Yucca Mountain Site Characterization Project

G-Tunnel Pressurized Slot-Testing Evaluations

Roger M. Zimmerman, Kevin L. Mann, Robert A. Bellman, Jr., Steven Luker, Donald J. Dodds

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G-TUNNEL PRESSURIZED SLOT-TESTING EVALUATIONS

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ABSTRACT

Designers and analysts of radioactive waste repositories must be able to predict the mechanical behavior of the host rock. Sandia National Laboratories elected to conduct a development program to enhance mechanical-type measurements. The program was focused on pressurized slot testing and featured (1) development of an improved method to cut slots using a chain saw with diamond-tipped cutters, (2) measurements useful for determining in situ stresses normal to slots, (3) measurements applicable for determining the in situ modulus of deformation parallel to a drift surface, and (4) evaluations of pressurized slot strength testing results and methods. This report contains data interpretations and evaluations. Included are recommendations for future efforts.
This report was worked on under the earlier WBS 1.2.4.2.1.2 but was completed under the current WBS 1.2.4.2.1.1.3.
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1.0 INTRODUCTION

This report is the third in a series covering the G-Tunnel Pressurized Slot Testing. The first report, covering experiment preparations (Zimmerman, 1992a) contains background information and provides the rationale and purpose of the experiment. The first report also provides details of the measurement systems and includes topics on instrument layouts, equipment, calibrations, and measurement procedures. The second report (Zimmerman, 1992b) provides the data summary in engineering units. Included in the second report are appropriate data histories and results. Data were collected in the G-Tunnel Underground Facility (GTUF) on the Nevada Test Site.

This third report contains the interpretations of the testing with emphasis on the measurement results as they apply to describing rock behavior. In particular, emphases are placed on (1) normal stress determinations using the flatjack cancellation (FC) method, (2) modulus of deformation determinations, and (3) high pressure investigations. Most of the material in the first two reports is not repeated here. Appropriate data are repeated in tabular form.
2.1 Normal-Stress Evaluations

The flatjack cancellation (FC) method is used to measure stresses near the surfaces of rock masses (Mayer et al., 1951, Tincelin, 1951). The method involves distance measurements taken before and after a slot is cut in the rock surface. As the slot is cut, the rock converges slightly because of the normal stress acting on it. A flatjack is installed in the slot and pressurized to return the slot reference dimensions to the pre-slot cutting values. The pressure in the flatjack at the restoration of the original dimensions is called the flatjack cancellation pressure and can be mathematically related to the stress acting normal to the slot. Measurements for this purpose were taken in Cycles C1 through C3 (Zimmerman, 1992a).

Normal stresses for the FC method are determined from equations derived by Alexander (1960). The theory is presented first and then the data are applied.

2.1.1 Theoretical Developments

Alexander used elasticity-theory to describe displacements of a pressurized slot. He assumed that (1) the slot had an elliptical shape in intact rock under plane stress conditions and (2) the slot was located in a principle stress plane. His resulting equations were

\[ W_0 = S(C/E)((1 - \nu)[(1 + Y^2/C^2)^{1/2} - Y/C)] + (1 + \nu)/(1 + Y^2/C^2)^{1/2} \]  \hspace{1cm} (2-1)

\[ W_1 = S(Y_0/E)((-2\nu)[(1 + Y^2/C^2)^{1/2} - Y/C)] + (1 + \nu)/(1 + Y^2/C^2)^{1/2} \]  \hspace{1cm} (2-2)
\[ W_2 = -W_1 (Q/S) \]  

(2-3)

\[ W_j = P_c (C_0/E)(1 - \nu)(1 + Y^2/C_0^2)^{1/2} - Y/C_0 \]  

+ \( P_c = \text{flatjack pressure at cancellation} \)  

(2-4)

\[ W_c = W_2 + W_1 + W_2 = W_j \text{ (at FC pressure)} \]  

(2-5)

where (see Figure 2-1)

2\( W_o \) = displacement during slotcutting due to the introduction or cutting of an infinitely thin slot

S = rock stress normal to the flatjack

C = half length of the slot

E = Young's modulus for the rock

\( \nu \) = Poisson's ratio for the rock

Y = distance of measuring point from the major axis of slot

2\( W_1 \) = displacement across the slot due to finite slot width

\( Y_0 \) = half width of slot

2\( W_2 \) = displacement across the slot due to biaxial stress (Q)

Q = rock stress parallel to the flatjack

2\( W_j \) = displacement caused by raising the flatjack pressure

}\]
Figure 2-1. Schematic Showing Geometry for Alexander (1960) Equations
2W_c - displacement across the open slot at FC pressure.

Alexander developed the following simplified formula to describe the stress state:

\[ S = \alpha P_c + \beta Q \quad (2-6) \]

where \( \alpha \) and \( \beta \) are coefficients representing slot geometry and Poisson's ratio, respectively.

Alexander showed that the coefficient "\( \alpha \)" varied from 0.8 to 0.9 and coefficient "\( \beta \)" was less than 0.1 for the geometries being considered in his efforts. Note that the solution is independent of the modulus of deformation of the rock. The significance of Equation 2-6 is that the slot normal stress \( S \) at FC is primarily related to the flatjack pressure \( P_c \) and only slightly related to the parallel stress \( Q \).

The fact that the constants \( \alpha \) and \( \beta \) are independent of \( E \) for this solution means that the FC pressure can be obtained as long as elastic conditions exist. Figures 2-5 through 2-10 (Zimmerman, 1992b) show that the stress-strain behavior was generally linear, but these and the measurements displayed in Figures 2-2 and 2-3 in Report B show that the behavior was not elastic. There was evidence of some fracture activation during the testing. The influence of the fracture on the analyses is difficult to estimate, and for the purposes of this report, it is assumed that Equation 2-6 is accurate.

The following quantities are used for making estimates for the constants \( \alpha \) and \( \beta \) applicable to GTUF testing:

- \( C = 500 \text{ mm} \)
- \( C_o = 365 \text{ mm} \) (considering effective length of 0.96 x 380)
  (Zimmerman, 1992a)
The resulting values are

\[ \alpha = 0.693, \quad \beta = 0.004 \]

Thus, Equation 2-6 can be written as

\[ S_i = 0.693P_{ci} + 0.004Q_i \quad \text{(2-7)} \]

where \( i = 1, 2, \text{ and } 3 \) for C Cycles (Zimmerman, 1992a).

Equation 2-7 shows that the normal stress is most strongly related to the FC pressure, \( P_{ci} \), and only slightly influenced by the orthogonal stress, \( Q_i \). \( P_{ci} \) can be determined from measurements, and \( Q_i \) must be assumed in the final stress determinations.

2.1.2 Determination of the Normal Stress Using Surface Pin Measurements

The quantity \( P_{ci} \) can be determined using the reference displacements in Table 2-1 (Zimmerman, 1992b). The approach was to determine an average displacement caused by cutting the slot and relate this to the average linear descriptions of the displacement-pressure responses shown in the figures. To facilitate computations, Table 2-2 lists the average slopes for the data from the figures (Zimmerman, 1992b). The FC pressure is calculated using

\[ P_{ci} = m_iD \quad \text{(2-8)} \]

where

\[ P_{ci} = \text{average flatjack cancellation pressure for cycle } i \]
**TABLE 2-1**

SUMMARY OF SLOT CLOSURE INITIAL VALUES FOR FLATJACK CANCELLATIONS

<table>
<thead>
<tr>
<th>Date</th>
<th>Condition</th>
<th>Lines</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>1 (mm)</td>
</tr>
<tr>
<td>5/5/86</td>
<td>Before Slot Cutting</td>
<td>0</td>
</tr>
<tr>
<td>5/13/86</td>
<td>After Slot Cutting</td>
<td>0.278</td>
</tr>
<tr>
<td>6/3/86</td>
<td>Before Cycle C1</td>
<td>0.230</td>
</tr>
<tr>
<td>6/3/86</td>
<td>Before Cycle C2</td>
<td>0.197</td>
</tr>
<tr>
<td>6/4/86</td>
<td>Before Cycle C3</td>
<td>0.187</td>
</tr>
<tr>
<td>6/4/86</td>
<td>After Cycle</td>
<td>0.193</td>
</tr>
</tbody>
</table>

Average for 5/13/86: \(0.293 \pm 0.056\) mm

**TABLE 2-2**

SUMMARY OF PRESSURE/DISPLACEMENT SLOPES FROM WHITTEMORE MEASUREMENTS

<table>
<thead>
<tr>
<th>Test Cycle</th>
<th>Whittemore Line</th>
<th>1 MPa/mm (±MPa/mm)</th>
<th>2 MPa/mm (±MPa/mm)</th>
<th>3 MPa/mm (±MPa/mm)</th>
<th>4 MPa/mm (±MPa/mm)</th>
<th>Average ± 1 Std. Dev. (MPa/mm)</th>
</tr>
</thead>
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<tr>
<td>C1</td>
<td></td>
<td>11.2 (±0.32)</td>
<td>8.5 (±0.31)</td>
<td>7.6 (±0.32)</td>
<td>9.1 (±0.45)</td>
<td>9.10 ± 1.53</td>
</tr>
<tr>
<td>C2</td>
<td></td>
<td>12.8 (±0.17)</td>
<td>9.8 (±0.10)</td>
<td>9.3 (±0.08)</td>
<td>11.2 (±0.25)</td>
<td>10.78 ± 1.57</td>
</tr>
<tr>
<td>C3</td>
<td></td>
<td>12.9 (±0.09)</td>
<td>9.8 (±0.09)</td>
<td>9.2 (±0.05)</td>
<td>11.2 (±0.15)</td>
<td>10.78 ± 1.65</td>
</tr>
</tbody>
</table>
\( m_1 \) = average slope of deformation-pressure data shown (Zimmerman, 1992b) and represented in Table 2-2 for cycle 1

\( D \) = average displacement across four lines at slot cutting as represented from data shown in Table 2-1 (0.293 mm).

One assumption with the approach used with Equation 2-8 is that the material properties do not change with successive cycles. Applications of the equation to different cycles provides information as to the possible changes that might be induced by the rock behavior. A second assumption is that the location of the flatjack relative to the surface has no influence on the results. This assumption implies that the pins do not rotate appreciably as the slot is pressurized. This assumption is consistent with the FC approach, but discussions in Section 3.2 suggest that this may not be the case. Rotations of pins would cause \( m_1 \) values to be lower.

Solving Equation 2-8, accepting that effects of the two assumptions and \( Q_i \) effects are negligible, results in

\[ P_{c1} = 2.7 \text{ MPa}, \text{ and} \]

\[ P_{c2} = P_{c3} = 3.2 \text{ MPa}. \]

The range of normal stresses (\( S \)) can be calculated using Equation 2-7 and the results are

\[ S_1 = 1.9 \text{ MPa}, \]

\[ S_2 = S_3 = 2.2 \text{ MPa}, \text{ and} \]

Average \( S = 2.1 \text{ MPa}. \)

The in situ stress state was measured in the test area (Zimmerman and Finley, 1987), and the resultant normal to the slot could be shown to be 5.9 MPa if no considerations are made for the stress redistributions or
relaxations due to the excavation. The average of predicted normal stresses is 36% of this theoretical in situ stress.

2.2 Normal Stress Measurement Discussions

Differences in the theoretical stress and predicted stresses is most likely a result of

1. inaccuracies in either the FC calculation process or the reference in situ measurements,

2. loss of continuity in the rock mass due to activation of a fracture and resulting inelastic behavior (see Chapter 3.0),

3. discontinuities (including the drift excavation) in the rock mass outside the test area causing a redistribution of the in situ stress state prior to the measurements, and/or

4. time-dependent stress relaxation at the surface of the drift before or in conjunction with the measurements.

It is difficult to quantify the four items listed above. For Item 1, use of linear-elastic and geometrical assumptions by Alexander (1960) was supported by laboratory investigations (Hoskins, 1966). Hoskins applied external uniaxial stresses to 0.5 x 0.6 x 0.8-m prisms containing flatjacks, in six tests, four on two different types of rock and two on concrete. The differences between the calculated stresses and applied stresses ranged from 1.5 to 5.5% in five out of six cases. In the sixth test, the calculated stress for a weak, creep-prone, irregular concrete block was 112% of the applied stress. Hoskins concluded that flatjack tests in sound rock should yield a good estimate of the rock stress normal to the slot. In less sound rock, perhaps irregularly stressed with a large creep component, the stress estimate may be less reliable. As mentioned earlier, our measurements were generally linear but not elastic.
Geometrical considerations also apply to Item 1. The ratio of the flatjack length to the slot length was 0.92 in Hoskin's study, while it was 0.73 for this testing. This geometrical difference accounts for a large part of the reduction from flatjack pressure to predicted stress. Errors could occur at the lower ratio, either due to theoretical or measurement limitations. Geometrical errors due to potential reference pin rotations could reduce the net values of $m_1$ used in Equation 2-8. As an additional point, Alexander's equations were derived for pin placements perpendicular to the centerline of the flatjack. The four sets of measurements pins in this experiment were located between the centerline and the end of the flatjack. It is possible that slightly smaller displacements were recorded. This would tend to increase the values of $m_1$ in Equation 2-8.

Finally, possible inaccuracies exist in the reference measurements because the standard error for the in situ measurements ranged from 1.2 to 1.5 MPa for the three principal stresses (Zimmerman and Vollendorf, 1982). The reference stress could easily be lower or higher by a similar amount.

The rock had a distinct fracture discontinuity in the plane of loading. This fracture appeared to propagate slightly in Cycle C1 because the most significant nonlinear behavior occurred in that cycle (see standard errors in Table 2-2). It appears that the fracture activated to accommodate that magnitude of flatjack pressure and then it was less influential thereafter. The pressure-deformation plots (Zimmerman, 1992b) show that there was more inelastic behavior in the first cycle. The influence of the fracture on the measurements would be extremely difficult to predict. It is assumed that larger pin deformations resulted because of the fracture. This condition would tend to make the $m_1$ quantities in Equation 2-8 smaller.

The alcove was mined some 5 years before the pressurized slot measurements were made. During this period there were other nearby mining activities that could have influenced the stress state at the surface of
the drift. In considering Item 3, it would be extremely difficult to quantify these effects on the reference in situ normal stress prediction. Additional measurements would help to assess this situation.

Finally the last item has been a concern for some time in FC measurements. Alexander (1960) noted that continued deformation occurred after the slot was cut and was observed to be associated with site jointing. He observed that creep deformations on the order of 20% of the slot cutting deformations occurred in a period of 4 to 6 days. He included these deformations in his FC determinations. Hoskins (1966) also considered the creep problem. He showed that the majority of the creep occurred in a period of 10 to 20 hours after the slot was cut for sound rock (marble) and that creep in an unsound concrete was still increasing after 150 hr.

The approach used in this testing was to measure the convergence of the pin immediately after the slot was cut, the cutting period took approximately 8 hr. These distances became the reference for FC determinations. Our goal was to minimize the time-dependent factors using the rapid slot cutting method. Available creep data (Zimmerman, 1992b) indicated that the slot diverged after the slot cutting, an apparent contradiction to the behaviors observed by Alexander and Hoskins. Apparently there were complications in the stress state around the slot that caused this behavior, the fracture could be the major influence. It is possible that measurement of the displacements immediately after the slot cutting incorporated some distortions caused by the slot cutting and that these residual deformations were later released. In this case, the reference deformations would appear to be too large and the net effect would be a reduction of the FC pressure. This contradiction would be difficult to resolve without further testing.

Alexander (1960) also reported that repeated cycles gave no significant change to the cancellation pressure or permanent deformation. This supports the observations made in these measurements.
2.3 Summary of Normal-Stress Determinations

This chapter has presented data from Whittemore surface pin measurement systems that were applied in three separate test cycles. The cycles were designed to provide data that could be used in determining the normal stress to the slot that was cut using a chain saw with diamond-tipped cutters. Data from the Whittemore measurements were reasonably uniform (see Zimmerman, 1992b) and were averaged to make normal stress estimates.

The surface pin measurements showed two trends (Zimmerman, 1992b). First, the initial values appeared to change with progressive testing and were thought to be related to a major fracture that was activated in the plane parallel to flatjack pressure. Second, the three cycles of flatjack pressure increases were quite repeatable.

The surface pin measurements were converted to normal stresses using elastic-based equations formulated by Alexander (1960). Some assumptions were made in representing the flatjack and slot that could have influenced the results because it was known that the rock was not elastic or intact. It could be expected that the stresses determined from the measurements would be low. The measured results were converted to a nominal stress range of 1.9 to 2.2 MPa for the three cycles. The mean was 2.1 MPa. This quantity is some 36% of the in situ estimate of 5.9 MPa. We were not able to determine the primary contributors to this difference.
3.1 Instrumented Flatjack Measurements

Insertion of a flatjack into a slot in a rock mass provides capabilities that can be used to evaluate the mechanical responses of the rock mass. Measurements of the displacements of the flatjack surface during pressurization provide data that can be used to determine rock mass stiffness characteristics (called the modulus of deformation) (Rocha, 1966). Modulus of deformation measurements with instrumented flatjacks were taken in Cycles D1 through D4 (Zimmerman, 1992a) in this testing.

3.1.1 Deformation Coefficient Analyses

Rocha and da Silva (1970) expressed the modulus of deformation as

\[ E_d = K(ΔP/Δw) \]  \hspace{1cm} (3-1)

where

\( E_d \) = modulus of deformation

\( K \) = constant which has the dimension of length and whose value depends on the position of measurement within the flatjack, the dimensions of the loaded area, the shape of the loaded area, the position of the loaded area in the slot, and Poisson's ratio

\( ΔP \) = change in flatjack pressure

\( Δw \) = change in displacements for sensors located in the flatjack.

The overall focus in this section is to determine quantitative values for the constant \((K)\), which is called the deformation coefficient. In Zimmerman, 1992a, a review of previous testing efforts indicated that interpretation of the results from instrumented flatjacks is a major
problem with this type of testing. In particular, Rocha and da Silva (1970) used small-scale model tests to determine the K factor. Small hard rubber bearing plates were placed on plaster and diatomite blocks and pressure-deformation responses were measured. This was a quarter-space representation in which the results could be checked with available analytical solutions. Rocha and da Silva recognized that the slot end effects were not accounted for in the modeling and they used a quasi-empirical numerical solution to investigate this. Their general conclusion was that slots should be much larger than the loaded areas to create a protection zone around the flatjacks.

The model and quasi-empirical approach were considered for this testing at SNL, but the expected variations in the testing with different size slots suggested that a refined analytical approach should be developed. The following paragraphs describe this approach.

The new concept is presented here to develop adjustment factors to provide a rational approach for converting the data to a useful quantity. These adjustment factors are discussed and quantified in the following subsections. The deformation coefficient is expressed in functional form as

\[ K_j = [F_{pj}F_{sj}F_cF_aF_p]C_e \quad (3-2) \]

where

- \( K_j \): deformation coefficient for sensor position \( j \) on the flatjack
- \( j \): index locating sensors on the flatjack, 1 is for sensors 1 and 2, 2 is for sensor 3, and 3 is for sensors 4 and 5. (See Zimmerman, 1992a.)
- \( F_{pj} \): adjustment factor for sensor position \( j \) in flatjack
- \( F_{sj} \): adjustment factor for surface proximity for position \( j \)
- \( F_c \): adjustment factor for slot continuity
Fa - adjustment factor for effective area of the flatjack

Fp - adjustment factor for pressure effect on sensor (see Zimmerman, 1992a)

Ce - reference deformation coefficient for center of flatjack (j = 2) as determined by an analytical solution for a semi-infinite medium.

Figure 3-1 shows a schematic illustrating the geometrical configurations of the adjustment factors.

A concern with the product formula presented in Equation 3-2 is the potential for cumulative error effects. Barry (1978) presents an approach to evaluate maximum errors. He advocates that the maximum possible errors are much more logically represented by probable rather than absolute values. The probable maximum error associated with the product of the six quantities in Equation 3-2 can be expressed as

\[
\Delta K_j = K_j \left[ \left( \frac{\Delta F_{pj}}{F_{pj}} \right)^2 + \left( \frac{\Delta F_{si}}{F_{si}} \right)^2 + \left( \frac{\Delta F_e}{F_e} \right)^2 + \left( \frac{\Delta F_a}{F_a} \right)^2 + \left( \frac{\Delta F_p}{F_p} \right)^2 + \left( \frac{\Delta C_e}{C_e} \right)^2 \right] \quad (3-3)
\]

where the Δ terms are the errors associated with each of the quantities. Equation 3-3 is applied after the individual terms have been presented.

3.1.1.1 Trends in Adjustment Factors

The five adjustment factors serve to modify the reference quantity Ce, which is determined from a linear-elastic solution. Each of the adjustment factors addresses specific corrections that have general trends. These trends are discussed here and then the reference deformation coefficient and each of the adjustment factors is quantified.

The first adjustment factor, Fpj, is a correction for sensors not located at the center of the flatjack. Off-center sensors deform less
Figure 3-1. Schematic Showing Representations for Adjustment Factors
because of the finite area of loading. The second quantity, $F_{s,j}$, accounts for larger deformations that occur nearer the free edge at the surface of a slot. The correction is an increase above the reference to balance larger deformations that are recorded. The third quantity, $F_C$, accounts for the effects of slot continuity. The reference solution is for a semi-infinite medium; the adjustment factor for the finite size of the slot is a reducing quantity because the slot continuity retards sensor deformations. The fourth quantity, $F_a$, is an adjustment factor for effective area. This is a reducing quantity because the loaded area is smaller than the flatjack dimensions. The fifth term, $F_p$, is also a reducing quantity because the sensors register smaller deformations with higher pressures (Zimmerman, 1992a).

Mention is made of the primary assumptions associated with the coefficient associated with Equation 3-2. The analyses are limited to two-dimensional representations, and assumptions of linear-elastic behavior are used. The data (Zimmerman, 1992b) showed that linear approximations for the internal sensor measurements are reasonable as long as initial nonlinear quantities are neglected. Also, stress effects are not considered in the analyses. Within the limitations of linear elasticity, this appears to be a reasonable assumption because loading changes are related to deformation changes as defined in Equation 3-1.

The following three subsections provide analyses and discussions that are based on (1) analytical solutions, (2) Rocha and da Silva's (1970) work, and (3) Conley's (1987) finite element modeling.

### 3.1 1.2 Determinations of $C_0$ and $F_{pj}$

Timoshenko and Goodier (1951) provide equations that can be used to describe the pressurized slot loadings. Figure 3-2 provides the geometry that is used to define the quantities. The Timoshenko and Goodier solution is for a square loading on a semi-infinite linear elastic medium. The following equations are used:
Figure 3-2. Linear-Elastic Loading Representation
\[ w(r = 0) = \frac{2.24(1 - \nu^2)aq}{E} \]  \hspace{1cm} (3-4)

\[ w(\text{corner}) = \frac{1.12(1 - \nu^2)aq}{E} \]  \hspace{1cm} (3-5)

\[ w(\text{av}) = \frac{1.90(1 - \nu^2)aq}{E} \]  \hspace{1cm} (3-6)

where

\[ w(r = 0) - \text{displacement at center of square} \]

\[ w(\text{corner}) - \text{displacement at corner of square} \]

\[ w(\text{av}) - \text{average displacement for square} \]

\[ \nu - \text{Poisson's ratio} \]

\[ a = 1/2 \text{ of square dimension} \]

\[ q - \text{normal pressure} \]

\[ E - \text{Young's modulus} \]

The quantities 2.24 and 1.12 are position coefficients.

Equations 3-4 through 3-6 can be rearranged in the form of Equation 3-1 as follows:

\[ E = (\text{position coefficient})(1 - \nu^2)aq/w \]
\[ = (C_e/2)(\Delta P/\Delta \omega_e) \]  \hspace{1cm} (3-7)

where

\[ C_e - \text{elastic deformation constant for center of flatjack. (The factor of 2 is necessary to convert from half-space to the instrumented flatjack configuration, where two surfaces are being pressurized.)} \]
\[ \Delta w_e = \text{deformation change of center sensor for the flatjack.} \]

The first quantity that is quantified is \( C_e \). Using Equation 3-7 and assuming \( q/w = \Delta P/\Delta w_e \), this is taken as

\[ C_e = 2(\text{position coefficient for center})(1 - \nu^2)a \]  
(3-8)

The position coefficient for the center is 2.24. Using \( a = 380 \text{ mm} \) and \( \nu = 0.2 \), the quantity \( C_e = 1634 \text{ mm} \).

One of the variables associated with the modulus of deformation determinations is Poisson's ratio. A value of 0.2 was used primarily in the determination of \( C_e \) (Equation 3-8). Zimmerman and Finley (1987) recommended an average value of 0.24 for rock masses, and a value of 0.29 for calculations near the surface of a drift. If this latter value were used, the coefficient \( C_e \) can be shown to be reduced by approximately 5%. This reduces the calculated modulus of deformation values by this amount. Use of field values of Poisson's ratio is not recommended because of the paucity of data available in G Tunnel and the relative insensitivity of the parameter. A common value for Poisson's ratio of 0.2 is used in calculations in this document.

Next, the quantity \( F_{pj} \) is determined. The Timoshenko and Goodier position coefficients for sensors located at the one-quarter point along the diagonal can be shown to be represented by 1.96. This factor is used to adjust analyses to account for actual locations of sensors away from the center sensor. Thus, the position coefficient for the center sensor (\( j = 2 \)) is represented by 2.24 and the others (\( j = 1, 3 \)) by the value 1.96. Sensor position values for \( j = 1 \) and 3 can be calculated to be 1,425 mm using a form of Equation 3-8. The ratio of the values at the one-quarter position and \( C_e \) (at the center) is 1425/1634 = 0.87. Thus, \( F_{p2} = 1.00 \) and \( F_{p1} = F_{p3} = 0.87 \) are the adjustment factors for Equation 3-2.

3.1.1.3 Determination of \( F_{sj} \)

For in situ investigations, the modulus of deformation is substituted for Young's modulus and elastic behavior is usually assumed. One of the
main contributions of Rocha and da Silva (1970) was identification of surface proximity or edge effects that differentiated quarter-space and half-space problems. Rocha and da Silva viewed PS testing as quarter-space equilibrium, where it was assumed that the rock mass was cracked in the plane of the slot, and the intersection of the drift and slot provided another surface. They knew of no analytical quarter-space solution and elected to determine a deformation coefficient quantity, called $C_s$ in this report, to correct for the quarter- and half-space differences. They used physical model tests to approach the problem and represented the quarter-space with a cube with an 80-cm edge. The cube was made of a mixture of plaster and diatomite. This material has a relatively high Young's modulus and a Poisson's ratio of approximately 0.2. They simulated multiple flatjacks with a hard rubber insert, which had dimensions of 3.00 x 3.75 cm with one edge rounded as shown in Figure 3-3. A single insert test compared very favorably with a Boussinesq analytical solution, thereby supporting the test procedure. Their results showed that the quarter-space edge effects were minimized beyond a distance of 3 m from the edge for a single, full-size flatjack. The solution shown in Figure 3-3 represents the deformation coefficient as it would apply to a flatjack test using the Rocha and da Silva configuration.

The computation of the quantity $F_{sj}$ involves several steps. It is useful to provide a theoretical bridge between the linear elastic square flatjack solution developed from Timoshenko and Goodier (1951) and the semi-rectangular shape of the flatjack used by Rocha and da Silva (1970) for later conversions. Timoshenko and Goodier provide an approximate solution for the rectangular loaded area. The equation is

$$w_{av} = \frac{mq(A^{-1/2})(1 - \nu^2)}{E} = \left(C_{av}/2\right)\frac{(\Delta P)/(\Delta w)}{,} \quad (3-9)$$

where

$m$ = numerical factor ranging between 0.94 and 0.95 for a rectangular shape
Figure 3-3. Rocha and da Silva (1970) C Factor Relationships
\( A \) = cross sectional area of rectangular loaded area

\( Ce_{av} \) = average deformation constant for rectangular flatjack.

Using (1) the same assumptions as in Section 3.1.1.2, (2) the lower of the numerical factors (0.94) and (3) assumption that there is no discount for the curved shape of the bottom of the Rocha and da Silva flatjack, the value of \( Ce_{av} \) for the rectangular shape \( (A' = 1.25 \times 760 \times 760 \text{ mm}) \) can be computed to be 1533 mm, which is some 11\% higher than the average value of 1386 mm for the square flatjack solution (Equation 3-6). Thus, it would be appropriate to decrease the \( C_s \) values by 11\% when converting Rocha and da Silva's \( C_s \) values to square flatjack applications. The shape factor used is 0.89.

The adjustment factor \( F_{sj} \) for sensors at locations at different depths from the surface can be computed using the curve on Figure 3-3. The term \( C_{sj} \) is used to apply Rocha and da Silva's quantities to this testing when \( h < 3.0 \text{ m} \) in Figure 3-3 (for \( j = 1 \) to 3). In this case, the distance to the sensor at the center of the flatjack must be determined and then a value of \( C_{sj} \) for other sensors can be taken from information contained in the figure. The surface proximity must be indexed to the half-space reference, and this is done by dividing each \( C_{sj} \) by the Rocha and da Silva's \( C_s \) value for the half space, \( C_{sr} = 2400 \). The adjustment factor is calculated using the following

\[
F_{sj} = 0.89 \frac{C_{sj}}{C_{sr}} \quad (3-10)
\]

Zimmerman, 1992a provided dimensions for the instrumented flatjack test cycles. For Cycles D1 and D2, the quantities \( h_1 \) (for sensors 1 and 2), \( h_2 \) (for sensor 3), and \( h_3 \) (for sensors 4 and 5) can be estimated to be 0.3, 0.5, and 0.7 m, respectively. Using Figure 3-3, the respective values of \( C_s \) are 4000, 3600, and 3300. Thus, the values of \( C_{sj} \) can be represented as \( C_{s1} = 4000 \), \( C_{s2} = 3600 \), and \( C_{s3} = 3300 \) mm for Cycles D1 and D2. The three adjustment factors are \( F_{s1} = 1.48 \), \( F_{s2} = 1.34 \), and \( F_{s3} = 1.22 \). The values of \( C_{sj} \) for Cycles D3 and D4 can also be estimated using Figure 3-3. In this case, the distance to the center was approximately 0.9 m. This means that the \( h_1 \) distance was 0.7 m, and the \( h_3 \) distance was 1.1 m. The
three values of $C_{sj}$ from Figure 3-3 are 3300, 3100, and 2900 mm, respectively, for these two cycles. The three adjustment factors are $F_{s1} = 1.22$, $F_{s2} = 1.15$, and $F_{s3} = 1.08$.

3.1.1.4 **Determination of $F_c$**

Assumptions in deriving Equation 3-2 are that the slot is infinite and that the loaded area is finite. It is realized that the slots are finite, and slot end effects should be considered. Rocha and da Silva (1970) studied the presence of a planar discontinuity in the plane of the flatjack using a linear-elastic finite element solution with circular geometry. They expressed their variables in terms of $D$, the diameter of the finite slot, and $d$, the diameter of the circular flatjack. They found that the predicted infinite slot displacements were 60% higher than for conditions where $D/d = 1.09$, and that the infinite slot displacements were only 20% greater when $D/d = 2$. They recommended that a protection zone be created around the flatjacks by cutting slots that were larger than the flatjacks. The problem was also addressed by Conley (1987). The following paragraphs provide background to Conley's results.

The conditions and interpretation problems known at the initiation of this testing effort (Zimmerman, 1992a) suggested that finite element representations could be used to improve results for instrumented flatjack testing. Conley (1987) conducted finite element calculations for typical PS testing configurations using the finite element code JAC (Biffle, 1984). A feature in the finite element modeling was the use of linear-elastic (L-E) and compliant-joint (C-J) material models. The L-E model incorporated an elastic modulus that was reduced to approximate field conditions. The C-J model was developed by Thomas (1982) to better represent conditions in a fractured rock. The C-J model is an equivalent continuum that has provisions for nonlinear compressive joint closure and joint shear. The C-J model requires a more elaborate joint representation and allows no tensile strength to be developed in a direction perpendicular to the plane of the joints. Major differences in the L-E and C-J model representations for the rock mass are (1) the lack of tensile strength and
Table 3-1 lists the material properties used by Conley. The table shows distinctions for in situ rock and blasted rock. Conley assumed that 1 m on the outer surface of the drift was blast damaged. For L-E representations, he assumed that the modulus of deformation for the blast damaged zone was one-half of the in situ rock. For C-J representations, he assumed that the joint spacing was reduced by one-half in the blast-damaged zone.

It is useful to review the Table 3-1 joint parameters used by Conley to establish a basis for the fractured rock description. A joint spacing selected by Conley (25 mm) for the in situ conditions is considered small. This joint spacing converts to a frequency of 40 joints/m. Langkopf and Gnirk (1986) reported that the normal frequency in G-tunnel is 3.0 to 4.5 joints/m. On the other hand, the unstressed joint aperture selected by

<table>
<thead>
<tr>
<th>Analyses</th>
<th>Property</th>
<th>In situ</th>
<th>Blast Damaged</th>
</tr>
</thead>
<tbody>
<tr>
<td>Linear Elastic</td>
<td>Modulus of Defor.</td>
<td>15.1 GPa</td>
<td>7.5 GPa</td>
</tr>
<tr>
<td></td>
<td>Poisson’s Ratio</td>
<td>0.22</td>
<td>0.22</td>
</tr>
<tr>
<td>Compliant Joint</td>
<td>Young’s Modulus</td>
<td>31.1 GPa</td>
<td>31.1 GPa</td>
</tr>
<tr>
<td></td>
<td>Poisson’s Ratio</td>
<td>0.22</td>
<td>0.22</td>
</tr>
<tr>
<td></td>
<td>Joint Unstressed Aperture</td>
<td>70 μm</td>
<td>70 μm</td>
</tr>
<tr>
<td></td>
<td>Joint Half Closure Stress</td>
<td>5 MPa</td>
<td>5 MPa</td>
</tr>
<tr>
<td></td>
<td>Joint Cohesion</td>
<td>0.1 MPa</td>
<td>0.1 MPa</td>
</tr>
<tr>
<td></td>
<td>Joint Coefficient of Friction</td>
<td>0.8</td>
<td>0.8</td>
</tr>
<tr>
<td></td>
<td>Joint Shear Stiffness</td>
<td>1E + 04 GPa</td>
<td>1E + 04 GPa</td>
</tr>
<tr>
<td></td>
<td>(Before Slip)</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Joint Shear Stiffness</td>
<td>1E + 02 GPa</td>
<td>1E + 02 GPa</td>
</tr>
<tr>
<td></td>
<td>(After Slip)</td>
<td>50 mm</td>
<td>25 mm</td>
</tr>
</tbody>
</table>

TABLE 3-1
FINITE ELEMENT MODEL MATERIAL PROPERTIES
Conley was 70 μm, where Zimmerman and Finley (1987) recommended a value of 262 μm for unstressed conditions. Zimmerman and Finley discuss the parametric effects on the modulus of deformation. Using their approach it can be shown that the predicted modulus of deformation would be 17.8 GPa at a normal stress of 5 MPa using the higher joint frequency. Conley's values would lead to a predicted modulus of 7.1 GPa at the same normal stress. Conley's value is consistent with the values used in the L-E analyses for the blast damaged zone (7.5 GPa). One could suggest that Conley's values represent a lower bound for fractured rock, but it should be remembered that Conley utilized only one set of joints that were parallel to the slot. Conceivably, the effects of joints with other bearings could tend to "soften" the rock mass response, suggesting that the representation by Conley is reasonable. In this report, Conley's results are used to define the slot adjustment.

Figure 3-4a shows Conley's representation of the instrumented flatjack test. The figure shows that pressure was applied over a length of 50 cm. This was a two-dimensional, plane-strain representation of the pressure of one-half of a flatjack. The boundaries next to the slot were roller boundaries, and the remaining boundaries were pressure or roller boundaries representing far field influences. Conley analyzed boundary effects and minimized them by using a mesh that was 10-m square.

Figure 3-4b shows Conley's representations of the finite and infinite slot representations. The solution was prepared for a large pressure range and is for a point located 125 mm from the edge of the slot, as shown in Figure 3-4a. Unfortunately this is the only usable solution presented by Conley. It is expected that differences at the slot surface could be slightly higher. The plots show that the L-E-infinite slot solution compares well with both of the C-J solutions. The only significant difference is the solution associated with the L-E-finite slot. For the maximum pressures associated with this study (10 MPa), the increase of the L-E-infinite slot displacement over the L-E-finite slot magnitude is taken as 1.14.
Figure 3-4. Conley (1987) Representations Using Elevation View
Unfortunately there are no direct correlations between Rocha and da Silva's work, Conley's results, and the results of this study because of differences in geometry. An empirical approach is used to account for these differences. The PS testing occurred in three different slots, each with a different geometry. Cycles D1 and D2 were in a rectangular shaped slot where the ratio of the nominal length of the slot, L, to the length of the flatjack, l, was 1.25. Cycle D3 was conducted in a slot with an irregular outline (Zimmerman, 1992a). The effect of the irregular shape can be approximated using areas. The area of the slot was approximately 3.3 times the area of the flatjack. Thus, the equivalent L/l ratio would be the square root of 3.3 or 1.8. The equivalent ratio L/l for Cycle D4 was approximately 2.5. Using these L/l ratios as D/d ratios and Rocha and da Silva's curves, the predicted decreases in displacements over the semi-infinite condition (D/d = ∞) would be approximately 0.68 for Cycles D1 and D2, 0.79 for Cycle D3, and 0.84 for Cycle D4. Conley's results show that the corresponding decrease in displacement for continuity should be nearer 1/1.14 = 0.88 for a very low L/l ratio. Rocha and da Silva would predict a ratio of approximately 0.6 for the same L/l condition.

It is thought that the Rocha and da Silva approach overpredicts slot continuity effects. An intuitive approach is to empirically reduce Rocha and da Silva apparent overpredictions by taking the square root of the Rocha and da Silva numbers. This approach would correspond to Fc = 0.82 for Cycles D1 and D2, Fc = 0.89 for Cycle D3, and Fc = 0.92 for Cycle D4. These are the numbers used in this report.

3.1.1.5 Determinations of Fa and Fp

The analyses in Section 3.1.1.2 through 3.1.1.4 have provided values for Fpj, Fsij, Ff, and Ce in Equation 3-2. One of two remaining adjustment factors deals with the effective area. A linear value used is compatible with Equation 3-8. A linear interpretation of the calibration results provides a value of 0.96 for Fa, which is the square root of the effective area factor 0.93, which was discussed (Zimmerman, 1992a).
The final adjustment factor, $F_p$, accounts for pressure variations on the sensor (discussed in Zimmerman, 1992a). Equation 5-1 (Zimmerman, 1992a) showed that the sensor reading was related to the pressure in the flatjack. The adjustment factor $F_p$ can be represented by

$$F_p = \frac{188.6}{188.6 + 0.8914P}, \quad (3-11)$$

The function of Equation 3-11 is to relate sensor pressure dependencies to the readings at zero pressure. The quantity $F_p$ can be calculated at the maximum pressures to be: 0.96 for Cycle D1, 0.94 for Cycle D2, 0.95 for Cycle D3, and 0.96 for Cycle D4.

### 3.1.2 Modulus of Deformation Determinations Using Instrumented Flatjack Measurements

Table 3-2 provides a summary of the adjustment factors and $K_j$ values determined in this report. Values for the modulus of deformation using the instrumented flatjack data are calculated using the information in Tables 3-2 and 3-3. Measured values in Table 3-3 are from Zimmerman, 1992b.

The results of the modulus of deformation determinations using Equations 3-1 and 3-2 are presented in Table 3-4. The quantities shown in the table are corrected for the adjustment factors discussed previously and represent estimates that can be extracted from the measurements.

Within limits, the use of the adjustment factors should reduce errors not associated with rock behavior. Equation 3-3 can be applied to estimate the errors. If it were assumed (1) that

$$F_{pj} = 1.00, \quad F_a = 0.96, \quad F_{sj} = 1.34,$$
$$F_p = 0.96, \quad F_c = 0.82, \quad C_e = 1634 \text{ mm}, \quad K_2 = 1655 \text{ mm}$$

and (2) the $\Delta$ terms for all of these quantities was a constant 10%, Equation 3-3 would predict a $\Delta K_j$ of 407 mm. Thus, the mathematical process to calculate the modulus of deformation has a built-in maximum error of $407/1655 = \pm 0.246$ if the 10% variations were randomly applied. Maximum errors of this magnitude suggest that precise determinations of the
TABLE 3-2
CALCULATIONS FOR DEFORMATION COEFFICIENTS

Instrumented Flatjack Measurements

<table>
<thead>
<tr>
<th>Cycle</th>
<th>j*</th>
<th>Fp1</th>
<th>Fs1</th>
<th>Fc</th>
<th>Fsa</th>
<th>Fp</th>
<th>Ce</th>
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</thead>
<tbody>
<tr>
<td>D1</td>
<td>1</td>
<td>0.87</td>
<td>1.48</td>
<td>0.82</td>
<td>0.96</td>
<td>0.96</td>
<td>1634</td>
<td>1590</td>
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<tr>
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<td>2</td>
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<td>1.34</td>
<td>0.82</td>
<td>0.96</td>
<td>0.96</td>
<td>1634</td>
<td>1655</td>
</tr>
<tr>
<td>D1</td>
<td>3</td>
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<td>1.22</td>
<td>0.82</td>
<td>0.96</td>
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<td>1311</td>
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<tr>
<td>D2</td>
<td>1</td>
<td>0.87</td>
<td>1.48</td>
<td>0.82</td>
<td>0.96</td>
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<td>1557</td>
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<tr>
<td>D2</td>
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<td>1.00</td>
<td>1.34</td>
<td>0.82</td>
<td>0.96</td>
<td>0.94</td>
<td>1634</td>
<td>1620</td>
</tr>
<tr>
<td>D2</td>
<td>3</td>
<td>0.87</td>
<td>1.22</td>
<td>0.82</td>
<td>0.96</td>
<td>0.94</td>
<td>1634</td>
<td>1283</td>
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<tr>
<td>D3</td>
<td>1</td>
<td>0.87</td>
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<td>0.89</td>
<td>0.96</td>
<td>0.95</td>
<td>1634</td>
<td>1408</td>
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<tr>
<td>D3</td>
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<td>1.00</td>
<td>1.15</td>
<td>0.89</td>
<td>0.96</td>
<td>0.95</td>
<td>1634</td>
<td>1525</td>
</tr>
<tr>
<td>D3</td>
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<td>1.08</td>
<td>0.89</td>
<td>0.96</td>
<td>0.95</td>
<td>1634</td>
<td>1246</td>
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<tr>
<td>D4</td>
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<td>0.87</td>
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<td>0.92</td>
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<td>0.96</td>
<td>1634</td>
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</tr>
<tr>
<td>D4</td>
<td>2</td>
<td>1.00</td>
<td>1.15</td>
<td>0.92</td>
<td>0.96</td>
<td>0.96</td>
<td>1634</td>
<td>1593</td>
</tr>
<tr>
<td>D4</td>
<td>3</td>
<td>0.87</td>
<td>1.08</td>
<td>0.92</td>
<td>0.96</td>
<td>0.96</td>
<td>1634</td>
<td>1302</td>
</tr>
</tbody>
</table>

*j = 1 = sensors 1 and 2
    = 2 = sensor 3
    = 3 = sensors 4 and 5

The modulus of deformation may not be possible with the adjustment factor formulation used in Equation 3-2.

The applications of the error analysis suggest that the modulus of deformation representations could be expressed in terms of averages of data from all sensors for each of the test cycles. This is shown in Table 3-4 where appropriate standard deviations of the averages are shown. The averages are the quantities used for further discussions.
**TABLE 3-3**

**SUMMARY OF LINEAR RESULTS FROM INTERNAL SENSOR MEASUREMENTS**
(Zimmerman, 1992b)

<table>
<thead>
<tr>
<th>Sensor</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>Average +1 Std. Dev.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>MPa/mm</td>
<td>MPa/mm</td>
<td>MPa/mm</td>
<td>MPa/mm</td>
<td>MPa/mm</td>
<td>(MPa/mm)</td>
</tr>
<tr>
<td>Test Cycle</td>
<td>(MPa)</td>
<td>(MPa)</td>
<td>(MPa)</td>
<td>(MPa)</td>
<td>(MPa)</td>
<td></td>
</tr>
<tr>
<td>D1</td>
<td>6.0</td>
<td>4.9</td>
<td>9.3</td>
<td>10.4</td>
<td>12.6</td>
<td>8.64 ± 3.17</td>
</tr>
<tr>
<td></td>
<td>(+0.08)</td>
<td>(+0.12)</td>
<td>(+0.34)</td>
<td>(+0.11)</td>
<td>(+0.21)</td>
<td></td>
</tr>
<tr>
<td>D2</td>
<td>5.6</td>
<td>4.8</td>
<td>7.3</td>
<td>11.3</td>
<td>13.8</td>
<td>8.56 ± 3.86</td>
</tr>
<tr>
<td></td>
<td>(+0.24)</td>
<td>(+0.32)</td>
<td>(+0.17)</td>
<td>(+0.15)</td>
<td>(+0.08)</td>
<td></td>
</tr>
<tr>
<td>D3</td>
<td>1.9</td>
<td>2.2</td>
<td>3.3</td>
<td>--</td>
<td>--</td>
<td>2.47 ± 0.74</td>
</tr>
<tr>
<td></td>
<td>(+0.26)</td>
<td>(+0.26)</td>
<td>(0.12)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>D4</td>
<td>1.2</td>
<td>1.1</td>
<td>1.2</td>
<td>1.3</td>
<td>--</td>
<td>1.20 ± 0.08</td>
</tr>
<tr>
<td></td>
<td>(+0.14)</td>
<td>(+0.11)</td>
<td>(+0.12)</td>
<td>(+0.09)</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**TABLE 3-4**

**SUMMARY OF MODULUS OF DEFORMATION DETERMINATIONS FROM INTERNAL SENSOR MEASUREMENTS**

<table>
<thead>
<tr>
<th>Modulus of Deformation</th>
<th>Sensor</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
</tr>
<tr>
<td>Cycle</td>
<td>(GPa)</td>
</tr>
<tr>
<td>D1</td>
<td>9.5</td>
</tr>
<tr>
<td>D2</td>
<td>8.7</td>
</tr>
<tr>
<td>D3</td>
<td>2.6</td>
</tr>
<tr>
<td>D4</td>
<td>1.4</td>
</tr>
</tbody>
</table>

Mean for Cycles D1 and D2 = 12.3 GPa.
Mean for Cycles D3 and D4 = 2.6 GPa.
The results of Cycles D3 and D4 were distinctly different than those in Cycles D1 and D2. These differences are large enough to warrant additional discussion. Cycle D1 and D2 testing was conducted in relatively shallow and small slots in a densely welded tuff and results were reasonably uniform. Cycles D3 and D4 were conducted in deeper and larger slots in a moderately welded tuff. Reasons to explain the differences in magnitudes for Cycles D1 and D2 and D3 and D4 include

1. an added steel sheet (thickness = 1.6 mm) that was used in Cycle D3 possibly caused the rock to appear to be stiffer than D4,

2. the presence of rubble pockets in the EXP slots (D3, D4) was not uniform possibly causing the stiffness to be lower.

3. the decreased size and increased continuity at the bottom of the slot EXP-NS (D3) caused the slot to be stiffer,

4. the presence of the horizontal fracture in slot EXP-EW (D4) caused the entire slot to be less stiff because of lack of continuity, and

5. the inherent differences in the behavior of the densely and moderately welded tuffs.

For Cycle D3 (Item 1), a stainless steel sheet was placed on the west side to facilitate flatjack removal and to cover a cavity observed during impression flatjack testing (Zimmerman, 1992a). A major difference in Cycles D3 and D4 was that in Cycle D3 one active flatjack surface under tension was pressing against the rock and one passive surface was sandwiched between the flatjack and the rock. In Cycle D4 there were two tensioned surfaces pressing against the rock. The influence of the untensioned metal sheet is not understood. It could act as a thin slab. The thickness of the sheet was small, but the stress-strain behavior of the sheet would be stiffer than the rock, and possibly this dominated the surface behavior of the flatjack. There is a possibility that complex friction forces developed between the flatjack surface, the metal sheet,
and the rock surface on the west side for Cycle D3 and these forces caused the rock surface to appear stiffer. Another test without the sheet would be necessary to resolve this issue.

The second item, the possible influence of the rubblized regions, could have influenced the measurements in slot EXP-EW the most and caused that slot to appear to be less stiff. There was visible evidence in the surrounding rock that rubble pockets were present (Zimmerman, 1992a). We encountered pockets in cutting the slots, but did not characterize them. The slot surfaces were filled with Sulfaset as part of the slot-cutting effort. We did not determine the extent of Sulfaset penetration into the pockets. The possible impacts of these rubble pockets could have been significant.

The third item deals with the uncertainty of the effects of the incompleted slot for Cycle D3. Figure 5-2 (Zimmerman, 1992a) shows that the lower corners of the flatjack were nearest to the edges of the slot. The distance was approximately 7.5 cm. Figure 5-2 also shows that the top edge of the slot was approximately 18 cm away from the top of the flatjack. Differences in testing conditions between the three slots can be represented in area ratios. For Cycles D1 and D2, the ratio of the gross area of the flatjack to the area of the slot was approximately 0.64. For Cycle D3, the ratio was estimated to be 0.30, and for Cycle D4, the corresponding ratio was 0.16. Based on a comparison of the area ratios, Cycle D3 should not have been overly influenced by the slot. On the other hand, sensor 3 in Cycle D3 was the closest to the edge of the slot and displayed the stiffest behavior. It is unfortunate that sensors 4 or 5 were inoperable in this cycle because these results could have been used to shed light on the possible slot edge effects.

The fourth item deals with the horizontal fracture. The presence of a major fracture near a pressure loading region suggests that the rock response could be less stiff. If the open horizontal fracture (see Zimmerman, 1992a) were of sufficient length into the rock, the rock below the flatjack would not have been fully resisting the pressurized area deformations and the slot would appear to be less stiff. Due to Boussinesq-type stress distributions, it would be reasonable to assume that
only some asperities of the horizontal fracture would be in contact and that there would be limited shear transfer to the lower rock.

The last item for consideration is the difference in the materials. The moderately welded tuff has more porosity than the densely welded tuff and is expected to have a lower intact rock Young's modulus. Price and Bauer (1985) developed a relationship expressing Young's modulus as a function of porosity and empirical constants:

\[ E_0 = 85.5e^{-6.96\phi_f} \]  

(3-12)

where

- \( E_0 \) - Young's modulus (GPa)
- \( \phi_f \) - functional porosity.

The nominal value for the porosity of the densely welded tuff is 15% (Zimmerman and Finley, 1987). This would result in a predicted value of \( E_0 = 30 \) GPa using Equation 3-12. Langkopf and Eshom (1982) characterized the moderately welded tuff. It might be conservatively estimated that the porosity of this tuff was 30%. Using this porosity, the average value of \( E_0 \) would be 10.6 GPa, some 35% of the value for the densely welded tuff. This magnitude can be applied to the quantities computed in Table 3-4. The values for the modulus of deformation could be reduced to account for material differences by multiplying the Cycle D1 and D2 values by 10.6/30 and the results are 4.4 and 4.2 GPa, respectively. These predicted values are still somewhat higher than the mean values of 3.6 and 1.6 for Cycles D3 and D4 as shown in Table 3-4.

Differences in modulus of deformation determinations for the two drifts cannot be totally accounted for. There were distinct material differences that could be quantified to some extent. Slot continuity effects due to different slot shapes could be a minor factor. This leaves the possible effects of undetected discontinuities around the slots. These
discontinuities were primarily rubble pockets that were prevalent in the moderately welded tuff of the Experiment Drift. The slot-cutting process in this drift required frequent applications of Sulfaset to stabilize the slots. The presence of the Sulfaset made slot inspections more difficult. In future efforts where testing has to be conducted in noticeably nonuniform materials, it is recommended that either (1) the slot should be inspected more thoroughly before Sulfaset applications or (2) the slot surfaces should be overcored to better characterize the materials after the testing is completed.

In summary, modulus of deformation estimates were determined using a new formulation and data from four sets of instrumented flatjack measurements. Results are in Table 3-4. Error analyses of the data conversion equations suggested that the average values from multiple sensors were perhaps the best representations of the moduli. Analyses of measurements in two different drifts indicated that there were major differences that could not be fully accounted for.

3.2 Surface Pin Measurements

The equipment and methods used in the determination of the normal stresses (Chapter 2.0) can be used to determine the modulus of deformation. Deformations of pins located on the surface of the slot were monitored during pressurizations of Cycles D1 and D2 and the results are presented here.

3.2.1 Deformation Coefficient Analyses

The overall approach in these analyses is the same as in Section 3.1.1. Adjustment factors are developed to correct for various physical conditions. The general formula for determining the modulus of deformation from surface pin measurements is taken from linear elasticity and can be represented as

\[ E_d = K' \left( \frac{\Delta P}{\Delta w'} \right) \]  

(3-13)
where

\[ E_d \] - modulus of deformation

\[ K' \] - general deformation coefficient incorporating various combinations of slot and flatjack dimensions, locations of measurement pins, magnitudes of principal stresses, and Poisson's ratio

\[ \Delta P \] - change in flatjack pressure

\[ \Delta w' \] - change in displacements for surface measurement pins.

The deformation coefficient for specific surface measurements is expressed in functional form as

\[ K_i = [F_d F_{li} F_s F_a] K_e \] , \hspace{1cm} (3-14)

where

\[ K_i \] - deformation coefficient for Equation 3-13 for measurement position \( i \)

\( i \) - index for measurement lines, \( i = 1 \) for lines 2 and 3 and \( i = 2 \) for lines 1 and 4. (See Zimmerman, 1992a for locations of lines relative to center of the flatjack.)

\[ F_d \] - adjustment factor for slot continuity

\[ F_{li} \] - adjustment factor for measurement position lines

\[ F_s \] - adjustment factor for surface proximity of flatjack

\[ F_a \] - adjustment factor for effective area (see Equation 3-2)
K_e - deformation coefficient for center of flatjack in a finite slot as determined by an analytical solution.

3.2.1.1 Trends in Adjustment Factors

Figure 3-5 shows a schematic illustrating the adjustment factors. The four adjustment factors serve to modify the reference quantity K_e, which is determined for a linear-elastic solution of a pressurized cavity. Each adjustment factor addresses specific corrections and general trends are evident. These general trends are discussed here. The first adjustment factor deals with slot end effects. The reference solution is for a slot where the length of the slot is the same as the pressurized length. In testing, the slots were larger than the pressurized areas. Therefore the slot continuity effects should show increasing trends to account for end restraints. The measurement position corrections (F_11) apply to off-center instrument sensors. Outlying pins measure smaller displacements, and therefore, corrections are reducing quantities. Corrections for surface proximity are increasing as the flatjack is nearer the surface as was the case with the instrumented flatjack corrections. Similarly, corrections for effective area are reducing.

3.2.1.2 Determination of K_e

Alexander (1960) published analyses of pressurizing an infinitely thin elliptical opening that was the length of the flatjack. This is the most complete analytical representation found that can be used in Equation 3-13. His solution (Equation 2-4) can be related to Equation 3.13.

\[ E - P(C_0/w') [(1 - \nu)((1 + (Y/C_0)^2)^{1/2} - Y/C_0) + (1 + \nu)/(1 + (Y/C_0)^2)^{1/2}] = (Ke/2)(\Delta P/\Delta w') , \quad (3-15) \]

where

\[ E = \text{Young's modulus} \]
Figure 3-5. Schematic Illustrating Adjustment Factors
\( \Delta w_j \) - displacement of single surface caused by raising flatjack pressure

\( \Delta P \) - flatjack pressure change

\( K_e \) - average deformation constant for square flatjack

\( Y \) - distance of measuring point from major axis of slot

\( C_0 \) - half length of flatjack

\( \nu \) - Poisson's ratio.

If the values of \( C_0 = 380 \text{ mm} \), \( Y = 125 \text{ mm} \), and \( \nu = 0.2 \) and \( P/w^1 = \Delta P/\Delta w^1 \) are substituted into Equation 3-15, the quantity \( K_e = 1306 \text{ mm} \).

3.2.1.3 Determinations of \( F_d \), \( F_{14} \), and \( F_s \)

Conley's (1987) finite element calculations provide the basis for estimating all but the \( F_a \) adjustment factor. The new factors needing quantification are \( F_d \), \( F_{14} \), and \( F_s \). These are discussed individually.

Slot end effects were discussed in Section 3.1.1.4. In that case, the correction was to account for additional restraints that were provided by the slots because the reference solution was for a semi-infinite medium. In this case, the reference is a closed slot so a different continuity factor is used. These adjustments apply only to slot SDH-N1, which had the smallest \( L/\ell \) ratio (1.25). Conley (1987) showed that the maximum correction should be on the order of 1.14 for an infinite slot while Rocha and da Silva (1970) would predict a value over 1.5 for a stress magnitude of 10 MPa. A review of the Rocha and da Silva solution shows that the correction for \( L/\ell = 1.25 \) should be on the order of 1.08. Conley's results suggest that this should be reduced for \( L/\ell = 1.25 \). Using the Conley solution as a guide, the correction should be relatively small. Again, using the empirical square root modification to account for Conley's maximum, the empirical value for \( F_d \) can be represented as 1.04.
The quantity $F_{II}$ can only be approximated. Conley provided no direct solution regarding the displacement profile of measurement lines that were not at the center of the loading area. Analyses in Section 3.1.1.2 showed that there was a displacement gradient for the sensors in the instrumented flatjack. It is expected that there is also a gradient for the surface pins. Figure 3-6 is used to make this estimate. Curves in Figure 3-6 show the displacement profiles at the loaded regions for the L-E and C-J conditions at a normal stress of 30 MPa. The L-E curve shows a larger displacement gradient because of the tensile restraints at the end of the finite slot. This representation is perhaps too severe in estimating the displacement gradient at the surface. It is reasoned that the displacement gradient at the surface would be significantly reduced because of the distance from the flatjack to the surface pins. As a means of quantifying the displacement gradient, it is assumed that the surface pin gradient is the same as the C-J displacement shown in Figure 3-6. The positions for lines 2 and 3 are assumed to be at a distance of 120 mm from the centerline, and the distance to lines 1 and 4 is assumed to be 300 mm (see Zimmerman, 1992a). The displacements can be referenced to the center by dividing by the maximum C-J displacement (3.56 mm). Thus, $F_{II} = \frac{3.53}{3.56} = 0.99$, and $F_{I2} = \frac{3.20}{3.56} = 0.90$.

The adjustment factor for surface proximity can be estimated using another finite element model prepared by Conley (1987). Figure 3-7a shows a schematic of the model. In this case, Conley applied a pressure over a 1.0-m length. He called this a plan view. He performed the calculations by placing the pressurized length in three positions. Position 1 is as shown in the figure. Position 2 represents the condition when the top of the pressurized length is positioned 0.5 m lower, and Position 3 represents the condition when the top is positioned 1.0 m lower. Figure 3-7b shows the predicted displacements at the surface for the three flatjack positions. For the testing in Cycles D1 and D2, the distance to the top of the flatjack was estimated to be 0.1 m. Using linear interpolation in Figure 3-7b at a normal stress of 10 MPa, the correction for the placement of the flatjack beneath the surface can be estimated to be the ratio $1.85/1.60 = 1.16$. Thus, the adjustment factor $F_s$ can be represented by the quantity 1.16.
Figure 3-6. Pressure - Surface Profiles Using Elevation Views (Conley, 1987)
Figure 3-7. Conley Representations (1987) Using Plan View
A consideration in determining the surface proximity effects is the effect of rotation of the surface pins on the measurements. Conley (1987) provided slot-surface displacement profiles for the three flatjack positions described previously. The profiles showed that the slot surface had a displacement gradient (in the plan view) that was dependent on the position of the flatjack in the slot. The predicted gradient was most significant at the surface for Position 1, as expected. Unfortunately, Conley provided no guidance for displacement gradients at the locations of the measurement pins. It is expected that the displacement gradients, hence pin angles, would be smaller away from the slot. Also, the placement of the flatjacks 0.1 m away from the surface should reduce the gradient somewhat. For purposes of these calculations, the displacement gradients at the pins were assumed negligible and all surface proximity effects were lumped into $F_s$ as described in the previous paragraph.

3.2.1.4 Determinations of $F_f$ and $F_a$

Adjustment factors $F_f$ and $F_a$ are the same as in the instrumented flatjack analyses.

3.2.2 Modulus of Deformation Determinations Using Surface Pin Measurements

The values of $K_1$ are summarized in Table 3-5. The determination of the modulus of deformation using the adjustment factors in Equation 3-14 involves considerations of errors using Equation 3-3. If it were assumed (1) that

$$F_c = 1.04, \quad F_a = 0.96, \quad F_{11} = 0.99, \quad F_s = 1.16,$$

$$K_e = 1306 \text{ mm}, \quad K_1 = 1497 \text{ mm}$$

and (2) the $\Delta$ terms for each quantity was in error by 10%, the maximum error of the product would be 335. This represents $335/1497 = 22\%$ of the base product. Again maximum errors suggest that precise determinations of the modulus of deformation are not possible using Equation 3-14.
Table 3-6 shows the linear representations of the pressure-deformation measurements at the surface (Zimmerman, 1992b). Table 3-7 shows the calculated values for the modulus of deformation for the surface pin measurements using the information from Tables 3-5 and 3-6. The values for each of the lines are averaged to represent a single quantity for each test cycle. Table 3-7 shows that there is reasonable agreement between the results of the two measurements. The value for Cycle D1 is larger by approximately 2%. Comparisons of these values with those determined from the instrumented flatjack in Table 3-4 are discussed in the next section.

**TABLE 3-5**
CALCULATIONS FOR DEFORMATION COEFFICIENT - SURFACE MEASUREMENTS

<table>
<thead>
<tr>
<th>Cycle</th>
<th>i*</th>
<th>Fc</th>
<th>F1i</th>
<th>Fg</th>
<th>Fa</th>
<th>Ke</th>
<th>Kl</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>1</td>
<td>1.04</td>
<td>0.99</td>
<td>1.16</td>
<td>0.96</td>
<td>1306</td>
<td>1497</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1.04</td>
<td>0.90</td>
<td>1.16</td>
<td>0.96</td>
<td>1306</td>
<td>1361</td>
</tr>
<tr>
<td>D2</td>
<td>1</td>
<td>1.04</td>
<td>0.99</td>
<td>1.16</td>
<td>0.96</td>
<td>1306</td>
<td>1497</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1.04</td>
<td>0.90</td>
<td>1.16</td>
<td>0.96</td>
<td>1306</td>
<td>1361</td>
</tr>
</tbody>
</table>

*i* = 1 for lines 2 and 3, *i* = 2 for lines 1 and 4.

<table>
<thead>
<tr>
<th>Whittemore Line</th>
<th>Test Cycle</th>
<th>1 MPa/mm (MPa)</th>
<th>2 MPa/mm (MPa)</th>
<th>3 MPa/mm (MPa)</th>
<th>4 MPa/mm (MPa)</th>
<th>Average MPa</th>
<th>+1 Std. Dev.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>D1</td>
<td>9.2 (+0.23)</td>
<td>6.7 (+0.20)</td>
<td>6.6 (+0.19)</td>
<td>7.5 (+0.30)</td>
<td>7.50</td>
<td>+1.20</td>
</tr>
<tr>
<td></td>
<td>D2</td>
<td>9.0 (+0.22)</td>
<td>6.6 (+0.14)</td>
<td>6.5 (+0.11)</td>
<td>7.2 (+0.21)</td>
<td>7.35</td>
<td>+1.16</td>
</tr>
</tbody>
</table>
TABLE 3-7
MODULUS OF DEFORMATION DETERMINATIONS USING SURFACE LINE MEASUREMENTS

<table>
<thead>
<tr>
<th>Cycle</th>
<th>Line 1 (GPa)</th>
<th>Line 2 (GPa)</th>
<th>Line 3 (GPa)</th>
<th>Line 4 (GPa)</th>
<th>Ave (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>12.5</td>
<td>10.0</td>
<td>9.9</td>
<td>10.2</td>
<td>10.6 ± 1.24</td>
</tr>
<tr>
<td>D2</td>
<td>12.2</td>
<td>9.9</td>
<td>9.7</td>
<td>9.8</td>
<td>10.4 ± 1.20</td>
</tr>
</tbody>
</table>

Mean value is 10.5 GPa.

3.3 Summary of Modulus of Deformation Determinations and Related Discussions

The pressure-deformation measurements for determining the modulus of deformation with instrumented flatjacks occurred in four cycles in three different slots. Numerical values for the modulus of deformation were computed using a newly developed formulation incorporating adjustment factors. Adjustment factors were developed for (1) sensor position in the flatjack, (2) surface proximity for the flatjack, (3) slot continuity effects, (4) the effective area of the flatjack, and (5) pressure effects on the internal sensors. Analytical solutions, reference solutions, and numerical modeling results were used to quantify the adjustment factors. These were all related to a reference analytical solution to facilitate computations.

Cycles D1 and D2 were conducted in slot SDH-N1, which was cut with the 1.1-m chain saw in densely welded tuff. A major influence in the testing was the fracture in the plane of loading. Even with the fracture influence, the results were repeatable; they were determined to be 12.6 and 12.0 GPa for the two cycles. These results compared within 5%.
Cycles D3 and D4 were conducted in two slots in the Experiment Drift. These cycles were conducted in larger slots that were cut with the 2.1-m chain saw in a weaker material. The measured values for the modulus of deformation were 3.6 and 1.6 GPa, respectively. If approximations were applied to account for differences in welded tuffs, the results of Cycles D1 and D2 would be empirically corrected to values of 4.4 and 4.2 GPa, respectively. These values are somewhat higher than the measured values. It is thought that undetected discontinuities in and around slots EXP-NS and EW influenced the measured results, and the presence of these discontinuities was the primary reason for the differences between the results in the two drifts.

Surface pin measurements were used to determine the modulus of deformation for Cycles D1 and D2. The measured results were converted to modulus values using adjustment factors that accounted for (1) slot end effects, (2) measurement position on the surface, (3) influence of surface proximity, and (4) the effective area of the flatjack. Numerical values were determined using the same sources used in the instrumented flatjack conversions. The computed moduli of deformation were 10.6 and 10.4 GPa for Cycles D1 and D2, respectively. Differences between measurements were within 2%. The mean of these measurements is approximately 15% lower than the mean of the measurements taken with the instrumented flatjack.

It is informative to compare the results of these measurements and analyses with those that would result from use of International Society of Rock Mechanics (ISRM) suggested methods (Loureiro-Pinto, 1986). The ISRM suggested method applies to (1) slots cut with a disk saw or line drilling method, and (2) simultaneous tests on coplanar flatjacks filling the slots. The modulus of deformability of the rock mass is expressed as

\[ E_i = k_i (1 - \nu^2) (\Delta p / \Delta d_i) \]  

(3-16)

where

\[ E_i = \text{modulus of deformability at measuring point } i \]
\( k_i \) - coefficient depending upon the stiffness, shape, arrangement and number of flatjacks; on the location of the measuring point \( i \); on the shape of the test chamber; and on the depth of the crack (\( h \)) formed in the application of the pressure in the slots

\( \nu \) - Poisson's ratio (generally assumed to be 0.2)

\( \Delta p \) - increment of applied pressure

\( \Delta d_i \) - increment of the slot opening at measuring point \( i \) corresponding to the variation of the applied pressure.

Values for \( k_i \) were calculated for sample cases in the suggested test that were similar to this PS testing. Perhaps the two most important factors in using the ISRM methods are the number of flatjacks used and the estimate of the depth of crack formed in the testing. We used a single flatjack in each test so this factor is identified. The ISRM suggested method is based on the assumption that a crack is formed in the plane of the flatjack and the determination of the depth of crack is more difficult. If values of the normal stress (\( S \)), the tensile strength of the rock, and the maximum test pressure are known, the value for \( h \) can be estimated. The average value \( S \) was determined to be 2.1 MPa in Chapter 2.0. The tensile strength of the rock can be estimated to be 9.6 MPa (Zimmerman and Finley, 1987). The maximum test pressure was 13.3 MPa (Zimmerman, 1992a). Using these values, the quantity \( h \) can be determined to be 0.25 m. The value of \( k_i \) would be approximately 1700 mm for a single flatjack according to the suggested method.

Using a single value of \( k_i = 1700 \) mm, \( \nu = 0.2 \), and \( \Delta p/\Delta d_i = 8.6 \) MPa/mm (Table 3-3), Equation 3-16 would predict a value \( E_i \) of 14.0 GPa. This quantity is some 14% higher than the mean of the two values (12.3 GPa) obtained in the instrumented flatjack testing (Table 3-4) and 33% higher than the value obtained in surface pin measurements (Table 3-7). If \( h \) were taken to be one-half the difference between the dimension of the slot and the flatjack used in the measurements (0.1 m), the value of \( k_i \) would be approximately 1550 mm and the corresponding value of \( E_i \) would be 12.8 GPa.

3-35
It is interesting that the values obtained from these measurements and the predicted values from the ISRM suggested method are so close. There are similarities in the testing methods but differences in the data interpretation approaches. The ISRM suggested method is based on a reference case, where flatjack placements are near the surface and the dimensions and shapes of flatjacks are specified. For the comparable testing used here, the approach was to modify available elastic solutions. The ISRM suggested method is based on a single size flatjack and testing configuration, and the suggested method states that numerical calculations will have to be used to develop \( k_i \) values for shapes and testing conditions other than the reference. The approach used in Equations 3-2 and 3-14 allows multiple parameters to be incorporated into the analyses. The three approaches differ widely in the applications of the slot continuity. The ISRM suggested method uses an uncracked slot as the base case and compensates for cracking. Equation 3-2 relates to a pressurized loading on a semi-infinite plane as the base case and compensates for continuity. Equation 3-14 relates to a finite size uncracked slot. Use of the approaches in Equations 3-2 and 3-14 is perhaps more complicated, but it allows for more flexibility in data conversions and in many cases without extensive numerical calculations.

The results of the instrumented flatjack measurements in Cycles D1 and D2 are considered the most applicable for comparisons with results from other investigations. Zimmerman and Finley (1987) summarized results from the nearby heated block experiment, which was also in densely welded tuff. The range was from 9.2 to 19.1 GPa. They recommended a single value modulus of deformation of 16 GPa and an intact rock value of Young's modulus of 26 GPa.

It is interesting that the average measured values (10.5 and 12.3 GPa) for the modulus of deformation came out to be 41 and 47% of Young's modulus. The suggested ISRM method would predict a value of 54% of Young's modulus. Tillerson and Nimick (1984) studied various measurements of the modulus of deformation and made a recommendation that the in situ modulus of deformation be represented as 50% of Young's modulus for modeling efforts. This is slightly lower than the 62% recommended by Zimmerman and
Finley (1987). It is recognized that there were only two cycles conducted in the densely welded tuff, and it is known that there was a fracture in the plane of loading that most likely influenced the results. Nevertheless, the results are quite close. The results of this testing suggest that the lower percentage (50%) is more appropriate.

It is clear that PS testing, with improvements resulting from the developments in this study, could be used for making modulus of deformation determinations in future efforts. Recommended additions to future testing procedures are (1) careful mapping of the slot surfaces before testing, (2) either careful mapping of slots during the slot-cutting process or post-measurement slot investigations, (3) use of special numerical calculations designed to better predict adjustment factors for the actual tests conducted, and (4) use of slots much larger than flatjacks.
4.0 HIGH-PRESSURE TESTING

The focus in this testing program was on experiment development with the emphasis on determinations of normal stress and modulus of deformation. Applications of testing methods for in situ strength evaluations provided an additional dimension to PS testing. PS strength testing concepts are presented in Section 4.1 so that the interpretations and potential applications can be better understood in discussions in later sections. Section 4.2 covers PS strength testing developments, and Section 4.3 provides a summary of the results.

4.1 PS Strength Testing Concepts

There are limited applications of PS testing that can be used for compression and shear strength testing. These applications are introduced with reference to a bearing type test. Figure 4-1 shows concepts of a modified bearing test (MBT) that can be associated with PS testing. The term modified is used to indicate PS testing with loadings parallel to the surface of a drift, while loadings would normally be perpendicular to the drift surface in a standard bearing test.

An important point regarding a normal bearing type test is the lateral confinement due to Poisson's effect of the medium. This confinement tends to cause the rock to be in a triaxial stress state. Coates and Gyenge (1966) reported that the bearing failure strength in a brittle rock can be three times the unconfined compressive strength because of the multiaxial stress contributions. Thus, a bearing-type strength test increases loading requirements and few bearing tests have been conducted on hard rocks. Heuze (1980) reported results of bearing strength tests on a number of relatively soft rocks, where the maximum bearing strength was 29.9 MPa. This means that the unconfined compressive stress could be as low as 10 MPa. Welded tuffs in G-Tunnel have unconfined compressive strengths in excess of 96 MPa (Board et al., 1987).
Figure 4-1. Concepts for PS Strength Testing
The bearing-type test does not seem applicable to welded tuffs because of the relatively high compressive strengths. Even a pillar-type compression test might be difficult to run in the jointed hard rock. The MBT is a biaxial stress type test that serves to evaluate strengths near the surface of a slot and has applications for higher strength rocks.

The MBT (Figure 4-1a) has a possible application in determining wall slabbing potentials. Fairhurst and Cook (1966) showed that splitting parallel to the direction of maximum compression, in the vicinity of a surface in areas of high stress, is a frequently observed mode of macroscopic fracture of brittle rock. A highly stressed brittle rock is composed of incipient slabs produced by imperfections and discontinuities and slabs can form parallel to the face because of the low confinement provided by the opening. Eventually slabs can develop and fail by buckling. If a slab buckles, the next layer is loaded higher than it had been previously and the process can continue; however, the buckling potential diminishes at distances away from the surface because the broken slabs provide some confinement. The slabbing phenomenon can be important to drift stability considerations because surface failures are important in the selection of the optimum shapes for underground openings and also for designs of ground support.

The MBT concept can be taken one step further. Figure 4-1b shows that the PS test can be used to activate a joint in a shear plane having unfavorable geometry. The figure suggests that the concept could be used as a simplified joint shear test if a distinct fracture were available, and the slot could be cut such that a desired angle, as shown in Figure 4-1b, can be achieved. The angle could be selected to either ensure or prevent joint slip based on laboratory testing. This test could serve to confirm predictions based on laboratory efforts. The analysis could be more difficult than a direct joint shear test because of the effects of the ends of the slab. However, the ends (parallel with the view shown) could be cut with the chain saw, and a distinct block could be isolated, tested, and analyzed.
One possible testing configuration for potential slabbing investigations is to pressurize a vertical slot in the rib or wall of an underground opening. The rib would be under a vertical stress because of the excavation, and the horizontal stress could be increased with a flatjack causing a change in the biaxial stress state. It is recognized that the in situ stress fields around the opening are nonuniform and the flatjack pressures would be uniform and averaging techniques would be necessary in the data interpretations. Another possible testing configuration is a vertical slot in the crown on a drift, with the slot parallel to the drift. This would address stability evaluations that might be important when thermal stresses are considered. In either case, a slab could be formed with increased flatjack pressures.

Figure 4-2 shows a representation of stresses and displacements in a shallow slot under MBT pressures as calculated by Conley (1987). Conley's results are for a relatively high flatjack pressures using both the L-E and C-J models. In each case, the rock was predicted to respond with a nonlinear stress distribution. The figure also shows that interpretation of stress results would not be easy at locations away from the loaded surface. Numerical modeling approaches might be required. It is expected that the applied stresses in MBTs would manifest strengths that are much closer to the biaxial compressive strength of the rock as compared to a triaxial stress state formed in a normal bearing test. Later analyses will show that a failure criterion can be related to the stresses at the slot surface.

4.2 PS Strength Testing Developments

The focus in this developmental testing effort was on applying high pressures in vertical slots and studying the results. The in situ fractures in G-Tunnel were nominally oriented vertically and at angles relative to the drift surfaces so that the test was a compression-type test where wall slabbing would be the logical mode of failure. The goals in the high-pressure test developments were to
Figure 4-2. X-Directed Displacement and Stress Contours at 30 MPa; Elastic and C-J Materials With an Infinite Slot
1. develop capabilities for high-pressure flatjack testing for pressures up to 35 MPa,

2. evaluate potential rock failure mechanisms and flatjack/rock interactions, and

3. investigate AE monitoring methods to determine if fracture development and/or propagation could be monitored.

These goals are discussed in the next three subsections.

4.2.1 High-Pressure Testing

High pressure testing was incorporated into PS testing because of a desire to perform flatjack tests at relatively high stresses to support repository applications. Thermal stresses up to 60 MPa can be developed around drifts in a repository and in situ testing programs are designed to assess these buildups (DOE, 1988). One of these tests is a thermal stress test in which a flatjack is to be used to monitor thermal stress buildups as high as 30 MPa near the surface of a drift. This effort was designed to test flatjacks up to pressures of 35 MPa for the purpose of allowing equipment problems to be evaluated prior to start of planned prototype thermal stress testing efforts.

4.2.2 Possible Rock Failure Mechanisms and Flatjack/Rock Interactions

Two fractures have been prominent in PS testing. A fracture in slot SDH-N1 (Cycle D2) was activated and another possibly developed and propagated in slot SDH-S4 during flatjack testing (Cycle S14). The expected failure mechanism for a slot loaded as shown in Figure 4-1a is for a slab to form, possibly within the region directly loaded with the flatjack. This failure would be due to a biaxial stress state in the proximity of the free surface as found in a MBT. The tensile-type fractures in the planes of loading in Cycles D2 and S14, with later shear displacements, suggested that the deformation of the flatjack relative to the rock surface should be considered. This concern led to the following analyses.
4.2.2.1 **Theoretical Considerations**

Theoretical considerations were focused on rock failure caused by possible flatjack/rock interactions and limiting rock strains. Rock strain discussions are presented first to serve as a reference for the flatjack/rock interaction presentation. The strategy in these analyses was to relate flatjack pressures to predicted tensile stresses and strains in the rock so that rock strength aspects can be investigated.

The potential for the rock tensile failure is discussed using normal strains oriented parallel to the flatjack/rock interface. The major variables are defined in Figure 4-3. The figure shows a diagram representing the normal stresses acting on the slot surface. The x direction would correspond to a vertical stress on a vertical slot. Elastic strain-stress relationships are also shown. The presence of the free edge of the slot causes the strain in the z direction to be predictably in tension. The quantity $\psi$ shown in the figure is used to represent the stress in the x direction as a function of the flatjack pressure. This formulation is useful in developing the potential for slabbing.

Figure 4-3 presents stress-strain relationships that can be used in predicting fracture developments in the z direction. The Maximum Principal Strain Failure Theory is used for this discussion. The failure theory was selected because of its (1) simplicity, (2) appropriateness in representing three-dimensional applications, and (3) direct calculation of strains in the unloaded direction. The failure theory has limited applications and has been used primarily to evaluate behavior of brittle metals (Murphy, 1946). The intent here is to provide a rational guide for evaluating potential failure under a biaxial stress state. The failure criterion is expressed as

$$\varepsilon_{\text{crit}} = \nu_{\text{f}}(\psi + 1)P/E_f < \sigma_{\text{ten}}/E_r$$  \hspace{1cm} (4-1)
Assume:
\[ \sigma_x = -\psi P \]
\[ \sigma_y = -P \]
\[ \sigma_z = 0 \]

\[ E_r, \nu_r = \text{elastic} \]
\[ \text{properties for rock} \]

Tension is +

Normal strains:
\[ \varepsilon_x = \frac{\sigma_x}{E_r} - \nu_r \frac{\sigma_y}{E_r} - \nu_r \frac{\sigma_z}{E_r} = \frac{P}{E_r} (-\psi + \nu_r) \]
\[ \varepsilon_y = \frac{\sigma_y}{E_r} - \nu_r \frac{\sigma_x}{E_r} - \nu_r \frac{\sigma_z}{E_r} = \frac{P}{E_r} (-1 + \nu_r \psi) \]
\[ \varepsilon_z = \frac{\sigma_z}{E_r} - \nu_r \frac{\sigma_x}{E_r} - \nu_r \frac{\sigma_y}{E_r} = \nu_r \frac{P}{E_r} (\psi + 1) \]

Maximum Normal Strain Failure Theory for Tension:
\[ \varepsilon_z \leq \frac{\sigma_{\text{ten}}}{E_r} \]

where \( \sigma_{\text{ten}} \) = tensile strength of material

\[ \nu_r \frac{P}{E_r} (\psi + 1) \leq \frac{\sigma_{\text{ten}}}{E_r} \]

Figure 4-3. Representation of Stresses and Strains on Slot Surface
The criterion used with the Maximum Principal Strain Failure Theory is that failure is assumed when any strain reaches a limiting condition that can be determined in a uniaxial strength test. In most applications, the failure theory is used where the material has equal properties in tension and compression. The application here is based on the assumption that the tensile failure criterion for a welded tuff governs. For example, if $\psi = 0$ and $\nu_T = 0.2$ the predicted flatjack pressure $P$ would be five times the tensile strength of the rock. For $\psi = 0$ and $\nu_T = 0.1$, a higher value of $P$ is predicted. Use of Equation 4-1 provides results that are more conservative when equal biaxial stresses are considered. For example, for $\psi = 1$ and $\nu_T = 0.2$, the predicted failure pressure is 2.5 times the tensile strength. Use of $\psi = 1$ and $\nu_T = 0.1$ would improve the situation, the predicted failure pressure would be five times the tensile strength.

Use of the tension failure criterion generally predicts lower failure pressures for compressive stress states. It is known that the compressive strength of intact welded tuff is approximately 10 times the tensile strength (Board et al., 1987). The previous examples have shown how the predicted failure stresses are lower than this number, particularly for biaxial applications. The use of the tensile failure criterion is rationalized by consideration of rock mass effects on the reference strength. Hoek and Brown (1980) have developed an empirical failure criterion for rock that takes intact rock responses and the presence of discontinuities into account in predicting strengths of a full-scale rock mass. The uniaxial compressive strength of a larger scale rock mass is expressed as

$$\sigma_{CS} = (\sigma_C^2)^{1/2}$$  \hspace{1cm} (4-2)

where

$\sigma_{CS} =$ strength of the larger scale rock mass specimen

$\sigma_C =$ uniaxial compressive strength of intact rock

4-9
s = constant reflecting the properties of the rock and the extent to which it has been broken in situ.

Zimmerman and Finley (1987) discuss welded tuff and show how a value of $s = 0.004$ can be used. If this quantity were used, the value $\sigma_{CS}$ would be $0.06 \sigma_c$, a value lower than the tensile strength of the intact rock but higher than the predicted tensile strength of the rock mass. Hoek and Brown (1980) point out that the tensile strength of a jointed rock mass is near zero. The application of the Maximum Principal Strain Failure Theory in this document implies use of the reference compressive strengths of the rock mass and the reference tensile strengths of the intact rock. This is an important limitation that must be recognized in any application of the failure theory.

Equation 4-1 provides a reference for subsequent discussions regarding the flatjack/rock interactions. Note that the quantity $E_r$ can be removed from the criterion as used. Also note that strains due to in situ stress effects should be considered in accurately applying Equation 4-1. For these discussions, in situ stress effects are not included.

As a guide, Equation 4-1 can be used to predict a maximum flatjack pressure that the rock would allow in the MBT configuration. If it were assumed that $\nu_r = 0.2$, $\psi = 1$, $\sigma_{ten} = 9.6$ (Board et al., 1987), and there were no in situ stress effects, the predicted maximum pressure would be 24 MPa. If $\psi$ were higher, implying a high vertical stress in a vertical slot, and in situ stresses were considered, the failure pressure would be lower. If $\psi$ were low, implying a low vertical stress, the opposite would be true. Limitations to Equation 4-1 have been discussed and it is recognized that the predicted failure pressures may be low. If allowances were made for discontinuities in the welded tuff, it could be argued that the under prediction in Equation 4-1 is compensated for by the effects of discontinuities and reduced strengths that occur on larger scales. It is beyond the scope of this document to resolve this issue. For purposes of this discussion, the results from Equation 4-1 are assumed to be a reasonable predictor of failure in PS testing.
Attention is now directed to the flatjack. The flatjack sheets are under tensile stresses in equilibrium with the fluid pressures. The sheets have tensile strains due to (1) equilibrium tensile stresses and (2) Poisson's-type expansion from the applied pressures. These tensile strains can be influenced by shear stresses formed at the flatjack/rock interface. The slot surface has strains parallel to the surface due to (1) lateral expansion from Poisson's effect and (2) likely shear stress transfer between the flatjack and slot surfaces. When certain conditions exist, the tensile strains on the surface of the flatjack can be greater than the strains on the slot surface resulting in additional tensile stresses on the slot surface and a premature failure mechanism. The basic analytical problem is in defining the potential for and the magnitude of the shear distribution acting between the flatjack and the slot surface. Quantitative determination of the magnitude and distribution of the shear stress is difficult because of the inability to mathematically define the interaction of the flatjack and slot surfaces. It is beyond the scope of this report to provide a detailed analysis of this complex phenomenon. The approach used here is to make simplifying assumptions to the problem and provide background and guidance for predicting the flatjack/rock interactions. A basic assumption in these analyses is that the rock is intact.

Figure 4-4 shows a schematic illustrating the normal strains in the flatjack sheets. The key equation for tensile strains in the z direction is

\[ \epsilon_{fz} = \frac{(K_z - \nu K_x + \nu)P}{E} \]  \hspace{1cm} (4-3)

Equation 4-3 has two factors, \( K_x \) and \( K_z \), that must be determined. Figure 4-5 is a schematic of the flatjack and defines the symbols. The figure shows the pertinent stresses on the flatjack. Using a simplifying assumption that the edge stresses are the same as the sheet stresses, the following equation results:

4-11
Assume: $\sigma_y = -P \quad \sigma_x = \sigma_{fx}$ - tensile stress in flatjack sheet in x direction

$E, \nu = $ Elastic $\quad \sigma_z = \sigma_{fz}$ - tensile stress in flatjack sheet in z direction

Properties

Normal strains:

$\varepsilon_x = \frac{\sigma_x}{E} - \frac{\sigma_y}{E} - \frac{\sigma_z}{E} = \frac{K_x P}{E} + \frac{P}{E} - \frac{\nu K_z P}{E}$

$\varepsilon_y = \frac{\sigma_y}{E} - \frac{\sigma_x}{E} - \frac{\sigma_z}{E} = -\frac{P}{E} - \frac{\nu K_x P}{E} + \frac{K_z P}{E}$

$\varepsilon_z = \frac{\sigma_z}{E} - \frac{\sigma_x}{E} - \frac{\sigma_y}{E} = \frac{K_x P}{E} - \frac{\nu K_x P}{E} + \frac{P}{E}$

At any point x use $\varepsilon_{fz} = \varepsilon_z$

$\varepsilon_{fz} = (K_z - \nu K_x + \nu) \frac{P}{E}$

Figure 4.4. Stress and Strain Relations for Flatjack Sheet
P  =  flatjack pressure
σfo  =  tensile stress in sheet at origin
σeo  =  tensile stress in edge beam at origin
t1  =  thickness of sheet
t2  =  thickness of edge beam
w  =  width of edge beam
L  =  length of flatjack
δ  =  expansion of flatjack due to seating and pressurization
s(x)  =  function describing shear stress

Equilibrium at origin for 1/2 flatjack:

\[ 2 \sigma_{fo} t_1 L + 2 \sigma_{eo} (t_2)(w) + \int_{0}^{L/2} s(x)(L) \, dx = P (L - 2w)(t_2 + \delta) \]

Assume \( \sigma_{fo} = \sigma_{eo} \)

\[ \sigma_{fo} (t_1 L + t_2 w) = P \left( \frac{L - 2w}{2} \right) (t_2 + \delta) - L \int_{0}^{L/2} s(x) \, dx \]

If \( s(x) = 0 \)

\[ \sigma_{fo} = \left[ \frac{(L - 2w)(t_2 + \delta)}{2 (t_1 L + t_2 w)} \right] P = \lambda P \]

Figure 4-5. Section Through Flatjack Showing Major Stresses
\[
\sigma_f \left( t_1L + t_2w \right) = P \left[ (L - 2w)(t_2 + \delta)/2 \right] - \left( L^2 \int_0^{L/2} s(x)dx \right) . \tag{4-4}
\]

If it is initially assumed that the shearing stress \( s(x) \) is zero, then Equation 4-4 simplifies to

\[
\sigma_f = \left[ (L - 2w)(t_2 + \delta)/(t_1L + t_2w) \right](P/2) - \lambda P , \tag{4-5}
\]

where \( \lambda \) is a constant incorporating the geometric variables. The quantity \( \lambda \) is assumed to be equal to \( K_x \) and \( K_z \) for use in Equation 4-3. This assumption is limited to descriptions of near equal tensile stresses near the center of the flatjack. The dimensions for the high-pressure flatjack can be represented by

- \( L = 500 \text{ mm} \)
- \( t_1 = 1.3 \text{ mm} \)
- \( t_2 = 4.8 \text{ mm} \)
- \( \delta = 2.0 \text{ mm} \)
- \( w = 9.5 \text{ mm} \).

Using these quantities, \( \lambda = 2.35 \). Thus, a mathematical relationship is established between the flatjack pressure and the tensile stress in the sheets and perimeter frame for conditions with a frictionless surface.

Flatjack strains are considered again with the assumption that the shear stresses are negligible. Equation 4-2 becomes

\[
\varepsilon_{fz} = P[\lambda(1 - \nu) + \nu]/E \tag{4-6}
\]

A limiting condition for the flatjack to cause excessive strains on the rock surface can be established by dividing Equation 4-6 by the rock strains defined in Equation 4-1. The result is

\[
[(\lambda(1 - \nu) + \nu)E_r]/[\nu_r(\psi + 1)E] > 1.0 . \tag{4-7}
\]
For \( \lambda = 2.35, \nu = 0.3, \) and \( \nu_r = 0.2, \) Equation 4-7 becomes

\[
4.86\left(\frac{E_r}{E}\right) > 1.0 \tag{4-8}
\]

Equation 4-8 shows, with the simplifying assumptions used, that the ratio \( (E_r/E) \) should be greater than 0.21 in order for the flatjack strains to be greater than the rock strains. Young's Modulus for the stainless steel can be assumed to be 207,000 MPa. The intact rock value for \( E_r \) is approximately 26,000 MPa (Zimmerman and Finley, 1987). The corresponding ratio is 0.13, and larger tensile strains for the flatjack would not be expected. As a point of interest, if a reduced modulus-of-deformation value of 16,000 MPa were used, the corresponding ratio would be 0.08, and potential flatjack/rock interactions would be reduced further. Equation 4-8 shows that the potential for the flatjack/rock interaction to cause an additional tensile strain in the rock is small for the flatjacks used. In fact, for the conditions assumed, the flatjack can appear to act as an inhibitor to rock expansion if shear stress is present.

It is useful to analyze the shear stress effects so that the potential of causing either tensile or compressive strains in the rock surface can be assessed. The effect of the action of a shear stress on the rock surface is analyzed first. The problem of pressure loadings on a semi-infinite surface has been presented by Timoshenko and Goodier (1951). Figure 4-6 presents the basic geometry and applicable equations that have been derived from the reference equations. The problem was solved as a two-dimensional problem using polar coordinates, and normal conditions of linear elasticity apply. Three solutions are shown in Figure 4-6. The solutions are for a normal pressure representing the flatjack pressure and two shear stress distributions. The constant shear stress distribution shown in Figure 4-6b is needed for later superpositions.

The linear shear distribution in Figure 4-6c is considered as a likely representation of the shear stress distribution. The flatjack has maximum displacements at the outer boundary because of the tensile stresses in
\[
\sigma_x = \frac{-P}{\pi} \left[ \tan^{-1}\left(\frac{y}{x}\right) + \frac{xy}{x^2 + y^2} \right]
\]
\[
\sigma_y = \frac{-P}{\pi} \left[ \tan^{-1}\left(\frac{y}{x}\right) - \frac{xy}{x^2 + y^2} \right]
\]
\[
\tau_{xy} = \frac{-P}{\pi} \left[ \frac{y^2}{x^2 + y^2} \right]
\]

\[
\sigma_x = \frac{s}{\pi} \left[ \ln\left(x^2 + y^2\right) + \frac{y^2}{x^2 + y^2} \right]
\]
\[
\sigma_y = \frac{s}{\pi} \left[ \frac{y^2}{x^2 + y^2} \right]
\]
\[
\tau_{xy} = \frac{s}{\pi} \left[ \tan^{-1}\left(\frac{y}{x}\right) + \frac{xy}{x^2 + y^2} \right]
\]

\[
\sigma_x = \frac{-s}{2\pi a} \left[ 2x \ln\left(x^2 + y^2\right) - 6y \tan^{-1}\left(\frac{y}{x}\right) - 4x \right]
\]
\[
\sigma_y = \frac{-s}{2\pi a} \left[ 2y \tan^{-1}\left(\frac{y}{x}\right) \right]
\]
\[
\tau_{xy} = \frac{s}{2\pi a} \left[ 2y \ln\left(x^2 + y^2\right) + 2x \tan^{-1}\left(\frac{y}{x}\right) - 2y \right]
\]

Figure 4.6. Theoretical Stress Solutions for Surface Loadings
the sheets. This means that there could be a quasi-linear displacement gradient between the origin, located at the center of the flatjack, and the boundaries of the flatjack. To the first order, a linear shear distribution can be used to represent the quasi-linear displacement gradient.

The analytical solutions shown in Figure 4-6 can be superposed to describe the finite shear distribution shown in Figure 4-7. The origin in the figure represents the center of the flatjack. The solution shown incorporates a sign convention where flatjack strains cause tensile strains on the rock surface. The resulting solution describing the normal stress parallel to the slot is

\[
\sigma_x = -P + \left[ \frac{(x/a)(\ln(x)^2 - \ln(x + a)^2) + 2}{a} \right] s/\pi
\]

where

\[\sigma_x = \text{normal stress in the x direction due to P and s loadings}\]

\[a = \text{distance from the center to the edge of the flatjack}\]

\[s = \text{magnitude of shear stress at distance a}\]

\[P = \text{pressure in the flatjack}\]

\[C(x) = \text{the magnification function [ ] for normal stress due to shear stress.}\]

Equation 4-9 was derived for the semi-infinite medium where lateral stresses could be developed due to Poisson's effect. For proposed PS testing, the development of lateral stress is limited because of the ability of the rock surface to move in a direction parallel to a slot.
at \( y = 0 \) and \( 0 < x < -a \)

\[
\sigma_x = -\frac{P}{\pi} + \frac{s}{2\pi a} \left[ 2x \ln \left( x^2 \right) - 4x \right]
+ \frac{s}{\pi} \left[ \ln(x + a)^2 \right] - \frac{s}{2\pi a} \left[ 2(x + a) \ln(x + a)^2 - 4(x + a) \right]
- \frac{s}{\pi} \left[ \left( \frac{x}{a} \right) \left( \ln(x)^2 - \ln(x + a)^2 \right) + 2 \right]
\]

Figure 4-7. Representation of Slot Loading Using Superposition
Thus, it seems reasonable to assume that the $P$ term in Equation 4-9 could be ignored and that all rock surface tensile stresses are caused by the shear stresses.

It is informative to review the normal stress distribution under the pressurized area. Figure 4-8 shows the variation in the quantity $C(x)$ for Equation 4-9 assuming a convenient value of $a = 1$. This figure displays the variations in the magnification function for the linear shear distribution. Figure 4-8 shows a predicted region of tensile stress because of the linear shear loading assumed. The solution blows up mathematically at the other end of the loaded region because of the mathematical discontinuity in loading (Timoshenko and Goodier, 1951). For comparison, the figure also shows the predicted $C(x)$ distribution for a constant shear stress over $1/2$ the flatjack. This latter solution shows how both the tensile and compressive solutions blow up near the boundaries.

Both of the $C(x)$ distributions in Figure 4-8 show tensile regions near the center of the flatjack for the assumed shear distributions. If the lateral restraint stresses were minimized by the edge effects, then the compressive stress quantities would tend to decrease allowing increased tensile stresses nearer the center of the flatjack. In the absence of any compressive stress buildup from the region $-(x/a) < 1.0$, the shear stress effects would have to be balanced by tensile stresses in the vicinity of the origin. It could be argued that a sizable portion of the compressive stress could be added to increase the effective tensile stress near the center of the slot. For computation purposes, it is assumed that a compressive stress factor of 4.0 can be added to the tensile stress to account for the lack of lateral support. In considering the overall flatjack, similar stress buildups occur because the other half of the flatjack would tend to double the tensile stresses near the centerline. Thus, there is a theoretical possibility of a significant tensile stress buildup in the intact rock near the center of the flatjack due to the shear conditions assumed. Application of Equation 4-8 has shown that the
Figure 4-8. Distributions of Coefficients Due to Assumed Shear Loadings
development of the shear stress in the direction shown is improbable. Thus, the conditions imposed by Equation 4-8 suggest that compressive stresses would be predicted in the center of the flatjack. Compressive stresses tend to inhibit the development of failure mechanisms in the rock.

It is useful for later discussions to make an estimate for the maximum value for $C(x)$ for a full flatjack. Again, for computational purposes it is assumed that the flatjack tensile strains are greater than the rock strains to be compatible with the solutions shown in Figure 4-8. The process is to establish a maximum tensile value for $C(x)$ by adding a representative value for the compressive effects, 4.0 from the previous paragraph, to the maximum tensile value shown in Figure 4-8, which is 2.6, and doubling it because of the presence of the other half of the flatjack. This total quantity becomes 13.2. The resulting maximum magnification factor for predicted tensile stress becomes $13.2/3.14 = 4.2$. In other words, there would be a possibility of a tensile stress developing in the center of a flatjack that is 4.2 times the maximum shear stress that is developed if the flatjack tends to extend the rock. On the other hand, if the flatjack was to restrain the rock through the shear effect, there would be a compressive stress of the same magnitude near the center of the flatjack.

Attention is now directed to the flatjack. Figure 4-5 shows a freebody diagram of the flatjack with appropriate stresses using the assumption of a linear shear stress distribution. Using the relationships shown in Figure 4-7 and the assumptions associated with obtaining the estimate for $A$, the following mathematical relationship can be developed:

\[
L \int_{0}^{L/2} s(x)dx = L \int_{0}^{L/2} \frac{2x}{Ls_{\text{max}}dx} = L^2 s_{\text{max}}/4 .
\]  
(4-10)

Using the same numerical values as before, Equation 4-4 becomes

\[
\sigma_{fo} = 2.35P - 89s_{\text{max}} .
\]  
(4-11)
Equation 4-11 is important because equilibrium requirements dictate that the shear stress must be relatively small if tension is to remain in the flatjack sheet. If \( \sigma_{\text{fo}} = 0 \), then \( s_{\text{max}} = 0.03P \). This is because the shear stress acts over a large area, and a relatively small shear stress magnitude has a significant effect on the flatjack. As an additional observation, the tensile stress in the flatjack is increased if the direction of the assumed shear stress is reversed.

These quantifications can be combined to make stress estimates for an intact rock. Using simplifying assumptions, it was established that the maximum tensile or compressive stress could be estimated to be 4.2 times the maximum shear stress. As an upper limit, the maximum shear stress can be 0.03 times the flatjack pressure. Thus, the maximum tensile or compressive stresses in the surface of the rock can be as high as 0.13 times the flatjack pressure with the magnification factor used. If the design pressure of 35 MPa were used, the corresponding estimated tensile or compressive stresses in the rock surface could be as high as 0.13(35) = 4.6 MPa.

In summary, these analyses have been applicable to a flatjack loading on intact rock, and they have shown that the flatjack/rock interaction is not significant in creating a tensile fracture. If anything, the flatjack/rock interaction for the flatjacks used in these high-pressure testing efforts would tend to inhibit fracture development. The most significant factor is the limited tensile strain on the rock surface as expressed in Equation 4-1. This equation shows that a fracture can be developed theoretically resulting from the Poisson's effect. It is reasoned that the tensile extension of the rock would tend to open fractures that are oriented in the plane of the compressive loading. This is a common phenomena in compression testing (Goodman, 1980), where compression failure is known to be a highly complex mode, including formation of tensile cracks and their growth and interaction through flexure and shear.
4.2.2.2 Failure Mechanism Summary and Recommendations

Failure mechanisms were analyzed assuming intact rock behavior. In summary, intact rock solution was relatively insensitive to the ratio of the modulus of elasticity for the flatjack and rock as far as failure was concerned. There was a possibility of developing relatively small shear stresses at the flatjack/rock interface for the high pressure testing. These shear stresses would most likely inhibit the development of fractures. The theoretical analyses showed that the development of tensile failure mechanisms within the loaded regions were possible in discontinuous rock.

In the actual testing, fractures propagated within the loaded areas of two slots, and segments of rock became permanently disturbed as a result of the testing. In Cycle S14, a nonintact segment of rock became dislodged when the flatjack pressure reached 28.3 MPa. The displaced segment was probably a combination of dilation and shearing of an existing fracture that was influenced by the proximity of a nearby slot. The flatjack/rock interaction could have prevented the fracture from dilating and propagating. A fracture on both sides of the flatjack was activated in slot SDH-N1. The failure occurred in Cycle D2, where the maximum pressure was 13.3 MPa, but there was clear evidence of limited propagation of an existing fracture in earlier cycles. There is a possibility in slot SDH-N1 that the flatjack/rock interaction possibly restrained the fracture from propagating at lower flatjack pressures. It is not known whether the fractures in the two slots were more influenced by the flatjack/rock interactions or by localized discontinuities.

These analyses have shown that the flatjack can theoretically influence the formation of slabbing-type cracks under limited conditions and that high-pressure flatjack tests can be adversely influenced by the flatjack/rock interactions. The analyses have shown that even small shear stress distributions between the flatjack and rock surface can cause appreciable stress changes parallel to the surface of the slot. This means that the MBT may be limited in accuracy without minimization of the surface.
shear stress. Friction reducers have been developed for multiaxial compression testing of concretes (Mills and Zimmerman, 1970). They could be adopted for use in high-pressure slot testing.

It is recommended that development of the MBT test be continued because it appears to be a feasible method to create a surface slab under high-pressure testing conditions. It is clear that pre-pressurization diagnostic work, possibly with impression flatjacks, must be included in any future high-pressure testing. It is also clear that there should be friction minimization between flatjacks and the rock surface to reduce adverse shear effects. Much work is needed before results can be easily interpreted, but, with better analyses and testing methods, the overall potential for use of modified bearing-type tests exists.

4.2.3 AE Monitoring

Zimmerman, 1992a lists the high-pressure tests that had AE measurements. The goal in the measurements was to compare AE information from different sensors as a function of flatjack pressure so that an increase in AE activity could be ascertained. Table 4-1 lists a summary of data (Zimmerman, 1992b) that were recorded for specific intervals chosen to be close to the maximum flatjack pressure for each of the tests so that potential AE applications can be assessed. Figure 4-9 shows a post-test surface map of the slots used with AE monitoring.

The counts in Table 4-1 were obtained from the tapes containing the AE measurements. The excitation voltage for each sensor was ±2 V. The output was produced in scales ranging from ±2 to ±4 V. The procedure to establish an acoustic count was to identify all signals that crossed a threshold of 50% of the output voltages for each of the channels. A count was simply a voltage that was higher than the threshold. The numbers of counts for each interval for each sensor were determined and are listed in Table 4-1.
### TABLE 4-1

**SUMMARY OF AE COUNTS FOR SELECTED PRESSURE INTERVALS**

<table>
<thead>
<tr>
<th>Test</th>
<th>Counting Interval (MPa)</th>
<th>Mass Press (MPa)</th>
<th>Slot</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
</tr>
</thead>
<tbody>
<tr>
<td>S1</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S1</td>
<td>45</td>
<td>0</td>
<td>30</td>
<td>59</td>
<td>52</td>
<td>164</td>
<td>0</td>
<td>1</td>
</tr>
<tr>
<td>S2</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S1</td>
<td>13</td>
<td>0</td>
<td>6</td>
<td>8</td>
<td>15</td>
<td>17</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>S3</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S1</td>
<td>0</td>
<td>4</td>
<td>0</td>
<td>1</td>
<td>34</td>
<td>1</td>
<td>5</td>
<td>0</td>
</tr>
<tr>
<td>S4</td>
<td>12.4-13.1</td>
<td>13.79</td>
<td>SDH-S1</td>
<td>2013</td>
<td>30</td>
<td>619</td>
<td>468</td>
<td>770</td>
<td>181</td>
<td>85</td>
<td>141</td>
</tr>
<tr>
<td>S5</td>
<td>9.6-10.3</td>
<td>10.69</td>
<td>SDH-S1</td>
<td>59</td>
<td>0</td>
<td>4</td>
<td>2</td>
<td>1</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>S6</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S4</td>
<td>0</td>
<td>0</td>
<td>1</td>
<td>134</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>S7</td>
<td>9.6-10.3</td>
<td>10.34</td>
<td>SDH-S4</td>
<td>36</td>
<td>1</td>
<td>67</td>
<td>437</td>
<td>99</td>
<td>66</td>
<td>167</td>
<td>22</td>
</tr>
<tr>
<td>S8</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S4</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>68</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>S9</td>
<td>13.1-13.8</td>
<td>13.79</td>
<td>SDH-S4</td>
<td>227</td>
<td>0</td>
<td>118</td>
<td>324</td>
<td>157</td>
<td>2</td>
<td>128</td>
<td>30</td>
</tr>
<tr>
<td>S10</td>
<td>16.6-17.2</td>
<td>18.34</td>
<td>SDH-S4</td>
<td>379</td>
<td>0</td>
<td>169</td>
<td>398</td>
<td>199</td>
<td>19</td>
<td>141</td>
<td>5</td>
</tr>
<tr>
<td>S11</td>
<td>6.2-6.9</td>
<td>6.89</td>
<td>SDH-S4</td>
<td>8</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>94</td>
<td>0</td>
<td>7</td>
<td>0</td>
</tr>
<tr>
<td>S12</td>
<td>8.3-9.0</td>
<td>9.51</td>
<td>SDH-S4</td>
<td>12</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>25</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>S13</td>
<td>12.4-13.1</td>
<td>13.10</td>
<td>SDH-S4</td>
<td>201</td>
<td>0</td>
<td>101</td>
<td>0</td>
<td>136</td>
<td>0</td>
<td>20</td>
<td>0</td>
</tr>
<tr>
<td>S14</td>
<td>22.7-23.4</td>
<td>28.34</td>
<td>SDH-S4</td>
<td>1513</td>
<td>35</td>
<td>703</td>
<td>359</td>
<td>2065</td>
<td>37</td>
<td>4088</td>
<td>83</td>
</tr>
</tbody>
</table>
Figure 4-9. Fracture Patterns Around Slots Used in AE Monitoring
The process to determine the counts involved converting the AE signals from analog to digital, using a 200-kHz carrier frequency. The specified counting interval was selected as 0.69 MPa for all sensors and cycles. The initial values for the counting intervals were taken as the highest full range (0.69 MPa) interval that could be recorded before the maximum pressure was reached or the flatjack failed. These tapes were marked at uniform pressure intervals so that the selected count intervals could be located. A computer program was written to count the number of times that the output signal exceeded the 50% threshold in the intervals.

The data in Table 4-1 are significant because there is a clear indication that the AEs increased as the maximum pressures increased. This trend suggests that fractures were propagating and/or microcracks were extending as the flatjack pressures were increasing. We were not able to isolate origins of AEs. The data also suggest that there were differences in AE on both sides of the slots. These two points are illustrated in Figure 4-10.

Figure 4-10 shows data representing the sum of the counts for four sensors on each side of the slots for a cycle. A data point in Figure 4-10 represents the sum of counts for sensors 1, 3, 5, and 7 or 2, 4, 6, and 8. Data points were selected for cycles S1, S4, S6, S7, S9, S10, and S14 because these were the first times that the flatjack pressures were extended to higher pressure ranges.

Two trends are apparent in Figure 4-10. First, it appears that the left sides of the slots showed the most acoustic activity. Slot SDH-S4 had clearly failed on the left side, but this was not readily observable for slot SDH-S1. The surface of slot SDH-S1 was less uniform on the left side, and there was a major fracture nearby as is shown in Figure 4-9. The second trend was that the AE from both right sides appeared to remain below 1000 cumulative counts. There is a possibility that no significant macro or microfailure planes were propagating on those sides. Data from individual sensors could be processed further to attempt to locate origins...
Figure 4-10. Representation of Counts Versus Pressure for High-Pressure Testing

BOTTOM OF PRESSURE INTERVAL (MPa)
or major AEs, but this exercise is beyond the intended scope of this report. The data trends suggest that, with development, AE has possibilities for use in detecting macro and/or microfracture propagations in the welded tuff.

4.3 Summary of High-Pressure Testing Investigations

The high-pressure testing presented in this chapter was explained in terms of a MBT. The MBT has potential for developing slab-type failures at the rock surface and possibly for limited application as a form of shear test. The testing and analyses were divided into three major components of (1) high pressure testing, (2) rock failure mechanisms and flatjack/rock interactions, and (3) AE monitoring. They are summarized individually.

The goal in the high-pressure testing was to develop capabilities for high-pressure testing up to 35 MPa. Fourteen high-pressure tests were conducted. The desired pressure was nearly reached as a flatjack was pressurized to 28.3 MPa before complications in the rock caused the flatjack to fail. Initially, the flatjacks failed at lower pressures, but modifications in the design and fabrication improved the situation, and it was assumed that the 35 MPa value was attainable had the rock not failed.

Analyses showed that there were limiting rock strains in a direction perpendicular to the rock surface in the modified bearing tests. Flatjack/rock interactions appeared to be important from a test conduction standpoint, but not from an enhanced failure consideration. The trend was for the flatjack/rock interaction to inhibit the generation of the fractures if it was not minimized. These analyses showed the potential for developments of fractures in slots. Limiting strains appeared to dominate the rock behavior resulting in the formation of slabs that may be important in stability considerations.

A final goal in this testing was to determine if AE monitoring methods could be used to determine fracture development and/or propagations. The results showed that AE signals appeared to increase with increased flatjack
pressures in regions where the rock was failing. The monitoring indicated a progressive fracturing of the rock, which was consistent with other measurements in earlier C and D cycles. This first data collection effort has shown that AE monitoring has some potential for monitoring the development of failure mechanisms in welded tuff.
5.0 SUMMARY OF OVERALL RESULTS AND RECOMMENDATIONS

This testing program was a developmental effort designed to improve pressurized slot testing concepts. The effort is considered a third stage in the evolution of testing that started in 1950s and was significantly advanced in the late 1960s. This third stage addresses earlier testing problems and expands the scope and applications of PS testing.

Specific objectives of this testing were to (Zimmerman, 1992a)

1. develop slot-cutting methods that would provide a smooth surface and minimize stress concentrations in the rock mass,

2. utilize surface pin measurements to determine in situ surface stresses normal to slots,

3. perform pressure-deformation measurements with instrumented flatjacks in thin slots for purposes of determining the modulus of deformation of jointed, welded tuff,

4. utilize uninstrumented flatjacks to develop high pressures for the purposes of evaluating in situ strengths, and

5. utilize new analytical techniques for data interpretations and conversions.

These objectives are discussed individually.

5.1 Summary of Findings Relative to Objectives

5.1.1 Objective 1

The basic objective was achieved. Planar slots were cut with chain saws that could accept flatjacks. This achievement eliminated the concern
expressed by Deklotz and Boisen (1970) regarding the presence of the guide holes in the slots.

A wire saw was installed and operated, but the results were less than satisfactory. There was considerable down time because of wire breakages and repairs. Breakages were attributed to a tight radius for the down-hole sheave and inhomogeneities in the moderately welded tuff. The resulting slot was not as smooth as the ones cut with the chain saw. The wire saw is not recommended for future developments associated with PS testing.

5.1.2 Objective 2

Surface pin measurements were used to determine the slot normal stress using the flatjack cancellation (FC) method of Chapter 2. The measurements were taken as Cycles C1, C2, and C3 in a slot (SDH-N1), which was located in a densely welded tuff. Two BSMs were applied to attempt to evaluate the normal stress determinations in these cycles.

The range of the stresses, calculated from the data, was from 1.9 to 2.2 MPa, and the mean value was 2.1 MPa. A predicted stress in the same direction assuming no excavation was 5.9 MPa. Differences in the measurements and the predicted values were assumed to be due to (1) inaccuracies in both the FC calculation process and the reference stress measurements, (2) fracture propagations in the rock, and (3) discontinuities in the rock mass.

A feature in this testing was the monitoring of the surface pin displacement histories at zero flatjack pressures. The monitoring showed that potential creep effects, identified as a problem by Deklotz and Boisen (1970), were minimal.

5.1.3 Objective 3

The modulus of deformation was determined from measurements with instrumented flatjacks in three slots in four test cycles. Cycles D1 and
D2 were conducted in the same slot (SDH-N1), and companion deformation measurements were also taken with surface pins used in the normal stress testing. Cycles D3 and D4 consisted of measurements taken in two slots that were orthogonal to each other (EXP-NS and EW). As a new thrust, impression flatjacks were used to determine potential slot surface problems prior to testing in Cycles D3 and D4.

A major task was establishing a mathematical relationship between sensor readings and the modulus of deformation. There was no closed form analytical solution available, and laboratory tests were not performed, as was the case with Rocha and da Silva's (1970) efforts. A new approach was taken involving the use of deformation coefficients and adjustment factors.

Table 3-4 summarizes the results of modulus of deformation calculations using the deformation coefficients. Table 3-4 shows that the modulus of deformation for the densely welded tuff ranged from 12.0 to 12.6 GPa with a mean of 12.3 GPa. These values were repeatable in spite of the presence of a major fracture that was propagating during the earlier FC testing. The fracture deformed excessively during Cycle D2.

Table 3-7 provides the results of the calculations based on the surface pin data. Table 3-7 shows that the range of modulus of deformation values is from 10.4 to 10.6 GPa. This range is uniformly smaller than the range obtained with the instrumented flatjacks.

The measured mean value for the densely welded tuff using the instrumented flatjack compares favorably with the range of values, 9.1 to 19.1 GPa, obtained nearby (Zimmerman and Finley, 1987). In that same report, there was a recommendation that the reference value for the modulus of deformation be 16 GPa. Results from the PS testing suggest that this value be lowered to at least the 50% factor (13 GPa) proposed by Tillerson and Nimick (1984).

Table 3-4 also shows that the data-based values for the modulus of deformation for the moderately welded tuff ranged from 1.6 to 3.6 GPa, with a mean value of 2.6 GPa. The values in the moderately welded tuff were
thought to be smaller because of (1) differences in the sizes of pressurized areas and slots, (2) differences in materials, and (3) the probable influences of discontinuities in the slots in the Experiment Drift. Testing methods were not totally uniform between Cycles D3 and D4.

The developments in this testing program have been encouraging. The use of the instrumented flatjack in smooth slots appears to be a viable way to measure the modulus of deformation in a jointed rock. Continued development is needed.

5.1.4 Objective 4

PS testing relating to strength evaluations are organized into summaries covering

1. high-pressure flatjack testing results,

2. evaluations of potential rock failure mechanisms, and


In high-pressure flatjack testing, the goal was to evaluate flatjack designs for pressures up to 35 MPa and demonstrate the feasibility of testing to at least this magnitude. The goal was not reached; a maximum pressure of 28.3 MPa was reached. Fourteen high-pressure tests were conducted in Cycles S1 through S14. All 5 cycle tests were conducted in slots SDH-S1 and S4 in densely welded tuff. Many of these cycles were repetitive low-pressure tests designed to record reference noise for AE monitoring. Seven of these tests were terminated because of flatjack failures in seam velds. In two of these seven, the flatjack failure was attributed to excessive flatjack deformation attributed to slot irregularities (nonuniform dimensions in one case and fracture propagation effects in the other). The collective evidence indicates that flatjacks such as those fabricated in the later stages are capable of reaching pressures on the order of 35 MPa.
The fracture propagations in the C and D cycle tests in slot SDH-N1 and the S cycle tests in SDH-S4 suggested that rock failure at relatively low flatjack pressures was possible and needed closer investigation. Investigations were focused on rock strength in the context of slabbing-type failure, and the identification of the modified bearing test (MBT) concept resulted. The essential failure in a high pressure test is development of tensile fractures parallel to the direction of the major compressive stress with expansion towards the unstressed surface. The splitting occurs because there is little, if any, lateral stress at the surface of the drift, and a biaxial stress state results. Scoping-type applications showed that slabbing type rock failure near the surface could occur in welded tuff with flatjack pressures as low as 24 MPa if allowances were made for discontinuities in the rock mass. This suggests that there might be some upper limit that should be considered in future high-pressure testing and thermal stress applications.

An important consideration in PS testing is flatjack/rock interaction in the modified bearing configuration. Analyses showed that there was a criterion that could be developed to examine these effects. For these testing conditions it is probable that the flatjack tends to restrain expansion of the slot and could cause the failure stresses to be higher. Analyses of the shear stress effects in flatjack/rock interactions showed that relatively small shear stresses could be significant. The findings suggested that friction minimization practices should be followed.

Eight AE sensors were used in all fourteen high-pressure tests. The results showed that the AEs increased with flatjack pressure increases and fracture propagations. Failure was not quantified, but results show that there might be an AE threshold that could be defined for predicting major extensions of fractures under flatjack loadings. The results suggested that AE-type monitoring be developed in future testing programs.

5.1.5 Objective 5

The discussions for the previous three objectives have highlighted the results of the analytical techniques that were used for data interpretations and conversions. Analytical solutions, laboratory testing, field
measurements and observations, and numerical modeling applications were used. In particular, adjustment factors (Equations 3-2 and 3-14) were introduced in the modulus of deformation determinations in Chapter 3. This is the first known use of a multi-parameter approach in these calculations and the approach is more sophisticated than the semi-empirical approaches that were used by Rocha and da Silva (1970), or the method suggested by the ISRM.

Numerical modeling results by Conley (1987) proved useful in establishing a number of the adjustment factors. Conley presented results from calculations using a linear-elastic model and a compliant joint model. The latter has potentials for establishing quantitative influences of fractures on rock masses. Both models appear to be useful for future PS applications and could be used to define the adjustment factors better.

5.2 Recommendations for Future Testing

No objective was specified for the development of testing methods, but a testing technique was inaugurated that is worthy of summarizing so that future PS efforts can benefit. This is the use of impression flatjacks (Zimmerman, 1992a). Impression flatjack testing proved to be a boon to installing and using flatjacks successfully and served as diagnostic devices for rock behavior analyses. The flatjacks used had problems and developed small leaks, but this is a design and fabrication problem that can be solved with careful attention. Results showed that relatively thin copper sheets that are pressurized to at least 1.4 MPa should provide good impressions in welded tuff.

This development effort has advanced PS testing concepts considerably and results are encouraging for future efforts. The following recommendations are presented to help guide those who wish to continue this work:

1. Improve cutting efficiencies for diamond-tipped chains.
2. Further develop strength testing concepts near drift surfaces, with emphasis on
   o reducing flatjack/rock surface friction effects,
   o incorporating AE monitoring methods for monitoring fracture propagations, and
   o improving predictive failure criteria to define upper limits for high-pressure testing.

3. Utilize flatjack impression testing techniques for inspections of all slots before pressurizations.

4. Utilize numerical modeling to enhance normal stress and modulus-of-deformation data conversions, with emphasis on
   o defining slot continuity effects on flatjack pressurizations for better adjustments,
   o defining surface proximity effects for better adjustments, and
   o defining the effects of fracture propagations in the plane of flatjack loading on continuum representations.

5. Utilize zero pressure displacement measurements to provide data histories.
6.0 REFERENCES


Langkopf, B. S., and E. Eshom, "Site Exploration for Rock Mechanics Field Test in the Grouse Canyon Member, Belted Range Tuff, U12g Tunnel Complex, Nevada Test Site," SAND81-1897, Sandia National Laboratories, Albuquerque, NM, 1982. (NNA.900403.0379)


APPENDIX A

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Reference Information Base

This report contains no candidate information for the Reference Information Base.

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