the compatibility of deformations of these regions. Article A-6000 of Division I Appendices to the ASME Code describes a general procedure for analyzing the discontinuity stresses which was used to evaluate the upper tubesheet thermal stress problem.

To determine the principal stresses at a discontinuity it is necessary to evaluate the stresses caused by pressure, mechanical loads, thermal loads, and discontinuity loads. The stress intensities are then obtained by superposition of the stresses. Following this procedure, the stress levels at the surface are obtained by solving the equations for stress using the determined values of redundant loads, Q and M . The results show that the most critically loaded area is in the rim lower surface at a stress intensity of 287,500 psi. This translates into a strain range of approximately .0123 in/in. Referring to Figure T-1430-1C of Code Case N-47, this results in an allowable number of cycles equal to much less than 10 (off the curve).

From this analysis, it is apparent that the upper tubesheet under the present thermal loading cannot sustain four occurrences of transient C-2a and meet ASME N-47 creep fatigue criteria.

The above analysis was obtained using some very unconservative assumptions which neglected additional restraint at the bolted flange and the

process gas cylinder. The above therefore does not allow reasonable margin for fatigue damage from all other events nor does it allow reasonable margin for creep damage. To reduce this strain range to a more reasonable value, a change in the support system is required. Proposed changes are discussed in Section 8.1.2.5.

8.1.2.4.3 Lower Tubesheet Analysis

The FY82 reference configuration showed a 23 inch thick tubesheet made from Inconel 617. It appears that such a thickness for the reference material is beyond the fabrication capacity for both the present and reasonably extrapolated future. Incoloy 800H has a greater potential for successful fabrication of this thickness and, thus, has been substituted in all structural analysis efforts. The required thickness has been analyzed against what is expected to be the most severe loading condition and found to be satisfactory. Just as for the upper tubesheet, differential expansion between the center and rim are shown to produce unacceptable strain ranges resulting in failure to meet fatigue criteria.

Lower Tubesheet Thickness

As for the upper tubesheet the methods of the ASME Code were used to determine the thickness of the tubesheet required to sustain the primary loading. Using the pertinent parameters the required thicknesses were determined as shown in Table 8.1-9

The loading conditions shown were developed from the transient descriptions given earlier. The Upset umbrella set of loading segments was derived by breaking each transient down into segments of loading defined by durations within a temperature range and using the highest pressure differential during this period. The two events were combined under an umbrella set of segments by adding the durations from like temperature segments from each event and then using the larger pressure differential. The set used is conservative by nature of its construction but simple to administer since it does not require following the load history in detail.

Although pressure is the significant loading, the weight of the supported equipment was also estimated and determined to be equivalent to a downward pressure of approximately 30 psi. The differential pressures shown in the summary of Table 8.1-9 have been adjusted to include this load. Table 8.1-9 results indicate that a tubesheet of 23 inches is satisfactory. However, the loading considered is for pressure and dead-weight only. In the final analysis, seismic loading will be required to be considered in combination with other loading in accordance with future requirements. The methods used are conservative in nature and do provide some margin for such loading if more detailed analysis is done in the future.

Lower Tubesheet Fatigue Evaluation

An interactive analysis of the lower tubesheet between the center and rim was performed by the same methods described earlier for the upper tubesheet. The results indicate the largest peak stress occurs at the rim upper surface at the outer tubesheet hole. The peak stress is 218,000 psi, which translates into a strain range of 0.0106 in/in. Referring to Figure T-1430-1C of Code Case N-47, it is found that this results in an allowable number of cycles well off the curve at a value much less than 10. (Extrapolating it would be 1 or 2 cycles). This is an unacceptably low value.

It is the conclusion of this analysis that the lower tubesheet under the present thermal loading cannot withstand four occurrences and meet code creep-fatigue criteria. As described previously, many aspects of the above scoping analysis are unconservative and it is judged that a more detailed analysis improving the conservative assumptions will not show an acceptable design. It is imperative therefore to reduce the thermal loading. Several improvements have been suggested as described in Section 8.1.2.5.

8.1.2.4.4 Reformer Support

For purposes of this analysis, the reformer support includes the lower tubesheet support skirt and flange and the support flange which is a part of the vessel wall. Analysis in this area is scoping in nature and is for a limited set of loadings.

Support Skirt

The analysis of the Lower Tubesheet Support Skirt concentrated on the requirement for primary loading. The loading consisted of dead weight pressure and for seismic loading (a 1.5g vertical load was assumed). The weight considered included the tubesheet, tubes, catalyst, shroud, baffles and the skirt itself. The resulting weights calculated for the separate components are shown in Table 8.1-10. The total weight supported was approximately 365,000 pounds. The cross section considered was of the larger upper end near the flange because the pressure effects (which dominate the combined loading) are higher for the larger diameter. The axial stress calculated at this location was 112 psi. Even adding a 1.5g seismic loads only increases this value to 280 psi.

Pressure loading was considered by selecting high temperature increments with significant pressures from the previous load set used for the Lower Tubesheet. The segments selected are shown in Table 8.1-10 along with the resulting Primary Membrane Stress (P_m) and the appropriate Code allowable stress. Since the increments of temperature from the tubesheet section reflect tubesheet temperatures and since the skirt runs cooler than the tubesheet, the temperature increments were reduced by 100°F. This is very conservative since indications are that, even at the hotter lower end, a value of 300°F or 400°F lower would be justified. The results indicate that, even with such conservatism, the P_m values do not exceed 42% of the allowable values. The Support Skirt appears more than adequate for the primary loading considered.

It should be noted that in this analysis the vertical seismic load of 1.5g was included in the Upset segment but not in the Emergency segments. This assumption is based upon past experience and should be combined in accordance with future Equipment Specification dictates. Because the seismic loading is small, this assumption has negligible effect upon the conclusions.

Vessel Flange

It was noted in the preliminary thermal analysis that the vessel flange which supports the upper and lower tubesheets sustained a large radial thermal gradient even at steady state conditions. Assuming a linear radial gradient of 220°F in a ring, the surface stress can be estimated at 31,500 psi. Assuming this to be the range of stress, stress amplitude, $S_a = 1/2 S_p = 15,750$ psi. From Figure I-9.1 of the ASME Code, the number of allowable cycles to failure (N_f) is 10^6 .

Subsequent design changes have further decreased the thermal stress in this flange to even more comfortable levels. The steady state radial gradient has been reduced to 160°F with the selective use of insulation on the shell exterior. This was reduced to a negligible amount (25°F) by insulating the inner surface of the lower tubesheet support cone and eliminating heat transfer from the upper tubesheet and process gas.

The conclusion of this evaluation of the flange is that using the latter design, thermal stresses have been reduced to a minimal level and should not pose a serious stress problem during the Emergency C-2a transient. The final structural evaluation using more exact and detailed methods will be necessary to fully evaluate the flange region, but it appears that the probability of showing a valid design in this region is high.

8.1.2.5 Approaches to Mitigating Effects of Reformer Transients

The structural evaluation of the FY82 MRS reformer design, as discussed earlier, has identified critical regions requiring additional analysis and, in many cases, sub-component redesign. Further, the limiting umbrella transients have also been identified. This section addresses approaches investigated to mitigate the effects of these transients, including design configuration changes and transient reclassification. A structural assessment of these changes was also addressed in part.

With regard to the reformer, the events which have a significant structural effect include the upset events, "Deactivation of Reformer Catalyst" and "Reformer Isolation", and both events described in the emergency category, "Concurrent Reformer and Steam Generator Isolation" and "Process Gas Line Rupture." Since the deactivation of the reformer catalyst is not a limiting event, consideration was directed towards mitigating the effects of the "Reformer Isolation", the heatup events (both upset and emergency) and the "Process Gas Line Rupture" event.

8.1.2.5.1 Reformer Isolation and Heatup

Transient Reclassification

The most severe of these event is where both the reformer and steam generator are isolated thereby resulting in a long term, high temperature transient. Initially, this event was categorized as an upset event with 20 events per plant life (the basis being that the reformer was isolated to protect the catalyst whenever the steam generator was not available). It was clear, based on the thermal and stress analysis of the reformer structures and the non-necessity of isolating the reformer when the steam generator was isolated, that this event should be classified as an emergency event. This is accomplished by continuing operation of the reformer when the steam generator is out of service, thereby continuing to remove decay heat and cooling the primary helium temperature below 1200°F before being seen by the steam generator. The details of continuing reformer process gas and steam flow following a scram are yet to be worked out, however, no major obstacles are foreseen. It is, therefore, with this classification of the above event that the reformer structural evaluation was based.

Process Gas Plenum Reconfiguration

The isolation and heatup of the reformer places severe temperature gradients on both the upper and lower tubesheets, with a hot center section and a relatively colder rim region. Further, very significant temperature gradients are also observed between the upper tubesheet support flange and the mating reformer support flange. To mitigate the effects of this

transient on the upper tubesheet and support flanges, a reconfiguration of the process gas plenum is recommended whereby the tubesheet rim is essentially eliminated and the process gas plenum is centralized, as shown in Figure 8.1-22. This configuration appears by examination to significantly reduce the thermal gradients and temperature mismatches seen by the lower tubesheet and its support flange.

The configuration shown in Figure 8.1-22 also incorporates a process gas buffer region which has several advantages:

- o Thermally isolates reformer support flange and vessel thereby achieving more uniform temperatures in this region.
- o A leak in the reformer support flange (helium-to- process gas barrier) will not contaminate the process gas.
- o Leakage of the reformer support flange can be readily detected.
- o Leakage of the process gas can be readily detected.
- o The vessel is thermally isolated from the process gas.

Lower Tubesheet Reconfiguration

The isolation and heatup of the reformer lower tubesheet results primarily by the process gas within the reformer tubes being heated up and rising through the tube in a natural circulation mode and convectively heating up the central portion of the tubesheet. Since the massive rim lags behind resulting thermal gradients produce very large strains within the rim at the outer tube holes. Several schemes have been suggested to mitigate this effect, namely:

- o Reduce rim thickness,
- o Install "flow diodes" within each tube,

- o Install ceramic insulator sleeves within each tube for length of tubesheet,
- o Install additional insulation to the bottom of the tubesheet, and
- o Model the natural circulation flow in the reformer tube.

Evaluation of the above schemes have resulted in estimated reductions of the tubesheet radial thermal gradient by about 42%, with the majority of this improvement resulting from the reduced rim thickness (radial gradient decreased from 385°F to 225°F). Although a significant improvement has been realized in the thermal gradient, stress analyses have shown that further reductions are needed. If the natural circulation flow was stopped altogether, the radial thermal gradient was determined to be decreased to 34°F with the additional effect of reducing the maximum tubesheet temperature to about 1187°F. This would, by examination, result in acceptable stress levels for the tubesheet. Because of the simplified natural circulation model used, it appears that the actual environment would yield a thermal gradient somewhere between these two bounds.

Based on the above, it is recommended that the tubesheet rim be reduced in thickness from 8" to 4", the tubesheet insulation be increased to two feet thick and that a more detail analysis be performed on the thermal convection phenomena within the reformer tube so that more realistic predictions of the natural circulation process gas flow rates and temperature gradients can be made.

Reformer Recuperator

A process gas recuperator (regenerative heat exchanger) located in the upper region of each reformer tube may also be a means of mitigating the reformer isolation and heatup transient. As shown in Figure 8.1-23, the application of a recuperator has the advantage of lowering the inlet process gas temperature (which controls the steady state lower tubesheet temperature), thereby reducing the tubesheet thermal loading. The Germans

have used this approach and have gone as far as recommending a 2½Cr-1Mo tubesheet. Further, this approach tends to block natural circulation currents within the process gas which has the effect of thermally isolating the lower tubesheet, as mentioned earlier. Time and resources did not permit a performance or cost benefit trade-off evaluation of this approach or to prepare any recuperator concept designs. However, from a structures standpoint, it is recommended that this approach be further evaluated in FY84.

8.1.2.5.2 Process Gas Line Rupture

This transient has the effect of imposing severe pressure loadings on both tubesheets with only a moderate temperature excursion. This transient is generally limiting with regard to the reformer tube size, sets the upper tubesheet thickness and is second only to the concurrent reformer and steam generation isolation and heatup transient in sizing the lower tubesheet.

Two approaches were considered to mitigate this transient. The first would incorporate a rupture disc between the process gas inlet and outlet plenums, thereby limiting the pressure loading on the upper tubesheet. A 200 psi differential is recommended for this rupture disc resulting in a tubesheet thickness of 7". This approach is recommended for incorporation in the reference design.

Secondly, consideration was given to the concepts of either placing a rupture disc between the primary helium and the process gas or by individually sealing each reformer tube on the process gas side as shown schematically in Figure 8.1-24. Both of these proposals, while mitigating the effects of a ruptured process gas line (non-isolatable from the process gas plenum) has significant drawbacks.

A rupture disc between the primary helium and the process gas would limit the pressure loading on the lower tubesheet but has the problem for a non-isolatable process gas rupture of leading to a depressurization accident, reducing plant availability by potential leaks or failures in the rupture disc and, of course, a source of contamination of the process gas.

The sealing of individual reformer tubes would limit a rupture in a process gas line to a select group of reformer tubes. However, those tubes would likely have to be designed for such an event, since collapse of any reformer tubes within the bundle can result in binding of the entire bundle. In addition, grouping of individual inlet and outlet process gas lines to each tube significantly increases the catalyst replacement complexity which must be considered.

Further, since this transient is not limiting for the lower tubesheet (requires 21" tubesheet vs 23" required by heatup transient), incorporating these latter concepts would not change the tubesheet thickness. Regarding the reformer tubes, any costs saved by a possible tube thickness reduction (14%) would be offset by the added complexity of the individual process gas lines. Therefore, the latter two concepts are not recommended for further evaluation or for inclusion in the reference design at this time.

8.1.2.5.3 Reformer Subcomponents

Described in Section 8.1.2.5.1 were several possible improvements to enhance the structural environment and, thus, improving the possibility of satisfying the structural criteria. As mentioned, some of these proposals, such as the transient reclassification, were incorporated early and included in the structural evaluation. Those involving the reconfiguration of the process gas plenum and the lower tubesheet were evaluated in part to determine the effect on the structural capability. The various schemes mentioned for reconfiguration of the lower tubesheet were evaluated for thermal response. Case eight (8) of the thermal results was used to reanalyze the tubesheet discontinuity analysis for strain range and possible fatigue damage. While the results are still not acceptable they are very nearly so and a significant improvement was noted. The reconfigured process gas region has not yet been analyzed for thermal response and consequently analysis of this region has been limited to primary membrane stress analysis for the purpose of provide reasonable sizing of the components that compose the region.

8.1.2.5.4 Lower Tubesheet Fatigue Evaluation

The interactive stress analysis between tubesheet center and rim was redone for transient C-2a. The thermal model for this case had 36 inches of insulation on the bottom of the tubesheet versus nine previously, a rim thickness of four inches versus eight inches, insulating tubesheet hole inserts and an improved convection model with 50% flow. Table 8.1-11 contains a comparison between some of the significant results of these configurations. The time used for the analysis was that when the largest difference existed between the tubesheet center and the rim surface. As noted, the revised case has a much lower average temperature for both center and rim with the differential between the two decreasing 180°F. The axial gradients have increased due to the change in heat path (larger fraction flowing vertically than previously). The results of the discontinuity stress analysis show significant improvement in the average rim stress levels, and even more improvement in the peak stress and strain ranges (strain range decreased from 1.06% to 0.63%). However, although the strain range is improved the amount of strain estimated is still above code allowables as shown in Figure 8.1-25.

The results suggest two things. The first is that perhaps other improvements and refinements could bring the strain range within acceptable limits. This could include improvements in both design and operating conditions. The second is that a detailed inelastic analysis using the upper curve could prove fruitful. At a minimum, future evaluations should include a detailed finite element elastic analysis of the region.

8.1.3 MRS Design and Development Plan

During FY83, General Electric, in cooperation with Bechtel Group International (BGI), Combustion Engineering (C-E), and GA Technologies (GA), wrote the "Advanced Systems Concepts Design and Technology Development Plan" (Ref. 8.1-3) for the High Temperature Gas-Cooled Reactor (HTGR). The plan addresses the present design status of the Modular Reactor System-Reformer (MRS-R) and -Steam Cycle (MRS-SC/C) plants. The plan also addresses future design needs and priority technical issues which require research and development in order to complete the MRS designs.

Technical issues amenable to design solutions include structures, components, reactor support systems, steam systems and plant arrangements. Other issues requiring research and development programs include LEU and HEU fuel and core performance, material characteristics and properties at high temperatures (above 900°C), performance of new types of catalyst and effects of reactor coolant impurities on materials and components. Safety and licensing issues, based on inherent safety features, need to be proven by analysis and by demonstration.

8.1.3.1 Scope, Work Breakdown Structure, and Overall Schedule

The scope of the design and technology development plan is to define all those actions necessary to design and develop the technology necessary to support the procurement and construction of a MRS-R demonstration plant and a MRS-SC/C lead commercial plant. The program includes a combined Design Plan and a Technology Development Plan, the priority technical issues, design data needs, and the design and development requirements which evolved from the results of the MRS design studies carried out in FY83.

The schedules for the HTGR programs phases are shown in Figures 8.1-26 and 8.1-27. The milestones shown by Figure 8.1-26 constitute the phases necessary to culminate in operation of an advanced system for process heat application. This schedule of major program milestones is an early target estimate of the time increments required to carry out the various phases.

As with the monolithic lead plant, a one-step licensing process is intended for the MRS-R demonstration plant and/or the MRS-SC/C lead commercial plant. This means that the construction permit and operating license will be granted at the start of construction of the respective plant. This is particularly important for the case of the MRS-SC/C lead commercial plant because it reduces the risk of deployment and gives confidence for early deployment of numerous commercial ventures. The MRS schedule dates are assumed to be constrained by the schedule of the monolithic HTGR-SC/C Lead Plant program. The schedule for the HTGR-SC/C Lead Plant is shown in

Figure 8.1-27. Constraints appear in four areas:

1. Funding support for design and technology development,
2. Fuel related technology from the Lead Plant,
3. Availability of test facilities, and
4. Licensing experience of the Lead Plant.

The advanced system represents a single full size modular system of either a reformer or a steam cycle plant. If the system is a modular reactor system reformer plant, a program may require a demonstration plant, followed by a lead commercial plant (the latter not shown on the schedule).

Due to the less stringent technology and temperature conditions for the MRS-SC/C relative to the MRS-R, it is currently judged possible to proceed directly to a lead commercial plant. Thus, the end point of the time increment shown by Figure 8.1-26 represents the time at which a commercial offering of a SC/C plant could be initiated. Some schedule improvement could be realized by initiating the procurement process earlier, but this would depend upon successful results from the earlier program phases.

For the MRS-R option, a lead commercial plant would follow the demonstration plant operation. The engineering process and procurement activities on the lead plant could be started before starting the demonstration plant, but the schedule to commercialize the reformer system may take an additional ten years longer than that of the MRS-SC/C system.

The technology development program is to be completed by the start of construction of the demonstration plant. The design phases of this program should also be completed at the start of construction. This results in approximately a ten-year program, but the time frame is subject to continuous evaluation depending upon the final application requirements for the advanced systems in relation to the current state-of-the-art technology.

8.1.3.2 Program Assumption

The major assumptions used to construct the Design and Technology Program for the Advanced Systems Concept Process Heat applications are the following:

- A. The program will support the design, licensing, procurement, construction, and operation of one or more demonstration systems which utilizes the HTGR to supply process heat. The systems addressed are the 950°C Direct Cycle Modular Reactor System Reformer to supply syngas, steam and electricity in the desired proportions to fit a wide variety of applications, and the 700°C MRS-SC/C system to supply process steam and electricity to industry.
- B. For the MRS-R and MRS-SC/C, a common 250 MWt NHS reactor vessel assembly will be addressed to supply each concept and with the capability to supply ROT helium of 950°C for the reformer system. The MRS-R and MRS-SC/C will have individual upper vessel assemblies which will also be addressed individually by this program.
- C. This advanced systems program is an extension of the 2240 MWt lead plant SC/C program and will derive benefits from that program to the greatest extent possible. However, the lead plant and MRS concepts are sufficiently different and the MRS requires a dedicated design and technology development program.
- D. The MRS is regarded as an advanced system and is not a competitor for the 2240 MWt monolithic lead plant.

8.1.3.3 Design Plan and Needs

Design plans include proceeding from preconceptual to conceptual to preliminary and completion of final design of the following systems and components:

- A. Both LEU and HEU fuel design to be used as a common NHS for both the MRS-R and MRS-SC/C plants. Also included in this activity is the reactor core and nuclear design, core fuel performance and the core/reactor internals design.
- B. Completion of the NHS design, including:
 - 1. Helium circulator,
 - 2. Control rod drives,
 - 3. Reactor vessel,
 - 4. Fuel handling system.

Completion of the NHS system and component designs also includes provisions for ISI and component maintenance.

- C. Upper vessel systems design for the MRS-SC includes:
 - 1. Upper vessel,
 - 2. Steam generator,
 - 3. Bypass valve.
- D. Upper vessel system design for the MRS-R includes:
 - 1. Upper vessel,
 - 2. Reformer,
 - 3. Steam generator,
 - 4. Bypass valve.

Completion of the upper vessel systems (C and D) also includes provisions for ISI, maintenance and interface features to the BOP.

- E. Heat removal systems design includes the reactor cavity (forced-water flow) cooling system and a passive natural convection air flow cooling system.

8.1.3.4 Development Plan

The development plan is classified into three areas as follows:

- A. Research and development needed to support the design of systems and components.

- B. Verification tests needed to prove that systems and components function as required, have an acceptable level of reliability and are cost effective.
- C. Research, development and tests required to support safety and licensing issues.

8.1.3.4.1 Design Support

Research, development and tests required to support system and component designs include (but are not limited to) the following:

- A. Fuel materials and manufacturing process developments. Development of computer codes to predict core performance and the fuel cycle are also required.
- B. Fission product transport studies are needed in order to predict its effect on systems and components and radiation levels that impact ISI and maintenance activities.
- C. Other development activities required to complete the NHSS are:
 - 1. Reactor internals (core barrel/core support),
 - 2. Neutron control system (CRDs),
 - 3. Primary coolant system (circulator),
 - 4. Fuel handling system,
 - 5. Decay heat removal systems,
 - 6. Core heatup studies (transients).
- D. Development of materials properties for the fabrication of:
 - 1. Reactor vessels,
 - 2. Reformer,
 - 3. Steam generators,
 - 4. Bypass valves
- E. Development of an appropriate reformer catalyst.

- F. Identification of coolant impurities and their effects on the core components and other components in the coolant circuit.

8.1.3.4.2 Tests

Verification tests for the function and performance of all systems and components will be required. For the MRS-SC/C plant, much of the verification tests of systems and component performance will have been proven in the development of the monolithic 2240 MWt HTGR-SC/C Lead Plant. For the MRS-R plant, which has a ROT of 950°C, verification tests of the reformer, steam generator, bypass valve and vessels will be required. Likewise, the effects of coolant impurities and temperature transients on these components must be identified.

8.1.3.4.3 Safety/Licensing Support

The Safety/Licensing Development Plan will be continuously updated, using information generated in the MRS program, the lead plant program and other related programs. The Safety/Licensing development work will continue through the phases of project definition, preliminary design, final design and field engineering and construction. The main areas of activities are test planning, including pre- and post-operation tests, conducting the planned tests, and updating the development plan as shown in Table 8.1-12.

8.1.3.5 Estimated Design and Development Costs

The MRS plant (both MRS-SC/C and MRS-R variants) is in a very early stage of design definition and many of the salient features have not been fully evaluated and defined. New design direction, towards the goal of identifying a minimum cost plant concept, will impact the program cost and schedule. Since no detailed planning has been performed to identify scheduler aspects of the MRS, the estimation of design-related cost, at this stage, must be regarded as somewhat rudimentary. The numbers given in Table 8.1-13 must be regarded as tentative since they reflect only judgment as regards the perception of what is necessary for the MRS design. The numbers are based on scaling/extrapolation (as appropriate) from related

activities on the lead plant, and at this stage must be used with circumspection. As design activities are clarified and put into perspective, the numbers will be updated, this being regarded as an on-going activity. Cost estimates have been arranged as follows:

- A. Design costs for the four design phases by systems - Table 8.1-12.
- B. Development costs by systems - Table 8.1-14.
- C. Summary of design and development costs by the performing organizations: Bechtel, Combustion-Engineering, GA Technologies and General Electric - Table 8.1-15.

8.1.4 Control Room Design/Operation for Phased Construction Approach

8.1.4.1 Control System

Modular reactor operation requires a sophisticated state-of-the-art control system to assure not only independent operation of the completed units, but also coordination of shared Balance of Plant operations. A control system has been conceptually designed which uses microprocessor-based, signal conditioning, data acquisition multiplexers, and microprocessor-based multiple controllers communicating via fiber-optic links with redundant system controllers to provide the appropriate isolation of operating units from each other and from those under construction. The use of such a state-of-the-art control system would provide the high-speed data acquisition and control necessary for multiple reactor operation. A reactor module can be described as a "system" of the total plant. Each "system" is independently operated using system (module) controllers and device controllers through the overall plant controller. The control system has been conceptually designed such that hardware components and software components can be easily tied into the system and allow expansion to accommodate additional process inputs. Each completed reactor module can thus be considered as the addition of multiple process inputs.

8.1.4.2 Control Complex

A preliminary control complex design has been planned to provide a complete centralized control complex facility with centralized control room during the initial construction phase. The centralized control complex would require less space than conventional control rooms due to the reduced space requirements of the fiber optic cabling and the control system configuration. This same control system configuration will allow operation of from one to eight reactor modules from a single control complex while coordinating balance of plant operations. The control building will consist of the main control room, the technical support center, operations support center and the alternate control facility (if needed). Complete system functional responsibilities can be fully integrated at the operator work station.

8.1.4.3 Man-Machine Interface

Full graphic color CRT's are provided as the real-time information display and manual control mechanism. Consoles, rather than bench boards, will be used with interactive capability provided by touch-panels and touch-screens. Preliminary analysis indicates that the primary man-machine interface within the control complex can be served by probably three and no more than four consoles, a reactor unit control console and two auxiliary consoles. Each console has 10 CRT's, 4 on each wing and 2 in the center section. Reactor unit control console wing groups would normally be assigned to the eight reforming-modular reactor system modules, including process gas trains. Auxiliary console wings would be assigned to the steam cycle systems and the balance of plant systems. The center sections of the consoles would provide interaction with the on-line simulator for fast-time diagnostics, predictive analysis and operator warning and guidance functions. The capabilities of full-graphic, color CRT's, real-time interactive displays and predictive and disturbance analysis integrated in the control complex is provided by an interactive display and control system with fast-time computational ability. This type of system in conjunction with an optimally automated control system allows the operator to control

multiple modular reactor systems from a single integrated work station. Flexibility and interchangeability of each interactive display and control system allows for isolation of operating and under-construction units. In addition, physical separation of the startup module can be provided within the control complex through the modular configuration of the control consoles (wing units are modular) if such separation is deemed desirable.

REFERENCES

- 8.1-1 Willie Hammer, "Handbook of System and Product Safety," Prentice Hall, Inc., N.J., 1972.
- 8.1-2 "Final Safety Analysis Report, Fast Flux Test Facility," US ERDA, December 1975.
- 8.1-3 "Advanced Systems Design and Technology Development Plan", GEFR-00691, (to be published).

TABLE 8.1-1

SAFETY MEASURES

ACCIDENT PREVENTION		DAMAGE MINIMIZATION AND CONTROL	
1	Hazard elimination	1	Isolation <ul style="list-style-type: none"> a Distance b Energy absorption c Deflection d Containment <ul style="list-style-type: none"> • Hazard • Operation • Personnel • Material • Critical equipment
2	Hazard level limitation <ul style="list-style-type: none"> a Intrinsic safety b Limit level sensing control c Continuous monitor and automatic control 	2	Personal protective equipment <ul style="list-style-type: none"> a Programmed dangerous operation b Investigations and corrections c Emergencies
3	Lockouts, lockins, and interlocks <ul style="list-style-type: none"> a Isolation b Lockouts and lockins c Interlocks 	3	Minor loss acceptance
4	Fail safe designs <ul style="list-style-type: none"> a Fail passive b Fail active c Fail operational 	4	Escape and survival <ul style="list-style-type: none"> a Point of no return warning b Crashworthiness designs c Escape and survival equipment d Escape and survival procedures
5	Failure minimization <ul style="list-style-type: none"> a Monitoring b Warning c Safety factors and margins d Failure rate reduction <ul style="list-style-type: none"> • Derating • Timed replacements • Screening • Redundancy 	5	Rescue <ul style="list-style-type: none"> a Procedures b Equipment
6	Backout and recovery <ul style="list-style-type: none"> a Normal sequence restoration b Aborting entire operation c Inactivating only malfunctioning equipment <ul style="list-style-type: none"> • Automatic • Manual 		

TABLE 8.1-2
REPRESENTATIVE PLANT TRANSIENTS

<u>Event</u>	<u>Transient</u>	<u>Frequency</u>
<u>Level A Service</u>		
A-1a	Startup from Refueling Temperatures	145
A-1c	Startup from Hot Standby Conditions	264
A-2a	Shutdown to Refueling Status	81
A-2c	Shutdown to Hot Standby Conditions	409
A-3a	Loading and Unloading (Normal 3%/min)	36,000
A-3b	Rapid Loading and Unloading (5%/min)	4,000
A-3c	Step Loading and Unloading (10%/min)	4,000
<u>Level B Service</u>		
B-1	Reactor Trip from Full Power with Nominal Decay Heat	115
B-5	Temporary Loss of Power to Primary Circulator	10
B-6	Primary Circulator Overspeed	7
B-7	Primary Circulator Pony Motor Failure	5
B-9	Feedwater Throttle Valve Failed Open	6
B-10	Partial Loss of Feedwater Flow to Steam Generator	50
B-11	Isolation and Blowdown of Steam Generator (Reformer Continues to Operate	20
B-12a	Turbine Trip Without Reactor Trip	50
B-12b	Turbine Trip With Reactor Trip (Loss of Main Condenser or Similar Problem)	20
B-13	Loss of All Offsite Power	16
B-14	Inadvertent Opening of Steam Generator Relief Valves	5

TABLE 8.1-2 (Continued)
REPRESENTATIVE PLANT TRANSIENTS

<u>Event</u>	<u>Transient</u>	<u>Frequency</u>
<u>Level B Service</u>		
B-16	Deactivation of Reformer Catalyst	20
B-17a	Isolation of Reformer Process Gas (Includes Small Process Gas Ingress to Primary Helium)	60
B-17b	Isolation of Reformer with concurrent trip of primary circulator	20
B-18	Inadvertent Opening of Steam Generator Helium Bypass Valve from 100% Power	5
B-19	Small Steam Ingress to Primary Helium	TBD
B-20	Operating Basis Earthquake	TBD
<u>Level C Service</u>		
C-1	Failure of Primary Circulator	Each component must accommodate 7 of the most severe of the emergency events.
C-2a	Isolation and Blowdown of Steam Generator concurrent with Reformer Isolation	
C-2b	Isolation and Blowdown of Steam Generator, W/SG Bypass Failed Shut	
C-2c	Steam Line Rupture	
C-3a	Uncontrolled Rod Withdrawal from Start up Automatic Trip	
C-3b	Uncontrolled Rod Withdrawal from 100% Power	
C-4	Slow Primary System Depressurization	
C-5	Large Steam Ingress to Primary Helium	
C-6	Large Process Gas Ingress to Primary Helium	
C-7	Process Gas Line Rupture	

TABLE 8.1-2 (Continued)
REPRESENTATIVE PLANT TRANSIENTS

<u>Event</u> <u>Level D Service</u>	<u>Transient</u>	<u>Frequency</u>
D-1a	Sudden Depressurization, Steam Generator and Circulation Available	1
D-1b	Sudden Depressurization, Steam Generator not Available	1
D-2	Safe Shutdown Earthquake	1
D-3	Failure of all Vessel Cavity Cooling with	1

TABLE 8.1-3
SUMMARY OF MRS-R UMBRELLA SERVICE LOADINGS EVENTS

<u>Upset Events</u>	Event Characteristics		
	Pressure (PSI)	Max Temp* (°F)	Total Duration (HRS)
1. Isolation & Blowdown of Steam Generator (B-11)	$\Delta P=700$	$T_{S.G.}=1150$ (External)	~160
2. Deactivation of Reformer Catalyst (B-16)	$\Delta P=65$	$T_{Wall}=1735$	14,700
3. Isolation of Reformer Process Gas			
Forced Circulation (B-17a)	$\Delta P=250$	$T_{T.S.}=1250$	~17
Natural Circulation (B-17b)	$\Delta P=350$	$T_{T.S.}=1600$	~160
4. Inadvertent Opening of SG Helium Bypass Valve at Power (B-18)	No	$\Delta T_{Wall}=421$ Change	TBD

Emergency Events

1. Isolation & Blowdown of SG Concurrent with Reformer Isolation (C-2a)	$\Delta P=330$	$T_{T.S.}=1764$	~520
2. Process Gas Line Rupture (C-7)	$\Delta P=710$	$T_{T.S.}=1350$	~32

* Either reformer tube wall (T_{Wall}) or lower tubesheet ($T_{T.S.}$) temperature.

Table 8.1-4. CONDITIONS FOR DUTY CYCLE ANALYSIS

Case	Model	Helium Flow	Feedwater		Process Gas Inlet		
			Flow	Temp	Flo.	Tem.	Composition ⁽³⁾
1R	Reformer Only	100% ⁽¹⁾	(2)	(2)	0.0	-	-
2R	" "	"			100%	1000°F	100% steam
3R	" "	"			100%	1000°F	S/M = 3
1L	MRS Loop	100%	100%	500°F	0.0	-	-
2L	" "	"	"	"	100%	1000°F	100% steam
3L	" "	"	"	"	100%	1000°F	S/M = 3
4L	" "	"	"	"	Fig.7	Fig.8	Fig.7

(1) With reformer model stand alone computations, helium temperature at the reformer inlet was taken from GA results.

(2) With reformer model stand alone computations, feedwater conditions not required.

(3) S/M = steam to methane ratio

Table 8.1-5

SUMMARY OF RESULTS

HTGR THERMAL ANALYSIS

(Configuration 1 Thermal Model, See Section 3.7 for Definition)

COMPONENT VARIABLE	TRANSIENT							
	B-17a		B-17b		C-2a		C-7	
	Time ~Hours	Temp. or Temp. Diff. ~ F	Time ~Hours	Temp. or Temp. Diff. ~ F	Time ~Hours	Temp. or Temp. Diff. ~ F	Time ~Hours	Temp. or Temp. Diff. ~ F
<u>LOWER TUBESHEET CENTER</u>								
Peak Average Temperature	0.2	1005	3.5	1580	9	1718	5.5	1226
Peak Axial Gradient	1.5(+)	93(+)	1.0	106	13	134	6.0	123
<u>LOWER TUBESHEET RIM</u>								
Peak Average Temperature	0.2	990	5.0	1266	18	1537	6.5	1081
Peak Axial Gradient	1.5(+)	222(+)	3.0	206	25(+)	258(+)	0.0	196
Lower Tubesheet Radial Temperature Difference	1.5(+)	121(+)	3.0	368	4	346	5.0	154
<u>UPPER TUBESHEET CENTER</u>								
Peak Average Temperature	0.1	1181	3.5	1437	9	1560	0.0	1174
Peak Axial Gradient	1.5(+)	17(+)	3.0	45	7	45	3.0	27
<u>UPPER TUBESHEET RIM</u>								
Peak Average Temperature	0.1	1005	4.0	1289	11	1439	0.0	1046
Peak Axial Gradient	1.5(+)	8(+)	10.0(+)	9(+)	6	8	0.0	7
Upper Tubesheet Radial Temperature Difference	1.5(+)	165(+)	2.5	171	25(+)	3(+)	10.0(+)	-6(+)
<u>TUBESHEET SUPPORT CONE</u>								
Peak Average Temperatures	0.6	895	9.0	1002	25(+)	1298(+)	10 (+)	917
Peak Axial Gradient	0.5	100	6.0	213	12	284	10.0(+)	172(+)
<u>TUBESHEET FLANGE</u>								
Peak Average Temperature	0.2	807	5.5	892	25(+)	1119(+)	0.0	804
Peak Radial Gradient	1.5(+)	217(+)	8.5	217	24	242	8.5	247
Peak Axial Gradient	0.2	98	4.0	218	8	247	0.0	93
<u>VESSEL FLANGE</u>								
Peak Average Temperature	0.6	632	7.0	704	25(+)	878(+)	0.0	631
Peak Radial Gradient	0.5	101	4.5	137	8	151	0.5	98
Peak Axial Gradient	0.5	120	4.5	220	11	256	0.0	117

+ ~ Peak had not been reached at end of transient

- ~ Gradient reverses direction during transient

Table 8.1-6

SUMMARY OF RESULTS

HTGR THERMAL ANALYSIS

LOWER TUBESHEET PEAK TEMPERATURE
AND THERMAL GRADIENT REDUCTION STUDIES

COMPONENT VARIABLE	TRANSIENT C - 2 a							
	Configuration 1		Configuration 2		Configuration 3		Configuration 6	
	Time ~ Hours	Temp. or Temp. Diff. ~ °F	Time ~ Hours	Temp. or Temp. Diff. ~ °F	Time ~ Hours	Temp. or Temp. Diff. ~ °F	Time ~ Hours	Temp. or Temp. diff ~ °F
<u>LOWER TUBESHEET CENTER</u>								
Peak Average Temperature	9	1718	9	1710	11	1605	25(+)	1123(+)
Peak Axial Gradient	13	134	11	195	11	238	25(+)	108(+)
<u>LOWER TUBESHEET RIM</u>								
Peak Average Temperature	19	1537	11	1610	17	1509	25(+)	1083(+)
Peak Axial Gradient	25(+)	258(+)	25(+)	228(+)	25(+)	182(+)	25(+)	185(+)
Lower Tubesheet Radial Temperature Difference	4	346	4	205	6	176	17	44
<u>UPPER TUBESHEET CENTER</u>								
Peak Average Temperature	9	1560	10	1427	10	1423	9	1418
Peak Axial Gradient	7	45	7	49	7	49	7	48
<u>UPPER TUBESHEET RIM</u>								
Peak Average Temperature	11	1439	12	1251	12	1245	12	1235
Peak Axial Gradient	6 25(+)	8 3(+)	9	16	9	16	9	16
Upper Tubesheet Radial Temperature Difference	4	162	6	209	6	212	6	214
<u>TUBESHEET SUPPORT CONE</u>								
Peak Average Temperatures	25(+)	1298(+)	25(+)	1296(+)	25(+)	1215(+)	25(+)	993(+)
Peak Axial Gradient	12	284	11	432	25(+)	122(+)	15	19
<u>TUBESHEET FLANGE</u>								
Peak Average Temperature	25(+)	1119(+)	24(+)	1002(+)	25(+)	982(+)	25(+)	930(+)
Peak Radial Gradient	24	242	13	316	12	298	12	276
Peak Axial Gradient	8	247	25(+)	28	24	204	15	19
<u>VESSEL FLANGE</u>								
Peak Average Temperature	25(+)	878(+)	25(+)	659(+)	25(+)	656(+)	25(+)	930(+)
Peak Radial Gradient	8	151	16	209	16	211	18	213
Peak Axial Gradient @ Vessel Wall	11	256	11	139	11	137	11	135

+ ~ Peak had not been reached at end of transient

- ~ Gradient reverses direction during transient

Configuration	Description
1	Reference
2	8" Removed from Lower Tubesheet Rim; Perfect Natural Circ., Inner Surf. Support Structure Insulated
3	Same as 2 plus Lower Tubesheet Hole Inserts
6	Same as 2 but no reformer tube natural circulation

TABLE 8.1-7

REFORMER TUBE PRIMARY MEMBRANE STRESS ANALYSIS RESULTS

Service Level	Transient Case	Temp. °F	Duration (Hr)	P (psi)	P _m (psi)	Allowable (psi)	Ratio	Status
A		1691	147,000	51	247	600	0.41	Pass
B	B-16	1735	14,700	65	314	800	0.39	Pass
B	B-17a	1700	17	210	1016	4700	0.22	Pass
C	C-7 Seg. 1	1735	7	-672	-3250	4770	0.68	Pass
	Seg. 2	1735	57	-500	-2420	3000	0.81	Pass
C	C-2a Seg. 1	1800	133	-140	677	1750	0.39	Pass
	Seg. 2	1700	112	-140	677	2700	0.25	Pass
	Seg. 3	1600	112	-50	242	4090	0.06	Pass
	Seg. 4	1500	560	+710	3430	4870	0.70	Pass

TABLE 8.1-8
TRANSITION WELD FATIGUE EVALUATION, EVENT C-2A

Material	800H	617
$T_{\max}$ (°F)	1800	1800
ΔP_{range} (psi)	100	100
Pressure Stress Range (psi)	470	470
Thru-Wall Gradient Range (°F)	102°F	102
Peak Gradient Stress Range (°F)	16090	13380
Ave. Temp. Range (°F)	1730°F	1730°F
Differential Stress Range (psi)	65460	65460
Total Stress Range (psi)	83520	80800
Strain Range (in/in)	.0044	.0039
N_f (cycles)	62	75
N_{fH} (hold)	6	7
Damage Fraction n/N_{fH}	.67	.57

TABLE 8.1-9
LOWER TUBESHEET SIZING RESULTS -
MINIMUM THICKNESS TO SATISFY PRIMARY LOADING

Upper Umbrella Condition (B-17a 3 Occurrences, B-17b 10 Occurrences)

<u>Load Segment</u>	<u>Temp. (°F)</u>	<u>Duration (Hr)</u>	<u>ΔP^* (psi)</u>	<u>Allowable (psi)</u>	<u>Minimum Thickness</u>
A	1600	10	300	6,410	21.28
B	1550	13	310	7,610	19.85
C	1500	10	320	9,250	18.29
D	1400	20	330	10,750	17.23
F	1200	24	340	20,700	12.60
F	1000	22	350	21,600	12.52

Emergency Condition C-2a (4 Occurrences)

A	1765	10	135	2,750	21.79
B	1700	16	65	3,500	13.40
C	1600	16	45	5,300	9.06
D	1500	80	40	6,490	7.72

Emergency Condition C-7 (4 Occurrences)

A	1400	10	430	11,500	19.02
B	1300	8	450	18,590	15.30
C	1200	14	470	21,920	14.40
D	1000	8	350	24,250	11.82
E	1000	2	629	24,250	15.84

* ΔP includes an effective downward pressure of 25 psi due to dead weight loading.

TABLE 8.1-10

LOWER TUBESHEET SUPPORT CONE ANALYSISSupported Weight

Tubesheet	59680
Tubes	217409
Catalyst	5350
Skirt	18810
Shroud	46068
Baffles	<u>16239</u>
TOTAL	363600

Geometry

Mean Diameter at Flange	143 in
Thickness	8 in
Cone angle	25°

Results

Axial stress due to Dead Weight	112 psi
Axial stress due to DW + 1.5g seismic	280 psi

SUMMARY OF PRIMARY LOADING RESULTS (Pm)

Event	Cond. (Level)	Temp. °F	Duration (Hr)	P (psi)	P _m (psi)	Allowable (psi)	Ratio	Status
B-17b	Upset	1500	23	285	2,835	6700	.42	PASS
C-7	Emergency	1000	0	-710	5,640	17280	.01	PASS
C-7	"	1300	10	-460	3,650	11700	.31	PASS
C-2a	"	1665	40	+165	1,315	3500	.38	PASS

TABLE 8.1-11

COMPARISON OF THERMAL RESULTS FOR LOWER TUBESHEET

	<u>Previous</u>	<u>Revised</u>
Rim Ave. Temp.	1608	1509
Center Ave. Temp. (°F)	1221	1304
Differential Temp.	387	205
Center Axial Gradient	40	285
Rim Axial Gradient	80	130

TABLE 8.1-12
Development Plan Main Activities

FY	83	84	85	86	87	88	89	90	91	92	93	94	95	96	97	98
Project Phase	Concept Evaluation			Project Definition		Preliminary Design			Final Design			Field & Construction		Engineering		

Safety/Licensing
Objective

-Develop method for evaluating test plans for demonstrating the walkaway safety concept and apply the method to establish test plans

-Update technology development plan

Licensing

-Conduct safety and licensing assessment

-Issue licensing plan

-Provide support for pre-application review

-Issue safety analysis report

-Conduct site evaluation

-Define safety criteria

-Issue environmental report studies

-Obtain operating license

TABLE 8.1-12 (Continued)
Development Plan Main Activities

FY	83	84	85	86	87	88	89	90	91	92	93	94	95	96	97	98	
Project Phase	Concept Evaluation			Project Definition		Preliminary Design			Final Design			Field & Construction		Engineering			
Design Tradeoff Support	-Adapt safety-oriented design optimization code to MRS and provide preliminary application and sensitivity analysis			-Extend code capability to meet preliminary design needs		-Apply code for preliminary design optimization											
Technology Base Development	-Conduct sensitivity studies to identify R&D needs to demonstrate the walkaway safety concept.			-Conduct tests per technology development plan		-Conduct tests per technology development plan			-Prepare pre-operational test plan			-Complete tests per technology development plan					
	-Develop method for evaluating reforming stability and apply it to demonstrate stability and identify R&D needs			-Update Technology Development Plan		-Update Technology Development Plan			-Conduct tests per technology development plan			-Update Technology Development Plan					

TABLE 8.1-13
DESIGN COST ESTIMATES
 (1983 \$ X 1000)

SYSTEM	DESIGN PHASES				REMARKS
	<u>PRE-CON.</u>	<u>CONCEPT.</u>	<u>PRELIM.</u>	<u>FINAL</u>	
5.1 Reactor Core & Fuel					
5.1.1 Fuel Cycle & Nuclear Des.	(1) 720	960	1,200	1,920	(1) Costs 360,480 600 and 960 more for MRS-R.
5.1.2 Core Fuel Performance	(2)				(2) Included in
5.1.3 Shielding Studies	300	400	500	800	5.1.1
5.2 Reactor Systems					
5.2.1 Reactor Core Design	1,013	1,350	1,687	2,700	
5.2.2 Helium Circulator	900	1,200	1,500	2,400	
5.2.3 Control Rod Drives	228	304	380	608	
5.2.4 Fuel Handling	254	338	423	676	
5.2.5 Safety-Related Systems	600	750	980	1,325	
5.2.6 Heat Removal Systems	(3) 615	820	1,025	1,640	(3) Costs 30, 40, 50 and 80 more for MRS-R
5.4 Upper Vessel Systems-R					
5.4.1 Steam Reformer (C-E)	160	350	400	500	
5.4.2 Steam Generator	270	300	600	600	
5.4.3 Upper Vessel	260	250	600	1,200	
5.4.4 Bypass Valve	180	270	200	400	

TABLE 3.1-13 (Continued)
DESIGN COST ESTIMATES
 (1983 \$ X 1000)

SYSTEM	DESIGN PHASES				REMARKS
	<u>PRE-CON.</u>	<u>CONCEPT.</u>	<u>PRELIM.</u>	<u>FINAL</u>	
5.5 Upper Vessel System-SC					
5.5.1 Upper Vessel	260	250	600	1,200	
5.5.2 Steam Generator	270	300	600	900	
5.5.3 Bypass Valve	180	270	200	400	
5.6 Balance of Plant (BOP) ¹	9,750	13,000	16,250	26,000	
5.7 Reformer System (GE)					
5.7.1 Reformer	450	600	750	1,200	
5.7.2 Catalyst	300	400	500	800	
5.7.3 Coolant Chemistry	150	200	250	400	
Other Design Activities					
A. MRS-SC/BOP Integration	(4) 750	1,000	1,250	2,000	(4) Costs 300, 400 500 and 800 more for MRS-R.
B. Pebble Bed/In-line Designs	750	1,000	1,250	2,000	
C. Pebble Bed/SBS Designs	450	600	750	1,200	
TOTALS:					<u>TOTALS</u>
MRS-Steam Cycle	18,050	24,067	30,083	48,133	120,333
MRS-Reformer	18,740	24,987	31,233	49,973	124,933

TABLE 8.1-14
DEVELOPMENT COST ESTIMATES
 (1983\$X1000)

SYSTEM/COMPONENT	COST	REMARKS
7.1 Reactor Fuel		
7.1.1 LEU (950 °C)	-	Cost in HTGR SC/C program
7.1.1.1 Fuel Process Dev.	-	
7.1.1.2 Fuel Material Devl	-	
7.1.1.3 Graphite	-	
7.1.2 HEU (700° C & 950°C)	-	
7.1.2.1 Fuel Process Dev.	2,100	
7.1.2.2 Fuel Materials Dev.	3,000	
7.1.2.3 Graphite	-	Same as 7.1.1.3
7.2.1.2 Fuel Cycle	700	
7.2.2.4 Core Test Programs	6,600	
7.2.3 Fission Product Transport		
7.2.3.1 MRS-SC	-	HTGR-SC/C
7.3.1.2 Test Programs	500	
7.3.2 Neutron Control System	TBD	
7.3.3 Primary Coolant System		
7.3.3.1 Circulator	TBD	
7.3.4 Fuel Handling System	TBD	

TABLE 8.1-14 (Continued)
DEVELOPMENT COST ESTIMATES
 (1983\$X1000)

SYSTEM/COMPONENT	COST	REMARKS
7.3.5 Decay Heat Removal	TBD	
7.3.6 Core Heatup		
7.3.6.1 Material Property Data	50	
7.3.6.2 Material Fragility Model	150	
7.3.6.3 Accident Conditions Fuel Performance and Model	150	
7.3.7 Availability Methods (Comp. Codes)	133	
7.3.8 In-Service Inspection (ISI)		
7.3.8.1 Mock-up and Models	250	
7.3.8.2 Tests & Demonstrations	380	
7.4 Upper Vessel System (R)		
7.4.1 Design Support		
7.4.1.1 Ref. Ther/Hyd Design Code	75	
7.4.1.2 Models to Analyze T/H Cycle Events	250	
7.4.1.3 Hydraulic Performance-Shell Side	630	

TABLE 8.1-14 (Continued)
DEVELOPMENT COST ESTIMATES
 (1983\$X1000)

SYSTEM/COMPONENT	COST	REMARKS
7.4.1.4 Helium-Side Heat Transfer		
Coef.	1,260	
7.4.1.5 Charact. Reforming Efficiency	200	
7.4.1.6 Structural Des. & Analysis		
Tests	900	
7.4.1.7 Proc. for Locating & Repairing		
Tubes	250	
7.4.1.8 Validate Thermochemical		
Performance-Single Tube	900	
7.4.1.9 Multi-Tube Thermochemical		
Performance Test	2,000	
7.4.2 Upper Vessel Development		
7.4.2.1 Upper Vessel Airflow & Seals	1,400	
7.4.2.2 Reformer ISW, 800H Forg,		
and Airflow	3,800	
7.4.2.3 Steam Generator Methods		
Update	150	
7.4.2.4 Bypass Valve Materials, Air		
Flow and Functional Tests	1,850	
7.4.3 Catalyst		
7.4.3.1 Experimental Data Generation	95	
7.4.3.2 Program Development	50	

TABLE 8.1-14 (Continued)
DEVELOPMENT COST ESTIMATES
 (1983\$X1000)

SYSTEM/COMPONENT	COST	REMARKS
7.4.4 Coolant Chemistry		
7.4.4.1 Extend Models for Inconel 617, 2- 1/4 Cr-1Mo and Perform Parametric Studies	300	
7.4.4.2 Extend Models for New Alloys and Perform Trade-off Studies	250	
7.5 Upper Vessel System (SC)		
7.5.1 Upper Vessel Air Flow & Seals	1,200	
7.5.2 Steam Generator Methods	150	
7.5.3 Bypass Valve Materials, Air Flow and Functional Tests	1,650	
7.6 Balance of Plant	No Dev. Costs Anticipated	
7.7.1 Safety/Licensing		
7.7.1.1 Technology Base Analysis and Planning	1,350	
7.7.1.2 Testing-Reformer/In-Line	3,350	
7.7.1.3 Testing-Reformer/SBS	900	
7.7.1.4 Testing-Reformer/Pebble Bed	1,650	
7.7.2 Licensing	3,600	

TABLE 8.1-14 (Continued)
DEVELOPMENT COST ESTIMATES
 (1983\$X1000)

<u>SYSTEM/COMPONENT</u>	<u>COST</u>	<u>REMARKS</u>
7.7.3 Materials		
7.7.3.1 Ret. Alloy Microstructural Stab.	230	
7.7.3.2 Inconel 617 Prop/Correlations for Code Case N47	175	
Other Development Costs		
A. MRS Natural Circulation Test Facility	8,000	
TOTAL	50,926*	

* \$50,926 Identified; Development Costs for 4 additional Items are TBD.

TABLE 8.1-15
ESTIMATED COSTS BY PARTICIPANTS
 (1983\$X1000)

<u>System/Component</u>	<u>COMPANY</u>			
	BGI	C-E	GA	GE-ARSD
<u>DESIGN ACTIVITIES</u>				
Fuel Cycle			4,800	
Shielding			2,000	
Reactor Core Design			6,750	
Helium Circulator			5,700	
Control Rod Drives			1,520	
Fuel Handling			1,691	
Safety Related Sys.			3,655	
Heat Removal Sys.			4,100	
Lower Vessel Systems		1,390		
Steam Reformer		1,410		3,000
Steam Generator (R)		1,770		
Upper Vessel (R)		2,310		
Bypass Valve (R)		1,050		
Catalyst (R)				2,000
Coolant Chemistry				1,000
Steam Generator (SC)		2,070		
Upper Vessel (SC)		2,310		
Bypass Valve (SC)		1,050		

TABLE 8.1-15 (Continued)
ESTIMATED COSTS BY PARTICIPANTS
 (1983\$X1000)

<u>System/Component</u>	<u>COMPANY</u>			
	BGI	C-E	GA	GE-ARSD
<u>DESIGN ACTIVITIES</u>				
Balance of Plant	65,000			
MRS-SC/BOP Integr.	1,000	1,000	1,000	2,000
Pebble Bed/In-Line	1,000	1,000	1,000	2,000
Pebble Bed/SBS	600	600	600	1,200
TOTALS	67,600	14,190	32,816	11,200

TABLE 8.1-15 (Continued)
ESTIMATED COSTS BY PARTICIPANTS
 (1983\$X1000)

<u>System/Component</u>	<u>COMPANY</u>			
	BGI	C-E	GA	GE-ARSD
<u>DEVELOPMENT ACTIVITIES</u>				
HEU Fuel Process			2,100	
HEU Fuel Materials			3,000	
Fuel Cycle			700	
Core Test Programmed			6,600	
Fission Product Transp.			1,000	
Reactor Internals Test			500	
Neutron Control Sys.			TBD	
Circulator			TBD	
Fuel Handling Sys.			TBD	
Decay Heat Removal			TBD	
Availability Methods				1,330
In-Service Inspect.			630	
Reformer Design Sup.				6,450
Upper Vessel Dev. (R)		7,200		
Catalyst				145
Coolant Chemistry				550
Upper Vessel Dev. (SC)		3,000		
Safety/Licensing				10,850
Materials				405
MRS Nat. Circ. Test Facility			8,000	
	-0-	10,200	22,530	19,730
			+ 4 TBD

Figure 8.1-1
SCHEMATIC OF MRS SIMULATION MODEL
DATA FLOW TO MODULES

NSTV(=8) AXIAL TUBE NODES FOR TUBE TEMP DYNAMICS
 IMSV1(1-8), TMSV3(1-8)
 NAXRF(=40) AXIAL NODES FOR HEAT TRANSFER AND CHEMICAL REACTION
 CALCULATIONS, GAS HEAT BALANCES TP(1-41), TT(1-41)

Figure 8.1-2
 SIMPLIFIED REPRESENTATION OF REFORMER
 DYNAMIC ANALYSIS MODEL

1/2-1.8

Figure 8.1-3
Helium and Process Gas Temperature
MRS - Loop Model Case 2L

Figure 8.1-4
MRS CONFIGURATION

8.1-73

Figure 8.1-5

Helium and Process Gas Temperatures

MRS - Loop Model Case 3L

Figure 8.1-6

HELIUM TEMPERATURES MRS LOOP MODEL

Figure 8.1-7
REFORMER PROCESS GAS INLET RUNBACK CONDITIONS

9/1-1'8

Figure 8.1-8 PROCESS GAS TEMPERATURE
MFS LOOP MODEL

Figure 8.1-9 HELIUM TEMPERATURE

Figure 8.1-10
 TEMPERATURE DIFFERENCE ACROSS REFORMER TUBE

Figure 8.1-11
Reformer Tube Temperature
MRS - Loop Model Case 2L

08-1'8

Refrmer Tube Temperature Differences at 0.6 Meters

Figure 8.1-13
REFORMER TUBE TEMPERATURE DIFFERENCES AT 5.6 METERS
MRS - Loop Model Case 2L

Figure 8.1-14
REFORMER PIGTAIL TEMPERATURES
MRS - Loop Model Case 2L

Figure 8.1-15
REFORMER TUBE TEMPERATURES
MRS - Loop Model Case 3L

Figure 8.1-16
REFORMER TUBE TEMPERATURE DIFFERENCE
AT 0.6 METERS
MRS - Loop Model Case 3L

58-1.8

Figure 8.1-17
Reformer Tube Temperature Difference at 5.6 Meters
MRS - Loop Model Case 3L

Figure 8.1-18
Reformer Pigtail Temperatures
MFS - Loop Model Case 3L

83-458-06

Figure 8.1-19 THERMAL PROFILE OF UPPER TUBESHEET REGION HEATUP TRANSIENT (2a) AT FIVE HOURS (°F)

PROCESS GAS TEMPERATURE PROFILE

FOR A RANGE OF NATURAL CIRCULATION FLOWS
@ 5NHS INTO TRANSIENT C-20

Figure 8.1-20

EFFECT OF NATURAL CIRCULATION FLOW
ON PEAK TEMPERATURES FOR

TRANSIENT C-2a

Figure 8.1-21

83-458-07

Figure 8.1-22 REFORMER PROCESS GAS PLENUM RECONFIGURATION

83-458-08

Figure 8.1-23 EXAMPLE OF PROCESS GAS TEMPERATURES WHEN INCORPORATING A RECUPERATOR

83-458-09

Figure 8.1-24 SCHEMATIC OF ISOLATING INDIVIDUAL REFORMER TUBES TO MITIGATE THE EFFECT OF A RUPTURED PROCESS GAS LINE

CASES OF ASME BOILER AND PRESSURE VESSEL CODE

Fig. T-1420-1C Design fatigue strain range, ϵ_f , for Ni-Fe-Cr Alloy 800H

Fig. T-1430-1C Design fatigue strain range ϵ_f for Ni-Fe-Cr (Alloy 800H) – elastic analysis

83-470-01

Figure 8.1-25 COMPARISON OF FATIGUE RESULTS WITH ALLOWABLE CYCLES AND PREVIOUS RESULTS FOR LOWER TUBE SHEET

*MRS-R DEMONSTRATION PLANT
MRS-SC LEAD COMMERCIAL PLANT

83-467-04

**Figure 8.1-26 HTGR HIGH TEMPERATURE PROCESS HEAT ADVANCED SYSTEMS CONCEPTS*
COMMERCIALIZATION PROGRAM MAJOR MILESTONES**

83-467-05

Figure 8.1-27 TARGET SCHEDULE FOR THE HTGR LEAD PLANT