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James R. Kelsey, Editor

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**GEOHERMAL TECHNOLOGY DEVELOPMENT PROGRAM
ANNUAL PROGRESS REPORT**

October 1981-September 1982

**James R. Kelsey
Editor
Geothermal Technology Development
Division 6241
Sandia National Laboratories
Albuquerque, NM 87185**

ABSTRACT

This report describes the status of ongoing Research and Development (R&D) within the Geothermal Technology Development Program. The work reported is sponsored by the Department of Energy/Geothermal Hydropower Technology Division (DOE/GHTD), with program management provided by Sandia National Laboratories. The program emphasizes research in rock penetration mechanics, fluid technology, borehole mechanics, diagnostics technology, and permeability enhancement.

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GEOTHERMAL TECHNOLOGY DEVELOPMENT PROGRAM
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1. SUMMARY

Introduction

The high cost of drilling and completing geothermal wells is an impediment to the timely development of geothermal resources in the United States. The Geothermal Hydropower Technology Division (GHTD) of the U.S. Department of Energy (DOE) has initiated a development program aimed at reducing well costs through improvements in the technology used to drill, complete, and log geothermal wells. Sandia National Laboratories (SNL) has been selected to manage this program for DOE/GHTD. Based on analyses of existing well problems, research goals have been set for the program.

To meet these goals, technology development in a wide range of areas is required. The near-term goal is being approached by improvements in conventional, rotary-drilling technology. The long-term goal will require the development of advanced drilling and completion systems. Research is being conducted in high-risk areas that are not being addressed by private industry due to the lack of near-term payoffs and small market potential.

Because of the large-scale reduction in resources for FY 1982, the program was restructured during the second quarter of FY 1981. Included in the restructuring effort were a reduction in the number of program subelements from six to four and the incorporation into the program of the work in logging and high-temperature instrumentation. The four new program subelements are Rock Penetration Mechanics, Fluid Technology, Borehole Mechanics, and Diagnostics Technology. A fifth subelement, Permeability Enhancement, was added to the program during FY 1982.

This report encapsulates the activities within the ongoing research and development (R&D) program for the 1982 fiscal year. Appendix A lists published comprehensive reports on completed projects and presentations made on program work during this reporting period. The reports are available from the National Technical Information Service, U.S. Department of Commerce, 5285 Port Royal Rd., Springfield, VA 22161.

Highlights

Management

- Symposium. A high-temperature electronics and instrumentation symposium, sponsored by Sandia for DOE/GHTD, was held 7-8 December 1981 in Houston, Texas. Over 350 persons attended the symposium, including R&D personnel from industry, universities, and several foreign countries. The symposium provided a forum for the broad-based transfer of the technology developed under Sandia's high-temperature logging and instrumentation program and concluded Sandia's planned efforts in this area. A report of the proceedings of the symposium was prepared and published.¹

- Program Review. Comprehensive presentations delineating the activities of the Geothermal Technology Development Program were prepared and presented to the Geothermal Technical Industry Review Panel and the Division of Geothermal and Hydropower Technology, Department of Energy, on 27-28 May 1982 in Albuquerque, New Mexico. The minutes of the meeting have been published separately. The members of the review panel are listed in Appendix B.

- Technology Transfer. On 8 September 1982, a technology transfer meeting was held at Sandia to disclose the results of the Wellbore Inertial Navigation System development and test program. The system design and test data were presented in detail to industry representatives. Thirty representatives from various oil companies, wireline service companies, and suppliers attended the meeting. The information was favorably received, and several attendees indicated that their companies had plans to commercialize the wellbore mapping system (See Section 6.1).

Systems Analysis

- Geothermal Drilling Forecast. The forecast of geothermal drilling activity was refined and updated by a new survey project, GEODRILL II. This survey complemented an earlier study, GEODRILL I, with respect to both the data base and the methodology and included an effort to document all sources of data. The results of the survey indicated that The Geysers area will retain most of the drilling activity through 1985 and that the Imperial Valley will become the center of most drilling by the 1990s. The forecast projected the drilling of about 5,000 geothermal wells between 1981 and the year 2000. The updated forecast is discussed in Section 2.

- Drilling and Completion Troubles. A study of the troubles encountered in drilling and completing geothermal wells in The Geysers, Baca, and Imperial Valley resource areas was completed. The study confirmed that the development of technologies to avoid or correct geothermal drilling and completion problems can significantly reduce well costs. A summary of the study efforts is presented in Section 2.

- Completion Technology Life-Cycle Cost Model. A life-cycle cost model, designed to evaluate technologies that impact the life or productivity of geothermal wells, was completed, documented, and tested. A variety of test cases of the production, scheduling, and costing of the code were run successfully, indicating that the model is a valuable tool for assessing new technologies. The code is available for use by industry and is described in Section 2.

Rock Penetration Mechanics

- Development of Improved Cavijet® Nozzles. Phase II of the project to investigate Cavijet® nozzle design principles and to develop Cavijet® nozzles that achieve increased incipient and optimum cavitation numbers was completed. This phase of the project resulted in nozzle systems having incipient cavitation numbers two to six times higher (hence, greater jet erosivity) than those obtained with either conventional Cavijet® nozzles or typical commercial bit nozzles. Also, adaptability of the Organ-Pipe Cavijet® to the physical restraints of existing bits was demonstrated, and the potential of self-resonating structured jets for improved bottomhole cleaning (hence, better drilling) was established. In Section 3.1, the

phase II efforts are summarized, and the early work of phase III is described.

- Improvement of Downhole Drilling-Motor Bearings and Seals. Seal testing and evaluation tasks under this project were completed with the successful test of a second-generation Variseal®. In this final test, the seal was run for 100 hours at a differential pressure of 10.3 MPa (1,500 psi) without failure and with less than 20% leakage of the lubricant reserve. Failure of the floating piston continued to be experienced in the bearing/seal package testing and evaluation part of the project. Both third-generation HTRC and Variseal® floating piston designs failed at test times of 48 hours or less. The project test results are described in Section 3.2.

- Flexible Drilling Pipe. The project to develop a liner material suitable for use with Coflexip flexible drilling pipe at high pressures and temperatures was completed. The experiments showed that DuPont Teflon PFA 350, L'Garde EPDM Y267, or L'Garde AFLAS 291 would meet the liner pressure/temperature requirements when backed by stainless steel braided tubing. The potential of a graphite-reinforced EPDM elastomer (without braided tubing) for meeting the liner requirements was also demonstrated. A summary of the project efforts is reported in Section 3.3.

- Improved Cutter Materials. Experiments with a broad range of cemented carbides consisting of NbC and TaC with Co and Ni binders were conducted to develop a new family of cemented carbides that would provide an alternative to WC-Co materials for geothermal drilling. Test results showed the most promising systems were those based on NbC-Co. Hardness and toughness tests indicated that the NbC-Co systems have a qualitative dependence on binder content similar to that found in WC-Co. In addition, examination of wear surfaces by scanning electron microscopy suggested that surface plastic deformation of the carbide phase occurs in the carbide grains of NbC-Co materials, in contrast to surface fracture in the carbide grains of WC-Co. The tendency to deform plastically rather than to initiate microfracture could improve the wear resistance of hard grades of NbC-Co. Additional wear resistance testing of NbC-Co compositions was planned. Experimental test results are discussed in Section 3.5.

• Wear Mechanisms for Polycrystalline Diamond Compacts (PDCs) as Utilized for Drilling in Geothermal Environments. The redirected final phase of the project experiments to determine the friction coefficients of preworn PDC cutters on various rock types using different cooling fluids and to measure cutter temperatures when cutting shale and sandstone at high speeds was completed. The experimental data were used in examining the validity of mechanical and thermal models (Section 3.8) being developed in other Sandia projects. The results of the project experiments are presented in Section 3.7.

• Frictional Heating of Polycrystalline Diamond Compact Drag Tools. Phase I of the development of a numerical-analytical model to predict temperatures in stud-mounted PDC drag tools during rock cutting was completed. The model developed was found to be in reasonable agreement with experimentally measured wearflat temperatures. Analysis of the data showed that the cutter temperatures vary directly with the friction force on the wearflat and approximately as the one-half power of the sliding velocity. The data also showed that the ability of liquid drilling fluids to reduce friction is far more important in reducing operating temperatures than is their ability to provide cutter cooling. Phase II to expand the numerical-analytical model was begun. Phase I project efforts are summarized in Section 3.8.

Fluid Technology

• Aqueous Foam Drilling Fluids for Geothermal Applications. Research to develop aqueous foams for geothermal drilling continued during FY 1982. High-temperature/high-pressure stability tests of foams using additives to improve stability identified a biopolymer, Kelco K1D-97, that increased the drainage time of a C₁₂₋₁₄ alpha olefin sulfonate surfactant by three to five times at 140°C (284°F). Construction of the foam flow heat transfer loop was completed, and verification of the experimental facility was begun. Construction of the foam rheology test facility was also completed, and shakedown experiments with the facility were started. In addition, development of an aqueous foam rheological model was begun. The aqueous foam drilling fluid research activities and descriptions of the experimental facilities are presented in Section 4.1.

- Inert Gas Generation. The design and costing of a full-scale, diesel exhaust gas purification unit was completed. The portable system can be mounted on three semitrailers and is capable of producing $0.94 \text{ m}^3/\text{s}$ ($2,000 \text{ ft}^3/\text{min}$) of an inert, low-density drilling fluid (N_2) at a cost of between \$1.47 and \$1.73 per thousand standard cubic feet. The system design specifications and cost factors are described in Section 3.2.

- Drilling Fluid/Cement Studies. Phase I of the study of the formation of cement minerals in drilling fluids was completed. The study included experiments on the effect of additives on the viscosity of bentonite drilling fluid, and the data showed that salts and hydroxides of potassium provide excellent control of the fluid viscosity at high temperatures. The addition of lime was found to increase the fluid pH and promote the high-temperature reaction between bentonite and lime, with the precipitation of tobermorite, the common cement mineral. Phase II was begun to study the factors that control cement setting in bentonite drilling fluids. The results of the phase I studies are presented in Section 3.3.

- Bit Hydraulics Studies. The bit hydraulics studies were closed out with the completion of fluid flow pattern experiments and convective heat transfer measurements on three hydraulic configurations of a Sandia-designed PDC bit. The studies identified hydraulic configurations that improve cooling of the PDC studs and determined heat transfer coefficients for selected cutters on the three PDC test bit configurations. The results of the bit hydraulic studies are discussed in Section 3.4.

Borehole Mechanics

- Lost Circulation Test Facility. The procurement, fabrication, and assembly of the Lost Circulation Test Facility, including the control and instrumentation subsystems, reached the preshakedown stage in FY 1982. The test facility is described in Section 5.1.

Diagnostics Technology

- Wellbore Inertial Navigation System. The development of the experimental system for positional survey of geothermal wellbores was completed in FY 1982 with the successful conduct of field trials and the transfer of the technology to industry. The field tests were conducted in

a 2,225-m (7,300-ft) well at the Nevada Test Site. Navigation results for the north and east directions were excellent; repeatability between results from the descent portions of two surveys was within 0.9 m (3.0 ft). Vertical navigation results were not as good, with the vertical closure error for the two 2,134-m (7,000-ft) surveys ranging from 18.3 to 24.4 m (60 to 80 ft). The results of the field trials and analysis of the data are summarized in Section 6.1.

- Upgraded Acoustic Borehole Televiwer. Upgrading of the televiwer tool was completed during this reporting period. Laboratory tests of the full-scale instrument were successfully conducted in an autoclave at a temperature of 280°C (536°F) and at 31 MPa (4,500 psi) pressure. Field tests were also conducted in the Los Alamos Fenton Hill EE-2 well at depths up to 1,981 m (6,500 ft). The results of this upgrade project indicate that the televiwer is a valuable tool for casing inspection as well as for fracture characterization in geothermal wells. The results of the laboratory and field tests of the upgraded instrument are presented in Section 6.2.

Permeability Enhancement

- Geothermal Stimulation Analysis. Work was begun in FY 1982 under the new subelement, Permeability Enhancement, of the Geothermal Technology Development Program. The initial project was a preliminary investigation of the feasibility of stimulating geothermal wells using a tailored-pulse gas-fracture concept. The results of the analytical study indicated that the tailored-pulse gas-fracture technique may be feasible for at least some geothermal applications. The study is summarized in Section 7.1.

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2. SYSTEMS ANALYSIS

During FY 1982, systems analysis efforts continued to focus on areas of investigation that would assist in defining geothermal technology needs and provide tools for the evaluation of the potential impact of new technologies on the cost of geothermal energy production. In this report period, an update of the forecast of geothermal drilling activity, an analysis of drilling and completion troubles, a completion technology life-cycle cost model, and documentation of an advanced wellbore thermal simulator were completed. Summaries of these activities follow.

Forecast of Geothermal Drilling Activity

The geothermal drilling survey project, GEODRILL II, was completed during this reporting period, and a final report was published.² This work supplemented and updated a previous study, GEODRILL I, that developed a methodology for estimating geothermal drilling activity in specific areas of the United States. Using available data, this earlier effort made best estimates of drilling activity in The Geysers, the Imperial Valley, Roosevelt Hot Springs, Valles Caldera, and Northern Nevada. A report on the earlier survey was published as Reference 3.

The GEODRILL II work refined and updated both the data and the methodology of GEODRILL I and included an effort to document all sources of data. The previous methodology was refined by adding time-dependent parameters, by accounting for wells drilled in undeveloped areas, and by making it possible to unambiguously account for wells drilled prior to the initial start of full-scale development. The process of updating and refining the data and methodology did not result in any major revisions of any of the basic results or conclusions presented in the GEODRILL I report.

The GEODRILL II efforts included estimating the number of geothermal wells to be drilled in the United States for each 5-year period to the year 2000 and forecasting the growth of geothermally supplied electric power. The different types of geothermal wells needed to support the forecasted capacity were then quantified. In order that the methodology may be easily used for future forecasts and sensitivity analyses, a computer code, GEODRL, was written and implemented. The results reported for GEODRILL II were obtained using the final version of the code.

The results of the GEODRILL II work showed that the rate of growth of electric capacity at geothermal resource areas is expected to be 15% to 25% per year, after an initial critical size is reached, until natural or economic limits are approached. Five resource areas in the United States should grow to significant capacity by the end of the century (see Figure 2-1). The best estimates of the number of wells that must be drilled in each 5-year period to support the electric capacity are given in Figure 2-2. About 5,000 geothermal wells are expected to be drilled in support of all electric power projects in the United States from 1981 to the year 2000. The number of rigs required to drill these wells is depicted in Figure 2-3. The determination of the number of rigs required was made using the number of wells reported on Figure 2-2 and an average drilling time of 70 days per well. This time was not based on optimized drilling but assumed a normal amount of trouble in drilling to 2,438 m (8,000 ft). The number of rigs grows from 16 for the period 1981 to 1985 to 95 for the 5 years, 1996 to 2000. The number of wells for each 5-year period, Figure 2-2, includes production, exploration, injection, and replacement wells and takes into account drilling other resource areas. The Geysers area is expected to retain most of the drilling activity through 1985. By the 1990s, the Imperial Valley is expected to contain most of the drilling activity, with the result that half of the geothermal wells drilled in the next 20 years will be in the Imperial Valley.

Drilling and Completion Troubles

The study of the troubles encountered in drilling and completing geothermal wells was completed during this reporting period, and the study results were published in References 4 and 5. A summary of the study efforts follows.

Figure 2-1. Forecast of Geothermal Electric Power Capacity

Figure 2-2. Numbers of Geothermal Wells

1981-1985

(TOTAL - 16)

1986-1990

(TOTAL - 30)

1991-1995

(TOTAL - 52)

1996-2000

(TOTAL - 95)

Figure 2-3. Number of Drilling Rigs Required

The cost of trouble in geothermal drilling was analyzed by combining a compilation of subjective opinions of trouble frequency and severity with the available historical drilling data for each region of interest. Historical trouble statistics were determined directly from drilling records. For example, Table 2-1 shows the frequency and mean duration for various types of drilling troubles at the Baca geothermal field. The table shows that lost circulation, stuck pipe, and cementing/casing problems were the most frequent problems at the Baca site. Unfortunately, the paucity of such data for most areas precluded direct statistical modeling of troubles and necessitated the use of subjective estimates of trouble frequency and severity. The type of opinion analysis described above was carried out for different depth intervals and various types of troubles in the geothermal resource areas of interest.

Representative results of the subjective estimates are tabulated in Table 2-2 for three potential problems in The Geysers, Baca, and Imperial Valley geothermal resource areas. The cementing problems shown in Table 2-2 are only those that can be recognized during the cementing operation and do not include many problems that arise after completion, e.g., due to poor cementing.

A primary source of the actual drilling-time data used to analyze the frequency and severity of drilling and completion problems was the drilling records filed with governmental agencies. For wells in many geothermal resource areas, these records were the only drilling data available in the open literature. The trouble information recorded on them is generally quite sketchy and includes only major problems, and then only what the operator chooses to include. For example, the drilling records are not likely to show the lesser levels of lost circulation. However, the data contained in the records were useful in the conduct of this study. For example, the results of the analysis of data on 49 wells in The Geysers area obtained from the California State Division of Oil and Gas open files are presented in Table 2-3.

The study confirmed the need for developing technologies to avoid or correct geothermal drilling and completion problems and the fact that these technologies can have a significant impact on well costs.

Table 2-1

Relative Frequency and Severity of Various Troubles in Baca Wells

Type of Trouble	Frequency of Occurrence, %	Average Lost Time, Days
1. Lost circulation	29	2-1/2
2. Stuck pipe	17	8
3. Twist off	9	1-1/2
4. Side track	4	16
5. Rig problems	13	2
6. Casing problems	9	11
7. Cementing problems	8	8-1/2
8. Fishing/Junk	<u>11</u>	2-1/2
	100	

Table 2-2

Results of Subjective Analysis for Three Potential Problems

Region	Type of Trouble	Average Time Per Well h	Average Cost Per Well \$ x 10 ³
The Geysers	Lost Circulation	41	53
	Stuck Pipe	24	14
	Cementing	<u>17</u>	<u>20</u>
	totals	82	87
Baca	Lost Circulation	75	125
	Stuck Pipe	39	20
	Cementing	<u>26</u>	<u>27</u>
	totals	140	172
Imperial Valley	Lost Circulation	6	10
	Stuck Pipe	15	9
	Cementing	<u>45</u>	<u>48</u>
	totals	66	67

Table 2-3

Trouble Frequency and Severity in The Geysers Area

<u>Problem</u>	<u>Portion of Wells, %</u>	<u>Average Lost Time per Occurrence, days</u>
Cementing Problems	54	2
Lost Circulation	46	2-1/4
Stuck Pipe	38	3-1/4
Fishing	26	3
Side Tracking	26	4
Twist Off	24	4-3/4
Sloughed Hole	16	2-1/4
Casing Problems	10	1-3/4
Rig Problems	10	2-1/4

Completion Technology Life-Cycle Cost Model

The final efforts of the project to develop a geothermal completion technology life-cycle cost model (GEOCOM), designed to evaluate technologies that impact the life or productivity of wells, were completed early in this reporting period. The results of the project were documented and reported in Reference 6. Additional reports on the project were made in References 7 and 8 in order to provide information on the capabilities and availability of the GEOCOM Code to interested segments of industry. Most of the GEOCOM model development effort was completed in FY 1981 and was reported in Reference 9. During FY 1982, sensitivity analyses were completed, testing of the model was conducted, and data needs to improve the model accuracy and reliability were identified.

The GEOCOM model includes sufficient detail to allow comparisons among various completion and usage alternatives for both production and injection wells. It combines capital cost of the well and of other equipment; continuing cost for workovers, the cost of routine O&M, electricity, and chemicals; and an analysis of productivity. In order to adequately analyze productivity, the model uses different data to characterize different

geothermal resources and associates the appropriate well drilling cost with each resource. The geothermal resource areas modeled were Brawley, Heber, The Geysers, Baca, Roosevelt Hot Springs, and East Mesa. Since the scheduling of workovers can greatly impact productivity, the model allows freedom in such scheduling. The user can select which workovers he wants and when they are to be performed. The user can also select how the well is to be produced (artesian flow, submersible pump, line shaft-pump, etc.). In order for the model to be appropriately sensitive to completion technologies, costs based on gross historical averages, such as annual workover costs equal to a percentage of capital cost, could not be used. Instead, the model calculates cost for each workover performed and determines production as a function of time.

The parameters used to define the characteristics for each of the geothermal resources modeled are well depth, length of completion interval, wellhead temperature, well life, initial well flow rate, total dissolved solids fraction, steam fraction, and reservoir performance decline rate. The decline rate is important in determining how long the well will flow at a usable rate. The decline rate includes such elements as drawdown, interference, and formation plugging, i.e., all effects outside of the well. In contrast, the well lifetime is a physical or mechanical parameter determining how long until a failure, such as casing collapse, destroys the well.

The GEOCOM model does not have a sufficient data base to analyze all potential completion technologies. For many new ideas, cost and performance data do not exist. GEOCOM can, however, help evaluate such ideas. First, it can be used to analyze the present completion method to which the new method will be compared. Second, the parameter changes that determine the cost difference between the new and old technologies can be identified. Last, GEOCOM can be used to estimate the impacts of the parameter changes that define the new completion method. The basic technologies that have been included in the GEOCOM model are as follows:

1. Logging
2. Perforating
3. Mechanical Descaling

4. Hydrojet Descaling
5. Chemical Scale Inhibition
6. Injection Pumps
7. Submersible Pumps
8. Line-Shaft Pumps
9. Remedial Cementing
10. Underreaming and Gravel Pack
11. Slotted Liner Replacement
12. Well Repair with a Liner

The parameters used in the calculation of an individual GEOCOM model case can be grouped according to function into the following categories: capital cost, continuing cost, productivity, schedule, and financial. A list of these parameters is given in Table 2-4.

During this reporting period, sensitivity studies were completed on the following relevant factors: (1) well life, (2) mechanical descale, (3) baseline parameters, and (4) injection pump. For each of the studies, parameters were varied to determine the sensitivity of the cost/effectiveness ratio to the varied parameter. In addition to the sensitivity studies, an analysis of the cost effectiveness of chemical scale inhibition was made. Also, a variety of test cases of the production, scheduling, and costing parts of the code were run successfully. Some of the conclusions drawn from this initial use of GEOCOM include:

1. Completion strategies for achieving long production lifetimes for wells are not necessary and, in fact, may not be valuable. The rate of productivity decline for flow of the geothermal fluids into the well from the formation determines, to a large extent, the importance of long life.

2. The most cost effective way to combat scale in a geothermal well may vary from resource area to resource area. The best choice depends primarily on the scale buildup rate and on the cost and effectiveness of mechanically removing scale from the well.

Table 2-4
Parameters of GEOCOM Model

<u>Parameter</u>	<u>Value</u>	<u>Type of Parameter</u>
Initial well flow	1	Productivity
Reservoir decline rate	1	Productivity
Discount factor	0.00878/mo	Financial
Inflation factor	0.00695/mo	Financial
Well life	1	Schedule
Study period	360./mo	Schedule
Well flow loss	0.125/mo	Productivity
Initial delay	0.0 mo	Schedule
Capital costs		Capital costs
Well	1	
Other	2	
Workover		
Costs	2	Continuing costs
Frequency	2	Schedule
Duration	2	Schedule
Effectiveness	2	Productivity
Routine O&M		Continuing costs
Well	1	
Other	2	
Per pound production costs	2	Continuing costs
Repair	\$121,000	
Cost		Continuing costs
Time	999. mo	Schedule
Duration	0.33 mo	Schedule
Effectiveness	40%	Productivity
Abandonment cost	\$18,000	Continuing cost

NOTES: 1 = Resource dependent value
2 = Configuration and usage dependent value

3. The effective cost of wells for reinjecting geothermal brines is quite sensitive to declines in injectivity. Formation damage, or other mechanisms that reduce injectivity, can greatly increase injection costs. A major component of the cost of an injection well is the required pumping cost.

In addition to being used to analyze specific completion situations, the model was used to study the relative importance to well cost effectiveness of various performance and cost parameters. The sensitivity of well cost per pound of production to changes in various parameters was investigated. For relatively inexpensive or infrequent workovers, the unit well cost was most sensitive to initial well cost and initial fluid flow; but for cases, such as injection, in which the continuing activities are frequent or expensive, their effect dominated that of the initial parameters.

During the use of the GEOCOM model, attention was given to the identification of additional data needs to improve the accuracy and reliability of the model. These efforts led to the inclusion of a more accurate injection pump work algorithm. In addition, limited information regarding the frequency and effects of workovers and of the applicability of current oilfield tools in the high-temperature environments were identified as data areas requiring special assessment until the experience base has expanded. The model will continue to be used (and improved) for the types of analyses required to aid evaluation of the potential impacts of completion technologies developed under the Geothermal Technology Development Program.

Advanced Wellbore Thermal Simulator

The development of the thermal wellbore simulator, GEOTEMP2, for prediction of transient wellbore temperatures during drilling, circulating, injection, and production in geothermal wells was completed in FY 1981. A report on the project work was included in Reference 9. During this reporting period, the user manual was revised, and complete documentation of the code and the final research report were published as References 10, 11, and 12. In addition, benchmarking of GEOTEMP2 was completed at Sandia. The code is available for use by industry and other interested organizations.

3. ROCK PENETRATION MECHANICS

Research in rock penetration mechanics is directed toward a basic description of rock penetration by candidate advanced drilling systems. Understanding the behavior of polycrystalline diamond compact (PDC) drag bits in relation to rock fracture and wear mechanisms is receiving major emphasis, but other research areas, including improved materials and advanced rock penetration concepts such as percussion drilling and cavitating jets, will be pursued until they can be accurately evaluated. For effective participation in a coherent geothermal technology development program, this work must have the breadth to identify the most promising concepts and the depth to incisively analyze those concepts.

3.1 Development of Improved Cavijet® Nozzles

Contractor: Hydronautics, Inc.
Principal Investigator: G. L. Chahine (301) 776-7454
Contract Period: 18 February 1980 to 31 January 1983
Contract Number: 13-5129 and 68-8419 (Sandia)
Technical Consultant: D. Glowka (505) 844-3601

Project Objective

The objective of this project is to increase the knowledge of Cavijet® nozzle design principles and to develop Cavijet® nozzles that achieve increased incipient and optimum cavitation numbers.

Background

The project's first phase, which included determining the effects of drilling mud on the performance of cavitating jets, was completed at the end of FY 1979. The results of this initial phase indicated that augmentation of mechanical bits with cavitating mud jets is feasible and could substantially improve drilling performance.¹³ An associated effort was started in late FY 1979 to determine the feasibility of designing cavitating jet nozzles that resonate passively to produce nozzle pressure pulsations. This work was completed in July 1980 with the findings suggesting that additional research should result in viable pulser-nozzle systems.¹⁴ Continuation of the investigation of downhole pressure-pulsing nozzles was included in the follow-on phase II project on cavitating jets, which was initiated in February 1980.¹⁵ The phase II work, directed at improving the performance of cavitating jets, continued throughout FY 1981. The work included development of theories for vaned nozzles, organ-pipe nozzle acoustics, jet structuring mechanisms, and boundary pressures induced by the structured jet. Laboratory rock-erosion experiments in air and water were conducted to confirm the theories and to assess the feasibility of the organ-pipe nozzle. The influence of tube geometry, Mach number, and cavitation number on the degree of structuring was also determined experimentally for approximately 30 nozzle shapes. FY 1981 ended with the

discovery that the critical Strouhal number can be controlled by nozzle design and that some nozzles apparently depend on cavitation for self-excitation and others do not.⁹

FY 1982 Activities

Phase II project work, directed at improving the performance of cavitating jets, was completed during this reporting period, and phase III was begun to develop analytical techniques to predict the flow field characteristics of cavitating jets and the upstream resonance characteristics of pulsing jets. The technology developed in phase II was transferred to industry and other interested agencies through presentations at technical conferences.¹⁶⁻¹⁸ Preparation of a final report on phase II work was also begun, with publication projected for early FY 1983.¹⁹ A summary of the final efforts in phase II and the initial phase III studies conducted during FY 1982 follows.

Phase II Activities

Nozzle and Flow Studies — During the early part of this reporting period, efforts were directed at investigation of Organ-Pipe Cavijet® designs that are suitable for operation at high cavitation numbers (σ), i.e., at greater drilling depths or lower nozzle pressures. Designs that provide increased values of the cavitation inception number, σ_1 , and designs that resonate at cavitation numbers greater than σ_1 were studied. The latter (noncavitating, self-resonating nozzles) were of interest because of the potential improvement in chip removal offered by the structured ring vortices created by such nozzles. Even if these nozzles are operated at depths where the ambient pressures are too high for cavitation to occur, the pressure fluctuations caused by the vortices sweeping across the hole bottom may enhance the chip-lifting process. Additional investigation of this process is discussed below.

Nozzle orifice shapes with σ_1 values in the range of 3 to 4 were examined. In the first mode resonance, these nozzles were operated at cavitation numbers as high as 8 to 9, with jet modulations of 25% to 30%.

These nozzles, suitable for operation in Stratapax™ types of bits, are typically operated at a pressure drop (Δp) of about 2.1 to 3.5 MPa (300 to 500 psi).

In second mode resonance, this same nozzle configuration had a design resonance peak at about 9.7 MPa (1,400 psi) and showed a jet modulation of about 18%. The σ_1 -value could not be established at this higher Δp due to pump limitations; however, the value was greater than 2.4. This same nozzle, with $\Delta p = 6.9$ MPa (1,000 psi), had a $\sigma_1 = 2.84$, with a jet modulation equal to 6.8%.

The test data indicated that these "high- σ ," resonating nozzle configurations, which also resonate at $\sigma < \sigma_1$, are more sensitive to orifice design details than the organ-pipe systems that are configured for peak resonances at $\sigma < \sigma_1$. But, with confirmation of the chip-lifting concept, these nozzles would offer the potential for improving rates of penetration over a wide range of hole depths.

Design procedures for Organ-Pipe Cavijet® nozzles were established that, within reasonable bit-configuration restraints, can be used to fit these self-resonating nozzles into a wide range of operating and downhole conditions. By following the quasi-empirical approach established under this project and with suitable analytical guidance, an understanding of the role of various nozzle orifice design details was developed to the point where high-amplitude jet modulations could be obtained for given values of nozzle pressure drop, Δp ; of ambient pressure, P_a ; and of the nozzle and feed-tube constraints, D/d and L/d , where d is the nozzle orifice diameter and D and L are the diameter and length, respectively, of the feed tube leading to the nozzle. While the quasi-empirical approach is not yet considered to be producing optimum designs, the jet modulations are quite high, 25% to 30% in most cases, and hold the promise for improving rates of bit penetration. Analytical work is continuing (Phase III) to provide a more complete understanding of the mechanisms whereby the structuring of the ring vortices "feeds back" into the organ-pipe section, further amplifying the jet modulation.

In order to examine the differences associated with measurements of upstream versus downstream resonance amplitudes, a test series was conducted in a large air facility. The facility uses a $0.5 \text{ m}^3/\text{s}$ (1,000 ft^3/min), 3.5 kPa (0.5 psi) blower that feeds air to an enlarged nozzle model through a 10.2-cm (4-in.) diameter feed pipe. With this facility, nozzles up to five times larger than field scale, $d = 5.1 \text{ cm}$ (2.0 in.) versus a typical $d = 1.0 \text{ cm}$ (0.40 in.) for a three-cone bit, can be examined.

Tests were conducted in the air facility with several large-scale, 5.1 to 6.4-cm (2 to 2.5-in.) orifice diameter, Organ-Pipe Cavijet® nozzle configurations. Pressure fluctuations measured just upstream of the nozzle contraction were observed to exceed the mean nozzle pressure by more than 200%, corresponding to velocity fluctuations in the jet as great as 50% of the mean speed (on the jet centerline, one to two diameters downstream of the orifice). The peak in pressure fluctuation usually occurred at a Strouhal number 10% to 20% higher than that for the maximum velocity fluctuation intensity. This pressure modulation has been shown to greatly increase cavitation and thus enhance the erosion potential of the jet. In general, the large-scale nozzle configurations for optimal self-modulation were very nearly the same as those determined for full-scale bit nozzles tested in air. Hence, it appeared that Reynolds-number effects cannot be the cause of the significant differences in resonant response of the full-scale Organ-Pipe Cavijets® tested in water and mud at the Drilling Research Laboratory (see discussion below).

Tests with large-scale nozzles were conducted to map the amplitude of mean and fluctuating pressures on a flat-plate target as functions of axial and radial locations. Data for the mean, $\bar{p}$, and fluctuating, $p'_{\text{--rms}}$, pressure distributions on a flat plate target are presented in Figures 3-1 and 3-2 for two stand-off distances. In each figure, the performance of a steady jet and a strongly modulated jet ($u'/U \approx 0.50$) are compared. In Figure 3-1, the impingement surface is just 1.2 (orifice) diameters from the nozzle exit. The pressure data have been normalized by the pressure drop across the nozzle, Δp , including the organ pipe feed tube length. The mean pressure falls rapidly from the peak centerline value to near zero at

Figure 3-1. Pressure Distribution on Target Plate for Standoff: Distance from Orifice/Nozzle Diameter, $X/d = 1.2$

Figure 3-2. Pressure Distribution on Target Plate for Standoff: Distance from Orifice/Nozzle Diameter, $X/d = 2.9$

a radius about twice the nozzle radius for both the steady and structured jets. The fluctuating pressures are very dissimilar, however. For the steady case, the turbulent (mixing) zone has not yet diffused to the center of the jet, so p' is minimal at $r = 0$ and is strongest around $r = 1.4 (d/2)$. The fluctuations are random, broad band noise.

For the structured jet, the peak p' is at the centerline where the signal is nearly a pure sine wave, hence; the normalized amplitude is $\approx 0.42 \sqrt{2} = 0.59$, and the instantaneous pressure swings from $0.20 \Delta p$ to $1.38 \Delta p$. Of greater significance is the instantaneous pressure for the secondary peak in p' at $r = 1.5 (d/2)$ as shown in the inset of Figure 3-1. The signal is not sinusoidal here but exhibits large negative pressure spikes corresponding to the passage of the expanding vortex rings across the surface. The pressure ranges from $-0.9 \Delta p$ to $+0.5 \Delta p$.

With the target further downstream, $X = 2.9d$, the amplitude of the negative pressure peak was not as large. Here, as shown in the inset in Figure 3-2, the pressure ranges from around $-0.5 \Delta p$ to $+0.5 \Delta p$.

These data were considered to have potential significance from the standpoint of bottomhole cleaning--with or without cavitation in the modulated jet's vortex rings. The large negative pressure peaks seen in the inserts in Figures 3-1 and 3-2 could contribute to lifting a chip from the hole bottom, by momentarily reducing the local hold-down pressure.

Full-Scale Bit Tests -- Full-scale Cavijet® bit tests were conducted at the Drilling Research Laboratory (DRL), Salt Lake City, Utah. The primary objective of the full-scale bit tests was to determine what changes, if any, would arise in the resonance behavior of self-resonating cavitating nozzles when the working fluid was changed from water to mud. The same piezoelectric transducers that were used in the High Pressure Cell (HPC) tests at Hydronautics were installed in the Well-Bore Simulator at DRL to monitor the jet modulations. In order to establish baseline data for comparison with Cavijet® test results, resonance measurements were made with a conventional 31.1-cm (12-1/4 in.) three-cone bit containing the standard set of nozzles usually used in this tool. Very low-amplitude

resonance peaks, 6.9 to 13.8 kPa (1 to 2 psi), were observed at frequencies of about 2 and 7.5 kHz. The baseline tests also demonstrated the feasibility of using the same piezoelectric transducers that were employed in water tests at Hydronautics. With water-based mud, the transducer, mounted in the flow pipe between the pressure cell and the return flow-choke, gave a well-defined reading of the pressure fluctuations.

After the baseline tests, a 31.1-cm (12-1/4 in.) three-cone roller bit with Organ-Pipe Cavijet® nozzles was tested using first water and then a 1,138 kg/m³ (9.5 lb/gal) water-based mud as the drilling fluid. The feed tubes leading to each of the three extended nozzles were modified in accordance with the guidance obtained from laboratory testing in water in the HPC. Significant differences were observed between the resonance response of this Cavijet®-augmented bit, when comparing results with water, and with the water-based mud.

Although the resonant frequencies were almost equal in mud and water, suggesting that the sound speeds in mud and water were about the same, the amplitude modulations were substantially lower in mud at each peak. For instance, at the second-mode peak that occurred at about 8.5 kHz; Mach number, $M = 0.073$; and critical Strouhal number, $S_d = 0.73$, the mud resonant amplitude was only about 0.5% versus about 2.0% for a comparable measurement in water (see Figure 3-3). It should be emphasized that these resonances were measured with a transducer mounted near the choke that controls the outflow from the pressure chamber. In water, resonance peaks measured in the feedpipe were seen to be larger, e.g., $p'/\Delta p = 8\%$ where p' = amplitude of fluctuating pressure signal and Δp = pressure drop across the nozzle. These results were compared to the baseline mud resonance results using standard nozzles, in which the peak resonances were only about $p'/\Delta p \approx 0.1\%$. Differences between the measurements made with the feedpipe pressure transducer and with the choke transducer were also seen when comparing the mud data with the water data. In addition, the relative amplitudes as functions of cavitation number for these two fluids showed different trends.

Figure 3-3. Pressure Modulation Amplitudes in Water (a) and in Water-Based Mud (b) with Modified Three-Cone Bit

Prediction of Jet Performance -- Development of analytical models to describe various aspects of self-resonating CaviJet® nozzle jets continued during the final project efforts in phase II. These efforts included organ-pipe resonance and ring vortex behavior analyses. The resonance analysis provided a closer correlation with experimentally determined resonance frequencies and an understanding of harmonic observations made in laboratory tests. The ring vortex model, originally containing only the collapse mechanics, was expanded to include circulation phenomena. The time-to-collapse for a toroidal-shaped, ring vortex was computed by this model to be greater than the collapse time for a comparable single, spherical bubble under the same conditions. The times for several toroids were predicted to be about twice that of the spherical bubble. In order to understand collapse energies and, thus, how potentially erosive the collapse is, the collapse speeds of spherical and toroidal bubbles were examined.

Representative results from the analysis of the behavior of cavitating vortex rings are shown in Figures 3-4 and 3-5. The definitions of the symbols used in the figures are as follows:

- K = polytropic constant for ideal gas
- P_{g_0} = partial pressure of gas inside bubble
- $\tilde{P}_{g_0}$ = nondimensionalized internal partial pressure, $P_{g_0}/\Delta P$
- $P_{\max}$ = maximum value of pressure outside the bubble
- P_0 = initial value of pressure outside the bubble
- R = radius of cross section of torus, time-varying
- R_0 = overall radius of torus
- R_0 = radius of cross section of torus, initial
- T = time
- W_e = Webers number
- $\Delta P = P_{\max} - P_0$
- $\epsilon = R_0/R_0$
- Γ = circulation
- ρ = density of fluid
- Ω = nondimensionalized circulation, $(\rho \Gamma^2 / 4\pi^2 R_0^2) / \Delta P$

Figure 3-4. Comparison of Collapse Times for Spherical and Toroidal Bubbles

Figure 3-5. Comparison of Collapse Times for Spherical and Toroidal Bubbles

As noted above, the time-to-collapse for a toroidal-shaped ring vortex was predicted to be greater than the time required to collapse a comparable spherical bubble. These times-to-collapse are compared in Figure 3-4, where it is seen that the times for several toroids are predicted to be about twice that of the spherical bubble. However, to understand collapse energies and hence how potentially erosive this collapse is, it is necessary to also examine the curves shown in Figures 3-5.

The comparisons in Figure 3-5 predict that, despite the large difference in time-to-collapse, these toroids should collapse at a speed that is about 80% to 90% of the maximum speed predicted for spherical bubbles. However, since the areas of the toroids are much larger than those of spheres with the same initial volumes, the implication of this part of the analysis is that the toroidal collapse should be substantially more erosive to an adjacent surface than a spherical collapse.

Polymer Additives — Tests were also conducted during this reporting period to determine whether polymeric additives mixed into the water would have any effect on the structuring of self-resonating jets. Quantities of a viscoplastic polymer were mixed at ratios of from 0.5 to 0.8 kg (1.0 to 2.0 lb) of polymer per 159 liters (42 gallons) of water for the tests. In the tests, no measurable effects on cavitation inception or resonance amplitudes were observed that were attributable to the addition of the polymer.

Chip Hold-Down Tests — During the final work in phase II, a low-pressure test cell was designed, fabricated, assembled, and debugged for chip hold-down experiments with self-resonating Cavijet® nozzles. Shake-down tests using a layer of sand over a sheet of filter paper showed that the operational concept for the facility was feasible. The pressure differential across the base of the cell caused the sand particles to cling to the bottom, as planned, until a ratio of jet pressure to pressure differential was achieved that allowed lifting of the particles. This process was readily observed through the relatively clear water in the cell.

Some data from tests conducted in the chip hold-down facility are presented in Figures 3-6 and 3-7. These data confirm that structured (modulated) jets can clean the hole bottom more effectively than a steady jet, even at very high cavitation numbers where there is no jet cavitation. In these tests, a thin 25 to 100 μ (1.0 to 4.0 mils) mud cake was formed over a woven nylon filter cloth that was supported by a porous stainless steel plate. The bentonite mud particle size distribution, measured with a Coulter Counter, ranged from 2 to 50 μ (0.08 to 2.0 mils) with maximum volume concentration around 8 μ (0.3 mil). Nominal (calculated pore area $\div$ total area) porosity of the filter cloth was 0.13 and 0.01 for the cloth of 30 and 5 μ (1.2 and 0.2 mil) maximum pore size, respectively.

The pores in the cloth were effectively clogged by the mud particles and caused a large pressure drop, Δp , of up to 1.0 MPa (150 psi) to build up across the cloth. This "hold-down" pressure was regulated by the flow, Q , of up to $3.2 \times 10^{-4} \text{ m}^3/\text{s}$ (5 gal/min) across the 23.5-cm (9.25-inch) diameter filter cake. A failure of the mud cake was noted by a sudden change in cake porosity, $C_p = Q/A \sqrt{2\Delta p/\rho}$, induced by slowly increasing the jet driving pressure, p_j . The above porosity coefficient may be likened to the product of the effective pore area $\div$ surface area and a discharge coefficient. Between test runs, the filter cake could reform (with the jet off), but the porosity would be changed owing to changes in the particle distribution. Smaller particles could be lost thorough the pores of the mesh, particularly with the 30 μ (1.2 mil) cloth.

The data were normalized to show the relative erosivity of the jet versus cake porosity, where the cake erosion strength is assumed to be proportional to the hold-down pressure. Hence, the ratio of the filter-cake pressure drop to the jet driving pressure at the point of cake failure is plotted versus the cake porosity before failure in Figures 3-6 and 3-7.

The cake erosion strength appeared to be fairly independent of porosity, but the modulated jet was 3 to 4 times more effective for the case shown in Figure 3-6. Here, the nominal rms modulation strength (measured with a pressure transducer mounted in the plate beneath the filter cloth) was observed at the jet centerline to be 16% of the mean impact pressure.

Figure 3-6. Comparison of Mud-Cake Failure for Steady and Modulated Jets

Figure 3-7. Comparison of Mud-Cake Failure for Steady and Modulated Jets

For the test parameters in Figure 3-7, the erosivity of the modulated jet was nearly twice that of the steady jet. The erosion strength of the filter cake formed on this 30 μ (1.2 mil) mesh must be less than with the 5 μ (0.2 mil) mesh because the changes arising from differences in standoff and mud layer thickness were found to be rather small.

In summary, the data from tests conducted in the chip hold-down facility confirmed that structured (modulated) jets can clean the hole bottom more effectively than a steady jet. This performance was true even at very high cavitation numbers where there is no cavitation.

Phase II Conclusions -- A variety of experimental and analytical tasks were performed during phase II to develop cavitating jets that would increase the rate of penetration of deep-hole drill bits. Based on the four criteria of (1) incipient cavitation number, (2) amplitude of pressure fluctuation (and hence enhanced structuring of the jet flow), (3) rock cutting, and (4) cleaning chips from the hole bottom, the new, self-resonating cavitating jets outperformed both conventional drill bit nozzles and the basic Cavijet® cavitating jets. Tests were conducted with individual nozzles at full scale, at reduced scale, and at very large scale models, using air, water, and drilling mud as the working fluid. Full-scale drill bits were also tested in these three fluids. In addition, analytical tools were developed for predicting the performance of Organ-Pipe Cavijet® nozzles and central-vortex generating nozzles. Some of the conclusions drawn from this phase of the project follow.

1. Several self-resonating nozzles types, the Pulser,* Pulser-Fed*, and Organ-Pipe Cavijet®* designs produce passive oscillations that cause the jet to structure into discrete ring vortices; they thus provide nozzle systems that have incipient cavitation numbers two to six times higher than those obtained with either conventional Cavijet® nozzles or typical drill bit nozzles.

*These designs belong to a class of self-resonating nozzles referred to collectively as STRATOJET (STRuctured Acoustically Tuned Oscillating JET) nozzle systems.

2. In rock cutting trials, higher incipient cavitation numbers have been correlated with greater jet erosivity.

3. The Organ-Pipe CaviJet® is adaptable to the physical restraints of existing bits and is considered to be the most promising concept for future bit designs.

4. STRATOJET nozzle systems will clean chips from a simulated hole bottom at a nozzle pressure drop of only about 25% of that required for standard nozzle systems.

5. Self-resonating structured jets can be achieved either with or without cavitation. Therefore, at hole depths below where cavitation is present, a STRATOJET nozzle may still contribute to improved bottomhole cleaning.

6. Several systematic empirical and analytical tools were developed that allowed the design of STRATOJET nozzles that provide high-amplitude pressure fluctuations under deep-hole conditions, with water as the working fluid.

7. Although nozzle designs that provide large amplitude pressure modulations were developed for operation in water, additional research is needed to optimize self-resonating jets for use (a) in mud, (b) in specific drill bit designs, and (c) at higher system pressure than currently used for deep-hole drilling.

Phase III Activities

Phase III of the project was begun in mid-FY 1982. The goals of this phase are (1) to develop analytical techniques to predict the flow-field characteristics of cavitating jets and the upstream resonance characteristics of pulsing jets, (2) to use high-speed motion and still photography of cavitating jets to guide and evaluate the success of the prediction techniques developed, and (3) to apply the prediction techniques developed to design several Organ-Pipe CaviJet® or other nozzles for operation with a high pressure drop across the nozzle, 27.6 to 55.2 MPa (4,000 to 8,000

psi), and to determine their rock cutting performance in the HPC. A summary of the phase III work performed during this reporting period follows.

Analytical Techniques -- The organ-pipe resonance model developed earlier to study the characteristics of the oscillations of a multistep tube was extended to include the study of forced oscillations. The numerical code was also improved to include the determination of mode shapes in addition to the natural frequencies of the tube. This improvement permits the determination of the phase lag between pressures and velocities for any particular geometrical configuration and inlet conditions. With the forced-mode oscillations added to the code, the amplification factor of any geometrical tube configuration can be computed and the optimum position of the step, for optimum amplification, determined.

The study of the flow-field characteristics due to the structuring of the jet was begun based on the following model. The flow in and around the jet, as well as in the organized shear layer, is reproduced by a repartition of ring vortices emitted from the nozzle at the oscillation frequency of the tube. By self induction, each ring moves downstream at a velocity depending on (1) the ratio between the viscous core size and the jet diameter for a noncavitating structured jet and (2) on the ratio between the radius of the bubble ring-section and the jet diameter for a cavitating structured jet. This self-induced velocity is corrected by that factor due to the influence of the other rings and depends mainly on the ring spacing, λ , and to a lesser extent on their number.

The case of an isolated ring was considered first. Defining $\epsilon \ll 1$ as the ratio between the characteristic size of the bubble ring section, R_o , and the torus radius, R_o , the bubble is shown to have a stable circular section, in the absence of ambient pressure variations, up to the order ϵ included. The induced translation velocity, V_t , is of the order $\epsilon \log \epsilon$, and has the value

$$V_t = \frac{\Gamma}{4\pi R_o} \epsilon \left[\log \frac{8}{\epsilon} - \frac{1}{2} + \frac{W_e^{-1}}{\Omega} \right] \quad (1)$$

where Γ is the circulation around a bubble section. Γ can be approximated by

$$\Gamma \sim \lambda \cdot V_j \quad (2)$$

W_e is the Weber number based on the bubble radius, R_o , the surface tension, γ , and the ambient pressure, P_o .

$$W_e = \frac{R_o P_o}{\gamma} \quad (3)$$

Ω is the value of the pressure drop at the bubble wall due to the rotation of the fluid, relative to the ambient pressure.

$$\Omega = \rho \left(\frac{\Gamma}{2\pi R_o} \right)^2 / P_o \quad (4)$$

When the ambient pressure is variable, a differential equation for the bubble radius equation $R(t)$ is obtained and can be solved numerically. The modification of this equation in the presence of a solid wall or other bubble rings is being investigated. The initial results indicate a lengthening effect on the bubble collapse time as well as a significant modification of the torus radius, $R(t)$.

The differential equation was solved numerically using a Runge-Kutta procedure. The influence of the following parameters were investigated: (1) ϵ , the ratio of the radius of the torus section to its overall radius; (2) $\tilde{P}_{g_o}$, the relative value of the initial gas pressure to the imposed pressure variations, ΔP ; (3) Ω , the relative value of the pressure drop due to the rotation of the fluid to ΔP ; and (4) W_c , the Weber number based on the torus section radius, surface tension, and the initial ambient pressure. Figure 3-8 shows the influence of ϵ , a geometrical characteristic of the bubble ring, and compares the collapse time of a torus with that of a sphere. A direct dependence on ϵ is obtained, and the collapse time is seen to be of the order, $\tau = R_o [(\rho \log 8/\epsilon)/\Delta P]^{1/2}$. The influence of the initial gas pressure, $\tilde{P}_{g_o}$, and the rotation, Ω , is shown in Figures 3-9 and 3-10. Both parameters have the same effect on the torus dynamics. An

Figure 3-8. Comparison of Collapse Times for Spherical and Toroidal Bubbles

Figure 3-9. Influence of Gas Content on Toroidal-Bubble Collapse

Figure 3-10. Vortex Strength Influence on Bubble-Ring Collapse

increase of $\tilde{P}_{g_0}$ or Ω significantly reduces the intensity of the collapse. Thus, higher vortical motion induces earlier cavitation inception, but also a damping of the collapse. An optimum value of the circulation is therefore to be sought for a maximum erosion rate.

In order to study the collapse of a vaporous torus near a solid wall, higher order terms than those considered in the preceding paragraph should be included. (The small parameter involved in the problem is the ratio, ϵ , of the torus initial section, R_0 , to its overall radius, R_0 .) The effect of the torus curvature on the oscillations of the torus section is to be compared with the effect of the nearby wall. These effects are of the same order when the cavity is at a distance from the wall of the order R_0 . Beyond this distance, the curvature effects appear first, and the wall effect is predominant at a lesser distance. The equations up to the high order ϵ , precisely the orders $\text{Log } \epsilon$, 1, and $\epsilon \text{ Log } \epsilon$, have been derived. Numerical solutions and the influence of the wall will be considered during the next reporting period.

In order to study the organized jet behavior, and in anticipation of later study of the feedback mechanism at the nozzle exit, a numerical simulation of the jet behavior was considered. In this study, the oscillating jet is replaced by a series of ring vortices (fixed size cavity at this point) that are emitted from the nozzle at a fixed rate. The motion of any of these rings is determined by the combination of its self-induced velocity and the velocity due to all the other rings; the real ones and their images when a wall is present. Study was initiated on the unsteady startup of the emission of rings in order to determine for future studies whether a stable configuration of the rings' locations exists. In reality, if the jet is allowed to develop to a sufficient distance, vortex pairing would present a steady behavior. The program was tested for the classical case of two coaxial rings of identical initial radii. The classical results were reobtained, namely alternative overtaking and passing of one ring through the other.

Development of a similar program was begun to study the impact of a ring on a solid wall, and the program will be extended during the next

reporting period to study the jet's impact on the wall and the case of an annular jet.

Evaluation Techniques — In preparation for the evaluation of the prediction techniques being developed, improvement of high-speed movie and still-picture capabilities was begun. These efforts included the assembly of a high-intensity, very short duration strobe ($<1\mu s$). In addition to updating the photographic facilities, preparations were begun for the visualization experiments. Three geometrically different nozzles were fabricated from altuglass in order to allow clear, high-speed visualization and the determination of the location where the ring cavities are initiated. These nozzles should also assist in understanding the influence of the cavitation number on the acoustic performance of the nozzles. The length of the organ-pipe feed tube was also doubled in order to decrease both the frequency of emission of the rings and the optimum jet speed. This change is expected to result in a slowing down of the phenomena for visualization purposes.

Nozzle Design — Three nozzles, designed to oscillate at low cavitation numbers, were fabricated--a pulser and two organ-pipes. The pulser was tested in the HPC and showed good oscillations for pressures as high as 34.5 MPa (5,000 psi) and for cavitation numbers of the order of 0.08. The optimum oscillation remained, however, at a lower ΔP of the order of 13.8 MPa (2,000 psi).

The organ-pipe nozzles were designed to prevent the formation of a steady cavity on the nozzle exit. One design consists of a very short lip thickness, e , and the other of shaping this exit to be closer to the jet contraction. Both shapes will be tested acoustically first, and if the tests are successful, their cutting performance will be evaluated.

As FY 1982 ended, the nozzle design efforts were continuing, along with the development of the numerical simulation of a cavitating jet and preparations for evaluation of the prediction techniques during the next reporting period.

3.2 Improvement of Downhole Drilling-Motor Bearings and Seals

Contractor: Terra Tek, Inc.
Principal Investigator: P. DeLafosse (801) 584-2400
Contract Period: 1 November 1980 to 31 January 1983
Contract Number: 74-6607 (Sandia)
Technical Consultant: J. Finger (505) 844-8089

Project Objective

The application of downhole motors to geothermal drilling has been limited because of short bearing and seal life in the high-temperature geothermal environment. The specific objective of this project is the development of a 200-hour-life bearing and seal package, adaptable to most types of downhole motors, that can soak at 260°C (500°F) and operate at a 121°C (250°F) circulation temperature.

Background

This project was initiated by the Energy Research and Development Administration (ERDA) in 1976.²⁰ The work was continued under DOE,²¹ and in mid-1978, DOE assigned management of the project to Sandia. Under the project, a seal tester, a bearing/seal package, and a bearing/seal package test facility were designed and fabricated. New lubricants, seals, and bearing/seal package designs were developed for testing under simulated geothermal drilling conditions.²²⁻²⁶ Lubricant testing was completed in 1979 with the successful development and subsequent commercialization of lubricants exhibiting exceptional high-temperature performance.²⁷ Seal testing continued during FY 1980, checkout of the Bearing/Seal Package Test Facility was completed, and testing of bearing/seal packages was initiated.^{15 28} Testing of the most promising candidate seals in the bearing/seal packages continued during FY 1981. A total of 16 seal tests was conducted, concentrating on the development of the high-temperature, corrosion-resistant (HTCR) SF design and the Variseal® design. For the first time, the goal of a 200-hour seal life was achieved in the test of an SF-design seal made of Utex compound HTCR, a glass-reinforced copolymer of

propylene and tetrafluoroethylene. Four bearing/seal package tests were also conducted in FY 1981, including the first attempt to use a mechanical face seal in the package. The face seal failed to hold pressure, and failure of the floating piston was experienced. As FY 1981 ended, preparations were being made for additional HTCR seal tests and for tests of second-generation floating pistons and new face seals.⁹

FY 1982 Activities

Seal Testing and Evaluation -- The seal testing and evaluation activities under this project ended early in this reporting period with the completion of Test No. 062. In this final test, a second-generation Variseal®, Figure 3-11, with a bronze backup ring was run at 412 rpm and with a Δp of 10.3 MPa (1,500 psi). Because of the limited test time available, the test was terminated at 100 hours, but there was no failure in either the upper or lower seal packs and leakage was less than 20% of the

Figure 3-11. Second-Generation Variseal® Cross Section

lubricant reserve. Posttest inspection of the shaft showed no wear on the shaft; the shaft was coated with Union Carbide Coating UCAR-LWIN30 (tungsten carbide). There was very little wear on the Variseal® seal ring, but significant wear was evident on the antiextrusion ring. There was also evidence that the antiextrusion ring was spinning against the bronze backup ring.

Bearing/Seal Package Testing and Evaluation -- The bearing/seal package tests continued during FY 1982 with the conduct of seven tests. The test conditions are tabulated in Table 3-1.

Table 3-1

Bearing/Seal Package Test Conditions

Test No.	Duration h	Seal Type	Shaft Coating	ΔP MPa (psi)	Diametrical Clearance: Shaft to Backup Rings	
					mm	(in.)
08	10.0	Gits Bros. face seal	N/A	6.9 (1,000)	N/A	
09	4.0	HTCR SSF design	Carburized steel	6.9 (1,000)	0.127 (0.005)	
010	11.0	Stein face seal	N/A	6.9 (1,000)	N/A	
011	48.0	HTCR SSF design	Carburized steel	6.9 (1,000)	0.127 (0.005)	
012	46.0	HTCR SSF design	Carburized steel	6.9 (1,000)	0.127 (0.005)	
013	48.0	Variseal®	Carburized steel	3.5 (500)	0.127 (0.005)	
014	24.0	Variseal®	Carburized steel	6.9 (1,000)	0.127 (0.005)	

Note: All tests were run at 450 rpm.

The first bearing/seal test during this reporting period, No. 08, tested a Gits Brothers Manufacturing Company face seal. After the test was run for 10 hours, the differential pressure was lost and could not be re-established. Dissassembly of the bearing/seal package revealed that the face seal had failed. The tungsten carbide coating on both faces of the seal had worn away, and one of the seal faces had broken the retaining pins and rotated in the seal carrier. Also, the O-rings on the outside of each seal face had been destroyed.

The bearing/seal package was used next to test the HTCR SSF-design seals with the second-generation HTCR floating piston. The test, No. 09, was run at 450 rpm with a differential pressure of 6.9 MPa (1,000 psi). The test was terminated after 4 hours when mud was discovered in the bearing/seal package. Posttest inspection showed that the floating piston had failed; the inside diameter was completely worn away.

Bearing/seal test No. 010 tested the Stein Seal Company face seal. The test ran for a total time of 11 hours. However, after 7.8 hours, trouble with the rotary speed and the mud pressure began. All data indicated that the seal began failing at that time. Posttest inspection showed that the carbon-graphite insert portion of the seal was completely destroyed and no part of it was found. The destruction of this sealing surface allowed the tungsten carbide face to run against a nonsealing surface that, in turn, led to the destruction of the seal.

In Test No. 011, HTCR-SSF design seals and the third-generation Utex floating piston were used. The new piston had the same geometry as the second generation but used a stiffer, reinforced HTCR material. The test ran for 48 hours, at which time the mud pressure could no longer be maintained. Upon disassembly, mud was found within the tool, indicating that either the seal or the piston or both had failed. The exact determination of the failure mode was not possible because both the seal and piston showed equal signs of wear.

A second test, No. 012, of the Utex HTCR SSF-design seal with the third-generation HTCR floating piston in the bearing/seal package was

conducted. The second test was terminated after 46 hours of running at 450 rpm and 6.9 MPa (1,000 psi) differential pressure. Posttest inspection showed excessive wear on the inner diameter lip of the floating piston. The lip wore to the point that the floating piston could not maintain the barrier between the oil and mud, allowing mud to enter the bearings area. The HTCR SSF seals showed slight wear, with the mud seal showing more wear than the oil seal.

The bearing/seal package was also used to test the Variseal® pressure seal with the Variseal® floating piston. This test, No. 013, was terminated after running 48 hours at 450 rpm and 3.5 MPa (500 psi) differential pressure. Reduced pressure was used in this test because of damaged dynamic actuator/crossover sub connecting threads. Posttest inspection revealed that the bearing/seal package was full of mud but that the Variseals® at both the pressure seal and the floating piston were only slightly worn.

The last test, No. 014, during FY 1982 was a second test of the Variseal® pressure seal with the Variseal® floating piston. The duration of the test was limited to 24 hours at 450 rpm and 6.9 MPa (1,000 psi) differential pressure. The purpose of the short duration test was to discover any possible failure of the floating piston. At the end of this second test, the bearing/seal package again contained mostly mud. The center shaft was worn at the upper end, indicating that the floating piston had not moved but that the mud had leaked by it and displaced the oil. The floating piston was also worn on the inside diameter.

At the close of this reporting period, preparations were underway for the conduct of the final series of bearing/seal tests and the completion and documentation of this project. A schematic of the bearing/seal test setup is shown in Figure 3-12, and the Bearing/Seal Package Test Facility is depicted in Figure 3-13.

Figure 3-12. Bearing/Seal Test Configuration

Figure 3-13. Bearing/Seal Package Test Facility

3.3 Flexible Drilling Pipe

Contractor: L'Garde, Inc.
Principal Investigator: G. Friese (714) 546-4671
Contract Period: 1 July 1980 to 30 September 1982
Contract Number: 62-1247 (Sandia)
Technical Consultant: B. Caskey (505) 844-8835

Project Objective

The objective of this project was to develop a liner material compatible with Coflexip flexible drilling pipe* when the combination is used at high pressures and temperatures: 2.1 MPa (300 psi) at 250°C (482°F) and 69.0 MPa (10,000 psi) at 125°C (257°F).

Background

At the present time, flexible drill pipe cannot be used in geothermal applications primarily because of the temperature and pressure limitations of the inner and outer liners. A flexible drill pipe is depicted in Figure 3-14. These liners are made from a thermoplastic called Polyamide II, which cannot be used at temperatures above 100°C (212°F). Above 100°C (212°F) and under pressure, Polyamide II extrudes into the gap of the steel, spiral "Z" section, causing this section to prematurely fail by fatigue during flexing. The "Z"-section/liner interface is illustrated in Figure 3-15. The general function of the inner thermoplastic liner is to contain the drilling fluids and prevent abrasion and corrosion of the steel carcass. The function of the outer liner is to prevent abrasion and corrosion and to prevent ingress of fluids from the outside.

Work to develop a liner that would allow use of the flexible drill pipe in geothermal drilling operations was initiated late in FY 1980. The early efforts included the design and fabrication of a material screening apparatus and the design of a high-temperature/high-pressure test fixture to simulate liner/pipe interaction under geothermal conditions.¹⁵

* Product of Coflexip, France

Figure 3-14. Coflexip Rotary Drilling Hose

Figure 3-15. Sketch of Cross Section of Spiral "Z"-Section/Liner Interface Indicating Gaps for Potential Extrusion of Liner Material

Screening of candidate liner materials, fabrication and checkout of the test fixture, and liner design were conducted during FY 1981. Ten different materials were tested, with Teflon PFA 350, EPDM Y 267, and AFLAS 291, in order, exhibiting the best extrusion resistance at the required temperature and pressure conditions. However, none of the materials could withstand a pressure of 68.9 MPa (10,000 psi), and an evaluation of metal and plastic reinforcements, compatible with liner materials, was conducted. Reinforced elastomers were found to exhibit poorer properties than the basic elastomers. Hence, efforts to compound a liner material, using the principles of rubber technology, were begun at the close of FY 1981. Fabrication, assembly, and checkout of the high-pressure/high-temperature device for testing of the flexible-pipe/liner sections were also completed during FY 1981. Initial tests of liners made of Teflon PFA 350, reinforced externally with a wire braid jacket, suggested such a system would prevent liner extrusion into the "Z"-section gaps; however, a one-piece liner was considered more economical.⁹

FY 1982 Activities

The project was completed during this reporting period with the compounding and testing of EPDM and AFLAS elastomers reinforced with Kevlar, Inconel, and/or graphite fibers. These final efforts in the project identified a graphite-reinforced EPDM elastomer that demonstrated the potential of meeting the temperature/pressure requirements of a liner for use in Coflexip flexible drill pipe. A final project report was also prepared for publication as Reference 29. A summary of the reinforced elastomer work performed during FY 1982 follows.

The development work to produce a reinforced elastomer for the liner in flexible drill pipe consisted of four compound approaches: (1) AFLAS elastomer and chopped Kevlar, (2) EPDM elastomer and Inconel wire, (3) AFLAS elastomer and chopped graphite, and (4) EPDM elastomer and chopped graphite. The chopped graphite was 6.4-mm (0.25-in.) long Hercules, Inc., graphite fiber, Type AS/1800. The AFLAS and EPDM materials were similar to L'Garde AFLAS 291 and EPDM Y-267. In general, the amount of reinforcement material and curing times were varied in the preparation of test samples of

the various compounds. In addition, some modifications to the basic L'Garde EPDM Y-267 were made.

As in earlier tests using commonly available elastomers, the reinforced compounds were molded into test pieces, termed "hats" (simulating the inner thermoplastic liners of Coflexip "Z" flexible drill pipe sections) and tested in the high-pressure/high-temperature test device. A cross section of the test device is illustrated in Figure 3-16. The test setup simulated bending by compressing and elongating the "Z" pipe rather than actually bending it. This "bending" was accomplished by placing the entire test device in a heated 450,000-N (100,000-lbf) hydraulic platen press. The distance between the hydraulic press platens was regulated to provide the desired "Z"-pipe gap during the test liner penetration tests. With the platen press setup heated to the desired temperature, the pressure on the inside of the test hat was raised to the levels specified for a given test, thus pressing the test liner against the "Z" pipe section. Penetration of each test liner material into the "Z" pipe gaps was measured under varying test conditions of temperature, pressure, and gap width. A penetration of 1.0 mm (0.04 in.) or less into the gaps was considered satisfactory performance.

Test Results--The first approach to producing a reinforced elastomer --AFLAS elastomer and chopped Kevlar--was a failure as mixing was found to be impossible. The material readily fell apart and could not be molded.

In the second approach, fine Inconel wire was chopped into lengths of about 13 mm (0.51 in.) and treated to enhance adhesion to EPDM. This material was then mixed with an elastomer similar to EPDM Y267. Two compounds were attempted; one was fully loaded with Inconel, having about three times as much Inconel as the second. Both compounds performed poorly, penetrating gap widths of 1.0 mm (0.04 in.) and 1.5 mm (0.06 in.) completely, or in excess of the 1.0-mm (0.04-in.) criteria, at a pressure of about 52 MPa (7,500 psi) and at a temperature of 125°C (257°F). It appeared that the rubber-Inconel adhesion was not good in either compound.

Although difficulties were encountered with mixing in the third approach (graphite-reinforced AFLAS), two compounds were molded. Tests of

Figure 3-16. Cross Section of Flexible-Pipe/Liner Test Device

the two compounds showed that both completely penetrated the 1.0-mm (0.04-in.) gap at a pressure of 69 MPa (10,000 psi) and exceeded the penetration criteria for the 1.5-mm (0.06-in.) gap at a pressure of 52 MPa (7,500 psi). Although the performance of the reinforced compounds was some improvement over unreinforced AFLAS 291, it was not considered significant. In fact, these graphite-reinforced AFLAS compounds were less desirable since both exhibited creep. The creep characteristic probably could be corrected. However, AFLAS 291 is a difficult compound to work with, and reinforcement makes it more difficult. Hence, development work with this elastomer was not pursued further because extrusion molding of pipe may not be possible with it.

The majority of the reinforced-elastomer development effort during this reporting period involved graphite-reinforced EPDM compounds. Improvements in performance over unreinforced EPDM were obtained early in this fourth approach in the reinforcement work. In general, the percentage

of graphite used increased as the compounding effort progressed from compound A through S. In addition, some modifications to the basic L'Garde EPDM Y267 compound were made. The performance of the final set of compounds, 0 through S, is shown in Table 3-2. These compounds were the ones considered to have the highest probability of meeting the requirements of a liner material for flexible drill pipe. Compound S was especially promising, penetrating both the 1.0-mm (0.04-in.) and 1.5-mm (0.06-in.) gaps by 1.0 mm (0.04 in.) or less at pressures up to 69 MPa (10,000 psi) and at a temperature of 125°C (257°F). Tests were also run at 250°C (482°F) and 2.1 MPa (305 psi). Under these conditions, there was no indication that high temperature was a problem, and there was no penetration of the gaps by the compounds. In addition, it was observed that the shorter cure time was, in general, beneficial. Table 3-3 presents the mechanical properties of compounds 0 through S. The ultimate elongations are low (40% to 60%) and are probably overstated because of the method of measurement used. Using ASTM methods, the values could be as much as one-third less. However, the compounds are expected to meet the requirement for 15% elongation at the elastic limit.

Conclusions — The major conclusions resulting from the work to develop a liner material for flexible drilling pipe that could withstand geothermal drilling conditions were as follows:

1. DuPont Teflon PFA 350, L'Garde EPDM Y267, and L'Garde AFLAS 291 meet the pressure/temperature requirements of a liner for flexible drill pipe when they are backed by closely woven, stainless steel, braided tubing. It is believed that braided tubing (installed between the liner and the "Z" pipe) can be bent without changing its diameter. However, such a test was not conducted, and therefore uncertainty exists regarding this conclusion.

2. Graphite-reinforced EPDM elastomer (without the braided tubing) has the potential of meeting the pressure-temperature requirements of a liner for flexible drill pipe. The test data on the graphite-reinforced EPDM showed consistent results at 52 MPa (7,500 psi) pressure and promising results at 69 MPa (10,000 psi). Only three variables were changed in the compounding, so the probability of further improvement in performance of the material is considered high.

Table 3-2

Penetration of Graphite-Reinforced EPDM Compounds
into "Z"-Pipe Gaps

"Z"-Pipe Gap Width mm (in.)	Pressure MPa(psi)	Basic EPDM Y267	Penetration into Gap at 125°C (257°F), mm (in.)				
			Graphite-Reinforced Compound*				
			O	P	Q	R	S
1.0 (0.04)	17 (2,500)	1.0 (0.04)	0.5-0.3 (0.02-0.01)	0.5-0.0 (0.02-0.0)	0.3-0.0 (0.01-0.0)	0.5-0.3 (0.02-0.01)	0.3-0.3 (0.01-0.01)
1.0 (0.04)	34 (5,000)	2.0 (0.08)	0.5-0.5 (0.02-0.02)	0.5-0.3 (0.02-0.01)	0.5-0.5 (0.02-0.02)	0.5-0.5 (0.02-0.02)	0.3-0.5 (0.01-0.02)
1.0 (0.04)	52 (7,500)	6.1 (0.24)	0.5-0.8 (0.02-0.03)	0.5-0.5 (0.02-0.02)	0.5-0.5 (0.02-0.02)	0.5-0.5 (0.02-0.02)	0.5-0.5 (0.02-0.02)
1.0 (0.04)	69 (10,000)	8.9 (0.35)	1.5-2.0 (0.06-0.08)	1.5-0.8 (0.06-0.03)	1.0-1.0 (0.04-0.04)	1.3-0.8 (0.05-0.03)	1.0-0.5 (0.05-0.02)
1.5 (0.06)	17 (2,500)	1.5 (0.06)	0.5-0.3 (0.02-0.01)	0.3-0.0 (0.01-0.0)	0.3-0.0 (0.01-0.0)	0.0-0.3 (0.0-0.01)	0.3-0.3 (0.01-0.01)
1.5 (0.06)	34 (5,000)	Full	0.5-0.5 (0.02-0.02)	0.5-0.3 (0.02-0.01)	0.5-0.3 (0.02-0.01)	0.3-0.5 (0.01-0.02)	0.3-0.5 (0.01-0.02)
1.5 (0.06)	52 (7,500)	--	0.8-1.5 (0.03-0.06)	0.5-0.5 (0.02-0.02)	1.0-0.3 (0.04-0.01)	0.8-0.8 (0.03-0.03)	0.5-0.8 (0.02-0.03)
1.5 (0.06)	69 (10,000)	--	2.0-2.3 (0.08-0.09)	1.5-1.0 (0.06-0.04)	3.0-1.0 (0.12-0.04)	1.0-1.0 (0.04-0.04)	1.0-1.0 (0.04-0.04)

*Two values are given for each compound. The left value is for a 60-minute cure, and the right value is for a 20-minute cure.

Table 3-3

Mechanical Properties of Graphite-Reinforced EPDM Compounds

Property	Basic EPDM Y267	Graphite-Reinforced Compound*				
		O	P	Q	R	S
Specific Gravity	1.12	1.24	1.26	1.26	1.28	1.29
Ultimate Strength,						
MPa	12	27	26	29	30	30
(psi)	(1,741)	(3,916)	(3,771)	(4,206)	(4,351)	(4,351)
		25	29	30	26	32
		(3,626)	(4,206)	(4,351)	(3,771)	(4,641)
		30	26	23	26	26
		(4,351)	(3,771)	(3,336)	(3,771)	(3,771)
Ultimate Elongation, %	200	50	45	50	40	45
		40	55	60	40	60
		45	50	50	50	60
"Ultimate Modulus,"						
MPa	6	54	56	61	72	70
(psi)	(870)	(7,832)	(8,122)	(8,847)	(10,443)	(10,153)
		62	52	51	66	54
		(8,992)	(7,542)	(7,397)	(9,573)	(7,832)
		68	50	45	54	44
		(9,863)	(7,252)	(6,527)	(7,832)	(6,382)
Hardness	92.0	95.0	95.5	95.0	95.5	95.5

*When three values are given under a compound for a particular property, they are, in descending order, for a 60-minute, a 30-minute, and a 20-minute cure.

3.4 Rock Mechanics for Polycrystalline Diamond Compact Cutters

Sandia National Laboratories

Technical Consultants: J. Finger (505) 844-8089

D. Swenson (505) 844-9398

Project Objective

The project objective is to understand the rock/cutter interaction well enough to predict and optimize the performance of polycrystalline diamond compact (PDC) bits. The approach to this goal has been to conduct analytical and experimental investigations of a single drag cutter in virgin rock.

Background

This project was begun in November 1978. Initial efforts included the modification of a two-dimensional, finite-difference model, designated TOODY, to investigate the rock/cutter interaction problem.³⁰ Work continued during FY 1979 on (1) the calculation of cutter stresses including the effect of rake angle, depth of cut, and confining pressure of drilling mud and (2) on the rock failure mechanism, using the biaxial fracture model available in the TOODY code.³¹ In FY 1980, code modifications--dealing with finer zoning, larger rock, and longer run time--improved calculations that identified four steps in the rock fracturing process.¹⁵ During FY 1981, work on rock fracture modeling was directed to experimental verification of the rock material model and to comparison of the fracture pattern in the model prediction and in laboratory experiments. The model was transferred from the TOODY finite-difference code to the HONDO II finite-element code in order to lower costs and provide for greater flexibility, for boundary conditions, and for shear as well as tensile failure. Verification of the material model was attempted by using the constitutive relations for Berea sandstone in HONDO and by loading sandstone blocks in the laboratory under different confining stresses. The verification tests showed good qualitative agreement, although calculated failure loads were considerably less than the experimental failure loads. This discrepancy was attributed to the granular behavior of the material model after failure.⁹

FY 1982 Activities

Analytical and experimental work to understand the drag-bit cutting process continued during this reporting period. A series of single-cutter drag-bit experiments was performed on Tennessee marble and Westerly granite employing artificially worn Stratapax™-PDC cutters. Tests on the marble and granite specimens were carried out in a modified milling machine at a fixed cutter speed of 3.8 cm/s (1.5 in./s) and depths of cut of 0.254, 0.127, or 0.064 cm (0.1, 0.05, or 0.025 in.). Forces on the cutter in the directions (1) parallel to cutter displacement, (2) normal to cutter displacement in the plane of the rock surface, and (3) normal to directions (1) and (2) were measured with an X-Y-Z dynamometer. Forces on the cutter were generally found to increase with depth of cut. However, the cutter experienced little force in direction (2) while cutting marble but experienced substantial force in this direction while cutting granite. The reason for this result was not clear, and investigation of this anomaly is continuing.

The marble and granite specimens tested were subsequently impregnated first with dyed epoxy and then with an alternate fluorescent dye penetrant, sectioned, and examined using reflected light and transmission optical microscopy. In the initial experiments, fracture patterns in marble compared favorably with the predictions of the two-dimensional, finite-element code being developed, and fracture patterns in specimens of Westerly granite tested at three different depths of cut were in agreement with those in the marble samples and with the predictions of the code. However, it was suspected that the dyed-epoxy decoration technique used to examine the rock was not revealing the full extent of the damage in either type of rock. Hence, the alternate decoration method, fluorescent dye penetrant, was used to imbibe both marble and granite specimens. The results of sectioning and examining these specimens indicated that the full extent of the damage was indeed not made clear by the first decoration technique. Furthermore, although there were similarities in the extent and type of damage that developed in the two rock types, some subtle variations that reflect differences in the rock type, structure, and mechanical behavior were apparent. For both rock types examined by the fluorescent dye technique, a discrete zone of damage was observed below the surface of the cut. Photographs of a Tennessee marble sample sectioned both parallel and perpendicular to a cut are shown in Figures 3-17 and 3-18. The subsurface damage

Figure 3-17. Photograph of the Tennessee Marble Specimen Impregnated with Fluorescent Dye and Sectioned Parallel to the Direction of Cut, to the Right in this View. Note that the fractures dip in the direction of cut. The depth of cut was 0.254 cm (0.1 in.). For scale, the marble block is 10 cm (3.9 in.) long.

Figure 3-18. Photograph of the Tennessee Marble Specimen Impregnated with Fluorescent Dye and Sectioned Transverse to the Direction of Cut. Note the damage zones that radiate down and away from the cut channel. For scale, the base of the cut is 0.85 cm (0.3 in.) wide.

for both rock types was characterized by numerous fractures that consistently dipped in the direction of the cutter displacement, at angles ranging from quite shallow up to 50° to 80° (Figure 3-17). These results suggest that similar fracture mechanisms are operating in both rock types. There was no observable displacement across these fractures, indicating that they were tensile in character. It was noted that the fractures extended downward away from the "corners" of the cut (Figure 3-18); it is likely that these are the fractures that will be of interest for the purposes of optimizing cutter placement.

Two other observations were made. First, the extent of subsurface damage diminished with depth of cut for both rock types. Second, although the damage patterns were quite similar for both the marble and the granite, the fracturing was apparently on a finer scale and, hence, more obscure in the latter rock. This type of fracturing evidently reflects the more brittle character of the granite in which it is relatively easy to propagate numerous small fractures. In the more ductile marble, apparently some plastic deformation occurs prior to the development of larger, more discrete fractures. This failure mechanism is also indicated by the size distribution of the particles removed by the cutting process. Chips removed from the granite showed a definite bias toward finer sizes when compared to those from the marble. This result suggests that numerous small fractures are interacting to produce the finer chips in the granite, while fewer, larger fractures are forming the chips in the marble. In any event, a definite damage zone was shown to exist beneath the cut. This finding suggests that experiments on "conditioned" rock may be more appropriate to understanding the forces acting on a cutter under actual field conditions.

Development of the two-dimensional, finite-element code is continuing in the effort to model the rock-cutter interaction and the chip formation process. The model is still in the evolutionary stages and includes some features not contained in earlier analyses, i.e., plastic deformation or formation of shear cracks in compression. The model predictions are quite sensitive to the assigned structure of the cutter/kerf interface. Nonetheless, for what appear to be reasonable assumptions, the model predicts that chip formation is produced by the development of tensile fractures and that

subsurface tensile fractures should be produced that dip in the direction of tool displacement (Figure 3-19). The latter prediction is in good agreement with the observations noted above for marble and granite. It appears that, with further development, the code will eventually capture much of the physics of the cutting process.

Work to date shows that the rock/cutter interaction process is difficult to model. Parameter studies also show that results are very sensitive to cutter contact assumptions. It is believed that the model should give better predictions when confining pressure is high, because of the suppression of tensile cracks by the confining pressure. Efforts during the next reporting period will be directed at determining whether cutter-contact sensitivity can be defined and/or mitigated and at trying relatively simple experiments with pressurized rock.

Figure 3-19. Finite-Element-Code Prediction for Fracture Development During Cutting. Note the tensile fracture that extends down and away from the base of the cutter at approximately 45°.

3.5 Improved Cutter Materials

Contractor: SRI International
Principal Investigator: David J. Rowcliffe (415) 859-5044
Contract Period: 30 March 1981 to 31 December 1982
Technical Consultant: J. Finger (505) 844-8089

Project Objective

The objective of this project is to develop a new family of cemented carbides suitable for application in geothermal drilling.

Background

This project was started during FY 1981. Initial efforts included procuring carbide and metal powders for fabrication of cemented carbides, obtaining WC-Co (tungsten carbide-cobalt) specimens for fracture toughness correlations, and discussing WC-Co system production and performance characteristics with knowledgeable manufacturers and researchers. Fabrication of cemented carbides and toughness evaluations were initiated. Both sintering and hot-pressing processes for preparing dense cemented carbides were investigated. Cemented carbides in the NbC-Ni (niobium carbide-nickel), NbC-Co (niobium carbide-cobalt), TaC-Ni (tantalum carbide-nickel), and TaC-Co (tantalum carbide-cobalt) systems were prepared using hot-pressing techniques. The density, microstructure, and hardness of the samples produced were determined and compared with the properties of rock-cutting grades of cemented WC-Co. By the end of FY 1981, suitable fabrication and testing techniques had been established, and preparation of test specimens of NbC-Ni, NbC-Co, TaC-Ni, and TaC-Co systems was underway.⁹

FY 1982 Activities

During this reporting period, test specimens of TaC and NbC with Ni and Co binders were fabricated by hot pressing, and hardness and toughness of specimens of various compositions were measured. Testing of the wear resistance of selected specimens was also begun in FY 1982. A summary of some of the activities during the fiscal year follows.

Specimen Fabrication -- The main variables studied during fabrication were the milling time of the powders and the temperature, pressure, and time of hot pressing. The conditions necessary to produce low-porosity, fine-grained cemented carbides were established for each of the 17 compositions given in Table 3-4. In earlier experiments, powders were used that had been milled for 36 hours and then hot-pressed at 6.9 MPa (1,000 psi) throughout the heating cycle to the maximum temperature of 1375°C (2507°F). Comparison of the hot-pressing cycles for many samples showed that significant compaction of the powder began at 1145°C (2093°F) with a change to more rapid consolidation around 1285°C (2345°F). At 1375°C (2507°F), the consolidation rate became very slow. The sample consolidation at 6.9 MPa (1,000 psi) and 1375°C (2507°F) had gone as far as slight extrusion, suggesting that lower temperature or pressure would still give adequate compaction. Extrusion was even more severe at 13.8 MPa (2,000 psi) and 1375°C (2507°F). Based on this analysis, the following conditions were established for later experiments designed to improve the materials:

Milling times	24, 48, 96 hours
Pressing temperatures	1145° and 1285°C (2093° and 2345°F)
Pressing pressures	6.9 and 27.6 MPa (1,000 and 4,000 psi)

Table 3-4

Compositions of Cemented Carbides Fabricated by Hot Pressing

<u>Carbide</u>	<u>wt% Co</u>			<u>wt% Ni</u>		
	<u>5</u>	<u>10</u>	<u>20</u>	<u>5</u>	<u>10</u>	<u>20</u>
TaC	X	X	X	X	X	X
NbC	X	X	X	X	X	X
NbC _{0.83}	-	X	-	-	X	-
NbC _{0.9}	X	X	X	-	-	-

The results of using the above conditions in fabricating samples of NbC-10% Co are shown in Table 3-5. The powders of NbC-10% Co were wet-milled for 24, 48, and 96 hours, dried, and then hot-pressed into rods 12.7 mm (0.5 in.) in diameter and 19.1 mm (0.75 in.) in length. These rods were

Table 3-5

Hot-Pressing Conditions and Results for NbC-10% Co

Sample Number	Milling Time, h	Pressing Temperature,		Pressing Pressure,		Pressing Time, min	Theoretical Density, %
		°C	(°F)	MPa	(psi)		
1	24	1145	(2093)	27.6	(4,000)	29.0	70
2	24	1285	(2345)	27.6	(4,000)	17.0	97
3	48	1285	(2345)	27.6	(4,000)	21.0	98
4	48	1375	(2507)	6.9	(1,000)	2.0	98
5	48	1375	(2507)	6.9	(1,000)	0.5	97
6 ^a	48	1375	(2507)	27.6	(4,000)	1.0	—
7	96	1285	(2345)	6.9	(1,000)	30.0	99
8	96	1285	(2345)	27.6	(4,000)	30.0	99
9 ^b	48	1145	(2093)	27.6	(4,000)	60.0	92

Notes: a. Density not measured because of extrusion of cobalt.

b. NbC_{0.83}-20%Co.

sectioned longitudinally and prepared for metallographic and microprobe examination. In the Sample No. 1, pressed at 1145°C (2093°F), there were indications of local melting of cobalt but no significant liquid-phase sintering had occurred. Both Samples No. 2 and 8, pressed at 1285°C (2345°F), showed fine-grained microstructures although the dispersion of cobalt was less uniform in the sample milled for 24 hours.

The pressing time is not an independent variable but is the time required for the sample (calculated as the fixed weight of powder for a 12.7-mm (0.5-in.) diameter rod of theoretical density) to be consolidated to a length of 19.1 mm (0.75 in.). The data showed that 1145°C (2093°F) was too low for any of the compositions under consideration. All samples could be densified at 1375°C (2507°F). However, control was very poor because densification was rapid even at 6.9 MPa (1,000 psi), and higher

pressures such as 27.6 MPa (4,000 psi) caused extensive extrusion of cobalt. The intermediate temperature of 1285°C (2345°F) was considered a good compromise because the pressure controlled the final density and because apparently theoretically dense pieces could be hot-pressed in a reasonable pressing time. The observed interdependence of pressing pressure and temperature suggested that shorter pressing times could be achieved with a slightly higher temperature, if required. The pressing run on NbC_{0.83}-20%Co at 1145°C (2093°F) and 27.6 MPa (4,000 psi) showed that, even though these conditions were insufficient for complete densification, an increase in temperature to about 1200°C (2192°F) should result in acceptable test materials.

Hardness Testing — Hardness measurements were made on the samples of most compositions. The results presented in Figure 3-20 show the dependence of hardness on binder type and content. The NbC-Co materials were systematically harder than the others, and these hardness values were in the same range as those of commercial WC-Co. Substoichiometric NbC_{0.83}-Ni was considerably harder than stoichiometric NbC-Ni and was as hard as NbC-Co, for the same binder content.

Toughness was determined from measurements of the length of cracks produced at hardness impressions. The data were examined by a fracture mechanics analysis and by an empirical method that measures relative crack resistance. First, the range of applicability of the fracture mechanics analysis was explored using a set of WC-Co alloys of known hardness, toughness, and modulus to establish the appropriate parameters. Good agreement was obtained between fracture toughness values measured by the short-rod technique and the indentation technique for cemented WC-Co alloys with toughness values in the range 12 to 15 MPa m^{1/2}. However, using the same parameters, the indentation method underestimated by 50% the toughness of a very hard grade of WC-Co whose short-rod toughness had been established as 9 MPa m^{1/2}. This result probably reflects a change in the crack geometry at indentations made in cemented carbides with high and low toughness values. On the basis of the crack patterns observed in the NbC and TaC cemented carbides, the appropriate analysis gives fracture toughness values between 6 and 11 MPa m^{1/2} for these materials. These data will be reviewed

Figure 3-20. Hardness of NbC and TaC Cemented Carbides

later in the project, after the short-rod technique has been used to make absolute measurements of fracture toughness of selected NbC-Co samples.

The relative toughness of the experimental cemented carbides determined using the crack-resistance-parameter method is shown in Figure 3-21. The data showed the same trends as those established using the fracture mechanics analysis. The NbC-Co and some of the NbC-Ni and TaC-Co materials spanned the range of hardness of WC-Co, but at this stage of their development they were less tough. The NbC-Co system appeared very promising because its hardness-crack resistance curve paralleled that of WC-Co over the same range of hardness. Another significant point is that NbC_{0.83}-10% Ni has similar properties to stoichiometric NbC-Co. Since stoichiometric NbC-Co is superior to stoichiometric NbC-Ni, it appeared that substoichiometric NbC-Co could have correspondingly better properties than substoichiometric NbC-Ni. Thus, in substoichiometric NbC-Co, obtaining the same range of hardness with a higher binder content than in the case of the stoichiometric cemented carbides can be expected. Such an achievement should lead to materials with higher toughness. It is important to note that the toughness-hardness levels in commercial WC-Co have been achieved by careful control of processing parameters. A comparable optimization has not been attempted in this project due to its exploratory nature. However, closer control of the processing parameters in the NbC-Co system can be expected to result in further improvements in properties.

Wear Testing -- Wear resistance was assessed using the ANSI/ASTM B611-76 standard test for abrasive wear of cemented carbides. No systematic correlations were found between weight loss and other measured properties for the experimental carbides or for several WC-Co rock-bit grades. (This experience was confirmed by representatives of the hard metal industry.) Examination of wear surfaces by scanning electron microscopy suggested that surface plastic deformation of the carbide phase had occurred in the NbC-Co materials, in contrast to surface fracture in the carbide grains of WC-Co. The tendency to deform plastically rather than to initiate microfracture is expected to improve the wear resistance of hard grades of cemented carbides. As this reporting period ended, plans for additional wear resistance testing using a proprietary procedure in a

Figure 3-21. Relative Crack Resistance of Cemented Carbides. W is the total length of cracks produced at an indentation, divided by the load.

bit manufacturer's laboratory were underway. These tests and the remainder of the project will be completed in FY 1983, and a detailed final report on the project will be published.

3.6 Drill String Dynamics

Contractor: Jordan, Apostal, Ritter Associates, Inc.
Principal Investigator: Jerold A. Baird (401) 294-4589
Contract Period: 1 March 1982 to 15 October 1983
Contract Number: 68-3061 (Sandia)
Technical Consultant: B. Caskey (505) 844-8835

Project Objective

The objective of this project is to develop a computer program for use in solving the drill string dynamics problem, including the interaction of the drill bit and the formation. The wellbore/drill string simulation is being developed in order to specify the dynamic environment near the bit sufficiently for design of advanced downhole equipment.

Background

An exploratory study by Jordan, Apostal, Ritter Associates, Inc., has established the technical feasibility of satisfactorily developing a finite-element computer model to solve the drill string dynamics problem. During FY 1981, the planning of a program to develop the drill string model was completed. The program concept was structured into three phases: (1) development of subsystems and assembly into a bit/formation interaction model, (2) refinement and extension of the model, and (3) incorporation of the bit/formation model into a complete wellbore/drill-string simulation. Efforts were initiated to establish a cost-sharing/joint-development agreement with industry for carrying out the program. These efforts were still under way as FY 1981 came to an end.⁹

FY 1982 Activities

Solicitation of industry participants for the cost-sharing/joint-development of the drill string dynamics computer model continued during this reporting period. By mid-FY 1982, agreement was reached among NL Industries; Superior Oil Company; Jordan, Apostal, Ritter Associates (the contractor for carrying out the project work), and Sandia for participation

in the project, and formal contractual actions were completed. Subsequently, Strata Bit Corporation requested admission to the project, was accepted, but eventually left the group when they found that they could not honor their financial obligation to the project.

Project work was begun in March 1982 along with the completion of administrative matters such as scheduling of quarterly project reviews and setting up of an off-premise extension data-line communications capability between Jordan, Apostol, Ritter Associates and Sandia's NOS computer system. In refining the project concept, the participants decided to logically segment the problem into three phases and to address in phase I the most complex part, the bit/formation interaction. Figure 3-22 is a graphic representation of the drill string dynamics program phases and the drill string segments that will be included in each phase.

Progress in the development of the bit/formation part of the drill string system model continued during the remainder of this reporting period with the subsystem formulations for stiffness, mass, damping, body force description, bit rotation, and the formation solution algorithm being completed by the end of the period. At the end of FY 1982, work was underway on the bit finite-mesh creation (using drawings of a Sandia, Smith-designed Stratapax™-type bit) and validation, "ghost cutter" spatial distribution on the bit housing, and time varying external load and boundary constraint module.

Figure 3-22. Drill String Dynamics Program Phases

3.7 Wear Mechanisms for Polycrystalline Diamond Compacts as Utilized for Drilling in Geothermal Environments

Contractor: General Electric Company, Corporate Research and Development (GE/CRD)
Principal Investigator: L. E. Hibbs, Jr. (518) 385-8330
Contract Period: 16 November 1979 to 30 September 1982
Contract Number: 13-9406 (Sandia)
Technical Consultant: J. Finger (505) 844-8089

Project Objective

The objective of this project was to provide technical assistance during Sandia's evaluation of the performance of polycrystalline diamond compacts (PDCs) in drill bits designed for use in geothermal environments. The technical support concerned (1) the determination of the effects of drilling fluids on the required cutting force, wear mechanisms, and wear rates of PDC cutters and (2) measurement of friction coefficients and cutter temperatures for a variety of cutting speeds and rock types.

Background

Work under this project was initiated in early FY 1980 with full-scale, PDC-bit drilling tests using preheated air as the drilling fluid. The test data were analyzed, wear characteristics studied, and recommendations developed for improving Stratapax™ geothermal bit design. Subsequently, laboratory facilities were assembled and prepared for PDC single-cutter experiments, three rock types were procured for use in the studies, and the single-cutter tests were started.¹⁵

The original scope of work under this project, phase I, was completed during FY 1981. A procurement action being processed for follow-on work under this project was retracted during FY 1981 and changed to a new scope of work, phase II, in order to reorient future research toward fundamental rock/cutter interaction studies rather than toward drill bit design. This action was required in order to achieve compatibility of this work with the overall reorientation of the Geothermal Technology Development Program in

consonance with the anticipated reduction in FY 1982 funding levels. The procurement action was nearing completion as FY 1981 ended.

Phase I of this project included (1) rock cutting experiments with single-point, sintered polycrystalline diamond compact cutters to quantitatively determine cutter wear rates and identify wear modes, (2) PDC rock cutting experiments to measure temperatures developed and to examine the effects of tool wear, cutting parameters, and coolant flow rates on temperature generation, (3) performing full-scale laboratory drilling experiments with PDC bits, using preheated air to simulate geothermal drilling conditions, and (4) establishing design specifications for geothermal hard-matrix PDC bits. The experimental work was reported in detail in References 9 and 32. A summary of some of the results of this phase of the project follows.

PDC wear tests cutting Nugget sandstone over a speed range of 32 to 135 m/min (104 to 443 ft/min), both dry (air) and with a water-base coolant, showed that in each case the wear rate was independent of speed and was a linear function of the distance cut. The wear rate cutting dry was 12 to 24 times the rate cutting with a coolant. The controlling wear mechanism for the dry cuts was thermally induced damage to the sintered diamond material, and the wear mode for the test run with a coolant was microchipping of individual grains (abrasion).

PDC-cutter temperature measurements were made at five locations within the diamond compact/tungsten-carbide-stud supporting assembly, while cutting Tennessee marble using a water-base coolant at several levels of flow--41.0, 11.4, 8.8, and $6.3 \times 10^{-5} \text{ m}^3/\text{s}$ (6.5, 1.8, 1.4, and 1.0 gal/min). Two cutting speeds, 26.6 and 67.4 m/min (87.1 and 221 ft/min), and two depths of cut, 0.051 and 0.152 mm (0.002 and 0.006 in.), were investigated. The forces acting on the PDC tool were recorded at the same time as the temperatures. Increased speed and/or increased depth of cut raised the cutter temperatures, but the speed effect was much more pronounced than the depth-of-cut effect. Increased coolant flow rate reduced temperatures as expected, but the effect was greater at the smaller depth of cut. It was postulated that rock debris that built up in front of the cutting edge at

the greater depth of cut, 0.152 mm (0.006 in.), reduced the efficiency of cooling near the edge and moved the position for efficient heat transfer back on the rake face of the diamond tool to a point where the lower coolant flow rates were almost as effective as the highest one.

The dry, full-scale laboratory drilling experiments demonstrated that PDC bits could perform well, even in relatively high-temperature environments, if the formation was comparatively weak. Tests using preheated air, up to $\sim 260^{\circ}\text{C}$ ($\sim 500^{\circ}\text{F}$), and drilling an abrasive sandstone (Nugget) did not wear the PDC cutters, although some of the cemented carbide studs were markedly in a short time. PDC gage cutters with insufficient back rake spalled badly in the Nugget sandstone test using hot air. Attempts to drill hard rock (granite) with preheated air rapidly damaged a PDC stud-type bit. Cutter wear produced excessive frictional heat, which thermally damaged the sintered diamond materials and reduced the braze bond strength to the point that several compacts became detached. Two experimental diffusion-bonded PDC gage cutters attached to the bit on steel studs survived the granite tests, even though one was severely worn.

Design criteria developed for geothermal hard-matrix PDC bits included (1) using at least 20° back rake on all PDC cutters, (2) using PDC cutters to maintain gage in place of natural stones, and (3) increasing nozzle cross-sectional area to reduce the pressure drop across the bit to permit an adequate aerated water flow rate.

FY 1982 Activities

The redirected phase II of this project was completed during this reporting period, and preparation of a report on these experiments for publication as a supplement to Reference 32 was underway as FY 1982 ended. The phase II work consisted of (1) experiments to determine the friction coefficients of preworn PDC cutters on various rock types using different cooling fluids and (2) additional phase I-type PDC-cutter temperature measurements to include tests at higher--5m/s (16.4 ft/s)--rock cutting speeds and with two additional rock types, shale and sandstone. The data generated by these experiments have been and will continue to be used in various aspects of the Sandia projects on frictional heating of PDC drag

bits, Section 3.8, rock mechanics for PDC cutters, Section 3.4, and bit hydraulics studies, Section 4.4. A summary of the Phase II work follows.

Friction Coefficient Experiments -- The friction experiments were performed using a large vertical turret lathe to run single cutter tools against four different rock types at low and high speeds, 1.00 or 1.12 and 5.00 m/s (3.3 or 3.7 and 16.4 ft/s). The diamond compact cutters were preground to simulate worn tools, and the cutting edges were intentionally dulled so that the cutters could not penetrate the rock effectively. Thus, thrust forces equal to or greater than those generated in actual drilling operations could be developed without significant rock removal. Friction coefficients were calculated from the forces measured. Five test environments were investigated: (1) four with liquids including water, water + soluble oil, diesel fuel (to simulate oil mud drilling), and water-based bentonite mud, and (2) one with air (no coolant). The rock types investigated were marble, granite, sandstone, and shale. In order to obtain the high speed required for the friction tests with shale, which could only be obtained in a smaller size than the other rocks, the shale experiments were performed using a 30.5 cm (12 in.) toolroom lathe. The experimental technique was the same as that used for the tests on the vertical turret lathe. A sketch of the test setup depicting the relative positions of the PDC cutter, coolant nozzle, and rock cutting zone is shown in Figure 3-23.

The friction experiments were performed with Tennessee marble, Sierra White granite, Nugget sandstone and Mancos shale. Four coolants were used for the experiments on marble and sandstone: water, bentonite mud, diesel oil, and air. The experiments on the granite and shale were confined to two coolants, water and air. The friction tests were conducted at two speeds in every case, 1 and 5 m/s (3.3 and 16.4 ft/s). The results of these tests are tabulated in Table 3-6. Some additional experiments with marble, using 40:1 (by volume) water + soluble oil fluids, were performed at speeds below 1 m/s (3.3 ft/s) and between 1 and 5 m/s (3.3 and 16.4 ft/s).

As indicated in Table 3-6, the marble generally produced the highest friction coefficients except for sandstone at high speed in air and diesel

Figure 3-23. Relative Locations of PDC Cutter, Coolant Nozzle, and Rock Cutting Zone for All Friction and Temperature Measurement Experiments

fuel. As the sliding speed was increased, friction coefficients generally became lower or remained about the same except, again, for sandstone in air and diesel fuel. At 1 m/s (3.3 ft/s), the diesel fluid was as effective as water or mud in lubricating the sandstone/cutter interface. The porous sandstone seemed to provide a reservoir to feed fluid into the rubbing zone.

The water + soluble-oil coolant, used for the additional marble experiments only, lubricated the marble/PDC-cutter interface, although not very effectively. The major effect of this fluid was to reduce the change in the friction in going from high to low speed. The friction coefficients resulting from the additional experiments with marble are shown in Table 3-7 along with the coolant flow rates and rubbing speeds for each test.

In the friction coefficient experiments, thermal damage of the sintered polycrystalline diamond layer was produced at 5 m/s (16.4 ft/s) when rubbing against marble, sandstone and granite. Also, there was thermal

Table 3-6

Friction Coefficient Test Results Arranged According to Rock Type

<u>Rock Type</u>	<u>Coolant Fluid</u>	<u>Rubbing Speed m/s (ft/s)</u>	<u>Friction Coefficient*</u>
Marble	Water	1.00 (3.3)	0.20
Marble	Mud	1.00 (3.3)	0.17
Marble	Diesel	1.00 (3.3)	0.34
Marble	Air	1.00 (3.3)	0.33
Marble	Water	5.00 (16.4)	0.15
Marble	Mud	5.00 (16.4)	0.11
Marble	Diesel	5.00 (16.4)	0.13
Marble	Air	5.00 (16.4)	0.17
Sandstone	Water	1.00 (3.3)	0.03
Sandstone	Mud	1.00 (3.3)	0.04
Sandstone	Diesel	1.00 (3.3)	0.05
Sandstone	Air	1.00 (3.3)	0.10
Sandstone	Water	5.00 (16.4)	0.05
Sandstone	Mud	5.00 (16.4)	0.04
Sandstone	Diesel	5.00 (16.4)	0.30
Sandstone	Air	5.00 (16.4)	0.30
Granite	Water	1.00 (3.3)	0.09
Granite	Air	1.00 (3.3)	0.18
Granite	Water	5.00 (16.4)	0.05
Granite	Air	5.00 (16.4)	0.18
Shale	Water	1.00 (3.3)	0.09
Shale	Air	1.00 (3.3)	0.14
Shale	Water	5.00 (16.4)	0.06
Shale	Air	5.00 (16.4)	0.15

* Average values used where more than one determination was made. All test results were used.

Table 3-7

Friction Coefficient Data From Additional Marble Tests

$\text{m}^3/\text{s} \times 10^{-5}$	Coolant Flow Rate	Rubbing Speed m/s (ft/s)	Friction Coefficient
	(gal/min)		
42.3	(6.7)	1.12 (3.68)	0.11
42.3	(6.7)	0.46 (1.51)	0.11
42.3	(6.7)	2.34 (7.68)	0.10
8.8	(1.4)	1.08 (3.54)	0.12
8.8	(1.4)	0.98 (3.22)	0.12
37.9	(6.0)	0.93 (3.05)	0.20
42.3	(6.7)	0.93 (3.05)	0.28
42.3	(6.7)	0.93 (3.05)	0.11
42.3	(6.7)	0.93 (3.05)	0.24
42.3	(6.7)	1.04 (3.41)	0.23
42.3	(6.7)	1.04 (3.41)	0.24
42.3	(6.7)	0.94 (3.08)	0.13
42.3	(6.7)	1.48-0.25 (4.86-0.82)	0.04
42.3	(6.7)	1.48-1.10 (4.86-3.61)	0.03
42.3	(6.7)	1.10-0.64 (3.61-2.10)	0.10
42.3	(6.7)	0.64-0.25 (2.10-0.82)	0.26
42.3	(6.7)	1.36 (4.46)	0.11
42.3	(6.7)	1.09 (3.58)	0.25
42.3	(6.7)	5.00 (16.4)	0.15
42.3	(6.7)	5.00 (16.4)	0.09

NOTE: All tests performed with Tennessee marble using
40:1 water + soluble oil.

degradation of the diamond layer as a result of running at 5 m/s (16.4 ft/s) on sandstone with diesel fuel. Apparently, the diesel fuel was rapidly vaporized from the cutter/rock rubbing interface and, thus failed to cool the diamond layer adequately.

Cutter Temperature Measurements — Using the same experimental methods as in phase I, additional temperature measurements were made using sharp and worn tools at high speed to cut marble with a water + soluble-oil coolant. In addition, temperature measurements were obtained for sharp cutters at low speed, using the water + soluble-oil coolant. An attempt was made to measure temperatures generated in cutters designated as "badly worn," with preground wear flat areas five times greater than the wear flat areas of "worn" cutters. However, these tests could not be conducted successfully because the excessive heat developed seriously degraded the sintered diamond compacts. In addition, the thrust forces necessary to maintain the desired penetration rates were beyond the capability of the turret lathe. Therefore, these tests were replaced by temperature measurements of both sharp and worn cutters cutting shale and sandstone. The tests on shale were run at both high and low speed, but the tests on sandstone were confined to low speed because the cutter wear rate at high speed was excessive.

The additional PDC-cutter temperature measurements were made at five locations within the diamond compact/cemented tungsten carbide stud supporting assembly. Representative thermocouple locations are shown in Figure 3-24 for a "worn" PDC cutter. In cutting Tennessee marble, using a 40:1 water + soluble oil coolant, two levels of flow, 8.8 and $42.3 \times 10^{-5} \text{ m}^3/\text{s}$ (1.4 and 6.7 gal/min), were used. Tests were performed at 5 m/s (16.4 ft/s) with "sharp" and "worn" tools and at two depths of cut, 0.05 and 0.15 mm (0.002 and 0.006 in.). Experiments with "badly worn" tools could not be conducted as noted above. The temperature measurement experiments on Nugget sandstone and Mancos shale were performed with "sharp" and "worn" PDC cutters at a depth of cut of 0.15 mm (0.006 in.). The shale tests were conducted at two speeds, 1.12 and 5 m/s (3.68 and 16.4 ft/s). The sandstone experiments were restricted to 1.12 m/s (3.68 ft/s) because of the excessive PDC wear rate at 5 m/s (16.4 ft/s).

NOTES:

THERMOCOUPLE LOCATIONS - A THROUGH E
DOTTED LINES - CENTERLINES OF 1.02-mm (0.04-in.)
DIAMETER, ELECTRICAL-DISCHARGE MACHINED HOLES
ALL DIMENSIONS - IN INCHES

Figure 3-24. Thermocouple Locations in "Worn" PDC Cutter for Temperature Measurement Experiments

The results of the experiments showed that increased cutting speed was the major factor producing increased cutter temperatures by frictional heat generation. The representative increase in temperature with increasing cutting speed for three thermocouple locations in a "worn" PDC cutter is shown in Figure 3-25. Increased depth of cut had a secondary and relatively minor effect on temperature. Temperatures above 500°C (1112°F) were measured close to the diamond cutting edge when cutting marble with "worn" tools at 5 m/s (16.4 ft/s). The temperatures developed when cutting sandstone at 1.12 m/s (3.68 ft/s) were only slightly less than the temperatures developed cutting the marble at a comparable speed. In comparison with marble, the temperatures developed cutting the Mancos shale were 100° to 150°C (180° to 270°F) lower over the entire speed range.

Figure 3-25. Typical Temperature Increase at Three Thermocouple Locations with Increasing Cutting Speed for a "Worn" PDC Cutter Cutting Tennessee Marble with a High Coolant Flow and a High Feed Rate

At a point close to the diamond cutting edge, coolant flow rate had little effect on temperature. A higher flow rate was only slightly more effective in controlling temperature at a smaller depth of cut. Temperatures at locations farther removed from the cutting edge were reduced, measurably but not markedly, by a higher flow rate. The ineffectiveness of the higher flow rate in reducing cutter temperatures near the diamond cutting edge was suspected to be the result of the inability of the coolant to penetrate (clean away) the rock debris so that it could directly contact the cutter face to provide efficient heat transfer.

Conclusions -- The friction coefficient experiments and cutter temperature measurements have produced data suitable for use in examining the validity of mechanical and thermal models being developed in other Sandia rock/cutter interface and bit hydraulics studies. The experimental procedure, which continuously saturated the rock being cut with drilling fluid, made some of the friction coefficient data suspect and subject to selective use in other projects. Additional work on cutter friction coefficients is needed, with the experimental technique designed to more closely represent the downhole cutter/formation interface.

3.8 Frictional Heating of Polycrystalline Diamond Compact Drag Tools

Sandia National Laboratories

Technical Consultant: D. Glowka (505) 844-3601

Project Objective

The objective of this project is to develop a numerical-analytical model to analyze temperatures in polycrystalline diamond compact (PDC) drag tools subject to localized frictional heating at a worn-flat area and to convective cooling at exposed lateral surfaces.

Background

The friction-heating studies of PDC cutters were begun in the latter half of FY 1979 using a conduction code, CINDA,³¹ and continued during FY 1980.¹⁵ By the end of FY 1981, development of the preliminary analytical model had been completed and comparisons of calculated temperatures and experimental data made. Analytical work focused on the development of a two-dimensional heat transfer model to predict the average cutter temperature over the surface in contact with the rock. This model was used to examine three cutter configurations--mildly worn, moderately worn, and severely worn--with different velocities, cooling rates, friction coefficients, and energy partitions. Although the calculated temperatures in the stud were generally higher than those observed experimentally, the analysis demonstrated the sensitivity of the temperature distribution to the various parameters and identified the coefficients that needed better experimental definition. The experimental work at General Electric Company, Corporate Research and Development (see Section 3.7) was expected to enable fine tuning of the heat transfer model and permit more accurate evaluations of the effect of bit design on cutter temperature.⁹

FY 1982 Activities

Early in this reporting period, priority within available resources was given to this project. Hence, phase I of the development of a

numerical-analytical model to predict temperatures in stud-mounted PDC drag tools during rock cutting was completed, and reports on the work were published in References 33 and 34. Phase II of the project was begun during FY 1982, employing different boundary conditions in the study of the effects of such factors as a thicker PDC diamond layer, bit bounce, and cyclic heating and cooling. A summary of the phase I efforts follows.

Phase I--Numerical modeling of PDC cutters was performed to compute local temperatures for assumed frictional heating and convective cooling rates. The assumptions used in this thermal modeling were:

1. The PDC cutter is two dimensional. Analysis showed that this assumption did not introduce significant error.
2. The cutter is in constant contact with the rock surface, implying that a constant frictional heat flux is imposed at the cutter/rock interface.
3. Constant convective cooling occurs at the exposed lateral surfaces.
4. No convective cooling of the rock surface occurs.
5. The top of the PDC cutter is fixed at the fluid temperature. The bit is thus assumed to act as a heat sink and is vigorously cooled by the fluid.
6. Thermal properties are not temperature-dependent.

Separate thermal models were developed for three cutter configurations--severely worn, moderately worn, and mildly worn cutters. For the mildly worn cutter, the wearflat included only the diamond layer of the PDC element. For the moderately worn cutter, the wearflat included the diamond layer and a portion of the tungsten carbide material in the PDC element. For the severely worn cutter, the wearflat extended across a major portion of the tungsten carbide stud.

Numerical simulation was performed with the finite-difference code CINDA over the following ranges of operating conditions:

Frictional heat flux into cutter = 250 to 5,000 W/cm²

Convective heat transfer coefficient = 0.001 to 10 W/cm².°C

Since the mean wearflat temperature rise is directly proportional to the friction coefficient, determination of representative values of the friction coefficient between the cutter and the rock for a given condition was required to accurately predict PDC operating temperatures. Measured values of the friction coefficient for a variety of conditions were compiled from various sources, including the experimental work of Section 3.7, and are presented in Figure 3-26.

Due to the complexity of the fluid-flow field around a PDC cutter, experimental data were needed to determine typical heat transfer coefficients for this study. The required data were obtained from the experimental work of Section 4.4.

The theory developed in this study was used to predict temperatures for typical operating conditions in the downhole environment. The effects of various parameters on the mean wearflat temperature of a PDC cutter mounted near the periphery of a 40-cutter, 22.2 cm (8-3/4 in.) bit were examined. This location for the cutter was chosen to permit the use of a simple relationship between bit rotational speed and cutter sliding speed. Some of the results are presented in Figures 3-27 and 3-28. In these figures, it was assumed that the penetration rate was maintained with a mildly worn bit at 8,896 N (2,000 lb_f) WOB, by a moderately worn bit at 51,155 N (11,500 lb_f) WOB, and by a severely worn bit at 169,032 N (38,000 lb_f) WOB.

Figure 3-27 presents predicted mean wearflat temperatures for the lowest measured friction coefficient used, i.e., 0.12, corresponding to a PDC-WC stud sliding on Tennessee marble with water-oil coolant/lubricant. Assuming a downhole drilling-fluid temperature of 150°C (302°F), not uncommon in geothermal drilling, the maximum safe operating limit corresponds to

MEASURED FRICTION COEFFICIENT FOR VARIOUS SLIDING PAIRS

Figure 3-26. Measured Friction Coefficients for Various Sliding Pairs

Figure 3-27. Computed Mean Wearflat Temperatures for a Low-Friction Condition

Figure 3-28. Computed Mean Wearflat Temperatures for a High-Friction Condition

the value $\bar{T}_w - T_f = 600^\circ\text{C}$ (1112°F), where $\bar{T}_w$ is the mean cutter wearflat temperature and T_f is the ambient fluid temperature. Under these conditions, safe operating temperatures are maintained for all three wear conditions up to at least 1,000 rpm. It should be remembered, however, that wear rate increases rapidly with temperature even below the maximum safe limit, and therefore rather high wear rates should be expected at higher rotational speeds for at least the severely worn tool. Also, it should be noted that convective cooling has only a moderate effect in reducing cutter temperatures.

Figure 3-28 presents predicted mean wearflat temperatures for the highest measured friction coefficient used, i.e., 0.72, corresponding to a tungsten carbide slider on sandstone under the dry conditions typical of air drilling. In this case, only the mildly worn tool is maintained at a safe operating temperature over an appreciable portion of the range in bit rotational speed. Furthermore, it should be recognized that the downhole drilling-fluid temperature could easily be higher with air, depending on the formation temperature. This temperature would effectively reduce the maximum safe operating limit shown in Figure 3-28.

Increased convective cooling could have a much greater effect in the high friction case, Figure 3-28; however, cooling greater than approximately $0.25 \text{ W/cm}^2 \text{ }^\circ\text{C}$ probably cannot be practically achieved with air, even if air jets directly impinge on the cutters. Hence, it appeared from a comparison of Figures 3-27 and 3-28 that, although liquid drilling fluids provide greater convective cooling than air, the major beneficial role of liquids in reducing cutter operating temperatures is in lubricating the sliding interface and reducing frictional heating.

The numerical-analytical model that was developed in phase I to analyze frictional temperatures in PDC drag cutters was found to be in reasonable agreement with experimentally measured wearflat temperatures determined during controlled laboratory single point cutting tests. Even though more data is considered necessary to fully test the predictive model, it appears that temperatures can be predicted with the model to an accuracy of 20% to 30%. This level of accuracy is at least as good as the uncertainty

in the physical parameters that must be estimated to predict cutter thermal behavior.

Based on the analysis and the available data, it was found that cutter temperatures vary directly with the friction force on the wearflat and approximately as the one-half power of the sliding velocity. Temperatures also varied inversely with a non-linear function of the convective heat transfer coefficient. The degree of wear on a tool had a significant influence on the ability of the tool to dissipate frictional heat from the wearflat surface. Because a sharp tool rubs only on the high thermal conductivity diamond leading edge, the frictional heat was dissipated efficiently to the convecting surfaces cooled by the fluid, and convection cooling can thus significantly lower frictional temperatures. As the wearflat area was increased for progressive wear, the ability to dissipate the frictional heat was reduced, principally because the lower thermal conductivity tungsten carbide or steel mounting materials began to occupy the majority of the wearflat area in comparison to the diamond. Since the ability to conduct heat from the wearflat to the lateral surfaces being cooled by convection was impaired, frictional temperatures in severely worn tools could not be reduced to levels comparable to those of mildly worn tools by convective cooling at any level.

The effect of coolant flow about PDC cutting tools was to reduce frictional temperatures by convective cooling at the lateral surfaces and by reducing frictional heating at the wearflat by lubrication. Analytically, the temperatures were more sensitive to the friction at the wearflat, i.e. to the magnitude of the dynamic friction coefficient between the tool and rock, than to the magnitude of convection cooling at the lateral surfaces. Experimental determination of the heat transfer coefficients on PDC cutting elements showed that heat transfer coefficients on the order of $1.0 \text{ W/cm}^2 \cdot ^\circ\text{C}$ were easily achievable under laboratory flow conditions. However, it is very unlikely that cooling coefficients on the order of $10.0 \text{ W/cm}^2 \cdot ^\circ\text{C}$ can be achieved in bit flows under conventional drilling conditions.

Analysis showed that the use of weighted drilling fluids can actually decrease convective cooling rates below those achieved with pure water, due

to the increase in viscosity that occurs when conventional weighting materials are added to water. It was further determined that air drilling provides essentially no cooling to PDC cutters. It is, therefore, unlikely that PDC cutter temperatures can be kept subcritical in certain formations, even at low speeds and low bit weights, when air is employed as the drilling fluid. Other low-density fluids, such as foam and mist, provide greater cooling than air but lower cooling than pure water. Further analysis is required to determine whether such fluids can keep PDC cutter temperatures subcritical under a variety of operating conditions.

The foregoing results indicated that the maximum possible convective cooling of PDC cutter assemblies is already achieved in present bit designs when water is employed as the drilling fluid. Any further reduction in frictional temperatures must, therefore, be achieved either by increasing the effectiveness of the fluid lubrication of the rock/cutter contact zone, or by increasing the probability of direct convective cooling of the wearflat itself. It has been suggested that individual cutters on bits are not in continuous contact with the rock during bit rotation because of drill stem flexing and cutter interaction. From the standpoint of cutter wear, this operating mode may be beneficial because it will allow direct forced cooling of the cutter wearflat and lubrication of the rock. On the other hand, bit bounce also leads to impact loading, causing increased frictional heating over at least part of the bounce cycle. Transient thermal analysis is necessary to determine the net effects of these competing factors.

The frictional heating rate of a PDC cutter depends on the wearflat area, the dynamic friction coefficient, the bit thrust (or weight on bit), the translation speed of the cutter relative to the rock, and the convective cooling rates on lateral surfaces. The latter dependency is a result of the division of thermal energy between the cutter and the rock. A larger percentage of this energy flows into the cutter when higher cooling rates are achieved. For the range of operating conditions considered in this study, between 3% and 80% of the total energy dissipated at the rock/cutter interface flowed into the cutter as heat.

On the basis of the phase I study, it can be concluded that the rate of wear of PDC drag cutters could be reduced substantially by reduction of

the wearflat frictional temperatures with drilling fluid. For cutter normal loads on the order of 2,224 N (500 lb_f), cutter temperatures can be kept subcritical in rock cutting at speeds on the order of 1.0 m/s (3.3 ft/s), a speed commonly achieved with bit rotation on the order of 100 rpm. A higher speed might be possible with presently used drilling flow rates, especially if rock lubrication and forced wearflat cooling enhance the convective cooling of the exposed tool surfaces. At cutting speeds on the order of 10.0 m/s (32.8 ft/s), approximately 1,000 rpm, it is doubtful that temperatures could be kept subcritical except under conditions of very good lubrication at the rock/wearflat contact zone and with forced cooling of the wearflat itself. Analysis of this study, therefore, indicates that the design of high-speed PDC bits should incorporate measures to enhance the probability of direct forced cooling of the wearflat and that the associated fluid system should provide maximum interface lubrication where feasible.

Conclusions--The following are the major conclusions resulting from the phase I efforts to develop a numerical-analytical model to predict temperatures in stud-mounted PDC drag tools during rock cutting:

1. Very high thermal gradients can develop at the wearflat of PDC cutters, and these high gradients probably contribute greatly to the heat checking and cracking observed under certain operating conditions.
2. Increasing the thickness of the diamond layer relative to the total thickness of the compact may reduce thermal deterioration by improving the diffusion characteristics of the compact.
3. The mean wearflat temperature rise of a PDC cutter above the ambient fluid temperature is directly proportional to the friction coefficient and weight-on-cutter. The temperature rise increases at a rate proportional to approximately the one-half power of the sliding speed and decreases at a non-linear rate with increased convective cooling. For a given contact pressure between the cutter and the rock, temperature increases with increasing cutter wear.

4. Convective cooling can continue to reduce wearflat temperatures as cooling rates exceed $5.0 \text{ W/cm}^2 \cdot ^\circ\text{C}$; however, cooling rates higher than this value probably cannot be achieved in practice.

5. Mean radial flow across the face of a PDC bit provides cooling rates on the order of $1.0 \text{ W/cm}^2 \cdot ^\circ\text{C}$.

6. Low-pressure water jets directed at the cutters should provide cooling rates greater than those obtained with mean radial flow alone.

7. "Mudding up" reduces convective cooling slightly.

8. Typical air drilling operations provide essentially no cutter cooling.

9. The ability of liquid drilling fluids to reduce friction is far more important in reducing operating temperatures than their ability to provide cutter cooling.

10. For certain combinations of conditions, particularly when air is the drilling fluid, the wearflat operating temperatures of PDC cutters can exceed the maximum safe level of 750°C (1382°F). Even below this level, bit life can be significantly increased if operating temperatures can be reduced by improving cutter cooling and lubrication.

Phase II--As this reporting period ended, phase II efforts to expand the numerical-analytical model were underway. Different boundary conditions are being employed in phase II, including enlargement of the thermal models to contain a portion of the bit body and allowing the heat flux and convective cooling of the wearflat to vary with time because of bit bounce. Other assumptions include constant convection at exposed lateral stud surfaces and along the inside of the bit body surface, adiabatic conditions on surfaces of approximate symmetry, and no constant-temperature surfaces. Transient thermal modeling and thermal stress modeling were initiated by the end of FY 1982, along with the modeling of a thicker diamond layer for the PDC stud.

4. FLUID TECHNOLOGY

The general goal of Fluid Technology research is to develop drilling fluids specifically tailored to solve problems commonly encountered in geothermal drilling. The projects include extensive study of properties of aqueous foams used in drilling applications and development of methods for generating large quantities of inert gas to be used as a drilling fluid. Foam and inert-gas-based fluids have very low densities and, thus, can be helpful in avoiding lost circulation and can help to reduce corrosion problems. Research into high-temperature clay morphology, methods to convert clay to cement, and techniques to improve bit cleaning and cooling is also being conducted.

4.1 Aqueous Foam Drilling Fluids for Geothermal Applications

Sandia National Laboratories
Technical Consultants:
P. Rand (505) 844-7953
A. Kraynik (505) 844-0780
B. Blackwell (505) 844-6375

Project Objective

This project will identify or develop aqueous foams for use as drilling fluids in the underpressured formations characteristic of many geothermal areas. The foams will be capable of operating in elevated temperature environments and will exhibit chemical and foaming stability and anticorrosive and antioxidant properties.

Background

The laboratory development phase of the foam project was begun in mid-FY 1979 after publication of a survey report that outlined the technical approach to development of the foam. A laboratory apparatus was built for conducting surfactant screening tests, and during FY 1980 surfactant screening was carried out at 260°C (500°F) and at 310°C (590°F).³¹ The effects of NaCl, HCl, brines, and deionized water on the drainage time, cell structure, and pH of the surfactants at the test temperatures were observed. More than 100 surfactants were evaluated, and several, representing all general classes, demonstrated stable characteristics in all test environments. At the close of FY 1980, preparations were under way to acquire an autoclave in order to test the most promising surfactants at high pressure and high temperature.¹⁵

During FY 1981, the effects of temperature and pressure on the behavior and stability of foam were extensively investigated in the autoclave. In addition, stability of individual foam bubbles was investigated by studying bubble coalescence using a spinning drop interfacial tensiometer, and thermal conductivities of foams were determined using a bench-scale foam generator and thermal probes. Test facilities were also assembled to

study the rheological properties of aqueous foams and the heat transfer characteristics of flowing foam. The high-pressure/high-temperature stability tests led to the discovery that increasing the pressure caused a large increase in the liquid drainage time, i.e., increased stability.

Investigation revealed that this effect arose from the decreased average bubble size, even at constant liquid/gas volume ratios, in foams made at high pressures. Rheological models of the liquid drainage in foams as well as experimental data established that cell size has a large effect on liquid drainage times. Also, all foams showed a large decrease in stability as temperature was increased. In the spinning drop interfacial tensiometer tests, all surfactant systems tested were affected adversely by elevated temperatures, and none of the systems produced stable thin films at temperatures above 98°C (208°F). The studies of foam thermal conductivity were completed and reported in Reference 37. By the end of FY 1981, a simple model had been developed for predicting the wellbore temperature distribution for drilling a geothermal well with drilling foam, and assembly of hardware to conduct rheology and flowing foam heat transfer tests was nearing completion.⁹

FY 1982 Activities

Research activities to develop aqueous foams for use as drilling fluids under geothermal conditions continued throughout this reporting period. The autoclave used in the high-temperature/high-pressure stability tests was modified to facilitate the handling and metering of viscous, non-Newtonian fluids, and experiments continued using different surfactants and additives in the efforts to improve foam stability. The fabrication, assembly, and checkout of test facilities to study the rheological properties of aqueous foams and the heat transfer characteristics of flowing foam were completed, and testing in these areas was begun. A summary of the foam development efforts in FY 1982 follows.

High-Temperature/High-Pressure Stability Tests — Previous work has shown that surfactants, or foaming agents, can survive in simulated geothermal environments and that aqueous foams can be made at temperatures as high as 260°C (500°F). These earlier tests also revealed that the foam

stability decreases rapidly at temperatures above the normal boiling point of water. Evaluations of selected surfactants and viscosity-increasing additives were begun in FY 1982 in an effort to identify systems that will provide better foam stability at high temperatures. The foam stability studies in the high-temperature/high-pressure autoclave identified several promising commercially available surfactants and viscosity-increasing additives for potential use as foaming agents and foam stabilizers. As shown by the data in Table 4-1, the alpha olefin sulfonates make the best foams at high temperatures. An amino carboxylic acid, Sherex Armeen Z, and a quaternary ammonium chloride, Sherex Adogen 461, have also shown promise as good foaming agents. Since the alpha olefin sulfonates will precipitate salt in high-calcium environments, these other surfactants may be required for use in some geothermal reservoirs. Several proprietry surfactants--Stepanflow 1350, Farbest BW-D, and DuPont Avitone F--also show potential as high-temperature foam producers.

Although aqueous foams have been observed at 260°C (500°F) in the autoclave, they collapse very rapidly. Hence, several viscosity-increasing additives have been evaluated for improving the stability of the foams at high temperature. Table 4-2 contains the results of these evaluations. One biopolymer, Kelco K1D-97, increased the drainage time of a C₁₂₋₁₄ alpha olefin sulfonate surfactant by three to five times at 140°C (284°F). Other polymers and a sepiolite clay also improved the foam's stability at that temperature. As this reporting period ended, the experiments with the additives were continuing with the objective of obtaining stable foams at higher temperatures.

Heat Transfer Modeling and Experimentation -- Fabrication and assembly of the Foam Flow Heat Transfer Loop (FFHTL) were completed during this reporting period and checkout of the apparatus was begun. A schematic of the FFHTL is shown in Figure 4-1. Surfactant and water are individually metered and mixed with air at the foam generator to produce foam of a desired liquid volume fraction. The foam then flows through a test section that has an inside diameter of 2.54 cm (1 in.) and is 3.1-m (10-ft) long; here the foam is heated by a counterflow of hot water in the annulus surrounding the foam tube. The temperature change and mass flow-rate

Table 4-1

Aqueous Foam Drainage at 140°C (284°F) in the Autoclave^{1,2}

Surfactant		Temperature		Liquid Volume Fraction (x10 ³)	Drain Time (min) to	
Trade Name	Chemical Type	°C	(°F)		25.0%/WT	50.0%/WT
Ethyl 2999-49	AOS ³	140	(284)	46.1	13.1	25.7
Shell Neodene 1618S	C ₁₆₋₁₈ AOS	141	(286)	47.2	13.1	24.8
Lakeway 301-10/Velvetex BC	AOS/Coco Betaine (0.65/0.35)	145	(293)	44.2	12.0	24.7
Ethyl SR-82-317	C ₁₆ AOS	143	(289)	42.5	12.3	24.2
N.L. Baroid Sureflow S-398	AOS	145	(293)	45.1	11.1	22.8
Lakeway 301-10	AOS	140	(284)	50.6	10.0	20.1
Stepanflo 1349	AOS	142	(288)	42.5	10.8	20.0
Stepanflo 1350	Proprietary Anionic	143	(289)	40.2	11.0	19.7
Ethyl SR-82-316	C ₁₄ AOS	143	(289)	50.0	10.5	18.0
Farbest BW-D	Proprietary	145	(293)	46.6	9.7	17.6
Stepanflo 30	C ₁₆₋₁₈ AOS	144	(291)	45.4	10.5	17.3
Ethyl SR-82-318	C ₁₂ AOS	144	(291)	42.5	9.2	16.3
Shell Neodene 1416S	C ₁₄₋₁₆ AOS	141	(286)	42.0	10.4	16.2
Diamond Shamrock UYO	Aromatic Ethoxylate Sulfate	142	(288)	46.6	9.2	13.8
Alcolac Siponate A-168	C ₁₆₋₁₈ AOS	141	(286)	44.6	8.5	13.2
Sherex Armeen Z	Amino-Carboxylic Acid	142	(288)	48.7	7.4	11.2
DuPont Avitane F	Proprietary	143	(289)	51.2	5.2	7.8
Diamond Shamrock WL	Anionic Sulfonate	143	(289)	51.2	4.8	6.0
Adogen 461	Quat. Ammonium Chloride	140	(284)	56.4	2.2	3.0
Velvetex BC	Coco Betaine	143	(289)	- - - Rapid Collapse - - -		
UDET 950	Alkyl Aryl Sulfonate	140	(284)	- - - Rapid Collapse - - -		
Diamond Shamrock DS	Quaternary Type	137	(279)	- - - Rapid Collapse - - -		
Petrolite 80-69068	Proprietary	140	(284)	- - - Rapid Collapse - - -		

- Note: 1. Surfactant solutions: 0.5 %/WT active except as noted
 2. Autoclave pressure: 7.2 ± 0.4 MPa (71.0 ± 4.0 atmospheres)
 3. AOS = alpha olefin sulfonate

Table 4-2

Effect of Viscosity-Increasing Additives on Aqueous Foam Liquid Drainage at 140°C (284°F)¹

Additive	%wt	Viscosity ²		Pressure		Temperature		Liquid Volume Fraction (x10 ³)	Drain Time (min)	
		Pa·s	(Cp)	MPa	(atm)	°C	(°F)		to 25%/wt	to 50%/wt
None	--	0.0011	(1.1)	7.31	(72.1)	140	(284)	50.6	10.0	20.1
Kelco K1D-97	0.25	0.0062	(6.2)	7.16	(70.7)	141	(286)	41.0	51.0	62.2
N.L. Baroid X-3648-70	0.5	0.0051	(5.1)	7.27	(71.7)	138	(280)	49.7	19.3	27.0
Milchem 529-9-1	0.5	0.0053	(5.3)	7.36	(72.6)	143	(289)	51.3	18.0	25.2
Sepiolite Clay	3.0	0.0098	(9.8)	7.09	(70.0)	142	(288)	43.6	15.0	25.2
Milchem "Miltemp"	8.0	0.0016	(1.6)	7.31	(72.1)	140	(284)	69.6	7.0	--

Note: 1. Surfactant - 0.5% wt active C₁₂₋₁₄ alpha olefin sulfonate

2. Measured at ambient conditions

Figure 4-1. Schematic of Foam Flow Heat Transfer Loop

measurements of the foam and hot water streams allow a test section energy balance to be performed and an average heat transfer rate to be determined. Figure 4-2 is a photograph of the test section mounted in its insulating box.

In order to verify the experimental facility and data-reduction procedure, a series of air-flow, convective-heat-transfer tests was performed over the Reynolds Number range of 20,000 to 74,000. The Nusselt Number ($Nu = hD/k$) results are shown in Figure 4-3 where they are compared with the recommended empirical correlation of Kays and Crawford.³⁸ The comparison is considered good, especially at the higher Reynolds numbers.

The reduction of heat transfer data requires that the "mixing cup" temperature of the fluid exiting from the test section be measured. The "mixing cup" temperature is the temperature the fluid would achieve if it were collected in a cup and allowed to reach thermal equilibrium without any heat loss to the surroundings; it accounts for both velocity and temperature gradients across the tube cross section. Since foams are very viscous, it is difficult to induce turbulent mixing of the fluid. Therefore, a special thermal mixer has been designed that uses a packed bed of copper wool. Thermal mixer design curves have been developed to determine a uniform outlet temperature profile. Experiments are under way to verify the desired thermal mixer performance.

Various foam generator orientations were also tried. The best results were obtained when the foam generator was vertical with the air and water/surfactant being injected from below. When the foam generator was horizontal, the gas and liquid phases tended to separate due to large density differences.

Various investigators have modeled the stress/rate-of-strain characteristics of foams by the Bingham plastic model. If the shear stress does not exceed the yield stress, the foam exhibits plug flow characteristics. Since the shear stress for tube flow is a maximum at the wall, it is possible for the local shear stress to be less than the yield stress in the central portion of the tube and greater than the yield stress in the wall

Figure 4-2. Foam Flow Heat Transfer Loop. Note the catch basket is being filled with foam.

Figure 4-3. Results of Qualification Tests of Foam Flow Heat Transfer Loop with Air

region. This condition will produce velocity profiles that are flat near the center portion of the tube, as depicted in Figure 4-4. Because the Prandtl number ($Pr = V/\alpha$, where V is the velocity and α is the thermal diffusivity), is very large, the velocity profile will develop much more rapidly than the temperature profile. Hence, the heat transfer rate will be governed by the development of the thermal boundary layer. Wissler and Schechter³⁹ have solved this problem for a limited range of Prandtl numbers; unfortunately, their analysis does not cover the range of parameters for foam flow. Therefore, it was necessary to extend their analysis to cover the conditions of the present Foam Flow Heat Transfer Loop. The results of this extension are shown in Figure 4-5 in terms of average Nusselt number as a function of Peclet number for various yield stress to wall shear stress ratios. The plug flow condition, $\tau_y = \tau_w$, yields the maximum computed heat transfer rate, while the laminar Newtonian flow, $\tau_y = 0$, yields the minimum heat transfer rate. Preliminary data indicates that the Bingham plastic model underpredicts the heat transfer data. The explanation for this discrepancy between theory and data will be explored as this work progresses.

Figure 4-4. Normalized Fully Developed Velocity Profile for Various Values of Yield Stress/Wall Shear Stress

Figure 4-5. Nusselt Number Averaged over the Test Section Length, 3.1 m (10 ft), for a Bingham Plastic

Considerable difficulty has been encountered in the measurement of pressure drop along the length of the foam test section. The pressure transducer lines fill with foam, the liquid component of the foam then drains with time, and then new foam flows into the lines. Consequently, the pressure transducer is exposed to a continuously changing pressure environment even though the pressure drop in the test section is nominally steady. In order to eliminate this effect, the pressure transducer lines are to be filled with water and a thin latex diaphragm will be used to separate the foam from the water. This technique was under evaluation as this reporting period ended. Verification of the experimental facility and data reduction procedure will be completed, and foam experiments will begin in FY 1983.

Rheology Studies -- The goal of the rheology work is to experimentally study the tube flow of aqueous foam and to develop rheological models that can be used to predict flow behavior in a geothermal well. This effort is expanding on the empirical rheological model developed by Beyer, Millhone, and Foote of Chevron.

Construction of the aqueous foam rheology facility, Figure 4-6, was completed during this reporting period, providing the capability to do capillary rheometry in long transparent tubes about 2.54 cm (1 in.) in diameter. Gas and liquid flow rates and the axial pressure distribution in the tube can be measured. This experimental configuration was chosen for the following reasons: (1) the tube diameter must be much larger than the foam cell size to ensure that bulk rheological properties are being measured, (2) a large tube length is needed to establish fully-developed flow, and (3) a transparent tube permits flow visualization. This configuration is especially important in foam flow because of slip at the wall, which can dominate foam transport due to bulk shearing flow. By measuring the axial pressure distribution in the tube, the achievement of fully-developed flow can be verified. This verification is especially important in foams because several phenomena inhibit fully-developed flow. These phenomena include structure changes caused by foam drainage and bubble coalescence and axial flow variations due to the compressibility of the foam.

Figure 4-6. Aqueous Foam Rheology Facility

The first measurements of pressure drop and flow rate for a foam with a liquid volume fraction of 0.05 were compared to the empirical correlation of Beyer et al. Discrepancies were greater than an order of magnitude. This result does not mean that the empirical correlation is wrong, but it indicates that it is not universal. Although different surfactant systems were used in these tests, differences in foam structure may also be responsible for the disagreement. Cell size, an important structural parameter, does not appear in the correlation.

A simple model for the yield stress in foams, developed by R. K. Prud'homme of Princeton University, predicts an inverse dependence on the cell diameter, showing the importance of structure on rheology. H. M. Princen of Exxon independently developed a very comprehensive model of the small strain behavior of foams. As in the Prud'homme study, a two-dimensional model of foam structure was chosen. The Princen model predicts that the yield stress decreases significantly with increasing liquid volume fraction. A flow visualization technique was developed that utilizes a

reference bubble injected into the flow to determine the yield stress. The agreement of the measured liquid-volume-fraction dependence with Princen's predictions was surprising in view of the fact that a real foam is three-dimensional. It is interesting to note that when the wall shear stress is below the yield stress, all flow is due to slip, and the bulk viscosity of the foam is infinite. The yield stress plays a large role in determining the chip-carrying capacity of a foam because it determines the conditions under which a suspended particle will not move relative to the surrounding fluid. The empirical model assumes a constant value for the yield stress. It is now known that the yield stress exhibits a strong dependence upon liquid volume fraction and cell size.

Flow data were obtained for wall shear stresses close to the yield stress where most of the transport was due to slip at the wall. The slip velocities predicted by the empirical model were about an order of magnitude larger than those measured. Assuming that a thin layer of liquid at the wall provides a lubrication layer for slip, the data suggest that such a layer was an order of magnitude larger in the Chevron experiments. Although the reason for this is not now known, it is speculated that such effects may be due to differences in the foam structure or wall roughness.

The experiments will continue during FY 1983 to explore the effects of foam structure, physical properties, and shear rate on rheological behavior of foam.

4.2 Inert Gas Generation

Sandia National Laboratories

Technical Consultant: B. Caskey/(505) 844-8835

Project Objective

The objective of this project was to develop a cost-effective method for onsite generation of an inert (anaerobic) gas for use as one component of a drilling fluid to control geothermal drill pipe corrosion.

Background

A major contributor to the high cost of geothermal wells (three to four times higher than a comparable oil or gas well) is extensive corrosion of drill pipe--principally caused by oxygen present in the drilling fluid. Since a low-density drilling fluid must be used during drilling in the typically underpressured geothermal reservoirs, air drilling techniques are commonly used. Water is usually injected into the air stream to aid cuttings removal. The combination of aerated water, high temperatures, and high velocities results in rapid drill pipe corrosion leading to pipe downgrading or premature failure. Existing corrosion-control techniques include adding caustic soda (NaOH) to raise the pH of the water to about 10 or 11. Another technique used for air drilling hot geothermal wells is the injection of Unisteam®, a high-molecular-weight amine resin that polymerizes at about 120°C (250°F) to form a viscous, water-insoluble, oily coating. For protection of lower-temperature sections of the drill string, ammonium hydroxide (NH₄OH) is injected, which condenses on the pipe to form a protective coating. Even with corrosion-control chemical costs of \$2,000 to \$7,000 per day, drill pipe corrosion can reduce drill pipe life to a few months (typical oil field life is a few years), resulting in drill pipe replacement costs of more than \$1,000 per day.

Another approach to reducing drill pipe corrosion involves removing oxygen from the drilling fluid. To assess the effectiveness of this method

under actual field conditions, a test was planned and conducted in early FY 1981. The results showed more than a tenfold reduction in corrosion rates when nitrogen was used rather than air.^{9 40} However, as predicted, the cost of trucking and vaporizing liquid nitrogen is prohibitive for drilling operations (\$15,000 to \$20,000 per day).

This project was begun in mid-FY 1981 to determine appropriate gas composition specifications and onsite gas generation systems that could reduce the cost of inert gas drilling. The major project activities included (1) laboratory tests and analyses to define the relationship between corrosion rates and gas composition and (2) investigations of two gas generation schemes: cryogenic air separation and diesel exhaust purification.

The laboratory tests were completed in FY 1981, and a report was published that provides gas composition specifications for the required inert gas. Functions were also presented in the report that relate corrosion to oxygen and salt concentrations, temperature, flow rates, and pH. The effect of NO_x , SO_2 , CO_2 , CO , and H_2O concentrations on corrosion during mist drilling is also discussed in the report.⁴¹

The work in FY 1981 also identified a portable cold box, fabricated by Yearout and Associates of Torrance, California, that can supply about 1 million standard cubic feet of nitrogen per day. The industry-developed unit is approximately 2.4 m wide, 3.7 m high, and 6.4 m long (approximately 8 by 12 by 21 ft) and was undergoing shakedown trials in July 1981. The unit, although expensive, is capable of producing gaseous nitrogen with less than 2 ppm of oxygen. Firm operating and maintenance costs are dependent upon final checkout and operational experience, but the overall cost was estimated at \$7,000 to \$10,000 per day. The unit was scheduled to be moved to Gabon, Africa, reportedly for use in a geothermal drilling project. Correspondence was initiated with Dowell Schlumberger in Paris in an attempt to obtain operational data on their use of the cryogenic nitrogen generator in Gabon. Schlumberger did not respond to the inquiry, and it was learned informally that the unit was still in California at the end of FY 1982. Evaluation of the unit was deferred pending availability of operational data.⁹

In the latter part of FY 1981, proposals were evaluated to design and cost a full-scale diesel exhaust gas purification unit, and Foster-Miller Associates, Inc., of Waltham, Massachusetts, was selected to perform the work. The project was getting under way as FY 1981 came to an end.⁹

FY 1982 Activities

The project was completed during this reporting period, and the results of the effort were reported in References 42 and 43. A summary of the work follows.

The design specification for the diesel exhaust gas purification unit called for a portable system that could produce 0.47 to 1.41 m³/s (1,000 to 3,000 ft³/min) (standard) of nitrogen containing no more than 50 ppm O₂, 50 ppm NO_x, 50 ppm H₂O, and 1% CO₂. During the course of the design program, these specifications were modified as follows: (1) capacity was fixed at 0.94 m³/s (2,000 ft³/min) (standard) as the size most compatible with typical air compressor flow, (2) NO_x concentration was reduced to 2 ppm to avoid warranty problems with the air compressors, and (3) CO₂ concentration was set at less than 1%, this level being the lowest practically achievable concentration with a compact scrubbing system. Additional specifications were the requirements to be self-supporting in electric power requirements and to operate when cooling water was not available. The system was also to be capable of operating in the temperature range from -46° to 52°C (-50° to 126°F). The upper design temperature was later changed to 43°C (110°F) to reflect the ASHRAE 2.5% summer design temperature in the regions where the unit was expected to be used—southern California and New Mexico.

The requirements that impacted the final design most were to operate without cooling water and to remove water from the product gas. A smaller, less costly system would result if these requirements could be relaxed. Unfortunately, water for cooling is generally not available at drilling sites, and the air compressors presently used for drilling operations require that no condensation occur during compression. The basic features and specifications of the system are listed in Table 4-3.

Table 4-3

Diesel Exhaust Purification System Design Features and Specifications

Basic features:

- Portable/readable
- No cooling water available
- No electrical power available (commercial)
- Ambient temperature: -46° to 43°C (-50° to 110°F)
- Long life
- Easy setup and operation
- Reliable
- Low cost

Inlet (diesel exhaust) specifications:

- Oxygen - 6% to 12%
- Carbon dioxide - ~6%
- Oxides of nitrogen - ~0.1%
- Allowable back pressure - <25 in. of water

Outlet (product gas) specifications:

- Oxygen - <50 ppm
- Carbon dioxide - <1%
- Oxides of nitrogen - <2 ppm
- Water - -48°C (-55°F) dewpoint
- Nitrogen - 98+%

Basic Design -- The system consists of three subsystems for O_2/NO_x removal, CO_2 removal, and H_2O removal. Both CO_2 and H_2O removal are accomplished with minimal incremental expense since the thermal energy generated at the catalytic converters is in excess of that required by the regeneration of monoethanolamine and desiccant.

O_2/NO_x Removal -- O_2/NO_x removal is accomplished with standard catalytic converters based on automotive technology; operating characteristics and life expectancy are well understood. An electric preheater maintains the diesel exhaust temperature at 343°C (650°F) to ensure proper reaction rates in the first catalyst bed (CB-1).

A schematic of the catalyst bed arrangement is shown in Figure 4-7. The fuel flow, $0.25 \text{ m}^3/\text{h}$ (65 gal/h) for 11% O_2 , is proportioned to each catalyst bed in order to remove the correct percentage of oxygen. NO_x is removed in the fourth stage, which runs slightly rich. Therminol heat

exchangers cool the gas stream after each stage to control catalyst and gas temperatures. As much as 8.4 GJ/h (8.0 MBtu/h) is generated during the catalytic reaction. Finally, the gas is cooled to 54°C (130°F) in a large air-cooled heat exchanger. Also, a centrifugal blower increases the gas pressure, limiting the back pressure on the diesel engine to 6.2 kPa (25 in. of water).

Figure 4-7. Diesel Exhaust Purification System Four-Stage Catalyst Bed O_2/NO Removal Scheme. Inlet oxygen level percentages are shown ^x for each stage.

CO_2 Removal — As depicted in Figure 4-8, a high-efficiency packed column scrubber, using aqueous monoethanolamine (MEA) at $0.53 \text{ m}^3/\text{min}$ (140 gal/min), is used to scrub the CO_2 . MEA absorbs CO_2 and is regenerated (CO_2 boiled off) with Therminol that was previously heated by the catalytic reaction. MEA is only 15% of the MEA stream; the remainder is water. Water-saturated nitrogen exits the top of the scrubber, still at about 54°C (130°F).

Water Removal — Desiccant (activated alumina) is used to remove water from the nitrogen stream as shown in Figure 4-9. Three large, >1.8-m- (>6-foot-) diameter dryers are required. Each dryer, in turn, will (1) absorb moisture, (2) be regenerated with Therminol-heated air, and (3) be cooled with dry air in a closed loop. Each step will be automatically selected by dryer valving. Step 1 will take about an hour and steps 2 and 3 about 45 minutes each.

Figure 4-8. Diesel Exhaust Purification System Carbon Dioxide Removal Scheme

Figure 4-9. Diesel Exhaust Purification System Desiccant Dryers. Illustrated are (D-1A) the product gas being dried, (D-1C) the desiccant being dried with hot air, and (D-1B) the desiccant being cooled with dry air in a closed loop.

Ancillary Equipment -- In addition to the three subsystems described above, the system design includes the complete Therminol loop, pumps, heat exchangers, and required valving. A 150-kW (200-hp) diesel electric generator is included to supply electric power for the entire system. Three 11-m (36-ft) trailers are required to mount and transport the system. The arrangements of each trailer are shown in Figures 4-10, 4-11, and 4-12.

Cost Analysis -- The system procurement and operational costs were estimated and are presented in Table 4-4. The annual capital cost covers the payment required for a \$1,126,860 loan to buy the system. Operating costs include fuel and labor; one operator is in attendance when the system is operating. Maintenance costs cover catalyst replacement at the end of its life, amine makeup, desiccant replacement every three years, and three man-weeks per year for in-shop cleaning and major component replacement.

Depending on the amount of oxygen to be removed, the resulting cost for nitrogen is between \$1.47 and \$1.73 per thousand standard cubic feet. Assuming the price to be \$2.00/1,000 scf, the cost for this service would be about \$4,000 per day.

Conclusions -- A design is available for a portable diesel exhaust gas purification system that produces an inert low-density drilling fluid at an acceptable cost. Construction and use of this system would solve geothermal drill pipe corrosion problems, potentially lowering the cost of drilling by reducing drill pipe failures and reducing chemical costs. More importantly, use of this technique may open some geothermal areas to development, areas where the corrosion problems are so severe that drilling is currently impractical.

The cost of producing 1,000 scf of nitrogen with this system ranged from \$1.47 to \$1.73, depending on the type of diesel engine used in conjunction with the system and the total number of systems purchased. The resulting cost of nitrogen to the drilling contractor is only about 25% of the cost of trucking liquid nitrogen to the site, even allowing a 100% markup to the system operator.

Figure 4-10. Diesel Exhaust Purification System Trailer No. 1 Containing the Catalytic Combustors and Carbon Dioxide Scrubber

Figure 4-11. Diesel Exhaust Purification System Trailer No. 2 Containing Ancillary Equipment

Figure 4-12. Diesel Exhaust Purification System Trailer No. 3 Containing the Desiccant Dryers

Table 4-4

Diesel Exhaust Purification System Cost
Estimates and Assumptions

COST ESTIMATES:

	<u>Oxygen in diesel exhaust</u>	
	<u>11% O₂ (2 cycle engines)</u>	<u>6.5% O₂ (4 cycle engines)</u>
Capital cost (per year)	\$276,080	\$276,080
Operating cost (per year)	370,232	278,112
Maintenance cost (per year)	<u>71,900</u>	<u>53,900</u>
Totals	\$718,212	\$608,092
Nitrogen cost (per 1,000 scf)	\$1.73	\$1.47

COST ASSUMPTIONS:

Capital costs

- G & A - 10%
- Warranty - 5%
- Profit - 20%
- Interest - 18%
- Loan duration - 8 yr

Operating cost

- Fuel \$1.10 per gallon
- Utilization 40% (16 hours per day for 18 days per month)
- Labor \$98,112 per year (\$14/h and 100% overhead)

Maintenance cost

- 6,000-hour catalyst life with 11% O₂ in exhaust
- 17,000-hour catalyst life with 6.5% O₂ in exhaust
- Amine \$0.64 per pound
- Desiccant \$1.00 per pound

4.3 Drilling Fluid/Cement Studies

Contractor: Texas Tech University
Principal Investigator: N. Guven (806) 742-3110
Contract Period: 1 October 1981 to 31 October 1983
Contract Number: 68-4752 and 37-2331 (Sandia)
Technical Consultant: C. Carson (505) 844-6477

Project Objective

The objective of this project was to study the formation of cement minerals in drilling fluids by observing the rheological changes in bentonite-based muds before cement formation.

Background

This project is a follow-on effort to the extensive studies previously conducted to develop a fundamental understanding of clay particle morphology under the influence both of various chemical species and elevated temperatures similar to the conditions encountered during geothermal drilling activities. On the basis of this understanding, clay-based geothermal drilling-fluid systems, complete with the additives dictated by downhole conditions, were designed for use in geothermal drilling. The results of this earlier work was published as Reference 44.

FY 1982 Activities

Phase I of the follow-on effort, investigation of clay-to-cement conversion, was completed during this reporting period. This phase of the investigation was directed at determining how common salts, cement additives, and other contaminants affect the rheology and other properties of bentonite fluids and at identifying the conditions that promote the formation of cement minerals in bentonite-based drilling fluids. Bentonite was selected for these experiments because it is the common viscosifier and fluid loss control additive used in water-based drilling fluids. Although widely used in drilling, completing, and cementing geothermal wells, bentonite creates operational problems due to its gellation at high temperatures. Also, the

high-temperature performance of bentonite muds is significantly influenced by mud additives and by contaminants from formations. Hence, there is an established need to develop fundamental criteria for improving or defining new drilling fluid formulations for drilling, completing, and cementing geothermal wells. In addition, study of the conversion of clay minerals to cement minerals should lead to a better understanding of the problems of displacing mud by cement in a geothermal wellbore.

In these experiments, pure bentonite and bentonite/lime fluids were autoclaved in a temperature range of 149° to 316°C (300° to 600°F) under a pressure of 110 MPa (16,000 psi) with a systematic addition of various salts, silica, polymers, and other chemicals. A FANN 50C viscometer and the Sandia high-temperature/high-pressure viscometer were used to measure the high-temperature rheology of the fluids before and after aging in an autoclave cell for 16 hours under the above conditions. Other properties of the fluids such as plastic viscosity, gel strength, yield point, pH, CEC (cation exchange capacity), fluid loss, and filter cake thickness were also measured. The mineral reactions in these autoclaved fluids were studied with x-ray diffraction and electron microscopy. The changes in the physical properties and in the rheology of the fluids were then correlated with the high-temperature mineral reactions in the fluids. A summary of the results of the experiments with the various fluid compositions follows.

Purified Bentonite Fluid -- The bentonite component of water-based muds is the main viscosifier and fluid-loss-control additive in these types of muds. In order to understand the fundamental role of bentonite on the fluid rheology, fluids containing the purified bentonite were examined first. The raw bentonite, as commonly used for the fluid preparation, contains other minerals such as mica, quartz, feldspar, calcite, and gypsum in addition to the major component, a smectite. The smectite is the component that generates the desirable properties of a drilling fluid. In order to follow directly the contributions of the smectite component to the fluid rheology, a purified Wyoming bentonite with a 98% smectite content was used. Wyoming bentonite contains a Na-montmorillonite variety of smectite.

The pure Na-bentonite mud swelled significantly at temperatures above 121°C (250°F) and displayed a viscosity "hump" in the temperature range of 121° to 232°C (250° to 450°F) when the pressures were kept below 6.9 MPa (1,000 psi). For a 6% purified bentonite fluid, the viscosity was 0.09 Pa·s (90 cp) at 21°C (70°F); this value increased to about 0.3 Pa·s (300 cp) at 121°C (250°F). In the temperature range of 121° to 204°C (250° to 400°F), the pure bentonite became a thick gel, and the viscosity could not be measured. The viscosity dropped from 0.3 Pa·s (300 cp) at 204°C (400°F) to about 0.04 Pa·s (40 cp) at 260°C (500°F). In order to autoclave the purified bentonite mud at high temperatures, the concentration was reduced to 2%, thus keeping the mud in a fluid state at the high temperatures. For the 2% bentonite fluids that were autoclaved at 149° to 260°C (300 to 500°F), the viscosity "hump" was relatively depressed; after autoclaving at 316°C (600°F), this "hump" became distinct. These results are depicted in Figure 4-13. The pH and CEC values of these fluids showed steady decreases from 21° to 316°C (70° to 600°F). X-ray diffraction analysis did not show any new phase in the mud. The electron microscopic examination revealed significant morphological changes in the individual smectite particles at and above 260°C (500°F). The thin smectite films appeared to disintegrate into smaller platelets at 260°C (500°F), and lath-shaped smectites grew in large quantities during autoclaving at 316°C (600°F). These lath-shaped smectites contained more aluminum than the original thin films of smectite. The decrease in CEC values was probably related to the dissolution of the smectite films during the autoclaving at 260° to 316°C (500° to 600°F).

The addition of 0.25% high-molecular-weight polymer to 2% bentonite mud significantly increased the mud viscosity below 149°C (300°F). However, when this fluid was autoclaved at or above 149°C (300°F), it became extremely low in viscosity (see Figure 4-14). Thus, the high-molecular-weight polymer acted like a thinner at temperatures above 149°C (300°F). In order to assess the thinning effect of a low-molecular-weight polymer, 0.25% low-molecular-weight polymer was added to a 6% bentonite fluid. The low-molecular-weight polymer eliminated the gellation of the fluid at temperatures up to 204°C (400°F), keeping it in a fluid state. At 260° and 316°C (500° and 600°F), the fluids became a gel, indicating that the low-molecular-weight polymers break down above 204°C (400°F) and become useless (see Figure 4-15).

Figure 4-13. Viscosity of 2% Pure Bentonite Versus Temperature Following Autoclaving at Temperatures Ranging from 149° to 316°C (300° to 600°F). Note the distinct viscosity "hump" in the fluid autoclaved at 316°C (600°F) and the relatively depressed "hump" in fluids autoclaved at other temperatures.

Figure 4-14. Viscosity Versus Temperature of Bentonite/High-Molecular-Weight Polymer Fluid Before and After Autoclaving

Figure 4-15. Representative Viscosity Change of Bentonite/Low-Molecular-Weight Polymer Fluid with Increasing Temperature After Autoclaving

Bentonite/NaCl Fluid — A 4% bentonite and 1% NaCl fluid exhibited extremely high viscosities with increasing temperatures. The untreated mud experienced a viscosity increase from 0.03 Pa·s (32.6 cp) at 24°C (75°F) to 0.18 Pa·s (181.2 cp) at 149°C (300°F), and it showed a similar decrease from the maximum at 149°C (300°F) to 0.01 Pa·s (11.3 cp) at 260°C (500°F). After autoclaving the fluid at temperatures of 149° to 260°C (300 to 500°F), the viscosities were significantly lower, although the viscosity hump was still observed. When the fluid was autoclaved at 316°C (600°F), the fluid again exhibited very high viscosities. The pH and CEC values of the fluids showed a steady decrease with higher temperatures.

No new mineral phase was detected in the X-ray diffraction analysis of the fluids subjected to high-temperature treatments. A large reduction of CEC at higher temperatures suggested a possible decomposition (dissolution) of smectite in salt solution at high temperatures.

The addition of NaCl resulted in an extremely high viscosity. Thus, NaCl at a 1% concentration level seemed to promote the "osmotic" swelling of bentonite particles, i.e., a maximum uptake of water.

Bentonite/NaOH Fluid -- The addition of NaOH to a bentonite solution initially inhibited the swelling of the bentonite. Therefore, the bentonite concentration was increased to 8% in order to obtain any appreciable viscosity at room temperature. However, as the temperature was raised, the fluid became highly viscous and reached a maximum of 0.27 Pa·s (267 cp) at 149°C (300°F) for the untreated (not autoclaved) mud. Among the autoclaved 8% bentonite/1% NaOH fluid, the highest viscosity was observed for the fluid autoclaved at 149°C (300°F). The viscosity declined steadily for the fluids autoclaved at 204° and 260°C (400° and 500°F). The fluid autoclaved at 316°C (600°F) became extremely thin as illustrated in Figure 4-16. The viscosity of the fluids autoclaved at and above 204°C (400°F) became extremely low. This reduction in viscosity seemed to be related to the dissolution of bentonite and subsequent precipitation of minerals such as analcime and albite. The amount of these new phases became larger as the temperature increased. Also, the CEC values of the fluids similarly showed

Figure 4-16. Representative Viscosity Change of Bentonite/NaOH Fluid with Increasing Temperature After Autoclaving

a drastic decline with the increasing temperatures. These reaction products are inert relative to the rheology of the fluid, but they form at the expense of smectite in the fluid. Furthermore, these products may cause serious permeability damage if they precipitate in the reservoir.

Bentonite/KCl Fluid -- The viscosity of a 4% bentonite and 1% KCl fluid showed a sharp increase from 0.02 Pa·s (18 cp) at 24°C (75°F) to a maximum of 0.24 Pa·s (238 cp) at 177°C (350°F). However, the viscosities of the autoclaved fluid were drastically reduced. Thus, KCl seemed to control the viscosity of the fluid at high temperatures. This factor is also important when trying to minimize the formation damage effects of the drilling fluid.

Bentonite/K₂CO₃ Fluid -- The untreated fluid containing 8% bentonite and 1% K₂CO₃ became practically a gel with very high viscosities beyond the measurement capabilities used. The fluid autoclaved at 149°C (300°F) showed rather high viscosities, reaching a maximum of 0.11 Pa·s (110 cp) at 149°C (300°F), and developed a typical viscosity "hump" within the temperature range of 120° to 232°C (250° to 450°F). Fluids autoclaved at and above the temperature of 204°C (400°F) demonstrated acceptable viscosities, ranging from 0.01 to 0.03 Pa·s (10 to 30 cp). The pH of the fluid evolved gradually from 9.0 to 7.0 in the temperature range of 149° to 316°C (300° to 600°F). The CEC values measured in milliequivalents (meq) per 100 grams, on the other hand, showed large reductions from 125 meq/100 g at 21°C (70°F) to 38 meq/100 g at 204°C (400°F) and to 31 meq/100 g at 316°C (600°F). These reductions in CEC values and in viscosities at higher temperatures were considered consistent with the mineral reactions in these fluids. Because of the higher pH, the conversion of the smectite to illite/smectite mixed layers took place at 204°C (400°F) as compared to 316°C (600°F) in the bentonite/KCl fluid. Furthermore, at and above 260°C (500°F), K-feldspars also formed in the bentonite/K₂CO₃ fluid.

Bentonite/KOH Fluid -- The properties of an 8% bentonite and 1% KOH fluid proved to be effective for the control of the high-temperature gelation of bentonite. The initial viscosity of the fluid at room temperature was so low that the bentonite concentration had to be increased to 8%.

The viscosity of the untreated 8% bentonite/1% KOH fluid was extremely low as compared with the high viscosities of the untreated bentonite/KCl and bentonite/K₂CO₃ fluids. The viscosity enhancement was relatively small when the fluid was autoclaved at 149°C (300°F) and above. The pH of the fluid decreased from 12.3 at 21°C (70°F) to 7.2 at 316°C (600°F). The CEC values showed a significant reduction with increasing temperatures. These reductions in CEC and pH were considered consistent with the mineral reactions at higher temperatures. Smectite seemed to dissolve in the alkaline conditions at higher temperature, and illite/smectite mixed layers and K-feldspar precipitated at 204°C (400°F). The quantity of these reaction products increased at temperatures of 260° and 316°C (500° and 600°F). However, the decrease in smectite content of the fluid and the nature of the reaction products caused an increase in fluid loss while depressing, favorably, the viscosity of the fluids.

Thus, the inclusion of the K⁺ ion in the mud system as KOH was found to be useful in controlling the viscosity enhancement of the bentonite fluids. At the same time, the permeability damage from swelling smectites in the formation can be also reduced by the K⁺ ion in the drilling fluid.

Bentonite/CaCl₂ Fluid -- Because of the strong inhibiting effect of calcium ion on the swelling of the smectite, the bentonite concentration in the fluid was increased to 8% in order that the fluid would have some measurable viscosities. However, the viscosities of the fluid remained extremely low at all temperatures. The pH of the fluid showed a slight decline from 7.7 at 21°C (70°F) to 7.0 at 316°C (600°F). However, the CEC values decreased significantly from 100 meq/100 g at 21°C (70°F) to 50 meq/100 g at 316°C (600°F). The X-ray diffraction data showed no new mineral phases in the fluid except for halite. This mineral results from the exchange of Na in smectite for calcium in the solution: halite forms when the chloride solution with Na has been dried at room temperature. The pH of the fluid seemed to be too low for the formation of tobermorite, a cement mineral.

Bentonite/Lime Fluid -- In field operations, "spud" muds are prepared from bentonite and lime. The thixotropy obtained from such muds is related

to the flocculation of the bentonite at a high concentration. However, such a highly viscous bentonite/lime fluid does not provide any fluid loss control. A major trouble in drilling technology is related to the poor bonding between the cement behind the casing and the filter cake on the borehole wall. The addition of lime to the bentonite fluid may lead to the formation of cement minerals at high temperatures. Therefore, the effects of lime additive on bentonite muds were examined at low concentrations, 2% bentonite/1% lime, and at higher concentrations, 8% bentonite/1% lime.

The addition of lime decreased the viscosity of the bentonite fluid at all temperatures from 21° to 316°C (70° to 600°F), as compared with the pure 2% bentonite fluid (see Figure 4-17). The pH of the 2% bentonite/1% lime fluid, decreasing from 12 at 21°C (70°F) to 9.2 at 316°C (600°F), remained consistently highly alkaline. The CEC values decreased from 125 meq/100 g at 21°C (70°F) to 25 meq/100 g at 316°C (600°F). This result indicated a substantial dissolution of smectite in alkaline conditions along with the formation of the subsequent reaction products.

X-ray diffraction analysis showed that the main reaction products in the 2% bentonite/1% lime fluids were calcite and tobermorite. Even at ambient temperatures, calcite was generated by the conversion of lime to calcite when the fluid was exposed to CO₂ in the air. This conversion happens readily during the air drying of the fluid solids. Tobermorite is a cement mineral with the formal composition $5 \text{ CaO} \cdot 6 \text{ SiO}_2 \cdot 5 \text{ H}_2\text{O}$. Tobermorite formed only in trace quantities at 149°C (300°F) but appeared in large quantities at 204° and 260°C (400° and 500°F). A Ca-feldspar similar to anorthite formed in small amounts at 260°C (500°F) and in larger quantities at 316°C (600°F). Consequently, the tobermorite content was reduced to about 20% at 316°C (600°F). Electron microscopic observations showed that smectite particles partially dissolved and disintegrated in the presence of lime at high temperatures.

8% Bentonite/1% Lime Fluid -- The higher bentonite concentrations gave rise to higher viscosity values for the untreated fluid and for those autoclaved at temperatures of 149° and 204°C (300° and 400°F). However, at higher autoclave temperatures, 260° to 316°C (500° to 600°F), the fluid

Figure 4-17. Representative Viscosity Change of Low-Concentration Bentonite/Lime Fluid with Increasing Temperature After Autoclaving

viscosities dropped drastically (see Figure 4-18). The pH of the fluids decreased from 12 at 21°C (70°F) to 7.4 at 316°C (600°F). Similarly, the CEC values showed a large decrease from 100 meq/100 g at 21°C (70°F) to 12.5 meq/100 g at 316°C (600°F).

X-ray diffraction analysis showed that no tobermorite formed in the system. This result may have been caused by the lower pH values as well as by the lower dissolved Ca/Si ratios in these fluids. Calcite was the prominent reaction product at and below 260°C (500°F), and it may have formed due to the same reasons noted above. A calcium-feldspar, however, formed in small quantities at 260°C (500°F) and in larger amounts at 316°C (600°F). Smectite remained in the system in Ca-forms with a basal spacing of 15.2.

Bentonite/Lime/Silica Fluid -- Free silica was added to the bentonite/lime fluid with the expectation of enhancing the cement formation since the cement reactions are generally based on the reactions between lime and silica. For this purpose, a fluid consisting of 2% bentonite, 1% lime, and 1% silica was autoclaved at temperatures ranging from 149° to 316°C (300° to 600°F). As in the case of the 2% bentonite/1% lime fluid, the viscosities of this fluid were extremely low at all temperatures (see Figure 4-19). The pH range of the fluid was also similar to the previous bentonite/lime fluid, decreasing from 12.8 at 21°C (70°F) to 9.2 at 316°C (600°F). One unusual parameter of the fluid was its low CEC value, 75 meq/100 g at 316°C (600°F), indicating a substantial decomposition of smectite in the alkaline conditions at high temperatures.

X-ray diffraction analysis showed that the major reaction products in this fluid were tobermorite, calcite, and Ca-feldspar, the same products that formed in the previous bentonite/lime fluid. Calcite formed readily even at ambient temperature due to the reaction of lime with CO₂ in the air. Small amounts of tobermorite developed in the fluid during autoclaving at lower temperatures--5% at 149°C (300°F)-- and larger amounts developed at higher temperatures--15 to 20% at 204° to 316°C (400°-600°F). It is interesting to note that significantly less tobermorite was formed in this fluid (with extra silica) than in the bentonite/lime fluid. Thus, the

Figure 4-18. Representative Viscosity Change of High-Concentration Bentonite/Lime Fluid with Increasing Temperature After Autoclaving

Figure 4-19. Representative Viscosity Change of Bentonite/Lime/Silica Fluid with Increasing Temperature After Autoclaving

excess silica, fine quartz powder, does not promote the formation of cement minerals in the fluid. The tobermorite seems, therefore, to be a product of the reaction between lime and smectite (perhaps involving the amorphous silica in the bentonite) but not between lime and silica from fine quartz powder.

Electron microscopic observations revealed that tobermorite crystals occurred mostly as thin lath-shaped units. A few of them appeared as thin platelets. The crystallinity of tobermorite seemed to increase with higher temperatures. Dense particles, with Si, Al, and Ca spectra, were observed mostly at and above 260°C (500°F) and were believed to be a Ca-feldspar. Smectite particles occurred as fragmented, dense films at 149° to 260°C (300° to 500°F), and they were mostly dissolved at 216°C (600°F). Also, there were Ca- or Si-rich spherules in the fluid autoclaved at 149°C (300°F); these were probably calcite and quartz.

Conclusions -- The conclusions resulting from the phase I investigation of clay-to-cement conversion were:

1. Pure Na-bentonite fluid becomes highly viscous in the temperature range of 121° to 232°C (250° to 450°F).
2. Low-molecular-weight polymer controls the viscosity only in a limited temperature range, 149° to 204°C (300 to 400°F).
3. High-molecular-weight polymer enhances the fluid viscosity only for the untreated mud. It reverses its function when the fluid is autoclaved at and above 149° (300°F).
4. Sodium hydroxide, and especially NaCl at 1% concentration, in the fluid tremendously enhances the fluid viscosity up to 260°C (500°F).
5. Salts and hydroxides of potassium provide excellent control of the fluid viscosity at high temperature. This reaction is related to the conversion of smectite to illite/smectite mixed layers at and above 204°C (400°F).

6. Salts and hydroxides of calcium depress the fluid viscosity to very low levels at all temperatures.
7. The addition of lime increases the fluid pH and promotes the high-temperature reaction between bentonite and lime with the precipitation of tobermorite, the common cement mineral.
8. The addition of free silica to the bentonite/lime fluid does not promote the formation of cement minerals in the fluid.

Preparations for phase II of the investigation of clay-to-cement conversion in drilling fluids were underway at the close of this reporting period. Phase II investigations will study the factors that control cement setting in bentonitic drilling fluids and will develop an understanding of the accelerating and retarding effects of additives on the conversion of clay minerals to cement minerals.

4.4 Bit Hydraulics Studies

Sandia National Laboratories

Technical Consultant: D. Glowka (505) 844-3601

Project Objective

The objective of this project was to discover methods for cooling and lubricating geothermal drill bits that would lead to development of bit technology for drilling in hard formations. Analytical and experimental investigations were conducted to determine the effects of position, size, and placement of drilling fluid nozzles on the capabilities of PDC drag bits to cut hard, fractured rock. A bit hydraulics laboratory, built in order to visualize the flow field along the bit face and to provide measurements of bottomhole pressure gradients, cutter heat transfer coefficients, and chip removal capability, was used in the conduct of the experiments.

Background

This project began in FY 1979 with the formulation of the bit hydraulics research program plan and the completion of the design of the laboratory test facility for performing the hydraulics studies.³⁰ By the end of FY 1979, the bit hydraulics test stand design had been reviewed in detail, the instrumentation for the test facility had been determined, the required components had been ordered or placed in fabrication, and preparations for construction of the facility had been completed.³¹ Work during FY 1980 focused on the assembly and checkout of the bit hydraulics test stand (see Figure 4-20). Replacement of a defective drive shaft delayed the start of full-scale bit hydraulics studies until FY 1981. A parallel effort was initiated late in FY 1980 with the construction of a small-scale water channel for use in measuring single-cutter heat transfer coefficients and flow patterns.¹⁵

The fabrication of the small-scale water channel was completed early in FY 1981 and used to test the heat transfer coefficient measurement

Figure 4-20. Bit Hydraulics Test Stand. Its capabilities include (1) 6.89-MPa (1,000-psi) maximum system pressure, (2) 900-rpm maximum bit rotational speed, (3) 16 channels for bit instrumentation, and (4) conventional or reverse circulation modes.

technique planned for use in the bit hydraulics facility tests. The accuracy of the technique was verified, and baseline data for the PDC stud placed in a uniform flow field were also obtained. After installation of a new drive shaft in the hydraulics test stand, full-scale bit hydraulic studies were begun using, first, a dye injection system and, second, a spherical acrylic tracer beads technique. The dye injection tests proved to be of limited value because of turbulence in the bit flow field. On the other hand, the tracer beads technique permitted the determination of the velocity and flow patterns, including stagnant and recirculating flow

regions, for three different bit hydraulic configurations. Convective heat transfer measurements were also made on the PDC bit using the three hydraulic configurations. The initial results in FY 1981 included identification of the configuration affording the best cooling, indications of no simple trend in the effect of bit rotation on cutter cooling, and evidence that gauge cutters received less cooling than other cutters.⁹

FY 1982 Activities

The bit hydraulics studies were closed out early in this reporting period with the completion of the analysis of the tracer bead tracking tests and convective heat transfer measurements on three hydraulic configurations using a Sandia-designed PDC bit. The results of this work were published in Reference 45. High-speed motion photographs, using plastic tracer beads, were obtained for the bit-face flow fields of four substantially different commercial PDC bits. But, because of reduced resources and shifting of priorities to the study of frictional heating of PDC drag tools (Section 3.8), analysis of this data was deferred until additional resources become available. A summary of the tracer tracking tests and convective heat transfer measurements follows.

Test Bit Configurations -- The test bit was a 16.5-cm (6.5-in.), 21-cutter, stud-mounted PDC bit designed and fabricated at Sandia. A photograph of the bit is shown in Figure 4-21 and the relative locations of bit cutters, nozzles, and junk slots are depicted in the schematic, Figure 4-22. Figure 4-22 is drawn as a mirror image of the bit face because photographs and drawings were produced by viewing the bit through a mirror.

The cutters are arranged in three spirals. For identification purposes, each spiral was assigned a letter, A, B, or C. Within the spirals, each cutter was assigned a number corresponding to its relative location in the spiral. Radial locations of the cutters were designed for equal rock removal volume per cutter, with the exception of cutters A-6, B-6, and C-6. These are the gauge cutters, located at the same radius to provide redundancy.

Figure 4-21. Sandia 16.5-cm (6.5-in.) PDC Stud Bit

Figure 4-22. Schematic of Test Bit Face (Mirror Image)

The original bit design employs 10 nozzles or ports for hydraulic cleaning and cooling. In the experiments, two other hydraulic configurations were also tested. These configurations are described in Table 4-5 and were achieved by simply plugging various nozzles to alter the bit face flow patterns.

Table 4-5
Hydraulic Configurations

Configuration	Total Nozzle Area		Nozzle									
	cm ²	(in. ²)	1	2	3	4	5	6	7	8	9	10
1	16.32	(2.53)	0*	0	0	0	0	0	0	0	0	0
2	4.39	(0.68)	0	0	0	0	X	X	X	X	X	X
3	6.00	(0.93)	X**	X	X	X	X	X	X	0	0	0
Nozzle Diameter, mm (in.)			7.94 (0.3125)	12.7 (0.500)	12.7 (0.500)	12.7 (0.500)	15.88 (0.625)	15.88 (0.625)	15.88 (0.625)	15.88 (0.625)	15.88 (0.625)	15.88 (0.625)

* 0 = Nozzle open

** X = Nozzle plugged

Tracer Tracking Tests -- High-speed motion photography (400 frames/second) was used to record the motion of flow tracers across the face of the test bit. Frame-by-frame analysis of the film produced tracer trajectories, defined as "pathlines."

If the flow were steady and the tracers infinitely small and of the same density as the fluid, the tracer pathlines would correspond exactly to the streamlines of the flow. Past studies in water have shown that 0.5-mm (0.02-in.)-diameter polystyrene spheres (specific gravity, 1.04) follow streamlines very closely under steady-state conditions. At the time of this study, however, such spheres were found to be unavailable commercially. Furthermore, the use of such small particles would make tracking extremely difficult, if not impossible, with the test facility. As a result, the tracers chosen for this study were 3-mm (0.12-in.) acrylic spheres with a specific gravity of 1.2. Analysis based on the equations of motion for a sphere in crossflow indicated that such particles would follow

steady-state streamlines adequately for the study, in which the primary objective was to identify stagnant flow regions.

Typical tracer pathlines for the three hydraulic configurations tested are shown in Figure 4-23 for zero bit-rotational speed. The distance between solid points (●) on the pathlines corresponds to the horizontal distance traveled by the tracers in two frames of film (1/200) second).

Open points (o) on the pathlines correspond to points where tracer velocities were zero. Isolated zero-velocity points most likely resulted from collision with a cutter or from unsteady flow conditions where a tracer changed from one streamline to another. Concentrations of zero-velocity points, however, indicated stagnant flow regions. Another indication of stagnant and recirculating flow regions was the presence of sinuous pathlines. These areas typically were characterized by very low, if not zero, tracer velocities.

Examination of pathlines for the three hydraulic configurations tested revealed the following:

1. Tracer velocities were generally lower for Configuration 1 than for the other two configurations. The maximum measured velocities for Configurations 1, 2, and 3 were 1.3 m/s (4.1 ft/s), 2.1 m/s (6.9 ft/s), and 1.5 m/s (5.0 ft/s), respectively. The principles of similitude imply maximum field velocities 8.3 times these values for the assumed mud characteristics. The corresponding Reynolds numbers, based on PDC stud diameter, are 2.3×10^4 , 3.9×10^4 , and 2.8×10^4 for both laboratory and field conditions.
2. Velocities generally decreased with radial distance from the originating nozzle, as expected.

Figure 4-23. Typical Pathlines for Bit Hydraulic Configurations: (a) Configuration 1 (All 10 Bit Nozzles, Open), (b) Configuration 2 (Outer Six Nozzles in the Bit Face, Plugged; Inner Four Nozzles, Open), and (c) Configuration 3 (Inner Seven Nozzles in the Bit Face, Plugged; Outer Three Nozzles, Open)

3. Large stagnant and recirculating flow regions existed near cutters in spiral positions 5 and 6 for all configurations. These regions are caused by the proximity of these cutters to the side of the borehole and by their remoteness from junk slots. Additional recirculating flow regions existed near various other cutters for different configurations. These regions were generally on the downstream side of cutters and were most likely a result of flow separation from the cutters.
4. The existence of vertical recirculating flow regions near junk slots was evidenced by the behavior of a few tracers that acquire zero velocity or movement parallel to the junk slots. These regions were evidently a result of flow separation from the bottom of the borehole. This separation is caused by pressure forces that act to turn the flow up the annulus. The existence of such zones suggested the potential for rock chips to be deposited in the "corner" of the hole, leading to reduced chip removal.
5. The approximate areas of the bit face covered by flow from each nozzle was determined by locating the outermost pathlines bounding all others originating from the same nozzle. Examination of movies made during bit rotation clearly revealed the effects of rotation on flow patterns. The tracer tracking tests thus provided more data than did the dye flow tests conducted earlier.⁹

Convective Heat Transfer Measurements -- Before measuring convective heat transfer coefficients for the PDC test bit, the accuracy of the technique developed for this purpose was determined. A flow channel with a test section flow area of 15.7 cm x 1.9 cm (6.2 in. x 0.75 in.) was designed and fabricated to provide uniform flow through the test section. A right circular cylinder, instrumented with a heat transfer gauge, was tested first for comparison with data obtained with different techniques reported in the literature. The accuracy of the present technique was determined to be quite good and was reported earlier in Reference 9.

Subsequent tests were also conducted with a single cutter to determine heat transfer coefficients for uniform crossflow velocities. The results are shown in Figure 4-24. As shown in this figure, the cutter was instrumented with heat transfer gauges at four different locations. The measured data were similar to most forced convection heat transfer data in that they correlated well in the form $N_{Nu} \propto N_{Re}^n$. The Prandtl number dependency was inferred from correlations reported in the literature for other geometries.

The PDC test bit was instrumented with 28 heat transfer gauges. Because of practical limits on the number of wires that could be passed through the bit body, each PDC stud could not be instrumented at all four locations as illustrated in Figure 4-24. Table 4-6 lists the gauge locations chosen for each stud. This arrangement placed gauges in all four locations for each relative cutter location within the spirals. For instance, among Cutters A-3, B-3, and C-3, gauges were located in all four positions illustrated in Figure 4-24.

The convective heat transfer test series for the PDC test bit consisted of measuring heat transfer coefficients for the three hydraulic configurations at all gauge locations, with the standard fluid flow rate and over the bit rotational speed range of $\omega_x = 0$ to 120 rpm. Measurements were recorded nominally every 30 rpm; however, if large changes occurred between nominal measurement points, data were recorded at more frequent intervals. Typical results are shown in Figure 4-25.

Comparison of the curves in Figure 4-25 for the three hydraulic configurations reveals the following features:

1. Configuration 2 had the highest heat transfer coefficients for this gauge location, whereas Configuration 3 had the lowest values. This result was considered reasonable, as Configuration 2 placed all flow through the center four nozzles; Configuration 1 placed only 27% of the flow through these nozzles; and Configuration 3 had these nozzles plugged.

Figure 4-24. Local Convective Heat Transfer Coefficient Measurements for a PDC Stud in Crossflow

Table 4-6

Heat Transfer Gauge Locations

<u>Cutter</u>	<u>Gauge Location</u>
A-1	2,3
A-2	2,3
A-3	2,3
A-4	2,3
A-5	2,3
A-6	2,3
A-7	2,3
B-1	4
B-2	4
B-3	4
B-4	4
B-5	4
B-6	4
B-7	4
C-1	1
C-2	1
C-3	1
C-4	1
C-5	1
C-6	1
C-7	1

2. The curves for Configurations 1 and 3 show little effect of bit rotational speed, whereas the curve for Configuration 1 shows moderate effects. The local minimum in this curve at 45 rpm is particularly interesting. Since Configuration 1 had the highest nozzle velocities due to a lower total nozzle area, it is probable that the resulting radial jets had higher circumferential velocity gradients. Rotation of the bit evidently caused regions of the radial jets with different velocities to impact the cutters at

Figure 4-25. Local Heat Transfer Coefficients for a Typical Location on a PDC Test Bit in a Simulated Downhole Flow Field

different rotational speeds. Another mechanism at work here could have been the change in area covered by flow from individual nozzles. For instance, the gauge mounted on Cutter A-1 could be impacted by flow from Nozzle 2 for $\omega_l = 0$ to 45 rpm and by flow from Nozzle 3 for rotational speeds above 45 rpm.

Measured heat transfer coefficients for several cutters were compared with predictions based on measured tracer velocities and the correlations of Figure 4-24. Differences as large as 70% were found, with the measured values being generally higher than the predicted values. These differences are probably caused by the different natures of the flow channel and bit-face flow fields. The correlations of Figure 4-24 were obtained for a steady, uniform flow field, whereas the bit-face flow field appeared to be unsteady in nature and experienced large velocity gradients. Furthermore, low free-stream turbulence was a design goal for the flow channel; the bit-face flow field appeared to be highly turbulent. This condition was considered to be the reason for the measured values being higher than the predicted cooling rates.

In order to present general conclusions about all the measurements made in this test series, results for each relative cutter location within the spirals were averaged and are presented in Figure 4-26. Because of the approximate symmetry of the bit, these results correspond approximately to the average heat transfer coefficients for cutters in each relative location.

Several cooling characteristics were apparent from these data.

1. There appeared to be no simple trend in the effect of rotation on average heat transfer coefficients. In about half the cases, heat transfer was higher with bit rotation; in the other cases, it was higher with no bit rotation. Modeling the flow field with simple models, such as augmenting mean radial flow velocities with circumferential components caused by rotation, is therefore, not considered adequate for predicting cutter cooling. Changes in the location of radial jets as a result of rotation should be the basic mechanism employed for modeling flow-field behavior.

Figure 4-26. Mean Heat Transfer Coefficients for PDC Test Bit in Simulated Downhole Flow Field

2. Configuration 2 cooled all cutters better than Configuration 1. This effect was higher near the center of the bit and gradually decreased toward the periphery of the bit. The increased velocities near the center of the bit caused by plugging the outer nozzles in Configuration 1 were clearly the reason for increased cooling of inner cutters. It is conjectured that these increased velocities result in a higher free-stream turbulence level throughout the flow field, and this increased turbulence more effectively cools the outer cutters as well.
3. There was no definite trend in the relative magnitudes of cooling between Configurations 2 and 3. Those cutters that were cooled better by Configuration 3 were generally closer to the open nozzles of that configuration than to the open nozzles of Configuration 2. It was not clear why the Configuration 3 value for Cutter Location 5 was lower; however, examination of Figures 4-23 (b) and (c) shows more extensive flow stagnation and recirculation for Configuration 3 than for Configuration 2.
4. Convective heat transfer coefficients for Cutter Location 6 were generally lower than for any other cutter location. Cutters in this location are given the task of cutting gauge. Therefore, they travel faster and generate more frictional heat than do cutters in other locations. Consequently, the lower cooling rates that these cutters experience could cause them to wear at higher rates, resulting in uneven bit wear.

For the field mud properties assumed in this study, the projected field cooling rates were about twice the measured laboratory values. Whether these cooling rates are sufficient to prevent thermal degradation or rapid wear of the various cutters during drilling is not known at this time. The temperature achieved by a cutter depends on its translation velocity and weight on bit, in addition to cooling rates (Section 3-8). Using thermal numerical models, the calculation of these temperatures is relatively straightforward, given a set of operating conditions. It is not

known quantitatively, however, to what extent elevated operating temperatures increase cutter wear. Until temperature guidelines have been established, the question of whether a particular hydraulic configuration adequately cools all the cutters cannot be answered. Rather, the cooling rate measurements recorded in these tests can be used only to rank various configurations as to their relative cooling effectiveness and to identify potential hot spots.

Conclusions -- The major conclusions resulting from the bit hydraulics studies were:

1. The dye flow technique for flow visualization provides useful data for flow across the face of a bit blank; however, turbulent diffusion of the dye when flowing across the face of a bit with PDC studs is too extensive to permit useful data for this geometry.
2. Tracking flow tracers with high-speed motion photography provides good data on the location of stagnant and recirculating flow regions. Approximate flow velocities across the face of the bit, as well as areas covered by flow from individual nozzles, can be determined with this technique. Tracking thus supplies all the data originally expected from dye flow tests.
3. Measurement of convective heat transfer coefficients is extremely useful. In addition to providing data that can be used to compute cutter temperatures during drilling, this technique provides insight into general flow-field characteristics. The location of stagnant flow regions identified with tracer tracking correlates with lower heat transfer coefficient measurements.
4. There appears to be no simple effect of rotation on the flow field. The lateral movement of radial jets caused by rotation is a more accurate description of the effect than simple augmentation of radial velocities with tangential velocity components.

5. Strong circumferential velocity gradients exist in flow emerging from nozzle areas.
6. Three-dimensional modeling would be necessary to account for vertical recirculating flow regions near the "corner" of the borehole.
7. Flow velocities and heat transfer coefficients were, in general, lower for Configuration 1 than for the other configurations.
8. Configuration 3 provided relatively higher cooling to some outer cutters than did Configuration 2; however, it provided lower cooling to a substantial number of cutters. Furthermore, Configuration 3 had potential problems in scavenging and removing chips generated by inner cutters since the center of the bit was theoretically a stagnation zone with this configuration. The extent of this potential problem requires additional tests in which bottomhole pressure gradients are measured. Low pressure gradients would indicate poor bottomhole scavenging potential.
9. Configuration 2 provided higher flow velocities and a generally better cooling pattern across the bit face. These characteristics appeared to result from a lower total nozzle area and the placement of nozzles near the center of the bit. Furthermore, this configuration would probably not have the bottomhole scavenging problem identified with Configuration 3. As a result, the nozzle placement of Configuration 2 was identified preliminarily as the most favorable of the three configurations tested.
10. Extensive stagnant and recirculating flow regions occurred near cutters in relative locations 5 and 6. If additional experiments show the resulting cooling rates inadequate to prevent thermal degradation, a potential design improvement would be to place small junk slots near these cutters, thereby increasing crossflow velocities in these regions.

11. Rotational effects did not appear to be extensive with this bit design. Additional testing with other bit designs should determine if this is a specific or a general conclusion.

The bit hydraulics test facility remains available for additional work in this area should resources become available in the future for further studies in the optimization of bit hydraulic configurations.

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5. BOREHOLE MECHANICS

The Borehole Mechanics program emphasizes research concerning borehole stability during drilling, completion, and maintenance. Specific areas of interest include lost circulation, lining and cementing the well, casing structural failure, and methods for prevention or removal of geothermal scale.

5.1 Lost Circulation Test Facility

Sandia National Laboratories

Technical Consultants:

G. Loeppke (505) 846-0301

B. Caskey (505) 844-8835

Project Objective

The first objective of this project is to construct a facility that will provide the capability to test lost circulation materials and techniques under simulated geothermal conditions. Following the construction of the facility, the evaluation of materials and techniques used for problems in completion and lost circulation in geothermal wells will begin. The final objective is to recommend the proposed solutions that stand the best chance for success in the field; thus, the facility will serve as a final screening facility before field testing.

Background

A substantial portion of the cost associated with drilling and completing a geothermal well can be attributed to lost circulation problems and poor cement displacement. Lost circulation that occurs during drilling may increase the drilling costs by 15% or more, while incomplete cementing jobs can result in loss of the well. The efforts to establish a test facility and a materials/techniques evaluation program to develop solutions to lost circulation/cementing problems were begun in mid-FY 1980. A survey of industry was conducted to determine whether suitable test facilities were available. Only one facility, a Halliburton test apparatus, was found with capabilities similar to those required for this project. However, the Halliburton facility was scheduled for exclusive use in cement studies by a major oil company for a period of three years. Also, the facility had an operating temperature of less than 121°C (250°F), which is below the temperatures that may be encountered in geothermal wells. Because of the lack of availability and the limited temperature, it was concluded that a new,

high-temperature facility that could be devoted to lost circulation material studies was required. The design of the facility was under way by the end of FY 1980.¹⁵ The detailed design of the test facility was completed during FY 1981. The fabrication and procurement of components continued throughout FY 1981, and modification of the laboratory building to accommodate the facility was begun.⁹

FY 1982 Activities

The modification of the laboratory building to accommodate the test facility was completed in FY 1982, and procurement, fabrication, and assembly of components and subsystems continued throughout this reporting period. A control and instrumentation system, including the necessary computer software and hardware, was designed, assembled, and installed to provide the required control, monitoring, and display of the operation of the test facility. As FY 1982 came to a close, final assembly and connection of all subsystems were underway in preparation for system shakedown tests.

The system is capable of operation at temperatures up to 204°C (400°F) and at a maximum allowable working pressure of 7.9 MPa (1,150 psi) with a pumping rate of 0.02 m³/s (280 gal/min). The test vessel core is 81.3 cm (32 inches) long and has a diameter of 50.8 cm (20 inches). The system volume is 2,763 liters (730 gallons), with the test vessel and accumulators holding 1,124 liters (297 gallons) and the heating and pumping subsystems containing the remainder. A 1,136-liter (300-gallon) mud tank is included in the facility. A 20-hp centrifugal pump-jet mud mixer and a 4,543-liter (1,200-gallon) ribbon blender are used for mixing. A 3.1 x 10⁹ J/h (2.9 x 10⁶ btu/h), tube (mud) and shell (steam) heat exchanger provides the means to heat the drilling mud during experiments. A 200-hp, variable-speed hydraulic mud pump and a nitrogen system are provided for fluid circulation and pressurization of the system. Three accumulators with a capacity of 254 liters (67 gallons) each are incorporated into the system in order (1) to regulate the pressure in the test vessel annulus, (2) to collect the filtrate through the test cores, and (3) to contain the "lost circulation material slug" for charging the test vessel. A schematic of the test facility layout is depicted in Figure 5-1, and photographs of the system components housed inside and located outside the laboratory building are

Figure 5-1.. Schematic of the Lost Circulation Test Facility

shown in Figures 5-2 and 5-3, respectively. A photograph of the control room is presented in Figure 5-4.

Testing will begin during the next reporting period to accommodate shakedown of the test facility and to develop baseline data. These initial experiments will attempt to duplicate earlier test results from a lost circulation materials screening program that employed a modified API slot tester.⁴⁶ The lost circulation material Thermo-Set Seal (ground battery casings) will be used, and investigations of cross flow and temperature effects will be conducted. The core for these tests will be a steel insert, 81.3 cm (32 inches) long with a bore of 17.5 cm (6.9 inches), having orifices that can accommodate up to 12 modified API slots or plugs or any combination thereof (see Figure 5-5). This steel core, which can withstand a differential pressure up to 6.9 MPa (1,000 psi), was designed as an insert for the facility test vessel shown in Figure 5-6. After completion of the baseline data tests, investigations are planned using permeable and fractured cores designed to simulate downhole field conditions. A schematic of the test vessel setup for these investigations is depicted in Figure 5-7.

Figure 5-2. Photograph of Lost Circulation Test Facility Components Located Inside the Laboratory Building

Figure 5-3. Photograph of Lost Circulation Test Facility Components Located Outside the Laboratory Building

**CONTROL ROOM:
COMPUTER
CONTROL
DISPLAY
INSTRUMENTATION
DATA GATHERING**

Figure 5-4. Photograph of the Lost Circulation Test Facility Control Room

Figure 5-5. Steel Core Designed for the Lost Circulation Test Facility Baseline Tests

Figure 5-6. Test Vessel of the Lost Circulation Test Facility

Figure 5-7. Test Vessel Setup for Permeable/Fractured Core Investigations

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6. DIAGNOSTICS TECHNOLOGY

The central goal of Diagnostics Technology research is the development of high-temperature capabilities for well-logging equipment and instrumentation. This program element also includes research into areas aimed at increasing the capability to measure and evaluate downhole conditions. The tools and techniques developed as a part of this effort are basic support devices for the other program elements, yielding definition of the downhole environment for better system definition.

6.1 Wellbore Inertial Navigation System

Sandia National Laboratories

Technical Consultants: S. Kohler (505) 846-0421

R. Wardlaw (505) 846-1447

Project Objective

The objective of this project was to develop an experimental system capable of making an accurate positional survey of a wellbore.

Background

The project was begun early in FY 1981 in an effort to overcome the uncertainties in determining the true position of a wellbore. This need results from more demanding drilling criteria brought about by requirements to

1. Drill more wells with closer interspacing from single platforms,
2. Drill deeper, more highly deviated wells,
3. Intersect smaller target formations, and
4. Rapidly drill relief wells for well control.

The technology of the Roll-Stabilized Inertial Measurement System (RIMS), developed as part of Sandia's efforts in fielding inertial navigation systems for flight vehicles, was used in this project. An error analysis (see page 205) indicated that, with gyros and accelerometers of moderate accuracy as used in RIMS, survey errors of less than 3.1 m (10 ft) in a 3,048-m (10,000-ft) borehole could be obtained with the Wellbore Inertial Navigation (WIN) system. The WIN tool, although packaged differently from the RIMS, has an inertial measuring unit that is functionally identical, both electrically and mechanically.

During FY 1981, the design of the WIN tool was completed, and two systems were fabricated. Laboratory component and system testing was also conducted, including tool motion tests in a shallow, 36-meter (120-foot) hole and oven tests of the platform. System software was developed in

parallel with the hardware. A Kalman filtering formulation for estimating the inertial navigation system errors and updating the navigator was included in the system in order to obtain improved accuracy. As FY 1981 ended, checkout of the WIN system was nearing completion and minor problems with hardware and software were being corrected.⁹

FY 1982 Activities

During this reporting period, final design modifications and software changes were made, and field trials of the WIN system were successfully conducted. A technology transfer meeting was held to disclose the details of the WIN tool design and test results to interested industry representatives. Final reports on the WIN system design, development, and test program were prepared and published as References 47 and 48. A summary of these reports follows.

System Description -- The basic WIN system concept involves using a three-axis inertial navigation system that is stopped periodically during a wellbore survey to obtain measurements of velocity errors. These velocity errors are operated upon by a Kalman filter to provide estimates of all system errors including instrument errors. This inertial navigation system is mechanized as a small-diameter platform that uses a single gimbal to provide platform stabilization against rotation around the longitudinal axis. This platform and associated electronics represent the surveying tool. An associated uphole digital computer performs strapdown navigation computations and Kalman filter computations. The block diagram, Figure 6-1, depicts the major functional elements of the WIN tool.

● Mechanical Design

The WIN tool consists of seven major parts: a pressure barrel, a single-gimbal platform, a gyro servo and analog-to-frequency (A/F) electronics module, an input/output (I/O) electronics module, a power converter, a battery pack, and cabling. Figure 6-2 is a schematic of the tool, without the pressure barrel.

Figure 6-1. Wellbore Inertial Navigation System Functional Block Diagram

Figure 6-2. Schematic of WIN Tool

The pressure barrel assembly is 10.2 cm (4 in.) in outside diameter (OD), about 6.1 m (20 ft) long, and has a wall thickness of 0.64 cm (0.25 in.), Figure 6-3. Its pressure rating is 69 MPa (10,000 psi). The shock-nose assembly contains a large spring that absorbs some shocks that may be encountered during handling.

The platform contains two Incosym™ Model III-E, tuned-rotor gyros and three Sundstrand™ QA-1200 single-axis accelerometers (see Figure 6-4). The slip-ring, dc torque motor, ac resolver, and bearings are commercially

Figure 6-3. WIN System Pressure Barrel Assembly

Figure 6-4. WIN System Single-Gimbal Platform

available, off-the-shelf items. Two printed circuit boards contain gyro-heater-control and gyro-preamplifier circuitry. The dc torque motor provides torque in response to gyro output to counteract bearing friction and

keep the gimbal essentially motionless if the tool rotates about its longitudinal axis. The resolver measures the gimbal angle relative to the barrel.

The gyro-servo--A/F module, the I/O module, and the power converter consist of circuit boards attached to each side of an aluminum plate that provides structural support as well as heat sinking for high-power electric components (Figures 6-5, 6-6, and 6-7). The interconnect packages associated with the modules each contain two boards holding 153 wire-wrap, solder-pot pins (Figure 6-8). Pins are interconnected to other pins on the same board if entering signals are to exit through another connector, or to pins on the opposite board if the signal is required inside the module. The complete versatility and ease of rerouting signals are reasons for choosing such a scheme for a complex, experimental system like the WIN system.

The gyros are the only elements of the WIN system that have active temperature control. Heaters and a temperature sensor on each gyro, in conjunction with temperature control electronics, bring the gyros to a control temperature of 60°C (140°F) within 10 minutes after the electronics are turned on. Other temperature-sensitive components include the accelerometers and the A/F converters whose temperatures are monitored so that compensation may be provided in the computer software.

The electronics are generally off-the-shelf commercial items with an advertised operating temperature of 125°C (257°F). Since this limit may be easily exceeded in deep wells, insulation must be provided between the inner wall of the pressure barrel and the inside of the tool. Silicone rubber foam was selected because it is inexpensive, readily available, and can withstand more than 200°C (392°F). Much better but more expensive insulation, such as the vacuum bottle type, is available but was not considered necessary for project purposes.

The single-gimbal platform is suspended on vibration isolators that reduce transmission of high-frequency radial and axial vibrations into the sensor block. The isolators are cut from a sheet of silicone foam rubber

Figure 6-5. Gyro Servo-A/F Electronics Module

Figure 6-6. I/O Electronics Module

Figure 6-7. Power Converter Module

Figure 6-8. Interconnect Package

and are clamped between aluminum rings. In addition, the platform is suspended on a set of axial shock absorbers consisting of metal bellows. O-rings at each end of the assembly further dampen vibration through friction with the pressure barrel. When the assembly was subjected to radial and axial shocks of about 100g for 1 ms, the axial shock was attenuated by a factor of about 3, but the radial shock came through unattenuated.

Vibration tests revealed an axial natural frequency of about 80 Hz, with a magnification at resonance of less than 3 to 1. Radial natural frequency was about 130 Hz with a magnification at resonance of 7.5 to 1. Obtaining good isolation against shock and vibration in the radial direction is difficult because of the small amount of radial sway space available within the tool. However, the centralizers on the pressure barrel should provide good radial isolation when the tool is in the wellbore. It is during transportation and handling that radial shocks are the greatest danger because they could easily exceed the gyro's shock capability of 100g.

To measure angular resonance, an empty pressure barrel was suspended on a set of centralizers within a cased wellbore. The barrel was then forced into angular oscillation by applying and quickly removing a lateral force at one end. The system exhibited an angular oscillation natural frequency of about 9 Hz, with a damping factor of 25% of critical. Oscillation decayed in about 0.5 second, indicating that the duration of oscillations induced by passage of the centralizers over casing points is small, relative to the total survey time. In any case, however, a 32-Hz attitude calculation rate is used so that the 9-Hz oscillations can be tracked with adequate precision.

● Electrical Design

The five electrical modules of the WIN system are the single-gimbal platform, the power converter, the gyro servo-A/F converter module, the I/O module, and the battery (see Figure 6-9).

Two printed circuit boards are mounted on the platform gimbal. The heater board contains two gyro heater control circuits, and each gyro has

Figure 6-9. Schematic of WIN System Major Functional Relationships

an integral heater and temperature sensor. The output from the temperature sensor, along with a reference voltage, feeds into an amplifier, which generates an output that is proportional to the difference between the two values. This error signal feeds into a power stage that drives the heater. Gyro control temperature, presently set at about 60°C (140°F), is determined by the selected reference voltage. The gyro preamplifier board contains four preamps, one for each gyro pickoff, that is, two per gyro. The 54-kHz ac pickoff signals are also filtered on this board.

The power converter module contains two boards: the power supply board for producing dc and the inverter board for producing ac. The power supply board receives raw +28, -28, and +9.6-V power from the battery module and outputs +28, -28, +18, -18, and +5 Vdc. If the battery fails, no-go logic switches off the +28-V outputs, which disables the gimbal motor

drive and gyro torquer excitation and prevents the platform's gimbal motor from an uncontrolled spin-up that could damage the gyros. The inverter board contains a 3.45-MHz crystal oscillator and a set of frequency dividers that produce ac signals at 54 kHz for the gyro pickoffs, 844 Hz for resolver excitation, and three phases of 450 Hz for the gyro spin-motor drives.

The gimbal caging servo is also located on the inverter board. A mode selection signal from the CPU board in the I/O module may command any one of several gimbal control states: inertial (the gimbal is inertially stabilized, as when the WIN system is navigating), slow or fast slew (in either direction), and caged. In the caged mode, the gimbal-caging servo loop rotates the gimbal to a predetermined position. A cage error signal, consisting of a voltage that represents the difference between the commanded and actual gimbal position, is filtered, demodulated, integrated (with rate feedback from a gyro), and sent to the platform gimbal motor. The motor rotates the gimbal until the cage error signal is zero, that is, until the gimbal is at the commanded position. The gimbal resolver provides the gimbal's angular position measurement.

The gyro servo-A/F module contains the X-Y gyro servo board and the X_R -Z servo board, which are identical. It also contains the X-Y gyro A/F board, the X_R -Z gyro A/F board, and the X-Y accelerometer A/F board, all of which are identical. The I/O module contains an A/F clock and Z accelerometer A/F board, an A/F counter board, a resolver-to-digital (R/D) board, a CPU board, and a line driver board. The battery module contains 56 D-size, 1.2-V, 4-Ah, rechargeable nickel-cadmium cells. The +28 and -28-V supplies each consist of 24 cells connected in series and the +9.6-V supply contains the remaining 8 cells. Estimated average power drain from each supply is about 1 A, so the battery module should be able to provide power for 4 hours of tool operation.

The data for a serial-data stream (or word) containing approximately 125 bits is transmitted uphole 32 times per second, for a data rate of 4 kb/s. Each word contains 20 bits for each of three gyro channels (the redundant X_R gyro channel is not sent), 10 bits each for each of the three

accelerometer channels, 10 bits for resolver information, 10 bits for 1 or 2 of the 15 temperature channels, and 5 bits for spacing. Since one set of accelerometer or resolver data requires 20 bits, 2 cycles are needed to complete the transmission of data from these items. In summary, the gyro data-transmission rate is 32 Hz, accelerometer and gimbal resolver rate is 16 Hz, and complete temperature data are transmitted at 4 Hz. The gyro data rate is fastest because the attitude calculation rate is probably the limiting factor of navigation accuracy in a dynamic environment.

Dedicating 20 bits to each gyro or accelerometer channel is somewhat conservative because, at the full-scale A/F pulse rate of 65 kHz, only about 2,000 pulses (2^{10} bits) are accumulated in 1/32 second. Since the Universal Asynchronous Receiver-Transmitter (UART) operates with 8-bit data words (+2 bits for overhead), 20 bits in the data stream are required to send 10 bits of data. If more efficient use of space were required, the data could be packed more densely, at the cost of some increase in complexity of microcomputer software. A UART and line driver circuit similar to that in the I/O module is also used at the uphole end of the cable to send and receive signals.

The seven conductors of the cable are used for the following functions:

Channel 1 - data from uphole (uphole data)

2 - spare

3 - uphole common

4 - data from downhole (downhole data)

5 - downhole timing signal

6 - downhole common

7 - master power switch

Battery power will be shut off if the system overheats or a catastrophic malfunction occurs.

Field Trials -- In preparation for the WIN system field tests at the Nevada Test Site (NTS), a trial run of an empty pressure barrel in the test well was conducted in October 1981. The test verified that the test well

was unobstructed to the 2,225-m (7,300-ft) depth and that the pressure barrel seals were adequate. Tool speeds of 122 m/min (400 ft/min) were reached on the descent and 61 m/min (200 ft/min) on the ascent. The round trip required one hour. Also, navigation tests were conducted in the laboratory, and the tests indicated high system reliability. In approximately 100 hours of operation, only one component, a transistor switch, failed. Data were transmitted through 3,658 meters (12,000 feet) of seven-conductor logging cable without difficulty. Simulated surveys typically resulted in navigation errors of less than 0.3 meter (1 foot) per 305 meters (1,000 feet) of depth. However, good navigation performance was not obtained when the system was subjected to translational movement. This problem appeared to be related to accelerometer scale-factor errors that arise from accelerometer temperature variations and asymmetries in the A/F converters. Work was undertaken to eliminate this problem through software compensation of the temperature effects and by adding trimming resistors to the A/F electronics.

The WIN system hardware and the computer trailer were delivered to NTS in February 1982. Several pre-test calibrations were run to obtain data on inertial sensor parameters (see Table 6-1). To obtain short-term stability data, the system was calibrated two consecutive times on the same day without turning the system off. Long-term results in Table 6-1 are from calibrations that were 1 to 2 weeks apart.

Table 6-1

Inertial Sensor Stability

<u>Calibration Parameters</u>	<u>Short Term (4 Samples)</u>	<u>Long Term (2 Samples)</u>
Accelerometer bias	20 μ g	34 μ g
Gyro mass unbalance	0.04°/h/g	0.04°/h/g
Gyro drift	0.08°/h	0.23°/h
Gyro scale factor	18 ppm	27 ppm
Accelerometer scale factor	207 ppm	190 ppm

Except for the variability in the accelerometer scale factor, this range of values was considered adequate to meet project goals. The variability in the accelerometer scale factor was probably caused by lack of control of the platform temperature. Before each calibration, the system was run for several hours so the temperature would stabilize. However, a 1.0° to 2.0°C (1.8° to 3.6°F) swing in the temperature of the stable element was not unusual from one calibration to the next and could account for most of the variation in the scale factor. In contrast, stability of the gyro scale factor was excellent, probably because gyro temperatures were tightly controlled by individual control circuitry and a heater and temperature sensor on each gyro.

The first attempt at in-hole operation of the system was conducted in late February 1982. After calibration and insertion into a pressure barrel, the tool was attached to a wireline unit and hung vertically in the wellhead where an attempt at static navigation was unsuccessful. The potential causes of the static navigation failure included changes in sensor parameters since calibration, misalignment of the pressure barrel in the wellhead, and problems with the initial alignment estimation software.

The tool was run down the well without any attempt at navigation, and the output data stream was recorded and displayed uphole. In the first 1,524 meters (5,000 feet), reached in about 30 minutes, the quality of the data was good. The tool was stopped every minute for a period of one minute. The data outputs of the tool at these static times were stable and repeatable. During periods of descent, at about 110 m/min (360 ft/min), acceleration oscillations of about $\pm 0.1g$ and angular oscillations of 3 degrees in roll and less than 1 degree in pitch and yaw were observed. Also, fairly steady rolling of the tool occurred, amounting to one rotation every few minutes.

At a depth of about 1,524 meters (5,000 feet), occasional anomalies in the data stream were observed. These anomalies increased in frequency and severity so that, at the maximum depth of 2,134 meters (7,000 feet), little meaningful data were being obtained. After the tool was returned to the

surface and the power to the system had been turned off for several minutes, the apparent data transmission problems cleared up completely. The test was repeated two days later with similar results.

The failure of the system to navigate inside the wellbore was attributed to defects in the software that estimates initial alignments, to the unexpectedly large variations in vertical accelerometer scale factor, and to unreliable transmission of data up the wireline. During March 1982, changes were made in the software that corrected the defects in initial alignment. The variable scale-factor problem was circumvented by inserting the accelerometer scale factor instead of bias into the parameter-estimation software (Kalman filter). This enabled frequent updating of the scale factor, which is much more variable than bias, so as to accommodate wide variations. The data-transmission problem was eliminated by adding a capacitor and resistor to the line driver-board in the I/O module, which sharpened the rise times of the data pulses.

On 4-5 May 1982, attempts to navigate with the WIN system in the second in-hole field trials at the Nevada Test Site were successful. The test results demonstrated that the initial alignment estimation problem and the degradation of the quality of the data transmitted below 1,524 meters (5,000 feet) encountered in the first field trials had been corrected.

On 4 May, a WIN tool was calibrated and subjected to a static navigation test. Performance was excellent; errors in calculated displacement were less than 0.3 meter (1 foot) after 1 hour. On 5 May, a battery pack was installed, and a final check of accelerometer scale factors was performed. The tool was attached to the wireline cable and hung by the alignment fixture in the top of the hole. After several minutes for initial alignment estimation and a short static navigation test, the trip down the hole was begun. The descent rate averaged about 107 m/min (350 ft/min). Every minute the tool was stopped for one minute so that the Kalman filter update, which assumes zero velocity, could be performed. A depth of 2,134 meters (7,000 feet) was reached in about 40 minutes. The tool was brought back to the surface, again stopping for a minute after each minute of travel. The ascent rate was about 53 m/min (175 ft/min). During the

tests, recordings were made of the raw data as received over the wireline and of the calculated navigation results.

Navigation results for the north and east directions were excellent. Comparisons between displacement data from different trips and between data from the ascent and descent portions of the same trip indicated an average error of less than 0.9 meter (3 feet).

Navigation results for the vertical direction required adjustments between the first and second run. As noted above, the vertical accelerometer scale factor is adjusted by the Kalman filter during each 1-minute update period when the tool is stopped. Prior to the first run, the limits of allowable scale-factor adjustment were set too narrow. As the temperature of the tool increased going down the hole, the required scale-factor adjustment exceeded these limits, and errors in the calculated vertical displacement began to accumulate. Relative to a cable length measurement, this error was about 12 meters (40 feet) at the maximum depth of 2,134 meters (7,000 feet). Prior to the second run, the scale-factor adjustment limits were increased. The calculated vertical displacement on the second run showed very good agreement with the wireline measurements--within about 0.9 meter (3 feet) at the total depth of 2,134 meters (7,000 feet).

During each survey, the tool rolled counterclockwise during descent and clockwise during ascent--25 revolutions during the ascent portion of the second survey alone (see Figure 6-10). Tool and centralizer asymmetries plus twists in the cable were possible causes for the rolling. The maximum roll rate was $\sim 160^\circ/\text{s}$. Although conventional strap-down gyroscopes can function through such rates, the single gimbal mechanization allows relaxed accuracy requirement for the gyro scale factor and high roll-rate capability, and is, therefore, preferred for this application. At higher survey rates of 305 m/min (1,000 ft/min), roll rates could exceed the capability of the conventional strap-down gyros.

Although stabilized in roll, the platform gimbal experienced momentary roll rates of up to $15^\circ/\text{s}$. Maximum gimbal roll angle was $\sim 1.0^\circ$. Pitch and yaw angular oscillations tended to be very small, $< 0.1^\circ$, at frequencies of

Figure 6-10. Tool Roll History

a few hertz. These frequencies are high enough, however, to affect selection of computer calculation rates. Maximum rates observed were $<6.0^{\circ}/s$.

Linear accelerations of the tool were well under 1g. Oscillations at frequencies of several hertz were observed. The low amplitudes demonstrate that the centralizers are effective in isolating the tool from shock and vibration.

Figure 6-11 plots platform, gyro, pressure-barrel air, and wellbore temperatures against time for the second survey. As expected, gyro temperature control is lost when the platform temperature approaches the gyro control temperature.

Figure 6-12 shows platform temperature, vertical accelerometer A/F electronics temperature, and the accelerometer scale factor plotted against time for the second survey. The correlation among curves indicates a dependence of scale factor on A/F electronics temperature. Since the accelerometers are temperature-sensitive, scale factor was expected to

Figure 6-11. Temperature Data Versus Time

Figure 6-12. Platform, A/F Electronics Temperature and X Accelerometer Scale Factor Versus Time

depend on platform temperature also. However, a linear relationship could not be discovered between the two temperatures and the scale factor, perhaps because of time shifts in the data that resulted from the physical separation between temperature sensors and the items of interest.

Before the tests, an accelerometer scale factor sensitivity to platform temperature changes of 150 ppm/°C and to A/F temperature changes of <10 ppm/°C was estimated. This estimate was obtained from temperature tests of A/F electronics of identical design that were part of a RIMS flight vehicle instrumentation system. When tested alone after the tests, the WIN system's A/F electronics exhibited a temperature coefficient of 42 ppm/°C. The reasons for the higher temperature sensitivity of the WIN system A/F electronics have not been determined.

Conclusions — The major conclusions resulting from the analysis of the WIN system test data were as follows:

1. The feasibility of the system concept was demonstrated with sufficient success to expect continued development by industry.
2. Using the single-gimbal navigator and updating it with Kalman filtering is a means of obtaining accurate directional surveys in a borehole. The gimbal is needed to isolate the gyros from large angular excursions and rates of the tool about its longitudinal axis. Error analysis predicted that in deviated wells the gimbal would also be useful in reducing the effects of gyro bias changes. With the gimbal, a gyro input angular rate range of $\pm 25^\circ/\text{s}$ is adequate.
3. Active temperature control of the entire stable element should be used to increase accelerometer bias and scale-factor stability. The sensitivity of the A/F electronics to temperature should also be reduced.
4. Computer temperature modeling should be performed to account for sensor errors when the gimbal control temperature is exceeded.
5. More computer capability would permit more frequent Kalman updates as well as the addition of more states to the system model. Inclusion of such states would permit models of presently defined error sources and the addition of other measurements.
6. The tool centralizers provided good shock and vibration protection in a cased wellbore. Hence, internal radial sway space in the platform

could be eliminated, either to reduce tool diameter or to make room for additional insulation.

7. The diameter of the tool could be reduced by using smaller gyros, and the tool length could be reduced by eliminating most of the battery pack by using a surface power source.
8. Better insulation and various heat-sink compounds could be used to increase the time that the tool can operate in hot formations. The temperature range of the system components could also be increased.
9. For uncased hole use, temperature capabilities should be improved and shock and vibration vulnerability reduced.
10. The data transmission rate should be increased to allow for more frequent navigation calculations. This increase could be accomplished by reformatting the data sent uphole. (The 4,096 bit/s rate is probably near the maximum rate that can be transmitted up a standard cable.)
11. Downhole navigation calculations should be considered in advanced designs of the system.

Error Analysis -- The decision to build and test a WIN system was based on favorable results of an error analysis.⁴⁹ This study assumed that the navigation system was mechanized as a single-gimbal platform in which the gimbal axis of rotation is parallel to the survey tool's longitudinal axis. This gimbal, containing the gyros and accelerometers, is gyro-stabilized so that the angular rate of the gimbal about its axis is essentially zero. An advantage of this mechanization is that it isolates the gyros from tool roll rates that might exceed the rate measurement range of the gyros. The system state vector of the Kalman filter formulation used in the analysis contains 16 elements. These elements are "errors" in the following system parameters: displacements and velocities in three directions, attitude angles (pitch, azimuth, and roll), three gyro biases, three accelerometer biases, and the vertical accelerometer scale factor. The standard deviations (σ 's) associated with sensor errors were assumed to grow as the square root of time (random walk). Typical σ 's and growth rates are given in Table 6-2.

Table 6-2

Typical Sensor Error Standard Deviations

Parameter	Initial Value	Random Walk
Accelerometer bias	50 μg	25 $\mu\text{g}/\text{h}^{1/2}$
Gyro bias	0.25°/h	0.1°/h/h ^{1/2}
Vertical accelerometer scale factor	200 ppm	200 ppm/h ^{1/2}

After each minute of tool travel during a simulated survey, the tool was stopped, and velocity errors were obtained and used by the Kalman filter in the estimation of the 16 states (system errors). The variance (σ^2) associated with the noise in the velocity error measurement was assigned a value of $10^{-4} (\text{ft/s})^2$. This value includes the effects of accelerometer quantization noise and of the residual motion present when the system is assumed to be at rest.

For purposes of error analysis, it is sufficient to calculate the system state covariance matrix as a function of time. The diagonal elements of this matrix are the variances associated with the errors in the estimates of the system states. Figure 6-13 is a plot of predicted north position error standard deviation versus time for a 3,048 m (10,000 ft) vertical wellbore.

The simulated trip downhole begins at $t = 900$ s after initial alignment estimation by the system. The sawtooth pattern, Figure 6-13, results from the periodic application of the Kalman filter error-reduction procedure. Results of a sensitivity analysis for various multiples of nominal values of system errors are given for a vertical wellbore in Table 6-3.

As indicated in Table 6-3, the position errors are insensitive to gyro and accelerometer bias variations. Variations in the vertical accelerometer scale factor and the time between updates affect depth error but have little effect on horizontal errors. Position error in all directions is

Figure 6-13. North Error Versus Time

Table 6-3

Sensitivity Results (Vertical Wellbore)

Error	Displacement Errors (σ), m (ft)		
	East	North	Depth
Nominal	0.9 (3)	0.9 (3)	0.9 (3)
10 x gyro, accelerometer bias	0.9 (3)	0.9 (3)	0.9 (3)
2 x time between updates (nominal = 1 min)	1.2 (4)	1.2 (4)	2.1 (7)
10 x vertical accelerometer scale-factor noise	0.9 (3)	0.9 (3)	4.3 (14)
5 x velocity error measurement noise	4.3 (14)	4.3 (14)	4.3 (14)
39 state filter formulation	0.9 (3)	0.9 (3)	0.9 (3)

strongly dependent on velocity error measurement noise. To verify that a 16-state formulation adequately predicts system accuracy, 23 other states (gyro and accelerometer misalignments and scale factors, and gyro g-sensitivities) were added to the formulation. This addition did not change the results.

A similar analysis was performed for a deviated wellbore having an east deviation of 1,829 m (6,000 ft) at a total depth of 2,591 m (8,500 ft). As might be expected, the results indicated a strong dependence of horizontal position error on azimuth errors, which arise mainly from variation in azimuth gyro bias. The simulations began with an initial alignment procedure that included a two-position gyro compass heading estimation. Also studied were cases in which the single gimbal platform was rotated 90° every 15 min (while the tool was stopped for Kalman update). The results are summarized in Table 6-4. The gimbal rotations reduced the error by a factor of ~2.

Table 6-4

Sensitivity Results (Deviated Wellbore)

Gyro Bias Random Walk, $(^{\circ}/h/h^{1/2})$	North Position Error, σ m (ft)	North Position Error With Periodic 90° Gimbal Rotation, σ m (ft)
0.1	2.4 (8)	1.5 (5)
0.3	5.5 (18)	2.7 (9)
1.0	15.2 (50)	6.1 (20)

The error analysis results indicated that a Kalman-aided, single-gimbal inertial navigation system was a viable concept for accurate wellbore surveying. In addition to isolating the gyros from tool-roll motions, the gimbal provided the means for reducing the effect of gyro bias changes on horizontal position errors.

6.2 Upgraded Acoustic Borehole Televiwer

Sandia National Laboratories

Technical Consultants: F. Heard (505) 844-6230

T. Bauman (505) 844-8885

Project Objective

The objective of this project was to upgrade the Acoustic Borehole Televiwer (ABT) for operation in temperatures as high as 275°C (527°F) and at pressures up to 34.5 MPa (5,000 psi).

Background

Because of the importance of fracture characterization in geothermal logging, the Sandia project to upgrade the acoustic borehole televiwer for geothermal use was begun in FY 1979. These early efforts drew upon the field test experience of the USGS for problem definition and on the tool manufacturer, Simplec Manufacturing, Dallas, Texas, for fabrication techniques. Sandia's efforts were directed at modification of the tool electronics and acoustic window for reliable operation at high temperatures, and Westinghouse R&D Center investigated improved materials for the acoustic sensor portion of the tool in order to increase the tolerance of the instrument to the hot, corrosive geothermal environment. The Westinghouse effort was completed in FY 1979, identifying several promising materials for use as damping materials, lead attachment and bonding, the coupling fluid, and acoustic window alternatives. The results of the Westinghouse work were reported in Reference 50.

The initial phase of this project was completed in FY 1980, and the results of the work were published in Reference 51. The primary upgrade accomplishments during this part of the project were the development of a high-temperature acoustic transducer, an improved acoustic window, more reliable electronics, and the elimination of troublesome slip rings. By the end of FY 1980, the upgrade project was ready to proceed into final component and subassembly laboratory testing and into fabrication of the modified, composite tool for laboratory and field testing.

During FY 1981, extensive laboratory tests of the upgraded televiewer components and subassemblies were conducted at design temperature and pressure limits to determine any weaknesses and to verify the reliability of the upgrade modifications. Preparations were also made for full-scale, laboratory testing of the composite tool in a large water-filled autoclave. In addition, two field tests of the modified tool were conducted in geothermal wells, with the second test resulting in several successful runs in an open-hole section after correction of an o-ring seal problem that was encountered in the first field test. The results of the FY 1981 upgrade efforts were reported in References 9 and 52.

FY 1982 Activities

The acoustic borehole televiewer upgrade project was completed during this reporting period, and a final report of the work was prepared for publication as Reference 53. The final upgrade efforts consisted of full-scale laboratory and field tests of the modified tool. The results of these tests are summarized below. A schematic of the acoustic borehole televiewer system is shown in Figure 6-14, and cutaway views of the instrument are depicted in Figure 6-15.

Laboratory Tests -- Early in FY 1982, the high-temperature acoustic borehole televiewer was successfully tested in an autoclave at Wyle Laboratories, El Segundo, California, under controlled temperature--280°C (536°F)--and pressure--31 MPa (4,500 psi)--conditions. A test target ring was placed in the autoclave with the tool to evaluate the tool's operation. The televiewer ran for 7 hours with no loss in sensitivity or resolution.

A schematic of the high-pressure/high-temperature autoclave is depicted in Figure 6-16. In order to simplify the test vessel design, the cablehead and centralizers were not used with the televiewer during the test. The top end of the electronics barrel was attached to the top flange of the vessel through an adapter assembly having a sealing configuration similar to that used in the centralizer assembly. The wires from the surface panel were fed through the upper sealing flange and adapter assembly into the electronics barrel. The tool was attached to the top flange so that the axis of the tool was normal to the flange surface. Since the

Figure 6-14. Schematic of Acoustic Borehole TelevIEWer

A. BELLOWS
B. JACKSHAFT
C. ACOUSTIC WINDOW
D. MAGNETOMETER
E. ROTARY TRANSFORMERS

F. MARK DISK
G. MAGNETIC PICKUP
H. GEAR BOX
I. MOTOR
J. BULKHEAD FEED-THROUGH
K. BELLOWS PROTECTIVE HOUSING

L. TRANSDUCER
M. DRIVE SLEEVE
N. JACKSHAFT CHAMBER
O. BULKHEAD
P. MOTOR CHAMBER
Q. LOWER CENTRALIZER
R. ACOUSTIC SENSOR HOUSING
S. ELECTRONICS BARREL
T. UPPER CENTRALIZER
U. DEWAR STOPPER
V. EUTECTIC HEAT SINK
W. DEWAR
X. ELECTRONICS PRESSURE HOUSING

Figure 6-15. Acoustic Borehole TelevIEWER (Center) with Cutaway View of Rotary Mechanism (Top) and Electronics Assembly (Bottom)

Figure 6-16. Schematic of High-Pressure/High-Temperature Autoclave

lower flange was normal to the autoclave wall, sealing the autoclave with the flange surfaces parallel was expected to center the tool. However, due to the irregularities in the sealing ring and the very high torque on the bolts needed for a good seal, the flange faces could not be made parallel. This condition caused very poor centering of the tool in the autoclave, especially near the bottom where the transducer was located.

The target was a 6-inch-long stainless steel ring with its OD in contact with the ID of the autoclave wall in the vicinity of the acoustic window. Figure 6-17 is a diagram of the target. The target contained 7

TARGET I.D. = 19.4 cm (7.625 in.) ALL GROOVES = 60°V

TARGET No.	TARGET WIDTH mm (in.)	GROOVE SPACING (CENTER TO CENTER) mm (in.)
1	6.4 (0.350)	NA
2	0.8 (0.031)	NA
3	1.6 (0.062)	NA
4	1.6 (0.062)	3.2 (0.125)
5	3.2 (0.125)	6.4 (0.150)
6	4.7 (0.187)	9.5 (0.375)
7	6.4 (0.250)	12.7 (0.500)

Figure 6-17. Laboratory Autoclave Test Target

groups of notches of varying widths. Notch groups 4 through 7 each had three notches spaced 3.2, 6.4, 9.5, and 12.7 mm (0.125, 0.250, 0.375, and 0.500 in.), respectively for the purpose of resolution determination.

Since the tool was not centered with respect to the target ring, the amplitude of the signal reflected from the target ring and intercepted by the transducer varied with the angular position of the transducer. Figure 6-18 illustrates how the amplitude of the signal received by the transducer

(a)

(b)

Figure 6-18. Reflected Amplitude Waveform for One Complete Rotation. (a) at 38°C (100°F) and (b) at 280°C (536°F). Numbers on photographs locate target notch reflections

varied over a complete revolution in this test. If the tool had been properly centered, the waveform would have been flat across the top with dips in amplitude only where the notches occur. Since the tool is not centered, the amplitude of the reflected pulses appears to be amplitude modulated by a sinusoid of twice the rotation frequency.

Figure 6-18 also shows that, in areas where background reflected energy was high, the notches tended to scatter energy away from the transducer causing a reduction in amplitude, as is normally the case. However, in areas where background reflected energy was low, the notches scattered energy in the direction of the transducer, causing an increase in amplitude. Notch groups 1, 2, and 7 occur in areas with high background reflectivity. Notch groups 3, 4, 5, and 6 occur in areas with low background reflectivity. There appeared to be very little change in sensitivity and resolution of televiewer over the temperature range. In both photographs in Figure 6-18, taken at temperatures of 38° and 280°C, (100° and 536°F), the 0.8 mm (0.031 in.) wide notch can be picked out, and the notches with the 6.4 (0.250 in.) spacing can be resolved.

The laboratory test lasted about 7 hours with three hours used in coming up to the test temperature of 280°C (536°F) and four hours at temperature. The test was terminated at the end of the seven hours when a Viton O-ring seal failed, letting moisture enter the tool and thus shorting out electrical signals.

Field Tests -- Grooving of the casing in the Los Alamos Fenton Hill EE-2 well was indicated by a Dialog 64-arm caliper log. The grooves were detected in two zones in the well: at depths of 201 to 213 meters (660 to 700 feet) and at 1,981 meters (6,500 feet). This damage raised concerns about the capability of the casing to withstand high-pressure fracturing operations in the well. Since the Dialog caliper measures only the precise depth of grooves, additional information as to width, length, and orientation of the grooves was required in order to predict the pressure limits of the damaged casing.

On 15 January 1982, the televiwer was used to log the two damaged zones in the well. Previous temperature logs showed that the downhole temperatures, which ranged from 150° to 180°C (302° to 356°F) in the lower damage zone, were well within the demonstrated temperature capability of the televiwer. The televiwer data were used to generate both caliper and reflectivity mode logs. The resulting logs showed two parallel vertical grooves in the upper damage zone running from about 198 to 210 meters (650 to 690 feet). A 4.6-m (15-ft) section of the log of this zone is shown in Figure 6-19. The grooves were as wide as 6.4 cm (2.5 in.) and were as deep as 3.8 mm (0.15 in.) in some places. In the lower damage zone, a 1.5-meter (5-foot) vertical groove was found with maximum dimensions similar to those of the upper zone. Figure 6-20 shows this groove running from 1,981 to 1,983 m (6,500 to 6,505 ft). The televiwer logs clearly showed the vertical and circumferential dimensions of the damage to the casing. In addition, a slight ovaling of the casing cross section was detected in the lower damage zone from 1,966 to 1,986 meters (6,450 to 6,515 feet). The televiwer data, coupled with the precision caliper data of the Dialog tool, provided vital information for determining the pressure limits of the casing. The experiment proved the acoustic borehole televiwer to be a valuable tool for casing inspection as well as for fracture characterization in geothermal wells.

Conclusions — The major conclusions resulting from the work to improve the Acoustic Borehole Televiwer for geothermal operations were as follows:

1. The upgraded Acoustic Borehole Televiwer has demonstrated the capability to operate at temperatures up to 280°C (536°F) and at pressures up to 31 MPa (4,500 psi) for 7 hours. New O-ring seals, fabricated from a L'Garde EPDM high-temperature compound, should extend the downhole operational life of the tool.
2. Field tests of the upgraded televiwer proved the instrument is a valuable tool for casing inspection as well as for fracture characterization in geothermal wells.
3. Technology developed in this project is being used in the design of new commercial televiwers by industry (Simplec Manufacturing Company).

Figure 6-19. Borehole Televiewer Logs of Los Alamos Fenton Hill EE-2 Well Casing

N W S E N

REFLECTIVITY

1,981 m
(6500 ft)

1,983 m
(6,505 ft)

N W S E N

CALIPER

Figure 6-20. Borehole Televiewer Logs of Los Alamos Fenton Hill EE-2 Well Casing

6.3 Acoustic Cement Bond Diagnostics

Sandia National Laboratories

Technical Consultant: Hsi-Tien Chang (505) 844-7588

Project Objective

The objective of this project is to develop high-temperature technologies for use in the design of an acoustic cement bond logging instrument for operation at temperatures up to 250°C (482°F).

Background

There are currently no commercially available high-temperature cement bond and casing integrity logging systems for geothermal wells with maximum temperatures in excess of 250°C (482°F). Most commercial logging tools, developed for the oil and gas industry, become unreliable above 150°C (302°F) in actual field tests. As a consequence, geothermal well operators must cool the well in order to run surveys. The thermal shock from cooling could cause well damage to both casing and cement. Also, loss in production time due to cooling prior to logging is expensive. Hence, a distinct need exists for development of high-temperature logging capabilities to support the timely growth of geothermal energy. In response to this need, Sandia began an upgrade research program in late FY 1980, focusing on improvement of the temperature capabilities of the acoustic cement bond logging tool. The program description and development approach were described in Reference 54.

Work to develop high-temperature circuits suitable for use in the design of an upgraded, geothermal acoustic cement bond logging instrument continued during FY 1981 and was reported in References 9 and 55. The FY 1981 efforts included successful operation for approximately 150,000 cycles at 10 pps of a redesigned Spryton tube. The tube used bismuth for the anode and cathode in a new trigger circuit design. Evaluations of various commercial electronic components to determine maximum operating temperatures were also conducted. The design of a 1:15 pulse transformer using

Silectron and magnet wire was completed by the end of FY 1981, and experiments were continuing to evaluate electronic components for use in high-temperature circuit design.

FY 1982 Activities

Design efforts on the acoustic cement bond logging instrument continued throughout this reporting period. Early in FY 1982, improved circuit design and component selection resulted in the temperature-caused variation in the output voltage and current of the transmitter system being reduced from 30% to 10%. A 10% variation in the exciting current/voltage will not cause any fluctuation in the acoustic signal output since the transducer will be operated beyond the magnetic saturation region.

The receiver circuit design was begun early in FY 1982 and completed in January 1982. The key element in the receiver is a Harris Semiconductor 2600-1 operational amplifier that works satisfactorily at temperatures up to 250°C (482°F). Although the amplifier's slew rate at 250°C (482°F) is only about one-half of the rate at room temperature, the amplifier could still provide an adequate amplification function for the required acoustic signal.

In order to test the performance of the complete system, the transmitter and the receiver were separated by 3 feet in deionized water, and the excitation current through the transmitter and the amplified signal from the receiver were recorded. The acoustic return from water in the performance tests was readily observed (see Figure 6-21). The noise in the initial 100 μ s was considered to be due to the electromagnetic (EM) interferences from the transmitter circuit. Such noise should be significantly reduced when the circuits are located inside the steel housings typical of logging instruments. The signal-to-noise ratio appeared to be quite good in the tests, even with EM interferences. The received signal was also considered large enough to be observable on uphole instruments.

The circuit designs for both the transmitter and the receiver were completed, and the circuits performed satisfactorily when tested in an oven at temperatures up to 250°C (482°F) for 2 hours. After the successful test

Figure 6-21. Acoustic Return From Water in the Transmitter/Receiver Performance Tests. (a) Direct Receiver Output, (b) After Receiver Amplifier Output

of the breadboard circuit, the layout for a printed circuit board design was completed. In order to meet the high-temperature requirement, DuPont's Pyralin was selected as the printed circuit board material. Nickel-clad laminates were used and were gold plated because copper plating oxidizes at high temperature. A Kester Company alloy of 97.5% lead, 1.5% silver, and 1.0% tin with a melting point of 310°C (590°F) was used as the solder for the printed circuit board. Sandia's prototype long-life Spryton tube and over-voltage gap, together with other high-temperature components, were included in the fabrication of the printed circuit board assembly.

The receiver circuit was also modified in the printed circuit board design in order to eliminate the 60-cycle power signal and increase the driving energy to the uphole recorder. The output from the receiver circuit ranges from 3 to 21 volts peak-to-peak and is reduced to 5 to 35 mV after passing through a cable simulator. This signal level is considered adequate for uphole detection of cement bonding conditions ranging from 100% bond to a free pipe. If the noise level becomes too high, a line driver can be added to the receiver circuit.

In order to evaluate the performance of the high-temperature circuit design in a logging instrument, the transmitter circuit was connected to the acoustic transmitter of a cement bond tool used in the oil and gas industry, and the tool's receiver output was measured at room temperature. Using Sandia's circuit, the output was about twice the amplitude of the existing tool. The reason for such a high acoustic output is probably the fast breakdown, quick recovery time, and low breakdown voltage of the Sandia Spryton tube in comparison to the SCR used in the industry instrument.

With the electronics design basically complete, the major tasks remaining in the prototype development of a geothermal cement bond tool were: selection of transmitter and receiver transducers, design of acoustic isolator system, and design of a high-temperature mechanical housing. Hence, design of a magnetostrictive transmitter and a transducer housing for a geothermal cement bond tool was begun in May 1982. The performance of the transmitter and receiver of available industry tools was analyzed,

and a mismatch between the transmitter and receiver frequencies was found in the two sets of tools selected for evaluation. It appears that the transducers used in these industry tools cannot be easily adapted for geothermal application; thus, it was considered appropriate to start with a new design. Four magnetostrictive cores with mid-frequency of oscillation ranging from 28 to 40 kHz were selected for further experimental testing.

The transducer housing design includes a metal bellows that is capable of withstanding high temperatures and that will provide space for expansion of the transducer fluid. Several "off-the-shelf" bellows were ordered for evaluations, and Mobil jet oil was selected as the transducer fluid.

The long-life Spryton tube development was initiated concurrently with the CBL instrument development in order to reduce the time span of the program. By May 1982, this switching tube had been run successfully for many hours at 10 pps repetition rate, 0.1 μ f input capacitance, and more than 1 kV breakdown voltage for the over-voltage gap. In order to overcome the difficulty of high-voltage wireline transmission, a downhole voltage multiplier and a transformer were designed to provide high trigger voltage for the Spryton tube. Although the transformer increases voltage, it concurrently reduces the current delivered. It was also discovered that the CBL instrument circuit design could only sustain 8,200 pf (instead of 0.1 μ f previously used) input capacitance in order to provide the required 10 pps repetition rate. Under these limitations, the input peak trigger current was reduced below 2 A, which was insufficient to trigger the Spryton tube consistently. In order to overcome this problem, effort was concentrated on improving the performance of the over-voltage gap and the transformer. After many trials, the peak trigger current was increased to 8.3 A, which is sufficient to trigger the Spryton tube continuously for several hours.

At the close of this reporting period, work was continuing on the design of the transmitter and receiver transducers, acoustic isolator system, and mechanical housing for the CBL instrument.

6.4 Geopressured-Geothermal Reservoir Diagnostics

Sandia National Laboratories

Technical Consultant: Hsi-Tien Chang (505) 844-7588

Project Objective

The objective of this project is to develop a high-resolution pressure transducer, 103 to 138 MPa (15,000 to 20,000 psi), together with associated high-temperature circuitry, 200° to 225°C (293° to 437°F), for use in geopressured-geothermal reservoirs.

Background

High-temperature and high-pressure reservoir diagnostics techniques are needed to assess geopressured resources of methane and hot water. Because of the lack of instruments with high temperature/pressure ratings, the accuracy of resource estimates of a geopressured-geothermal reservoir using various currently available techniques can vary by a factor of up to 10. For this reason, a project was begun in mid-FY 1980 to investigate circuit modifications that could be readily implemented into the high-pressure gauge of existing equipment to extend the temperature capability.

Early efforts included the use of W. P. C. Worth temperature tools in tandem with a Hewlett Packard pressure gage in laboratory experiments with circuits redesigned for 200°C (392°F) operation. This work continued into FY 1981 with a new selection circuit (a circuit that selects between temperature and pressure signals) surviving 200°C (392°F) oven tests. However, both the pressure tool and the temperature sensing circuitry failed since the oven tests were above their specified operating temperatures.

The Worth temperature tool was further modified for high temperature use and tested at 200°C (392°F). The Worth tool qualified at this temperature in the laboratory, but it failed to function in an attempted field test in a well near Zapata, Texas. A new chopper-stabilized amplifier for use with a strain gage transducer was designed and successfully tested in

the laboratory. Before these efforts could be completed, work was suspended pending redirection of the project in response to forecast reductions in FY 1982 funding.

FY 1982 Activities

A redirected project continued during this reporting period, focusing on longer-term, higher-risk development at a lower level of effort than that of the original short-term, hardware improvement work. Investigations were concentrated on designing a bottom-hole pressure gauge with a transducer having a pressure rating between 103 and 138 MPa (15,000 and 20,000 psi) and a temperature rating between 200° and 225°C (392° and 437°F). In order to reduce reservoir test time and minimize lost production, a resolution of 690 to 69 Pa (0.1 to 0.01 psi) was set as the design goal for the device. The circuitry will consist of high-temperature voltage regulators, a chopper amplifier, and a 12-bit A/D convertor.

Bottom-hole pressure gauges currently available and under development are being evaluated in order to identify the best system for geopressure application. In most pressure transducers, a force-summing element converts pressure into a physical displacement. Because of the high resolution requirements, such a device ideally should have minimum mechanical movement, and the element should have very low mass. Thus, conventional mechanical or electro-mechanical devices, such as the Bourdon tube potentiometer, were not considered appropriate for this application. Some electrical devices, such as capacitor transducers or vibration wire transducers, are very sensitive to temperature change and were ruled out. Quartz resonators, on the other hand, are very difficult to temperature compensate. The most promising devices appeared to be the strain gage transducer and the eddy-current transducer. The initial investigation identified the 4 transducers listed in Table 6-5 for laboratory comparison tests. Due to budget limitations, a less expensive Teledyne-Taber transducer, Model 2201, was subsequently substituted for the Kaman transducer. The pressure rating for the Teledyne-Taber transducer is 138 MPa (20,000 psi), and its compensated temperature rating is in the 204°C (399°F) range. At the close of this reporting period, laboratory comparison tests for the selected transducers were scheduled for completion by February 1983.

Table 6-5

Transducers Selected for Laboratory Comparison Tests

<u>Manufacturer</u>	<u>Pressure Rating MPa (psi)</u>	<u>Upper Boundary, Compensated Temperature Range, °C (°F)</u>
CEC (Bell and Howell)	138 (20,000)	239 (446)
Kaman	103 (15,000)	540 (1004)
Kulite	138 (20,000)	232 (450)
Sensotec	103 (15,000)	316 (601)

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7. PERMEABILITY ENHANCEMENT

The goal of permeability enhancement is to develop the technology and methodology required to increase the production rates or injection rates of geothermal wells. Work under this program will include definition and evaluation of existing reservoir production/injection zones to determine the nature of the formation permeability, quantification of the geometry of fractures intersecting the wellbore as well as their extent, definition of performance requirements and identification of research needs for enhancing the permeability of geothermal wells, and development of the specific technology to meet these performance requirements and needs.

7.1 Geothermal Stimulation Analysis

Sandia National Laboratories

Technical Consultants: J. Finger (505) 844-8089

D. Glowka (505) 844-3601

D. V. Swenson (505) 844-9398

Project Objective

The objective of this project is to investigate the feasibility of stimulating geothermal wells using a tailored-pulse gas-fracture concept.

Background

Production or injection rates of existing wells can usually be increased by enhancing the permeability of the production/injection zone. In the case of oil and gas wells, this enhancement is accomplished by increasing the near-wellbore permeability, using such techniques as hydraulic fracturing, gas or foam fracturing, and explosive fracturing. However, in geothermal wells, much higher production/injection rates require that this increase come from establishing fractures that connect the wellbore to existing reservoir fracture systems. The differences between geothermal reservoirs and oil and gas reservoirs preclude the use of much of the technology developed for the oil and gas industry, and due to the small size of the geothermal industry (0.1% of energy production), there are currently no economic incentives for industry involvement in the necessary research.

In the case of production, the need for enhanced permeability has been recently evidenced by the experience in the Valles Caldera in New Mexico. Several commercially productive wells were drilled adjacent to several "dry holes." Apparently, the nonproductive "dry holes" do not intersect the existing reservoir fracture systems, and previous attempts have not been successful in creating connecting fractures. In order for geothermal areas like the Valles to be viable energy resources, a reliable technique

for enhancing permeability is needed. The mechanics of fractured production are not adequately understood in order to design the enhancement methodology for geothermal wells with any confidence of success.

Enhancement techniques will also be needed in injection wells in order to allow for disposal of the geothermal brine once the heat energy has been removed. ReInjection is needed to assure that the reservoirs are not depleted of fluids by the production process, to prevent subsidence of the surrounding terrain, and to avoid surface environmental damage from the produced fluids. Since reinjection wells must accept comparable or larger flow rates than production wells, they must also intersect existing reservoir fracture systems.

Recent field tests performed as part of the DOE stimulation program have met with only limited success. Thus, there is an apparent need for additional research aimed at understanding the previous tests and developing the technology required to successfully enhance the permeability of geothermal wells.

FY 1982 Activities

Work was begun during this reporting period on the geothermal permeability enhancement program with a preliminary investigation into the feasibility of stimulating geothermal wells using the tailored-pulse gas-fracture concept. A summary of these efforts follows.

Methodology--Previous experiments have been performed at the Nevada Test Site (NTS) to develop tailored-pulse fracturing technology for stimulation of natural gas wells.⁵⁶⁻⁶⁰ The technique seeks to form multiple radial fractures by using propellants rather than explosives to load the wellbore. Because propellants can be obtained with a wide range of burn rates, it is possible to tailor the loading rate to the desired value. The NTS experiments featured measurements of the borehole pressure pulse and posttest mineback to visually observe fractures. The tests showed:

1. Three fracture regimes: (a) hydraulic fracture for slow loading rates, (b) multiple fracturing for intermediate loading rates, and

(c) borehole crushing, with inhibited crack growth, for rapid loading rates. Tailoring of the pressure amplitude and rise time is important when the principal stresses in the horizontal plane are significantly different. Slow pressurization will then result in fractures in the "natural" direction, normal to the minimum compressive stress and usually parallel to existing reservoir fractures. As the borehole pressure reaches a value that will start a fracture normal to the minimum compressive stress, the borehole fluid will fill that fracture and begin to propagate a compressive wave around the borehole circumference. Since that compressive stress will suppress fractures in other directions, the pressure pulse rise time must be tailored to reach a level that will start fractures normal to higher compressive stresses before the original compressive wave reaches those locations where the new fractures will originate. If these new fractures can be propagated, then they are much more likely to intersect the existing reservoir fractures.

2. A borehole size dependence, where faster loading rates are required to cause multiple fractures in small boreholes.
3. Major fractures typically oriented normal to the smallest in-situ stress.

Finite-element analyses of the gas-fracture experiments have correlated well with available data and have captured the essential features of the dynamic fracture behavior.⁶¹ Since the process for geothermal stimulation is similar to stimulating gas wells, the same analysis methods were used in this investigation.

Based on information provided by the Drilling Research Laboratory,⁶² The Geysers and Baca geothermal sites were chosen as representing the range of geothermal rock properties. The Geysers site well depth is approximately 1,829 (6,000 ft) and is drilled in strong rock (Graywacke) that has an unconfined compressive strength of approximately 255 MPa (37,000 psi). The Baca wells are approximately the same depth, 1,524 m (5,000 ft), but

are drilled in low-strength rock (Tuff) with an unconfined compressive strength of 33 MPa (4,750 psi).

The in-situ stresses assumed for The Geysers site were an overburden stress of -45 MPa (-6,500 psi) and in-plane stresses of -41 and -48 MPa (-6,000 and -7,000 psi). The overburden stress was calculated using the rock density and well depth. The smallest in-plane stress, -41 MPa (-6,000 psi), was justified based on hydraulic fracture data.⁶³ The stress difference was chosen as being reasonable, but no data is available to justify the actual value. For Baca, the in-situ stresses used were -31 MPa (-4,500 psi) overburden (calculated), with in-plane stresses of -28 and -35 MPa (-4,000 and -5,000 psi). The in-plane stresses were chosen as being possible, but with no supporting data. Table 7-1 summarizes rock properties and conditions at the two sites. The analyses were performed using the HONDO finite-element code.⁶⁴

Table 7-1

Geothermal Site Parameters

PARAMETERS	THE GEYSERS	BACA
Rock Type	Graywacke	Tuff
Well Depth, m (ft)	1,829 (6,000)	1,524 (5,000)
Base Case In-Situ Stresses		
Overburden, MPa (psi)	-45 (-6,500)	-31 (-4,500)
Smallest in-plane, MPa (psi)	-41 (-6,000)	-28 (-4,000)
Largest in-plane, MPa (psi)	-48 (-7,000)	-35 (-5,000)
Typical Rock Properties		
Density, g/cm ³ (lb/ft ³)	2.49 155.5	2.12 132.4
Unconfined modulus, Pa (psi)	23.4×10^9 3.4×10^6	6.9×10^9 1.0×10^6
Unconfined strength, MPa (psi)	255 (37,000)	33 (4,750)

The material model includes both tensile cracking and plasticity. The inclusion of cracking allows calculations of the fractures extending into the formation, while the inclusion of plasticity represents the ductile response of rock.

In order to evaluate cracking, the maximum principal stress (tension is positive) is calculated at each integration point and, if it exceeds a specified failure stress, a crack forms normal to the principal stress direction (see Figure 7-1). In subsequent time steps, the strain normal to each crack is monitored to determine if the cracks are open or closed. If a crack is open, the global stresses at that point are rotated to the crack orientation. The shear stress is set equal to zero and the normal stress is set equal to a fraction of the borehole pressure. These modified stresses are then rotated back to the global coordinates. If a crack closes, the material is assumed to heal and retains no memory of previous cracks.

Figure 7-1. Orientation of Tensile Crack

The plasticity portion of the constitutive model was developed by Krieg⁶⁵ and is the same as Material 7 in HONDO. It includes the capability to represent both volumetric and deviatoric plasticity (although volumetric plasticity was not used in the present calculation).

In the analysis, the propellant burn pressure was defined as a function of time on the surface of the borehole. Previous calculations have shown the importance of gas pressurization of the fractures extending from the borehole. This gas pressure both inhibits the formation of new cracks by placing adjacent material in compression and increases the length of existing cracks. The actual pressure on a fracture face will be a complex function of crack geometry and gas parameters. For these studies, the crack pressure was specified as a fraction of borehole pressures with the fraction ranging from 0.75 for the slower cases to 0.20 for the fast cases. The value of 0.30 for the intermediate cases is based on previous correlations with experimentally measured stresses. Varying the fraction is an attempt to recognize that at lower loading rates more gas can flow into the cracks and at high rates less pressurization of the cracks can occur. Applying the pressure to the crack as soon as it opens corresponds to the instantaneous gas flow assumption.

Calculations--The first parameter study examined the effect of varying the loading rate on the predicted fractures. Table 7-2 shows the cases used to study the rate effect. A triangular pressure pulse was assumed (see Figure 7-2). All ten cases were analyzed for The Geysers site, but only the first five cases were calculated for the Baca site. For the loading rate studies, the in-situ stresses were not varied.

Typical results for the loading rate study are depicted in Figures 7-3 and 7-4 which show in detail the calculated fracture pattern and stresses for Case 8 of The Geysers study and Case 3 of the Baca. The fractures and minimum (most negative principal stress) stress contours are given for the initial conditions, the time of peak pressure, and at the completion of the transient after motion has been damped out. For The Geysers calculation, the material remained elastic, as indicated by the final stresses which returned to approximately the same stresses as before the transient. However, in the Baca calculation, the rock flows plastically and a residual "stress cage" is formed. Plastic flow is predicted in the calculation because plasticity theory is used. However, in actuality, it is expected that the material would crush under these conditions.

Table 7-2
Parameter Study Cases

Case	Pressure Loading Rate GPa/s (psi/s x 10 ⁶)	Maximum Pressure (P _{max}) MPa (psi)	Rise Time (t _p) (s)	Fraction of Borehole Pressure Applied to Open Cracks
1	35 (5)	172 25,000	0.005	0.75
2	69 (10)	172 25,000	0.0025	0.50
3	345 (50)	172 25,000	0.0005	0.30
4	690 (100)	172 25,000	0.00025	0.30
5	3,447 (500)	172 25,000	0.00005	0.20
6	35 (5)	345 50,000	0.010	0.75
7	69 (10)	345 50,000	0.005	0.50
8	345 (50)	345 50,000	0.001	0.30
9	690 (100)	345 50,000	0.0005	0.30
10	3,447 (500)	345 50,000	0.0001	0.20

Figure 7-2. Triangular Pressure Pulse

Figure 7-3. Fracture Pattern and Minimum Stress Contours for The Geysers Case 8. (a) Initial Condition, (b) At Peak Pressure, and (c) After Transient

Figure 7-4. Fracture Pattern and Minimum Stress Contours for Baca Case 3. (a) Initial Condition, (b) At Peak Pressure, and (c) After Transient

Figures 7-5 and 7-6 show all the calculated fracture patterns as a function of pressure load at the two geothermal sites. The behavior of the two sites is at opposite extremes. Because The Geysers rock is strong, the material behaves elastically and no plastic flow occurs. Therefore, fractures form due to tensile tangential stresses and the patterns are similar to previous calculations. At the Baca site, however, the rock is relatively weak. When combined with the relatively high in-situ stresses, plastic flow is predicted rather than fracturing. Fractures are predicted only for the slow-loading case.

That the prediction of plastic flow is consistent with the input can be shown by calculating the elastic stresses around the borehole due solely to the in-situ stresses. For the Baca site, these stresses exceed the yield strength of the rock and the rock is in an initial yield (failure) state. Pressurizing the borehole then leads to plastic deformation.

Two operational pieces of data also lend credence to the plastic flow calculation. First, when drilling at Baca, the wells must be cased to prevent collapse. This indicates that the stresses exceed the failure strength of the rock. A second substantiation of plastic flow is the failure of an explosive stimulation attempt to enhance recovery at the Baca site. Although the loading rate may have been too fast to form multiple fractures in any case, the failure of the explosive stimulation correlates with plastic deformation of the borehole.

The prediction of plastic flow even under relatively slow loading rates indicates that tailored-pulse fracturing may not be applicable under all conditions but may be possible for only certain combinations of in-situ stress and rock strength. That is, obtaining multiple fractures can be expected for a high-strength rock even with relatively high in-situ stresses, since the rock will still behave elastically and will form tensile fractures. However, for a low-strength rock under the same in-situ stresses, yielding will occur and fractures cannot form. A very rough approximation of the fracture boundary can be calculated if the condition of yielding due to the initial in-situ stresses is used as the criterion. Assume that if the initial elastic stresses around the borehole exceed the

Figure 7-5. Fractures Predicted for The Geysers Loading Rate Study

Figure 7-6. Fractures Predicted for Baca Loading Rate Study

yield strength of the rock, pressure loads will plastically deform the rock and no fractures will form. If the initial stresses are below the yield strength, assume fracture can occur.

To develop a strength-versus-depth curve, several simplifying assumptions must be made. First, assume the overburden stress can be calculated using the well depth (d) and rock specific weight (γ):

$$\sigma_{\text{over}} = \gamma d \quad (1)$$

As a very rough approximation, assume that the difference between the in-plane stresses is 0.2 of the overburden stress with the average in-plane stress equal to the overburden. Also, assume that the rock failure envelope is described by the Coulomb criterion⁶⁶ then,

$$\sigma_1 [(\mu + 1)^{1/2} - \mu] - \sigma_3 [(\mu^2 + 1)^{1/2} + \mu] = \frac{C_o}{[(\mu^2 + 1)^{1/2} + \mu]} \quad (2)$$

where σ_1 is the largest principal stress and σ_3 is the smallest principal stress (compression positive). The unconfined strength is C_o and μ is the coefficient of internal friction. For a typical rock specific weight of 2.49 g/cm^3 (0.09 lb/in.^3), the relation between well depth and required strength to remain elastic is then:

$$d = 0.4 C_o \quad (3)$$

where d is depth in feet and C_o is unconfined strength in psi. It must be repeated that many assumptions led to the development of this relation; however, it does indicate a believable trend.

A word of caution also needs to be given with regard to the fracture lengths shown in Figure 7-5 and 7-6. A main parameter governing fracture length is gas pressurization of the cracks. Since in this study gas pressure was instantaneously applied to the cracks, crack length was controlled by the peak pressure and duration of the pressure pulse. Either increasing peak pressure or increasing the time of crack loading had the effect of

increasing crack length. In reality, the pressurization of the cracks is a complex function of propellant burn rate, dynamics, material properties, and probably several other parameters. Until more of these effects are included in the model, prediction of crack length will be uncertain. The present model probably overestimates crack length for the specified pressure pulse.

A second parameter study looked at the effect of varying in-situ stresses for a fixed loading rate. The Geysers Case 8 was used as the nominal stress condition and loading rate. Figure 7-7 shows the predicted fractures as in-situ stresses are varied. Multiple fractures are predicted for all cases except when all stresses are equal. For this case, the stress is uniform around the borehole and, as a result, fractures are predicted at all locations around the borehole at the same time. Whether this uniform failure would occur in reality is not certain, since local inhomogeneities could lead to local crack initiation with a resulting multiple fracture pattern. A general conclusion to be drawn from Figure 7-7 is that multiple fracturing can be obtained for a fairly wide range of in-situ stresses.

Conclusions--Tailored-pulse gas fracture appears to be a feasible stimulation technique for at least some geothermal applications. Multiple fractures were predicted for The Geyser geothermal site (strong rock), while plastic flow was predicted for Baca (weak rock).

These calculations suggest a previously unrecognized limitation of tailored-pulse fracture in a regime where the initial borehole stresses do not exceed the strength of the rock. The limitation occurs because the rock must remain predominantly elastic if tensile fractures are to form. Using several assumptions, the following relation between unconfined rock strength (C_o in psi) and allowable depth (d in ft) was developed.

$$d = 0.4 C_o$$

The effect of varying the in-situ stress over a fairly large range appears to be of less importance as long as the initial stresses remain elastic.

Figure 7-7. Fractures Predicted for The Geysers In-Situ Stress Study

Confidence in these calculations would be increased if material property data was obtained for the actual wells. Also, in-situ stress data would allow a better calculation of the initial borehole stresses and would improve the fracture calculation.

Although the present model satisfactorily predicts the fracture regime (hydraulic, multiple, crushed) that will occur due to a given pulse, it is not capable of predicting crack length or crack propagation in a fractured medium. For this stimulation method to be of practical use, it is important that fracture lengths be long enough to either significantly increase the effective wellbore diameter or intersect existing fractures. To predict crack length with confidence will require coupling the gas flow dynamics to the structural model.

Future plans include the modeling of prefractured rock and the presence of fluid in the borehole.

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APPENDIX A

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APPENDIX B

Geothermal Technology Industry Review Panel Members

Tom Anderson
Venture Innovations
P.O. Box 35845
Houston, Texas 77035

W. Ed Bingman
Shell Oil Company
Two Shell Plaza
P.O. Box 2099
Houston, Texas 77001

Larry Diamond
Dyna-Drill
P.O. Box C-10576
Irvine, California 92713

Professor Mel Friedman
Dept. of Geology
Texas A&M University
College Station, Texas 77843

John Fontenot
NL Petroleum Services
P.O. Box 1473
Houston, Texas 77001

Tom Turner
Geothermal Operations Headquarters
655 East 4500 South
Salt Lake City, Utah 84107

Jim Kingsolver
Smith Tool Company
P.O. Box C-19511
Irvine, California 92713

James W. Langford
Dresser Industries, Inc.
P.O. Box 24647
Dallas, Texas 75224

Harvey E. Mallory
P.O. Box 54696
Tulsa, Oklahoma 74155

Ed Martin
Superior Oil
Eastern Division
P.O. Box 51108 OCS
Lafayette, Louisiana 70505

Gene Polk
NL Petroleum Services
City Centre Building, Suite 365W
6400 Uptown Boulevard
Albuquerque, New Mexico 87110

Del E. Pyle
Union Geothermal Division
Union Oil Company of California
Union Oil Center
Los Angeles, California 90017

John C. Rowley
Los Alamos National Laboratory
Mail Stop 570
Los Alamos, New Mexico 87545

William D. Rumbaugh
Otis
P.O. Box 34380
Dallas, Texas 75243

Dwight Smith
Halliburton
Drawer 1413
Duncan, Oklahoma 73533

Tom Warren
Amoco Production Company
Research Center
P.O. Box 591
Tulsa, Oklahoma 74102

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Amoco Production Company
Research Center
P.O. Box 591
Tulsa, Oklahoma 74102
Attn: Tom Warren

Department of Energy
Albuquerque Operations Office
Albuquerque, NM 87185
Attn: W. P. Grace

Dresser Industries, Inc.
Security Division
P.O. Box 24647
Dallas, Texas 75224
Attn: James W. Langford

Dyna-Drill
P.O. Box C-19576
Irvine, California 92713
Attn: Larry Diamond

Halliburton
Drawer 1431
Duncan, Oklahoma 73533
Attn: Dwight Smith

B. J. Livesay
129 Liverpool
Cardiff, California 92007

Los Alamos National Laboratory
Mail Stop 570
Los Alamos, New Mexico 87545
Attn: John C. Rowley

Harvey E. Mallory
P.O. Box 54696
Tulsa, Oklahoma 74155

NL Baroid Petroleum Services
P.O. Box 280
Sandia Park, NM 8704
Attn: Gene Polk

NL Petroleum Services
P.O. Box 60087
Houston, Texas 77205
Attn: John E. Fontenot

Otis
Research and Development
P.O. Box 34380
Dallas, Texas 75234
Attn: William D. Rumbaugh

Phillips Petroleum Company
Geothermal Operations
655 East 4500 South
Salt Lake City, Utah 84107
Attn: Tom Turner

Shell Oil Company
Two Shell Plaza
P.O. Box 2099
Houston, Texas 77001
Attn: Ed Bingman

Smith Tool Company
Geothermal Operations
P.O. Box C-19511
Irvine, California 92713
Attn: Jim Kingsolver

Superior Oil
Eastern Division
P.O. Box 51108 OCS
Lafayette, Louisiana 70505
Attn: Ed Martin

Texas A&M University
College Station, Texas 77843
Attn: Professor Melvin Friedman
Dept. of Geology

Union Geothermal Division
Union Oil Company of California
Union Oil Center
Los Angeles, California 90017
Attn: Del E. Pyle

U. S. Department of Energy (4)
Geothermal Hydropower
Technologies Division
Forrestal Bldg., CE 324
1000 Independence Ave., S.W.
Washington, DC 20585
Attn: J. Bresee
D. Clements
R. Toms
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