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The Thermomechanical Response of WIPP Repositories

MASTER

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Sandia National Laboratories

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THE THERMOMECHANICAL RESPONSE OF WIPP REPOSITORIES

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DISCLAIMER

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Fig 1

FORWORD

The calculations presented here were performed for Sandia Laboratories in 1978 and are consistent with the evaluation of options for the Waste Isolation Pilot Plant (WIPP) at that time. The storage of both spent fuel and high-level wastes were assessed to determine the impact of heat production on the WIPP site and to provide reference cases for further development of the experimental program.

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SECTION 1

INTRODUCTION

1.1 CANDIDATE CONFIGURATION

The thermomechanical response of an extended WIPP site was investigated by computer simulations using the STEALTH 2D Code (Reference 1). Two candidate cases, called 30SF and 75BLK, were considered. The same stratigraphy applied to both.

30SF had a 20-acre circular disk source corresponding to spent fuel rated at an initial thermal load of 30 kW/acre after ten years of precooling before deposition. The disk was at a depth of 650 meters and extended to a radius of 160.5 meters.

75BLK has a 1717-acre toroidal disk source of high-level waste rated at an initial thermal load of 75 kW/acre after its ten-year cooling period before deposition. The toroid was at a depth of 815 meters and had inner and outer radii of 590 and 1600 meters.

1.2 GRID DESIGN

The same grid and zoning was used for all cases. The axisymmetric grid of 552 points extended to a depth of 3 km and a radius of 4 km to provide for 100,000 years of response. In hindsight, it was seen that 100,000 years was much too long. The important effects occurred between 1000 and 2000 years and were well attenuated by 5000 years. The 100,000-year grid design was not optimized for the important early time effects and future grids should be designed accordingly. In particular, it was necessary to accept non-physical oscillations for about 1000 years in the present calculations because of computing techniques in conjunction with cost constraints. A better design, at constant cost, would have restricted these oscillations to the first 100 years, well before the development of the important responses.

1.3 INITIAL AND FINAL CALCULATIONS

The calculations were first performed with an erroneous initial condition corresponding to a linear temperature distribution with depth. This is incorrect for a site with layers of differing thermal conductivity. The result was an anomalous surface subsidence at late times as the temperature field readjusted to thermal equilibrium consistent with the applied thermal boundary conditions. These calculations were then complemented by a special "no-source" calculation to isolate the effects and to provide a first-order means of correcting the final results of the baseline calculations. The results were reasonable, but were compromised by uncertainties in the correction method.

The calculations were repeated later with correct initial conditions and provide all of the results that are reported here.

1.4 SPECIAL COMPUTING TECHNIQUES

Historically, the thermomechanical response of nuclear waste repositories has been computed with time-implicit finite-element codes because of the very long times of interest. The complexity of such codes makes it very difficult to model highly nonlinear features such as primary creep in salt. This has resulted in the invention of clever compromises between real features and code convenience. Unfortunately, the effect on the computed response is difficult to assess because of the complexities of the many interacting mechanisms that are present.

Time-explicit finite-difference codes such as STEALTH 2b, though not constrained by model complexity, have been expensive to employ because of the small mechanical time steps associated with numerical stability. This constraint has been relaxed by recently developed techniques that are applicable for cases of quasi-static equilibrium. These cases correspond to the condition that momentum effects are insignificant. Such is the case for repository response.

The first of these techniques is called "pseudo-time scaling" and is presented in Reference 2. Briefly, it assigns a different clock to each mass point in the grid to unfold the mechanical motion. The pseudo-time scale of each clock is optimized such that each mass point accelerates toward local stress equilibrium as vigorously as stability allows. Thus, the stress-strain fields are constantly and vigorously driving toward equilibrium in response to the thermal expansion as the temperature field unfolds on the real time scale. The vigorous mechanical motion toward equilibrium is desirable. However, the mass points tend to coast through and overshoot their equilibrium positions due to their enhanced momenta.

Dynamic relaxation, also described in Reference 2, is employed to damp the pseudo-mechanical oscillations. In particular, if the assigned damping coefficient corresponds to the lowest natural frequency of the system, then the oscillations are critically damped and the mechanical response is optimized with respect to following the true equilibrium as it unfolds in space and real time.

These techniques have been included in a special version of the STEALTH 2D code and have been demonstrated in salt repository studies. One of these studies (Reference 3) also contained a sophisticated model of primary creep and the results agreed well with the observed room closures (Reference 4). Another study (Reference 5) examined an extended repository for three cases of waste levels. There were no experimental data for comparison, but the results agreed well with two other contractors performing companion calculations. Unfortunately, creep was not included and these calculations provided only thermoelastic simulations.

Creep was present in all but one of the calculations of the present study. It was turned off during a repeat of the 75BLW case to quantify the differences between thermoelastic and thermoelastic + creep effects. Plasticity was purposely excluded in all of these calculations by code input parameters that produced a very high yield surface.

The existing STEALTH primary creep model was employed with the coefficients adjusted to give a reasonable fit to the experimental data of Reference 6. The model is described in detail in Reference 3. A quick overview is presented next. Let $e_x, e_y, \dots$ be components of the Cauchy strain tensor and $\sigma_x, \sigma_y, \dots$ be components of the stress tensor and define:

$$S = \sqrt{\frac{1}{6} \left[(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 \right] + \sigma_{xy}^2 + \sigma_{yz}^2 + \sigma_{zx}^2}$$

$$e = \sqrt{\frac{1}{6} \left[(e_x - e_y)^2 + (e_y - e_z)^2 + (e_z - e_x)^2 \right] + e_{xy}^2 + e_{yz}^2 + e_{zx}^2}$$

Thus, S and e correspond to the usual definitions of the second invariant of deviatoric stress and strain. By definition of elastic and non-elastic strain,

$$\dot{S}/2. = \dot{e}_{\text{elastic}} = \dot{e}_{\text{total}} - \dot{e}_{\text{plastic}} - \dot{e}_{\text{creep}}$$

The GAI creep model of Reference 3 is:

$$\dot{e}_{\text{creep}} = a \left[A S^c F(\theta, b) \right]^{\frac{1}{a}} / e^{\frac{1}{a} - 1}_{\text{creep}}$$

where θ is the absolute temperature and A, a, b , and c are material parameters with $a \leq 1$ for primary creep. The Reference 3 creep model temperature dependency is $F(\theta, b) = \theta^b$. In the present study, $F(\theta, b) = \exp[-b/\theta]$, which gives equally good results compared to experiment and is more plausible by dislocation theory.

The Prandtl-Reuss flow rule completes the link required for insertion of creep into the code,

$$\dot{e}_{ij\text{creep}} = \dot{e}_{\text{creep}} \frac{\sigma_{ij}}{S}$$

The simultaneous solution of the set of equations involves highly non-linear terms and it was necessary to employ a fourth-order Runge-Kutta method with subcycling to provide high accuracy and to provide a good control for trade-off against computing costs.

SECTION 2

COMPUTATIONAL SETUP

2.1 LAYERS AND MATERIAL PROPERTIES

The site stratigraphy was supplied by Sandia Laboratories and was idealized by assuming homogeneous isotropic materials and blending thin layers into macrolayers as shown in Figure 1. The assumed thermoelastic properties of the materials are given in Table 1. The properties of the unmixed materials correspond to those of Reference 5, which are handbook values. The salt and anhydrite thermal conductivities for the WIPP site were obtained from Reference 7. The mixture properties were constructed from the pure materials by standard means. Briefly, the pressure changes were expected to be of the order of the overburden so that density changes would be small. Bending stresses, generated by thermal expansion in the presence of a temperature gradient, were expected to be very important with respect to uplift shape and potential mechanical failure. These considerations are consistent with treating Young's modulus and Poisson's ratio as the dominating elastic moduli. Their average values, obtained by volume weighting, were then used to construct the average values of bulk and shear moduli of the mixed materials of Table 1. The remaining mixture properties were constructed from simple volume averages.

2.2 CREEP MODEL PARAMETERS

The parameters of the creep model (p 4) were fit by trial and error to triax experimental creep curves from Reference 6. Tests of 50 to 500 hours duration were used to minimize the large creep rates observed in short experiments. The resulting parameters were:

$$a = 0.4655$$

$$L = 4100. \text{ K}$$

$$c = 3.0$$

$$A = 3.1386\text{E-}20 \text{ Pa}^{-c} \text{ s}^{-a}$$

TABLE 1. THERMOELASTIC PROPERTIES

Material	Density (kg/m ³)	Bulk Modulus (Pa)	Shear Modulus (Pa)	Linear Expansion (m/m-°K)	C _v (J/kg-°K)	K (J/S-m-°K)
Sandstone	2600	0.76612 E10	0.57459 E10	10.8 E-6	837.3	3.4612
Dolomite	2800	5.74593 E10	2.65197 E10	5.4 E-6	837.3	3.4612
Anhydrite	2800	7.6612 E10	2.55375 E10	23.4 E-6	837.3	0.7 K _{salt}
Salt	2100	2.2984 E10	0.4925 E10	39.6 E-6	921	K _{salt}
Mix A	2800	7.273 E10	2.569 E10	20.52E-6	837.3	K _{mix A}
Mix B	2247	2.694 E10	0.593 E10	36.2 E-6	899.1	0.927 K _{salt}

$$K_{\text{salt}} = 1549.5 / (T - 100) + 0.13199E - 7 \quad , \quad v_k$$

$$K_{\text{mix A}} = 0.554 + 0.588 K_{\text{salt}}$$

The value of b corresponds to an activation energy of 8.15 kilocalories/mole to be compared to the four values, 8.4, 9.1, 11.0, and 19.7 that were estimated in Reference 6 from four pairs of experimental creep rates for this salt.

The fit of the model to the experimental creep data is displayed in Figures 2 and 3 where the model response (solid line) is plotted with the available test data (dashed lines labeled with experiment number) for four cases of temperature and differential stress. The model has no obvious bias in the regions of primary creep, but does not account for the apparent tertiary creep behavior seen in experiments 6, 7, and 12.7 when the strain exceeded about 15%. It was anticipated that much smaller strains would occur in the calculations and it was assumed that primary creep would dominate.

2.3 GRID DESIGN, BOUNDARY CONDITIONS, AND INITIAL CONDITION

Normally, as in the Reference 5 calculations, the region around the heat source would be zoned finely to capture the peak temperatures that would apply to pillars if room closure estimates were desired. Successive dezones would then be applied as the gradients decrease with time to conserve computer costs consistent with the need for spatial resolution. This was not done in this study and the coarse grid of 552 mesh points was employed at all times. Thus, the computed peak temperatures correspond to averages over large zones (150 X 150 meters). However, the volumetric expansion of a region of constant thermal expansivity depends only on the total heat in the region and not on the temperature distribution. The thermally generated stress in the coarse grid will have the correct zonal averages.

Figure 4 displays the coarse grid, corresponding to an axisymmetric space with a 4-km radius and a 3-km depth. These dimensions were based on estimates of heat diffusion to the boundaries in a time of interest of 100,000 years. The zones labeled (1) correspond to the 7500K toroidal disk source. The zone labeled (2) shows the 400°F source.

Figure 2a. Creep model compared to 24°C (297K) experiments.

Figure 2b. Creep model compared to 24°C (297K) experiments.

Figure 3a. Creep model compared to 20°C (68K) experiments.

Figure 4b. Creep model compared to 100°C (373K) experiments.

Figure 4. Natural representation of model.

The stress boundary conditions were:

- surface - free
- bottom - horizontal
- side - vertical roller

The selected thermal boundary conditions were:

- surface - isothermal at 21.1°C (70°F)
- bottom - isothermal at 103.13°C (217.6°F)
- side - isothermal in time with a depth dependency consistent with the surface and bottom temperatures and thermal equilibrium

The thermal equilibrium temperature versus depth was established by a 1D calculation. The results are displayed in Figure 5. These results were used for the isothermal side condition and for the grid initial condition. The stress-strain initial condition was constructed from the densities and elastic moduli under an assumed condition of hydrostatic equilibrium in the gravity field of 9.81 m/s^2 . Thus, deviatoric stresses were not present initially.

2.4 HEAT PRODUCTION MODEL

The HLW heat production rate data were previously employed in the OWI study of Reference 5. The SF rate data were supplied by Sandia. Both sets of data were normalized to 1 watt/acre at the ten-year cooling time and then scaled as appropriate for the baseline calculations. Figure 6 displays the normalized power versus time for these materials. The integrals of the power curves give the heat released. These are shown in Figure 7.

Figure 1. Temperature profile with depth (m) for the study area.

Figure 6. Normalized heat rate vs.

Figure 1. Normalized Energy Release

2.5 DAMPING FREQUENCY FOR DYNAMIC RELAXATION

The damping frequency, ω_0 , required for optimum damping was established by first computing T_{DLR} for 6000 years with $\omega_0 = 0$. The lowest response frequency was $\omega_0 = 8 \times 10^{-11}$ seconds. This can be seen in the solid curve of Figure 8, which shows the vertical motion at the surface at a radius of 160.5 meters. The inherent damping due to salt creep can also be seen. An input value of $\omega_0 = 8 \times 10^{-11}$ would give critical damping if creep were not present, but will give overdamping for our cases.

The calculation was recomputed with values $\omega_0 = 6.3 \times 10^{-11}$ and 3.15×10^{-11} . The result of the latter is shown as the dashed curve of Figure 8. The results of the former were practically identical except that the jog on the first upswing was missing.

The latter value, 3.15×10^{-11} , was employed in the final calculations to insure slight underdamping so that peak stress and strain would not be underestimated.

Figure 8. Surface motion without critical damping.

SECTION 3

COMPUTATIONAL RESULTS

3.1 COMPUTER PLOT SYMBOLS

It is not feasible to discuss or display all of the computer graphics for the calculations. The text will show selected plots pertinent to the discussion. The plot data are in SI units with the exception of temperature, which is in degrees Centigrade. Table 2 provides a concordance of symbols. The zone-centered quantity locations are misleading. They are labeled by I,J and XPN,YPN on time history plots. These are not true locations. The true locations are $I-1/2$, $J-1/2$ and $XPN-\Delta x/2$, where $\Delta x/2$ and $\Delta y/2$ are the zone half-width and half-height.

Figure 9 locates and indicates typical plots. The points shown in the grid region locate various time history plots. Profile and contour plots at fixed time are also noted.

3.2 HEAT AND TEMPERATURE RESULTS

The heat balance versus time is shown in Figure 10 for 30SF and 75HLW. The heat input curves are consistent with the Figure 7 curves when they are appropriately scaled. The scale factors are:

$$30SF : (30,000 \text{ W/acre}) \times (20 \text{ acres})$$

$$75HLW : (75,000 \text{ W/acre}) \times (1717 \text{ acres})$$

The difference between the heat input and grid heat corresponds to the loss through the top surface by the assumed isothermal boundary condition. The loss through the side and bottom surfaces was insignificant.

The peak grid heats occurred at 1500 years for 30SF and at 1000 years for 75HLW. It will be seen later that the peak uplifts occur at similar times.

TABLE 2. PLOT SYMBOL CONCORDANCE

<u>Plot Symbol</u>	<u>Meaning</u>
TIM, TIME	Time (sec) = years $\times 3.1536 \times 10^7$
YPN	Depth (m) of point at I,J
XPN	Radius (m) of point at I,J
DSF	Distance (m)
PRH	Pressure (Pa) at zone center $I-\frac{1}{2}, J-\frac{1}{2}$
PEI	$\sqrt{3}P$ (Pa) at zone center $I-\frac{1}{2}, J-\frac{1}{2}$
TMP	Temperature ($^{\circ}\text{C}$) at zone center $I-\frac{1}{2}, J-\frac{1}{2}$

○

△

Figure 10. Heat balance (see text).

Temperature rise versus radius at selected times and depths are shown in Figures 11 and 12 for 75HLW and 30SF. The lowest depth in each of these figures corresponds to the repository level. The earliest time shown is 500 years. The maximum temperature rise in the repository level was about three-fold larger than that shown at 500 years and occurred before 80 years. These data are misleading and are not shown because large temperature gradients which cannot be handled by the coarse grid occur at early times. However, by 500 years the computed gradients extend over several zones and the results are realistic.

3.3 SURFACE UPLIFTS

Figure 13a shows the surface profiles of the final 75HLW calculations at selected times to 3000 years. The 50-cm peak uplift decreased smoothly after about 2000 years, halving at 6300 years and halving again at 16,500 years, reaching a value of 10 cm at 20,000.

The peak surface uplift of the final 30SF case was very small. Figure 13b shows the final 30SF profiles at selected times. The peak was always on axis with a maximum value of 2.9 cm at 950 years, decaying to half of this value at 3000 years, halving again at 7300 years, with a value of only 5 mm at 10,000 years.

3.4 CREEP RESULTS

The generalized creep strain defined in Section 1 was examined in the 75HLW and 30SF grids. The maximum creep strains in both grids occurred at a depth of 788 meters. The maximum in the 75HLW grid occurred in the innermost source zone, centered at a radius of 662 meters. The maximum in the 30SF grid occurred on axis, just below the single source zone of 30SF.

Figure 11. Bulk temperature rise versus rate at selected depths and flows.

Figure 17. 30°F Temperature rise versus radius at selected depth and times.

Figure 13a. 750MW curves plotted at different times.

Figure 13: Intensity profile $I(r)$ versus radius r .

Figure 14, top and bottom, displays the first 10,000 years of creep in these zones. The shapes are similar. The relatively large creep rates in the first 1000 years are followed by an ever decreasing rate in response to the combined effects of creep hardening and decreasing deviatoric stress.

Though the creep strains were small, the volume of salt was large and the effects were significant.

3.5 STABILITY OF THE RUSTLER FORMATION

The Rustler formation lies at a depth of between 165 and 259 meters in the calculations and corresponds to the MIX A layer of Figure 1. This formation is dominantly anhydrite and dolomite. Two dolomite layers with layer thickness of approximately 25 feet, constitute important aquifers. The presence of aquifers makes it important that the anhydrite confining layers do not fail.

The mechanical stability of these layers is undoubtedly limited by planes of weakness due to joints or faults with failure modes corresponding to crack opening (tensile failure) or slip (shear failure). It would have been expedient to insert tensile and shear failure models in the code to interrogate the Rustler layer zones at every cycle. This was not done and it was possible only to examine selected regions in space and time by hand analysis.

3.5.1 Tensile Failure Assessments. The effective stress acting to clamp a crack is $P_e = \sigma_n - P_{fluid}$. P_{fluid} is the fluid pressure in the crack and σ_n is the rock stress normal to its crack plane. Negative values of σ_n are compressive by standard convention. A joint with cohesive C will not open provided that $P_e + C > 0$. The condition for tensile survival is then

$$\sigma_n > P_{fluid} - C$$

Figure 14. Creep strain versus time at most highly strained zone.

The tensile assessments in this study used the conservative values

$$\begin{aligned}\sigma_n &= \text{the most tensile of the three principle stresses} \\ \sigma_c &= 0 \\ P_{\text{fluid}} &= P_0/2 = 2.74 \text{ MPa} \\ &= \text{half of the Rustler layer overburden}\end{aligned}$$

The above value of P_{fluid} , present in aquifers with ducts to the surface, would result in artesian springs.

The zones of the Rustler layer were examined at 1000 years for tensile failure. The principal stress components were determined by rotating the coordinate system to the principal axis. The most tensile principal stress was always compressive with a magnitude that exceeded the half overburden pressure, $P_0/2$. These results are displayed in Figure 15 for 75HLW and 30SF. The current mean stress, P , is also shown.

It can be seen that 75HLW results vary greatly with radius with the axis region being the least compressive. The pressure-time histories of 75HLW in Appendix 1 show substantial non-real oscillations in the axis region ($I = 2$, $J = 21$) that were not well damped by 1000 years. Thus, the near axis dip of stress in Figure 15a is suspect.

The 30SF results of Figure 15b show very little change from nominal conditions indicating that the 30SF repository has little effect with respect to promoting tensile failure.

3.5.2 Joint Shear Failure Model. Handin in Reference 8 provides a summary of the concepts of (1) internal friction for the brittle fracture of virgin rock and (2) sliding friction for slip failure along pre-existing crack planes. Byerlee in Reference 9 provides similar data for westerly granite in a series of meticulous experiments involving controlled surface roughness and interstitial water. These studies and

FIGURE 1. STRESS-STRAIN BEHAVIOR OF POLYMER FILMS

FIGURE 15b. Tensile strength (MPa) of 70/30 in the 40/60 series at 10 years (1980-1990).

others provide an accepted model for slip failure along a pre-existing crack that is triggered when

$$\tau_p = C + \mu \cdot P_e$$

where τ_p is the shear stress acting to slip the crack, C is the cohesion for sliding, μ is the sliding friction coefficient and P_e is the effective clamping stress previously defined.

The cohesion, C , for tensile and slip failure can be different. The former is related to the tensile strength of the cementation. The latter depends on the shear strength of the cementation and the effects of interference if the joint is rough or if the joint pattern is irregular.

The value of μ has an insensitive dependency on rock type and the confining pressure. The value 0.6 is a reasonable average for rock at the low confining pressure of the Rustler layer.

The ratio of the effective shear strength, $C + \mu \cdot P_e$, and the applied shear stress, τ_p , provides the safety factor

$$F = (C + \mu \cdot P_e) / \tau_p$$

The value $F < 1$ corresponds to slip failure while $F > 1$ measures the margin of safety. This safety factor depends on the orientation of the stress tensor to the crack plane and can be expressed as

$$F(\theta) = [C - \mu \cdot \sigma_n(\theta) + P_{\text{fluid}}] / \tau_p(\theta)$$

where θ represents a particular orientation. The minimum value of $F(\theta)$ over all possible joint orientations provides the most conservative safety factor and is obtained in the following manner.

Let $\sigma_1, \sigma_2, \sigma_3$ be the three principal stresses. Now consider a crack plane whose normal direction lies in the 1,2 plane of the principal axis. Let F_{12} be the minimum value of F for all rotations of this normal in the 1,2 plane. Similar remarks apply to the 2,3 and 3,1 planes. Then the safety factor for the joint at the worst orientation is given by

$$F = \text{minimum of } F_{11}, F_{23}, F_{31}$$

3.5.3 Mohr-Coulomb Failure Model. The Mohr-Coulomb failure stress based on the concept of internal friction is

$$Y = C_0 + 2\mu \cdot (P - P_{fluid})$$

where P is the rock pressure and P_{fluid} is the pore pressure. C_0 is the failure stress when $P - P_{fluid} = 0$. The coefficient of internal friction at low values of P is about 0.6, the same as for sliding friction. To generalized shear stress to be compared to the failure stress is $\sqrt{3}J'_2$ where

$$\sqrt{3}J'_2 = \sqrt{\frac{1}{2}(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2}$$

Then the corresponding safety factor does not depend on joint orientation and is given by

$$F = Y / \sqrt{3}J'_2$$

3.5.4 Application of the Mohr-Coulomb model. It was not practical to examine every Rustler layer zone at all times. Instead, contour plots of $\sqrt{3}J'_2$ were examined at 500, 1000, 1500, and 2000 years to find potential failure regions. A typical plot, corresponding to 75HLK at 1500 years, is shown in Figure 16. The uppermost contour island is centered in the Rustler layer at a depth of 200 meters and a range of 1100 meters. This is due to the relatively large stiffness of this formation. This island was well

Figure 10. Contour plot of the results of the analysis.

developed by 1000 years and was starting to decay by 2000 years. It did not migrate with the migrating surface uplift peak.

The time history of $\sqrt{J'_2}$ for the zone in the middle of this island is shown in Figure 17. Reading through the non-real oscillations, the peak value of $\sqrt{J'_2}$ is 3.5×10^6 Pa and occurs at about 750 years. It slowly decays, becoming about 3.2×10^6 Pa at 2000 years.

The corresponding display of pressure is shown in Figure 18. The large initial rise and fall are not accurate because of the inherent oscillations. These are fairly well damped by 1000 years and a pressure of 9×10^6 Pa is a realistic estimate. The initial overburden pressure is 5.5×10^6 Pa, which is twice the joint fluid pressure by our assumption. Using $C_0 = 0$ the corresponding safety factor estimate is

$$F = 1.2(9 \times 10^6 - 2.74 \times 10^6)/(3.2 \times 10^6 \sqrt{3}) = 1.4$$

Several other 75 BLF zones were examined in the Rustler layer. None had lower safety factors.

The safety factor for 30SF was similarly analyzed. In this case, the contours of $\sqrt{J'_2}$ showed potential trouble in the Rustler layer nearer the axis. The time history plots of $\sqrt{J'_2}$ and P were examined in this region. A typical $\sqrt{J'_2}$ plot is shown in Figure 19, with a corresponding pressure plot in Figure 20. Reading through the unreal oscillations, the worst condition occurs at about 500 years (1.6×10^{10} sec) with $P = 5.4 \times 10^6$ Pa and $\sqrt{J'_2} = 3.0 \times 10^5$ Pa. The corresponding safety factor is then

$$F = 1.2(5.4 \times 10^6 - 2.74 \times 10^6)/(3.0 \times 10^5 \sqrt{3}) = 6.1$$

Other zones gave higher or similar results to the accuracy of estimating the average of the unreal oscillations.

Figure 17. $\bar{S}_0$ versus time for 75ELW zone 1=9, 3=11.

Figure 18. P versus time for 75HLW zone I=9, J=21.

Figure 19. $\sqrt{J_2}$ versus time for 30SF zone l=6, J=21.

Figure 20. P versus time for 30SF zone I=6, J=21.

3.5.5 Application of the Joint Shear Failure Model. A short code was written to process the printout data to obtain the angle dependent values of F in the Rustler layer at 1000 years under the conservative assumption that $C = 0$ and $P_{fluid} = P_J/2$. Figure 21 shows the 75HLW results. The symbols F_{12} , F_{23} , F_{13} refer to the minimum values in the three planes of the principal axis. The 1 and 2 principal axis are both in the radius-axis plane of the grid while 3 is in the hoop direction. The dashed curve provides the Mohr-Coulomb $\leq F \leq$ values for comparison.

The low and high excursions near the axis are suspect for reasons previously discussed. The smallest value of F_{12} , F_{13} , F_{23} at fixed radius corresponds to F , the safety factor at the worst orientation. It is usually but not always less than $\leq F \leq$, the Mohr-Coulomb angle independent safety factor.

Figure 22 also applies to 75HLW at 1000 years, with only one curve being displayed. The top figure shows the results for dry joints where $P_{fluid} = 0$. The bottom figure applies for $P_{fluid} = P_J/2$. In regions far from axis regions, the critical region is at about 100 meters. The safety factors here are marginal and a refined calculation is required with finer zoning and more accurate material properties.

Figure 23 provides the corresponding Rustler layer results at 1000 years for 40SF. The safety factors are comfortably large.

3.6 CREEP VERSUS THERMOELASTIC RESPONSE

The effects of creep were determined for the 75HLW case by recomputing the first 2000 years with creep turned off. This thermoelastic calculation was called 75HLW-NC and provided comparisons to isolate the creep induced effects.

FIGURE 21. Values of F_{12} , F_{13} , F_{23} , and $\langle F \rangle$ in the 75th% Failure layer at 1000 years (see text).

Figure 22. Value of safety factor versus time for the case of $\alpha = 0.01$ and $\beta = 0.01$.

Figure 3.3. Value of the safety factor for the design of the structure of the bridge deck.

3.5.1 Surface Uplift Comparisons. Figure 24 shows the surface profiles for 75HLW-NC at selected times for comparison to the creep case results of Figure 13. The creep case had larger peak uplifts due to the lower resistance to bending in the salt layers. These differences indicate that the stress fields may be quite different. This is important to the stability of the Rustler layer.

3.5.2 Tensile Failure Comparisons. The tensile safety margin curves of 75HLW-NC for the Rustler layer at 1000 years are displayed in Figure 25 for comparison to the creep case counterpart of Figure 15a. The thermoelastic results are quite different, with tensile failure occurring in the region where $\sigma_1^{\max}$ dips below $P_0/2$ according to our previous criterion. The generally lower values of P out to 2000 meters also suggests lower shear failure safety margins.

3.5.3 Mohr-Coulomb Failure Model Comparisons. Figures 26, 27, and 28 are the thermoelastic counterparts of Figures 16, 17 and 18. A comparison of Figures 26 and 16 shows that the upper island of $\sigma_1^{\max}$ contours in the Rustler layer is at a much larger radius for the no-creep case. Comparisons of Figure 27 and 17 show that the time dependencies of $\sigma_1^{\max}$ are slightly different in peak values and much different in shape. Comparisons of Figures 28 and 18 show that the pressure time dependencies have similar peaks but much different average values after the peak. In particular, the late time pressures of Figure 28 are about half that of Figure 18. This has a large effect on the Mohr-Coulomb safety factor.

Referring to the discussion of 3.5.4, the Figure 27 and 28 data at 1000 years (smoothing through the oscillations) indicate values of $\sigma_1^{\max}$ and P of about 2 and 4 MPa, respectively. Then, assuming $P_{\text{fluid}} = 2.74$ MPa as before, we have

$$\langle F \rangle = 1.2(4 - 2.75)/(2\sqrt{3}) = 0.43$$

CONTOUR LEVELS

A	1.00E+06
B	2.00E+06
C	3.00E+06
D	4.00E+06
E	5.00E+06

Figure 26. Contours of C_{12} at 1500 years for 75HILW-NC.

Figure 27. $\hat{Q}_1^2$ versus time for 75 BLK-NC zone 1=9, 1=11.

Figure 28. P versus time for 75HLL-NC zone I=9, J=21.

Thus the strength is only 43% of the generalized shear stress and the zone fails by this criterion. The corresponding creep case value was 140%, which does not fail.

3.6.4 Joint Failure Model Comparisons. The joint shear failure model based on the worst orientation was used to construct Figure 29 for comparison to the creep case Figure 22. Note the factor 5 in the vertical magnification of Figure 29. The differences are enormous. The thermoelastic case fails everywhere out to 2500 meters when $P_{\text{fluid}} = P_0/2$ and even fails when $P_{\text{fluid}} = 0$ between about 400 and 760 meters.

Figure 14

Comparison of
Safety Factor

and

Stress

Figure 10. Value of λ_{eff} at 1.0 hr in the early phase of the ^{125}I decay curve.

SECTION 4 SUMMARY AND CONCLUSIONS

Coarsely zoned STEALTH 2D calculations were used to investigate two candidate WIPP repositories. The grid was designed for one hundred thousand years of response with modest computing costs. As a result, the early time mechanical response was compromised by non-real oscillations that could not be damped completely before a few thousand years.

In spite of these oscillations, it was possible to see that the dominant effects of stress and strain peaked between one and two thousand years, at the time of maximum heat in the site. This time corresponded to the condition that the surface heat loss rate balanced the heat generation rate.

Though the creep strains were quite small, a large volume of salt was involved and the effects were significant. The peak surface uplift at 75BLW was increased by about 25% due to creep. However, the deviatoric stress relaxation due to creep produced large changes in the stress fields. The Rustler layer survived reasonable failure criterion for the 75BLW case with creep, and failed both in tension and shear, according to these same criterion, when the calculation was repeated without creep.

The deviatoric stress fields, with and without salt creep, concentrated near the repository as expected and also in the Rustler layer due to its relatively high Young's modulus compared to the neighboring layers. This points out the importance of Young's modulus and its distribution with depth. A lower Young's modulus in the aquifer confining layers would reduce the bending stresses. In addition a high Young's modulus in the neighboring layers would help carry the bending loads. Thus it does not protect the confining layers if creep occurs close to the aquifer confining layers. However, creep near the repository is highly beneficial with respect to reducing the deviatoric stress field and lowering the bending stresses in the upper strata. The tradeoff of these effects can be handled by computer simulation even if they may not be traded off in a real repository.

Since the time of interest is so much smaller than the 100,000 years this calculation was designed to examine, it is possible to model the KIPP stratigraphy in much more detail and still be able to calculate the response for the time of interest. Billy Joe Thorne, of Civil Systems Inc., has completed a finer zoned calculation of the response of the KIPP stratigraphy to a repository similar to the 75 K watt/acre repository modeled in this report. In this calculation the Rustler formation is modeled as a five layered formation using material properties derived from data taken at the Nemo site. This calculation is currently being documented in a full length report (CSI 2053-04) titled, "Calculation of Uplift for an Elastic Model of the Rustler Formation over a 75 K watt/acre repository at the KIPP Site."

SECTION 5

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