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DEVELOPMENT OF CORING, CONSOLIDATING.

SUBTERRENE PENETRATORS

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ABSTRACT

Coring penetrators offer two advantages over full face-melting penetrators: formation of larger boreholes with no increase in power; and the production of glass-lined, structurally undisturbed cores which can be recovered with conventional core-retrieval systems. These cores are of significant value in geological exploratory drilling programs. The initial design details and fabrication features of a 114-mm-diam coring penetrator are discussed; significant factors for design optimization are also presented. Results of laboratory testing are reported and compared with performance predictions, and an initial field trial is described.

1. INTRODUCTION

Rock-melting penetrators (Subterrenes) are under development at the Los Alamos Scientific Laboratory (LASL) to pierce rock and soil by progressive melting rather than by conventional methods that employ chipping, grinding, or spalling.¹ When perfected, this new technique will offer the possibility of achieving desirable advancements in drilling and tunneling technology,² and thus will permit enhanced and more efficient utilization of the earth's resources.

The small-diameter prototype penetrators developed successfully thus far are of two basic types: the melting-consolidating penetrator (MCP) and the universal extruding penetrator (UEP).³ The MCP is a solid body of revolution which melts loose or low-density rock or soil to form a dense glass lining on the hole; no debris remains to be removed. The UEP has a central extrusion port through which the molten rock flows in order to be chilled and hardened into either discrete particles such as glass rods or pellets, or into a fibrous product that resembles commercial rock wool; the end product, either glass rods or rock wool, is transported to

the surface by a fluid. The transport fluid also provides necessary cooling for the stem. The "universal" aspect of the UEP stems from its ability to penetrate fully dense, hard rocks such as basalt and granite, as well as low-density and unconsolidated formations. Schematics of these penetrators and melting modes are illustrated in Figs. 1a and 1b.

The coring penetrator described in this report represents one major variant of the MCP. The coring penetrator is hollow, and the melting region is consequently an annulus (see Fig. 1c); the major portion of the molten rock from the annulus flows outwardly to form a glass lining on the borehole, but a small fraction flows inwardly to form a glass lining on the outside of the core. Except for diminished moisture content, very slight changes in density, and overburden release, the inside of the core is an undisturbed sample of the geological formation being penetrated. Because it is rigidly encased inside its glass lining, the core can be transported intact to the surface by conventional core-retrieval techniques. The fact that the core remains relatively undisturbed is of significant value for geological examination and characterization of samples.

Fig. 1. Schematics of penetrator melting modes and melt-handling methods.

II. DESIGN DESCRIPTION

A. Overall System Description

A schematic of the coring penetrator depicting all the major penetrator subsystems is presented in Fig. 2. Only the components closest to the melting body are shown -- the rest of the penetrator is relatively uncomplicated, consisting of inner and outer stem tubes and of extensions to the electrodes and water-coolant tubes. As in conventional drilling, stem sections and utility extensions are added at the surface as required. A hydraulically actuated stem gripping-and-thrusting assembly is also required at the surface to provide the required thrusts. Significant dimensions of the electrically heated and water-cooled coring penetrator are:

Outer diameter	114 mm (4-1/2 in.)
Inner (core) diameter	64 mm (2-1/2 in.)
Melting-body length	58 mm (2-1/4 in.)
Penetrator length	275 mm (9 in.) - not including stem
Penetrator mass	20 kg (44 lb.) - not including stem

B. Description of Major Subsystems

1. Electrical Heating System.

The coring penetrator, in common with most small-diameter prototype penetrators, is heated electrically by means of high-resistance pyrolytic-graphite heaters located inside the melting body. Electrical current is transmitted

Fig. 2. Schematic of coring penetrator.

from the surface to the heaters via four copper electrodes 3 mm (1/8 in.) in diameter. Because the resistivity of copper is low, the amount of power dissipated in these transmission electrodes is small - about 25 W per metre per electrode. At the junction between the stem sections and the main penetrator body, the four copper electrodes are joined to four short molybdenum electrodes, which have a much higher temperature capability. These electrodes, in turn, terminate at a relatively thick, low-resistance graphite transition ring. Where required, boron-nitride (BN) electrical insulators are used to separate the electrodes and transition ring from components that are at ground potential. The transition ring distributes the electrical current from the four separate electrodes to the cylindrical pyrolytic-graphite heater. This heater has a relatively high resistance so that the majority of the electrical power is converted to thermal power which radiates from the heater. After passing through the heater, the current return is through the low-resistance melting body and stem.

Typically, the heater dissipates 10 kW of direct current electrical power at ~ 30 V and 330 A. The heater operates at a temperature level of ~ 2300 K ($\sim 3680^\circ\text{F}$). At these temperatures, most of the heat is transferred by radiation* from the heater to the molybdenum melting body, which operates at ~ 1800 K. To increase radiative heat transfer from the outside surface of the heater to the melting body, the reflective molybdenum surface is covered with a high-absorptivity graphite radiation receptor. Because only a thin glass lining is desired on the core that is inside the penetrator, the design aim is to retard heat transfer from the heater to the inner region of the melting body by inserting a low-emissivity molybdenum radiation shield between the two surfaces.

2. Melting Body and Glass-Forming Sections.

Heat transferred from the heater to the inner surface of the molybdenum melting body is conducted to the outer surface of the melting body, through the molten rock or soil surrounding the melting body, to the as-yet unmelted rock. Separating and providing

a transition from the very hot melting body to the water-cooled afterbody is a set of load-bearing and thermal-conditioning rings collectively termed the glass-forming section. Such sections exist both on the outer and inner diameters of the coring penetrator. The glass-forming sections provide a rapid, but smooth, transition from regions of very high to regions of low temperature and act as nonsticking surfaces on which the molten rock can be controllably cooled below the melting-temperature range, through the "working" range, and finally subcooled to a smooth glassy solid without adhesion. Furthermore, because the glass must be cooled while holding the heat-conduction losses from the melting body to a minimum, the coring penetrator utilizes the highly anisotropic properties of pyrolytic graphite to minimize the heat loss from the melting body and to maximize the radial inflow of heat from the cooling rock melt. A simplified theoretical model that was used to optimize the design parameters for maximum thermodynamic efficiency in the design of the glass-forming section is described in Appendix A.

The glass-forming system incorporates a pyrolytic-graphite ring just behind the melting body which provides a transition region of low axial thermal conductivity, resulting in a sharp decrease in temperature. Following the pyrolytic graphite is a relatively thick ring of isotropic graphite, which is designed to be slightly larger (1/4 mm) in diameter than the melting body and to operate in a temperature range to match the working temperature range of the rock glass. The function of the larger diameter is to provide slight compression and working of the glass. Finally, backing up the conduction ring is another pyrolytic-graphite insulating ring, which provides the final transition from the glass working temperature to the water-cooled outer afterbody temperature. This final pyrolytic-graphite insulating ring is not required on the inner diameter because the inner afterbody is made of molybdenum and because the thermal coupling to the water-cooling system is designed to be poor.

Figure 3 is a closeup of the melting body and glass-forming section after extended laboratory testing (~ 5.5 h). As can be seen, the graphite parts corrode and deteriorate with time; for this reason they are designed to be quickly and easily replaced when required. Figure 4 shows a coring

*Because of the presence of helium gas, which serves to inert the heater cavity, a significant fraction (20%) of the heat is transferred by means of gas conduction. The convective heat-transfer contribution is smaller yet.

Fig. 3. Close-up of melting body and glass-forming section after laboratory testing.

penetrator operating at low power prior to a laboratory test. The very sharp temperature transition provided by the glass-forming section is evident.

3. Water-Cooling System. The stem of the coring penetrator is water-cooled rather than air-cooled. Water-cooling is more efficient in the sense that much lower flow rates and pressures are required for removal of a given amount of heat, but suffers the disadvantage of requiring an absolutely leak-free flow system.* This disadvantage can be overcome by minimizing the number of joints and connections required.

Cooling water is supplied at the surface at a regulated supply pressure of ~ 0.4 MPa (60 psia) for the 1 metre long laboratory version of the coring penetrator. In field applications the supply pressure varies linearly with the depth of penetration. The required flow rate for adequate cooling is 0.06 kg/s (1 gal/min). This flow is provided down to the penetrator by means of 8-mm-(5/16-in.-) diam copper tubes that are contained in the annular space between

*Unlike air-cooled systems, where small leaks can be tolerated.

Fig. 4. Coring penetrator operating at low power just prior to penetration in laboratory test.

the inner and outer stem tubes. The lower end of the copper tube is brazed into a large toroidal copper block. A small annular flow passage is machined from the inner diameter of this torus. This flow passage is covered and sealed by a brazed copper sleeve. Water from the downcomer tubing enters the toroidal flow area, splits into two flows and then recombines to flow up an upcomer tube (located 180 deg from the downcomer) and return to the surface. The toroidal flow area is small, to increase the Reynolds number of the flow and thus to enhance the convective heat-transfer coefficient. Keeping this area small also maximizes the allowable thickness of copper in the copper torus, thereby providing adequate area through which the holes for the electrodes and instrumentation can be drilled. The need for many brazed connections is thus avoided, without affecting

the transmission of electrical power and other services through the cooling system to the heater and melting body.

Heat from the molten rock is conducted through the inner and outer glass-forming sections, through the inner and outer afterbodies, through the high-thermal-conductivity copper torus and sleeve, and finally to the water-coolant flow where it is removed and transported to the surface by convection.

4. Thrusting and Extraction System. The thrust loads required for advancing the coring penetrator are very low, ~ 5 kN (1000 lb). This thrust is supplied at the surface by a hydraulically actuated gripper-thruster. The outer stem is gripped in a chuck and the thrust is transferred by friction from the gripper-thruster to the outer stem of the penetrator. In sequence, the rest of the load train consists of the outer afterbody, the glass-forming section, and finally the melting body. To extract the stem the thrust direction is reversed; the load train remains the same, except that, in tension, the glass-forming section is bypassed and the extraction load is reacted by the molybdenum extractors instead. The major molybdenum parts of the system are shown in an exploded view in Fig. 5.

Two problems affected the initial design of this system. First, the inner and outer extractors were connected originally to the melting body with threaded joints. However, during the first series of laboratory tests we found that the molten rock was able to flow through the extremely small gaps between the roots and crests of these threads, thus providing very dramatic evidence of the very low viscosity of molten rock at high temperature. To eliminate this problem, the remaining two units were fabricated with high-temperature-brazed joints for these connections.

Second, the connection between the molybdenum outer extractor and the stainless-steel outer afterbody utilized very fine threads. Because of the difference in materials, the mating parts were fabricated by different vendors. However, due to design error, not enough dimensional control was specified on the fabrication drawings and, as a result, the mating parts could not be assembled upon delivery. These threads had to be reworked, resulting in a considerable loss in design strength, as will be seen in a subsequent section.

Fig. 5. Exploded view of major molybdenum components of coring penetrator.

C. Description of Fabrication Techniques and Costs

Three coring penetrators were made for \$1280 each plus \$325 for furnace-brazing to join the melting bodies and extractors. Blanks for the bodies were made by upset-forging 106-mm-diam bar to a diameter of 120 mm (106 mm was the largest diameter for bar stock available at that time). Machining the molybdenum melting body was difficult and required that the cavity be "hogged out" by using electric-discharge machining with graphite electrodes. The cavity was finished by fabricating a special cylindrical cutter with three brazed-on cutting tools to eliminate chatter and to overcome excess cutting pressures encountered with typical single tool shanks.

Brazing was accomplished by using both titanium (melting point, ≈ 1990 K) and vanadium (melting point, ≈ 2170 K) as the braze alloy. We believe this is the first time that either metal had been used with molybdenum. A vacuum of 6×10^{-5} torr was necessary. Although the exact brazing temperature was not measured, the brazing temperature was determined by observation of a braze wire lying on a coupon placed inside the inner bore of the penetrator. The final braze conditions were approached slowly by initially holding

at 100 K below the estimated brazing temperature, and then increasing the temperature slowly until the braze wire melted.

III. TEST RESULTS

Three prototype coring penetrators were fabricated and tested in three phases: laboratory calibration tests, rock-penetration tests, and an initial field trial. These tests are discussed below.

A. Calibration Tests

Calibration tests were conducted in the laboratory to compare measured system parameters, e.g., coolant flow rate, pressure, and temperature, with predicted values. Such comparisons are invaluable in assuring the penetrator designer that design criteria, assumptions, mathematical models, and prediction tools (computer codes) are valid.

To aid visual examination of the penetrator and to allow optical pyrometer measurements of penetrator surface temperatures during this calibration testing, the penetrator was not operated in rock or earth but instead was held stationary in a transparent enclosure filled with inert gas (Fig. 4). This mode of operation allowed more instrumentation than would have been possible in a hole-melting test.

Figure 6 presents a comparison of measured and predicted parameters as a function of total penetrator power. Average heater temperature was inferred from the separately measured heater electrical resistivity vs temperature calibration curve. Melting-body temperatures were measured with tungsten against tungsten-rhenium thermocouples. Both the measured and predicted temperatures refer to an axial distance of 40 mm from the leading edge of the melting body. Water-cooling power was calculated from measured flow rates and from inlet and outlet temperatures. Design predictions were obtained with the AYER⁴ heat-transfer computer code. As can be seen, the agreement between experimental and predicted values is excellent over the entire operating range.

Figure 7 presents the results of a detailed temperature scan of the surface of the melting body and part of the glass-forming section while the penetrator was operating at a constant power of 7 kW. Measured temperatures were obtained with an optical pyrometer (effective wavelength, 0.65 μm). Note again that excellent agreement between measured and

Fig. 6. Comparison of experimental and predicted penetrator parameters.

Fig. 7. Comparison of measured and predicted penetrator temperatures.

predicted temperatures prevails, except in the pyrolytic and isotropic graphite rings. The small discrepancy in the temperatures for these rings is easily explained by the rather large uncertainties in the value of the thermal conductivity to be used for the pyrolytic graphite in the axial (low-conductivity) direction.

B. Laboratory Penetration Tests

Laboratory penetration tests were performed with two of the coring units. The third and last penetrator was reserved for field testing. As mentioned previously, molten rock was able, during the first laboratory test, to penetrate the melting body-to-extractor threaded joint and to enter the heater cavity, thus shorting the heater and terminating the test. Subsequent efforts to disassemble this penetrator, welded together with frozen glass, were unsuccessful and this first penetrator unit had to be abandoned. We then decided to braze the threads on the remaining two units.

1. Core Specimens. The coring penetrator was evaluated in the laboratory in seven separate tests, in rocks or soils consisting of Los Alamos tuff, alluvium (from ERDA's Nevada Test Site), and Green River shale. Figure 8 shows a laboratory version (very short stem) of the coring penetrator after a

short penetration of tuff. The unmelted surrounding tuff was removed to show the rock-melt glass lining in a cut-away view. Figure 9 shows the same penetrator partially disassembled, to show the core extending out of the penetrator coring region.

Figure 10 shows some typical cores recovered from subsequent laboratory testing of the corer. Notice that these cores, like most of those made, were melted entirely rather than having the desired thin, melted glass liner surrounding an unmelted and undisturbed core. A notable exception was the core made in Green River shale (Fig. 11). This material had regions of low density and voids. When the penetrator entered such regions, penetration rates increased dramatically. This, as explained in Appendix B, resulted in regions with much less radial penetration of heat and, consequently, less melting.

2. Penetration Rates. Predicted⁵ and experimentally measured penetration rates achieved in laboratory testing of Los Alamos tuff specimens are compared in Fig. 12. As can be seen, when the applied penetrator thrust is greater than the minimum

Fig. 8. Coring penetrator after short-depth penetration of tuff.

Fig. 9. Core extending out of penetrator coring region.

threshold thrust, the penetration rate is essentially independent of penetrator thrust and agrees well with predicted penetration rates.

The experimentally observed minimum threshold thrust of 2 kN was in qualitative agreement with lithothermodynamic⁶ predictions of 4 kN. These predictions are only approximate because of the great uncertainties in the data for the viscosity of molten tuff in the temperature range of interest.

3. Structural Damage. In attempting to extract the penetrator from the rock specimen at the end of the third test, the penetrator was accidentally overloaded to an extraction force of 22 kN (5000 lb).^{*} Although the outer extractor-to-afterbody

^{*}Until this time the penetrator had sustained the usual extraction loads of 9 kN (2000 lb).

threaded joint was designed for 27 kN (6000 lb), this accidental overload was sufficient to strip the threads, which had been weakened by reworking, as described earlier. After stripping, the threaded joint had little tensile strength and no further attempt was made at extraction during the subsequent tests.

C. Field Testing

The third and last penetrator was used in conjunction with a longer stem for field testing. This unit is shown in Fig. 13 mated to the inner stem and to extensions of the water-cooling and electrical connections. Also shown are three thermocouples extending from the penetrator. The outer stem lies to the right of the unit.

The inner and outer stem, water lines, and electrical extensions are made in modules 4 m (12 ft) long, and more modules can be added with simple threaded and soldered connections, as required.

Figure 14 shows the field unit, with one stem module, being readied for testing in the tower rig.

During field testing the penetrator performed as it did in laboratory testing except that the penetration thrusts had to be increased with increasing penetration depths, possibly because of friction between the hole and the outer stem.

After penetrating to a depth of one metre an attempt was made to extract the penetrator under a modest tensile load of 9 kN (2000 lb), at which time the threaded connection between the outer extractor and afterbody parted. This failure, too, is attributed to the thread reworking described earlier.

IV. SUMMARY, CONCLUSIONS, AND RECOMMENDATIONS

The initial development of melting coring penetrators has been described.

The development of the coring penetrator has successfully demonstrated the basic feasibility of obtaining continuous core specimens while melting fairly large holes with glass linings at a useful rate of penetration.

Initial testing revealed a serious problem, namely, the unintentional and unnecessarily weak outer extractor-to-afterbody threaded connection. However, this problem can be solved easily by re-designing the mating parts so that a standard, coarser thread can be specified.

Fig. 10. Typical cores.

Fig. 11. Core made in Green River shale.

Fig. 12. Penetration rates in tuff as a function of total penetrator power.

Fig. 13. Field testing unit prior to final assembly.

One important design goal that was not accomplished (except in limited cases) was the extraction of relatively undisturbed unmelted cores. However, this limitation is understood (see Appendix B) and a new version of the coring penetrator can be designed, which will extend the penetration-rate performance and will produce unmelted cores in alluvium.

Fig. 14. Field unit being installed in tower rig test facility.

Thus, it is recommended that such a new coring penetrator be developed, capable of penetrating at a much higher rate, 0.4 mm/s, and capable of retrieving unmelted cores. A preliminary design has established the initial feasibility of this improved corer. Initial calculations and specific design data are contained in Appendix B.

DESIGN OPTIMIZATION OF PENETRATOR EXTRACTORS AND GLASS-FORMING SECTIONS

A. Introduction

Previous penetrator designs for the glass-forming sections have been governed by three design criteria:

- Provide enough net area of pyrolytic graphite to safely withstand the compressive stresses produced by penetrator thrust loads.
- Provide enough net area of molybdenum extractor to safely withstand the tensile stresses produced by extraction (withdrawal) loads.
- Minimize the net cross-section area of the molybdenum extractor, while at the same time making it as long as practical, to minimize conduction heat losses from the penetrator melting body.

In this Appendix we show that the third criterion should be replaced as follows: size the extractor and glass-forming sections so that (a) the rock-glass liner, which is initially very hot at the extractor/melting-body interface, is cooled below the sticking-temperature range when it reaches the extractor/afterbody interface, and so that (b) the conduction heat losses are minimized by efficiently utilizing the heat removed from the cooling glass.

B. Problem Description

Figure A-1 shows schematically the usual details of a typical penetrator design. Generally, the surface temperature of the melting body is ~ 1800 K. The temperature of the surrounding molten rock varies from 1800 K at the melting-body surface to the rock-melting temperature (~ 1450 K) at the melt-solid interface.

Separating the melting body from the water-cooled afterbody is a subassembly consisting of a molybdenum extractor and a set of pyrolytic and ordinary graphite rings collectively known as the glass-forming section. The purpose of the extractor is to provide a mechanism for applying tensile withdrawal loads to the penetrator. The purpose of the various graphite rings in the glass-forming section

Fig. A-1. Schematic diagram of extractor/glass-forming region of the penetrator.

is, first, to provide a nonsticking surface along which the temperature can be systematically and controllably reduced so that the molten glass can make the transition from the melt temperature, through the working temperature, and finally to the solid-glass temperature; second, to reduce heat-conduction losses from the melting body; and, third, to provide a mechanism for exerting axial compressive (penetrating) loads on the penetrator melting body. In Fig. A-1, at $X = L$, the extractor and load washer adjoin the afterbody. The afterbody is normally made from a low-temperature material that is either water or air cooled; thus the temperature of the afterbody is ~ 300 to 400 K.

C. Thermal Model and Analysis

The differential heat-transfer control volume is indicated on Fig. A-1 and shown in more detail in Fig. A-2. Several assumptions should be explained.

- The outer boundary, at the melt/rock interface, is assumed adiabatic. This implies that the thermal diffusivity or conductivity of the unmelted rock is zero. If we assumed a finite rock conductivity, the results of this simplified analysis would be too conservative and nonoptimum, i.e., the molten rock would be cooled below the sticking point and the conduction heat losses would be higher than the minimum obtainable.

Fig. A-2. Differential heat-transfer model.

- The inner boundary, next to the extractor, is also assumed adiabatic. This assumption seems reasonable because almost all designs incorporate a poorly conducting gas at this interface.
- Radial or transverse conduction is accounted for only on a lumped-node basis. To approximate a fully two-dimensional conduction problem, it is assumed that heat transfer from the glass, of thickness t , can be described by means of an "effective conductivity", λ_r , and as effective conduction length, b , measured from the midpoint of the glass to the molybdenum extractor. The parameters λ_r and b are adjustable and take into account not only the glass conductance but also the radial conductance of the graphite glass-forming section, the resistance of any gaps present, and (if the glass thickness is large compared to the overall penetrator diameter) radial heat flow through the glass. For the simplest case where the glass thickness is small compared to the penetrator diameter, no gaps are present, and the conductivities of both

the pyrolytic and ordinary graphite rings in the glass-forming section in the radial direction are much higher than the conductivity of the glass, λ_r simplifies to λ_g (glass conductivity) and b becomes $t/2$.

- Properties are assumed constant (no variation with temperature).

With these assumptions, the axial (x -direction) heat transfer occurs by convection in the glass and by conduction in both the glass-forming section and the molybdenum extractor. The glass and the molybdenum extractor conduct heat radially to each other by means of the adjustable effective conductivity and conduction length, b , described above. To further simplify the problem we note that the effective thermal conductivities of the glass and the graphite glass-forming section in the x direction are small (~ 2 W/mK) compared to that of the molybdenum (~ 100 W/mK). Thus, we eliminate these axial conduction terms. With these simplifying assumptions, the heat-transfer equations become

$$-\rho C V t \frac{dT_g}{dx} = \frac{\lambda_r}{b} (T_g - T_m), \text{ for the glass } (1)$$

and

$$-\lambda_m f \frac{d^2 T_m}{dx^2} = \frac{\lambda_r}{b} (T_g - T_m), \text{ for the molybdenum extractor } (2)$$

where

- ρ = rock-glass density
- C = specific heat
- V = penetrator velocity
- t = glass thickness
- T_g = radially averaged glass temperature at any point x
- T_m = molybdenum-extractor temperature at any point x
- λ_r = effective radial thermal conductivity between the lumped glass thickness and the molybdenum extractor
- b = effective radial conduction length
- λ_m = thermal conductivity of the molybdenum extractor
- f = thickness of the extractor.

²² For fully consolidating penetrators, t can be calculated from the relationship which requires conservation of mass.

Denoting differentiation with respect to x by primes, Eqs. (1) and (2) can be combined to give

$$\rho C V t T_g' = \frac{\lambda_r}{b} f T_m' \quad (3)$$

Differentiating Eq. (2) once:

$$-\frac{\lambda_r}{b} f T_m'' = -\frac{\lambda_r}{b} f (T_g' - T_m') \quad (4)$$

and substituting Eq. (3) into Eq. (4) yields the linear third-order equation

$$\lambda_m f T_m''' + \frac{\lambda_r \lambda_m f}{b \rho C V t} T_m'' - \frac{\lambda_r}{b} T_m' = 0 \quad (5)$$

Nondimensionalize these equations by letting

$$\theta = \frac{T_m(x) - T_m(L)}{T_m(0) - T_m(L)}$$

$$\psi = \frac{T_g(x) - T_m(L)}{T_m(0) - T_m(L)}$$

where L is the length of the glass former (Fig. A-1), and let:

$$\frac{d\theta}{dx} = \frac{1}{L} \frac{d\theta}{d\gamma}$$

where

$$\gamma = \frac{x}{L}$$

Thus, in nondimensional form Eq. (5) becomes

$$\theta''' + \beta \theta'' - \alpha \theta' = 0 \quad (6)$$

where

$$\beta = \frac{\lambda_r L}{b \rho C V t}$$

$$\alpha = \frac{\lambda_r L^2}{\lambda_m b f}$$

and the superscript prime now denotes differentiation with respect to γ . Furthermore, ψ is found from Eq. (2)

$$\psi = \theta - \frac{1}{\alpha} \theta'' \quad (7)$$

Solution of Eqs. (6) and (7) requires three boundary conditions. At $x = 0$ ($\gamma = 0$), $T_m = 1800$ K, and T_g , the average glass temperature, is taken to be the average of T_m plus the melting point of rock (1450 K); so, at $\gamma = 0$, $T_g = 1625$ K. At $x = L$ ($\gamma = 1$), $T_m = 400$ K. The nondimensional equivalents are

$$\begin{aligned} \theta(0) &= 1.0 \\ \psi(0) &= 0.875 \\ \theta(1) &= 0.0 \end{aligned} \quad (8)$$

In the interests of generality, let $\psi(0)$ be an adjustable parameter called ψ_0 .

The solutions to Eqs. (6), (7), and (8) are

$$\theta(\gamma) = A e^{R_1 \gamma} + B e^{-R_2 \gamma} + K \quad (9)$$

$$\psi(\gamma) = \left(1 - \frac{R_1^2}{\alpha}\right) A e^{R_1 \gamma} + \left(1 - \frac{R_2^2}{\alpha}\right) B e^{-R_2 \gamma} + K \quad (10)$$

where

$$A = \left[(\psi_0 - 1) (e^{-R_2} - 1) - \frac{R_2^2}{\alpha} \right] / D$$

$$B = - \left[(\psi_0 - 1) (e^{R_1} - 1) - \frac{R_1^2}{\alpha} \right] / D$$

$$K = \left[(\psi_0 - 1) (e^{R_1} - e^{-R_2}) \right]$$

$$- \frac{R_1^2}{\alpha} e^{-R_2} + \frac{R_2^2}{\alpha} e^{R_1} \Big] / D \quad (11)$$

$$D = - \frac{R_1^2}{\alpha} (e^{-R_2} - 1) + \frac{R_2^2}{\alpha} (e^{R_1} - 1)$$

$$R_1 = -\frac{\beta}{2} + \sqrt{\left(\frac{\beta}{2}\right)^2 + \alpha}$$

$$R_2 = \frac{\beta}{2} + \sqrt{\left(\frac{\beta}{2}\right)^2 + \alpha}$$

The conduction heat loss, Q , occurs through the extractor and amounts to

$$Q = -\pi d f \lambda_m \frac{dT_m(0)}{dx}, \text{ where } d = \text{extractor diameter;}$$

or

$$Q = \pi d [T_m(0) - T_m(L)] \left(\frac{f}{L}\right) \lambda_m \theta'(0) \quad (12)$$

where $\theta'(0)$ is given by

$$\theta'(0) = AR_1 - BR_2 \quad (13)$$

Because d , $T_m(0)$, and $T_m(L)$ are constant for a given penetrator design, it is seen that to minimize the parasitic heat loss, it is necessary to

Fig. A-3. Dimensionless extractor conduction heat loss.

minimize not only f/L but also $\theta'(0)$, which in turn is a function of f/L and other parameters. *

D. Results

The pertinent design results are expressed in Figs. A-3 and A-4 in terms of the nondimensional design parameters α and β , respectively (ψ_0 was taken to be 0.875 for all of the following discussions). Figure A-3 presents the dimensionless parasitic heat loss from the extractor and Fig. A-4 presents the dimensionless final glass temperature (after chilldown). Obviously, for a given penetrator, the design variables α and β should be selected to yield the desired final glass temperature and the minimum dimensionless heat loss.

Due to the choice of boundary conditions it is not possible to attain a $\theta'(0)$ of zero unless $\psi_0 = 1.0$.

Fig. A-4. Dimensionless glass temperature after chilldown ($x = L$).

FEATURES AND ANALYSIS OF A REDESIGNED CORING PENETRATOR

A. Introduction

In this Appendix we briefly discuss how to achieve the primary goals, namely, faster penetration rates and unmelted cores, with a redesigned coring penetrator. To use existing hardware, e.g., stems and afterbodies, the new coring penetrator would have the inner and outer diameters of these components.

Numerically, the new goals would be to achieve a penetration rate of 0.4 mm/s (four times faster than with previous designs) and to consistently and reliably obtain unmelted cores with relatively thin (~10-mm-thick) glass linings.

B. Increasing Penetration Rates

Recent lithothermodynamic⁶ calculations for both coring and solid melting-consolidating penetrators have suggested that melting bodies of composite geometry with longer overall lengths offer the best compromise for obtaining very high penetration rates at modest thrusts. Such a composite melting body, depicted in Fig. B-1, consists of a thin parabolic shape from the leading edge to an axial station, L_p (defined in Fig. B-1), followed by a straight melting section to make up an overall length, L_T .

The melt-flow phenomena in such a composite penetrator can be qualitatively described as follows: penetrator thrusts, and consequently fluid-melt pressures, are high enough so that the fluid-melt velocity with respect to the penetrator is higher than the penetration rate everywhere

except at the very aft edge of the melting body. Thus, the melt is extruding everywhere except to the very end of the melting body, where the consolidation relationship is finally satisfied.⁵

The melt velocity can be controlled so that it is highest at the leading edge and lowest at the aft edge. Increasing the melt velocity results in smaller melt thicknesses, and higher heat fluxes* (for the same melting-body surface temperature), but also in higher pressures and larger penetrator thrusts and, consequently, in greater stresses and associated high-temperature creep. By varying the shape of the penetrator (by adjusting L_p and L_T) one can alter the axial distribution of melt velocity to arrive at an optimized (in the sense of maximum penetration rate for minimum thrust and total length, L_T) melting body.

Such calculations have been performed for a melting body operating at 1800 K and a penetration rate of 0.4 mm/s, and the results are presented in Fig. B-2, where the tradeoffs between L_p , L_T , and required thrusts are readily apparent.

For initial design purposes we have selected a thrust of 6 kN for a melting body of $L_T = 190$ mm and $L_p = 110$ mm. A penetrator of this size will fit inside commercially available brazing furnaces.

This design would require a melting power of 19 kW if operated in Los Alamos tuff (density, $\approx 1.5 \text{ Mg/m}^3$). Average heater energy flux would be 0.2 MW/m^2 , which corresponds to an average heater temperature of 2000 K. To obtain this flux of energy from a resistance heater of 190-mm length will require the use of a composite pyrographite and isotropic-graphite heater to yield reasonable electrical characteristics (overall voltage $\leq 100 \text{ V}$). Such heaters can be obtained commercially,⁷ fabricated either by carbon-fiber techniques or built up with alternating rings of pyrolytic and isotropic graphite.

C. Obtaining Unmelted Cores

As shown in the previous section a redesigned corer will require a cylindrical heater of 190-mm

Fig. B-1. Composite melting body.

*This in turn leads to greater melting power and penetration rates.

Fig. B-2. "Lithothermodynamic" optimization study of a "fast" coring penetrator.

length operating at an average temperature of 2000 K and supplying an average energy flux (from both sides of the heater) of 0.2 MW/m^2 to advance 0.4 mm/s . In this section we examine methods of ensuring that most of this flux leaves the outer surface of the heater, so that most of the melting power is directed outwardly rather than inwardly toward the core.

1. Allowable Inward Heat Flux. A simple* one-dimensional heat balance shows that

$$q''A = \pi p V (CT + L) [(R_i + \Delta R)^2 - (R_i - t)^2] \quad (\text{B-1})$$

where

q'' = average heater flux directed inwardly

A = inside surface area of heater

p = density of unmelted rock (soil)

* Assuming that axial outflows of heat approximately equal the enthalpy rise of the unmelted core.

V = penetration rate

C = specific heat of rock or soil

T = temperature rise from ambient to melting

L = latent heat of fusion of rock

R_i = inside radius of penetrator

ΔR = location of parting line (see Fig. B-1)

t = thickness of glass liner on core.

Note that for a fixed geometry, the allowable q'' can increase if the penetration rate increases. If one substitutes the usual properties for rock or soil,¹ and a penetration rate of 0.4 mm/s , the allowable inward heater flux for various ΔR and t combinations can be plotted as in Fig. B-3. From Fig. B-2 it is seen that the predicted ΔR is reasonably constant at $\sim 7 \text{ mm}$; thus, for a glass-lining thickness of $\sim 10 \text{ mm}$ (leaving an unmelted core of 43-mm diam) the inward heater flux should not exceed 0.055 MW/m^2 . Therefore, only 25% of the total heat flux can be directed inwardly. To attain this reduced inward heat flux requires that radiation shields be placed between the heater and the inner diameter of the penetrator.

2. Heat Transfer Calculations. If we assume that n shields of molybdenum (total hemispherical emissivity, 0.3) are inserted between the heater and the inner wall of the penetrator, the heat flux due to thermal radiation and conduction through the argon* gas used to inert the heater cavity, from the k^{th} shield is:

$$q_k'' = \frac{\epsilon \sigma (T_k^4 - T_{k+1}^4)}{2 - \epsilon} + \frac{\lambda_A (T_k - T_{k+1})}{h} \quad (\text{B-2})$$

where

ϵ = total hemispherical emissivity

σ = Stefan-Boltzmann constant

T_k = temperature of k^{th} shield

T_{k+1} = temperature of $k + 1^{\text{th}}$ shield

λ_A = thermal conductivity of argon (as a function of temperature)

h = spacing between shields.

The nonlinear set of Eq. (B-2) has been solved, subject to the boundary conditions that the average heater temperature is 2000 K and the average inner-

* Initial calculations have shown that an inert low-conductivity gas such as argon must be used to inert the heater cavity in the redesigned coring penetrator.

Fig. B-3. Allowable inward heat flux.

wall temperature of the inner diameter of the melting body is 1200 K. The results, expressed parametrically as a function of h and n (the total number of shields), are shown in Fig. B-4. It is seen that to yield an inward heat flux of 0.055 MW/m^2 , either three shields spaced 1 mm or more apart, or four shields spaced as close as 0.25 mm, are required.

Note that the original coring penetrator, which usually penetrated at only 1 mm/s, had only one shield, filled with helium, which explains why the cores were completely melted.

Fig. B-4. Effect of shielding on inward heat flux.

REFERENCES

1. R. J. Hanold, "Rapid Excavation by Rock Melting - LASL Subterrene Program," Los Alamos Scientific Laboratory report LA-5459-SR (1973).
2. R. J. Hanold, "Large Subterrene Rock-Melting Tunnel Excavation Systems - A Preliminary Study," Los Alamos Scientific Laboratory report LA-5210-MS (1973).
3. J. W. Neudecker, A. J. Giger, and P. E. Armstrong, "Design and Development of Prototype Universal Extruding Subterrene Penetrators", Los Alamos Scientific Laboratory report LA-5205-MS (1973).
4. R. G. Lawton, "The AYER Heat Conduction Computer Program," Los Alamos Scientific Laboratory report LA-5613-MS (1974).
5. R. G. Gido, "Subterrene Penetration Rate: Melting Power Relationships," Los Alamos Scientific Laboratory report LA-5204-MS (1973).
6. R. D. McFarland, "Numerical Solution of Melt Flow and Thermal Energy Transfer for the Lithothermodynamics of a Rock-Melting Penetrator," Los Alamos Scientific Laboratory report LA-5608-MS (1974).
7. Bob Froberg, Pfizer, Inc., Easton, PA, personal communication, January 1974.