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PRESSURE - TEMPERATURE CREEP TESTING
AS APPLIED TO A COMMERCIAL ROCK SALT

Richard K. Dropek
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June 1976

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PRESSURE-TEMPERATURE CREEP TESTING AS

APPLIED TO A COMMERCIAL ROCK SALT

Submitted TO

Office of Waste Isolation
Oak Ridge, Tennessee

operated by

Union Carbide Corporation
Nuclear Division
for the
Energy Research and Development Administration

Submitted By

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PRESSURE-TEMPERATURE CREEP TESTING
AS APPLIED TO A COMMERCIAL ROCK SALT

by

Richard Kenneth Dropek

A thesis submitted to the faculty of the
University of Utah in partial fulfillment of the requirements
for the degree of

Master of Science

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University of Utah

June 1976

UNIVERSITY OF UTAH GRADUATE SCHOOL

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ABSTRACT

A triaxial compression apparatus was evaluated in its performance of quasi-static and creep experiments on rock salt under confining pressures to 5000 psi and temperatures to 200°C. Included is the capability to measure both axial and lateral (radial) deformation.

The evaluation of the apparatus was conducted in two stages. First stage evaluation consisted of quasi-static compression tests with metal specimens of known mechanical properties. These experiments served to either verify or to determine several critical systems parameters. During the second stage, the suitability of the apparatus was tested by means of commercial block salt. This latter effort included a suite of quasi-static and creep experiments. Because of the compositional similarity of the commercial block salt and natural rock salt attempts were made to compile the experimental data for salt to analyze them with the objectives of guiding future material properties characterizations of natural salt.

The evaluation of the triaxial testing machines included the resolution of force, pressure, temperature, the measurement of temperature gradients throughout the test material and, particularly, the accuracy of all deformation measurements which were utilized to calculate specimen strains. Specifically it was paramount to ascertain anticipated errors in the measurements of lateral specimen deformation not generally considered in experiments of this type.

The system checkout lead to the following results: (1) force, pressure and temperature could be resolved to within 2 lb, 2 psi and 0.5°C, respectively; (2) at the maximum temperature of 200°C, the temperature gradient throughout a 4" long sample was 2°C; (3) axial strain could be resolved to within 2% accuracy; (4) where applicable, lateral strain measurements constituted lower bounds of the true strains which develop in the absence of end effects. The measured values lay within -4% at $\epsilon_1 = 0.04$ (4% greatest compressive strain) and within -6.3% at $\epsilon_1 = 0.08$ (8% greatest compressive strain) when considering both end effects and thermal drift at elevated temperature. The technique utilized to monitor lateral strains proved acceptable in all quasi-static experiments at ambient temperature and in triaxial creep tests regardless of temperature. It is unsuitable in quasi-static loading tests at elevated temperature except possibly at deviatoric loading rates of $d/dt(\sigma_1 - \sigma_3) \geq 10$ psi/min.

Triaxial compression tests were conducted in artificial salt under 500 psi to 3000 psi confining pressure and up to 129°C. All quasi-static tests were limited to loading rates of approximately 10 psi/min and .01 (1%) axial strain. Triaxial creep experiments were carried out over periods up to 230 hours at principal stress differences of $730 \text{ psi} \leq (\sigma_1 - \sigma_3) \leq 6400 \text{ psi}$ and at four temperatures up to 129°C. Because of the high porosity of the commercial salt and its effects, all experiments above were preceded by hydrostatic consolidation tests. The latter in turn were based on the series of hydrostatic loading experiments under pressure up to 2000 psi. The results of all salt tests are summarized below: (1) Under hydro-

static pressure porous commercial salt undergoes considerable consolidation (principal compressive strains of approximately equal magnitude) with time. The consolidation increases with pressure in a non-linear fashion. (2) Characteristic curvatures in both axial and lateral stress-strain plots and associated changes in the principal strain ratios $|\epsilon_2/\epsilon_1|$ suggested the phenomenological definition of a yield stress in quasi-static compression. (3) The yield stress increased by a factor of 2 to 3 between 500 and 3000 psi confining pressure with the maximum change occurring between 500 and 1000 psi confining pressure. (4) The yield stress decreased with temperature. (5) Creep was monitored at all levels of principal stress difference in multi-stage tests above $\sigma_1 - \sigma_3 = 730$ psi where $(\sigma_1 - \sigma_3)$ was raised to successively higher values. Creep appeared to be decelerating throughout, i.e., no secondary or steady state creep was observed. In addition the strain history effects appeared to follow the rule of time hardening.

Based on empirical data fits of the form $\epsilon = at^n$, the observed 10 hour to 100 hour creep rates and the projected 1 year creep rates, $\dot{\epsilon}_1$, were in the order of 10^{-6} to 10^{-8} sec^{-1} . For the most part the principal strain ratios, $|\epsilon_3/\epsilon_1|$, or the ratio of the principal strain rates lay between .37 and .6 suggesting volume changes during creep which are relatively small compared to the changes in shear strain and shear strain rates.

Beyond these general observations no specific trends could be identified concerning, for example, the effects of pressure, deviator stress and even temperature. This is due to gross data scatter on one hand and to insufficient number of data points for meaningful statistical analyses on the other. It is likely that some of the data

scatter must be attributed to differences in the initial salt properties, e.g. porosity. The specimens used were drilled from four different salt blocks each of which might have experienced differing processing histories. At the same time, creep results normally exhibit a wider scatter range than quasi-static test data. Hence future work on natural salt is likely to require considerable duplication of individual experiments beyond what short term tests and modeling requirements might indicate.

INTRODUCTION

Over the past 20 years rock salt deposits have been regarded as promising candidates for the location of underground storage sites to isolate commercially generated low, as well as heat generating radioactive waste. Among the principal reasons for this choice are the abundance of salt deposits, their stability over geologic times and their location in regions of low seismic risk. In addition, salt has a high thermal conductivity compared with other geologic materials, tends to flow rather than fracture, and, where fracture does or did occur, tends to be self-healing with time. With such considerations, a need arose to determine the mechanical properties of rock salt for the evaluation of various waste isolation design concepts. Included, for example, is the assessment of events and steps to be anticipated to insure the retrievability of all radioactive waste during the pilot phase of the first plant or plants.

Salt has been the subject of intensive study by geologists and mining engineers for several decades. As a result, there exists a considerable amount of information concerning the response of rock salt in uniaxial compression at ambient temperature and under high pressure and temperatures greater than 300°C. Unfortunately, the problem at hand called for data in a window of parameter variations of past investigations: confining pressure between zero and 5000 psi and temperatures between ambient conditions and 200°C. Equally

regretable is the fact that, besides the lack of existing data of interest, there did not exist standardized or even demonstrated experimental procedures which could be applied directly without any development work.

With these goals and realizations it became the objective of this study to evaluate apparatus which were designed specifically to test rock salt in quasi-static compression and in triaxial creep up to 5000 psi confining pressure and temperatures up to 200°C. A particular requirement was that the apparatus should permit both axial and radial rock deformations to be measured. This requirement was based on the observation that rock salt deformation under low mean stress is accompanied by considerable micro-cracking. Under these conditions it cannot be assumed a priori either experimentally or analytically that rock salt deforms iso-volumetrically as is frequently assumed.

To insure the satisfactory performance of the testing equipment detailed calibrations were carried out. These were then followed by experiments on salt specimens drilled from commercially pressed salt blocks. The choice of artificial salt was predicated by the intent not to waste natural salt core in trial experiments while using a material similar to natural rock salt whose behavior might guide future tests on core from sites considered for the location of waste isolation plants.

PREVIOUS WORK

Considerable research has been done in determining time-dependent material properties for rock resulting in numerous mathematical and empirical models describing the experimentally observed behavior. However, many of the models were developed for specific testing conditions and materials, thus reducing their usefulness. Most of the models exhibited the following deficiencies:

1. The tests were conducted uniaxially, or if conducted triaxially, only axial strains were monitored.
2. Temperature effects were not included.

Furthermore, the inherent complexity and variability of time-dependent mechanisms among rocks makes application of solutions to specific cases doubtful when applied to different materials. Consequently, only the literature pertaining to the time-dependent properties of rock salt shall be discussed.

Previous research may be divided into two categories: a) polycrystalline, and b) single crystal studies. The halite single crystal investigations have focused upon determining the deformation mechanisms which occur at various pressure and temperature states. From this basis polycrystalline studies have attempted to describe the rheological behavior of the bulk material using a) the single crystal deformation models, b) empirical models based on a power law with time, or c) mathematical viscoelastic models consisting of a combination of springs and dashpots.

Single Crystal Studies

Early investigations into salt behavior lead to the determination of deformation mechanisms for halite single crystals. Buerger (1930) and others had established that at ambient conditions the predominant deformation mechanism for halite was through slip on the $T = (110)$ plane in the $t = (1\bar{1}0)$ direction. Other less dominant slip systems for halite were also documented by Buerger and more recently by Carter and Heard (1970), these being $T = (100)$, $t = (100)$; and $T = (110)$, $t = (1\bar{1}0)$. The less dominant crystal mechanisms were observed to be enhanced if the maximum shear stress was located in the same plane as the slip plane. However, for elevated temperatures greater than 250°C polygonization by dislocation climb has been observed to be the dominant deformation mechanism as Carter and Heard reported (1970).

Although the slip systems for single crystal deformation were known, the energies involved in flow and a means of mathematically describing it were not well understood. A mathematical representation of slip plane formation was possible only after the theory of dislocations had been advanced. Since lattice vacancies and the movement of ions to fill the vacancies forms the basis of dislocation glide, it was first necessary to understand the self-diffusion characteristics of NaCl. Etzel and Maurer (1950) and Mapother, et al. (1950) were two early studies concentrating on NaCl self-diffusion and ion mobility. Their studies revealed that ion mobility (μ) and the self-diffusion coefficient were governed by an exponential relationship for temperatures between 250°C and 550°C . Numerous dislocation etch pit and birefringence experiments were conducted to determine dislocation behavior for NaCl by Mendelson (1961, 1962), Amelinckx and Strumane

(1960) and Davidge and Pratt (1964) to name a few. These investigations suggested that plastic flow appeared to be controlled by either the long-range stress interaction of single system glide in early flow stages or with the development of multiple slip systems as characterized in secondary plastic flow. The forced interaction of dislocations formed on oblique planes on the primary slip system appeared to cause the work hardening. Furthermore, the activation energy for subgrain formation (polygonization) was determined by Amelinckx and Strumane (1960) and shown to vary with temperature (49 Kcal/mol for $T < 375^{\circ}\text{C}$ or 30 Kcal/mol for $T > 375^{\circ}\text{C}$). A shortcoming of the above investigations was that the tests were conducted only in a uniaxial stress state.

The most extensive NaCl single crystal research to date has been conducted by Carter and Heard (1970) where crystals were confined at 2 kilobars and subjected to temperatures up to 500°C . It was determined, using constant strain rate experiments, that slip systems formed the predominant deformation mechanisms at temperatures less than 250°C . For temperatures greater than 250°C polygonization by dislocation climb was shown to be increasingly dominant. To fit the data a best fit test involving the Eyring and Weertman equations was attempted. Following this procedure, the Weertman exponential rate process equation ($\dot{\epsilon} = A \exp(-E/RT) \sigma^n$) was seen to fit the data where steady state flow from dislocation glide became dominant. When the crystal was stressed parallel to the (001) plane it was seen that dislocation glide began on one or maybe two planes. Then with the beginning of dislocation pileup of conjugate planes, strain hardening would begin thus activating more slip systems, more pileups, and

consequently more work hardening. A general observation during crystal deformation was that as either temperature increased or the strain rate decreased, the strength of the NaCl crystals was decreased (strength being defined as the maximum differential stress attained for a given strain rate and temperature). Similar results for NaCl were reported by Phillips (1962).

Whereas the above investigations involve primarily quasi-static loading, single NaCl crystal uniaxial creep studies are very limited. Two creep investigations which tested single crystal NaCl were done; one by Griggs (1939) and the other by LeCompte (1965). Both experiments involved only one crystal and at the end of testing fractures parallel to axial loading were observed, thus putting doubt into data accuracy.

Griggs conducted a uniaxial test on a single crystal prism of halite at 61 bars axial stress for 42 days. The halite strain-time plot exhibited instantaneous, transient and steady-state response. An equation of the form

$$S = A + B \log t + Ct \quad (1)$$

where

S = axial strain

A = instantaneous response

$B \log t$ = elastic flow (recoverable)

Ct = pseudoviscous flow (irrecoverable)

was used to model the axial creep. Diagonal lines coinciding with the (110) planes were observed which suggested plastic flow by translation along the (110) plane. By assuming constant volume newtonian flow an equivalent viscosity was defined by Griggs to be

$$\eta = \frac{P}{3ds/dt} \quad (2)$$

where ds/dt was the steady state strain rate. For the halite crystal the equivalent viscosity was calculated to be 2.6×10^{17} poise where ϵ was determined in the steady-state region.

LaCompte attempted to fit his single halite crystal creep data to the equation $\epsilon = at^n$ which he used in fitting his polycrystalline material data. A fit to the empirical relationship was not obtainable presumably because of the induced fractures parallel to the direction of loading.

Polycrystalline Studies

Polycrystalline NaCl studies of both quasi-static and creep experiments are numerous. One of the earliest attempts to determine the viscous nature of rock salt was conducted by Dewar (1894) using an extrusion method developed by Tresca. Stokes (1966), Handin (1953), Parker (1959) and Dreyer (1972) studied the response of rock salt to quasi-static loading. From these investigations it was learned that as temperature increased, so also did ductility, i.e., work hardening decreased. Regardless of definition (0.2% effect, secant intercept, etc.) yield strength also decreased with temperature. Irradiation of the rock salt had little or no effect on the strength and ductile properties while confining pressure for compression tests increased work hardening at room temperature only slightly, indicating that strength and strain hardening were a function of maximum shear stress only. The effect of increased strain rate was to increase the strength. Thus it was seen that for a constant strain rate, thermal

effects dominated over confining pressure effects for natural rock salt and for low porosity artificial rock salt.

Other effects such as grain size, sample impurities, homogeneity and degree of interlocking were investigated by Dreyer (1972) and for grain size effects only, Stokes (1966). As grain size increased with respect to sample dimensions, the strength decreased, but plastic flow increased since single crystal behavior became dominant. Dreyer suggested a minimum of 2260 grains in the sample to maintain polycrystalline behavior based on triaxial compression tests conducted on cubic samples. Dreyer also found that as impurities in the salt increased, so also did the strength. This is verified by Parker (1959). Strength was also shown to increase for increases in degree of homogeneity and interlocking.

Several polycrystalline creep investigations began when storage of nuclear wastes in salt cavities was proposed. Parker (1959), Gloyna, Serata, et al. (1959) and Brown and Jessen (1959) conducted creep studies on domal salt with primary concern for enhanced creep from elevated temperatures induced by stored nuclear waste. Tests at elevated temperatures with unirradiated and irradiated samples showed little if any difference in creep strain at 2500 psi uniaxial load over 100 minutes. It was noted that the creep rate increased with increasing temperature of 80°F to 770°F. Salt submerged in saturated water exhibited additional increase in creep rate. This increase in plastic behavior was also observed by Barnes (1933). Thus it was determined for nuclear waste storage that temperatures less than 300°F be used with a cavity depth not greater than 3000 feet. Using simple geostatic loading which was shown to occur at 3000 foot depths

in salt according to Gloyna and Serata (1959), this would correspond to 3000 psi.

More recent studies involved with waste storage compiled by Bradshaw and McClain (1970) compared laboratory pillar models to *in situ* pillar convergence. The study showed that the empirical equation used to fit the experimental data

$$\epsilon = B\sigma^m t^n \quad (3)$$

gave satisfactory approximation to pillar verticle closure rates for mine openings for up to several years where B is a dimensionless constant, σ the average pillar stress, t is time and m and n are slopes of the log-log plots ϵ versus σ and t respectively.

LeCompte (1965), Heard (1972b), and Dreyer (1972) investigated salt creep independent from the above studies. It was generally observed that creep rates were only slightly changed with confining pressure indicating again the dependence of mechanical properties on maximum shear stress and not on confining pressure for room temperature creep tests. However, for elevated temperatures it was shown that the confining pressure effect was greatly enhanced for temperatures from 20°C to 200°C. In addition, for a given deviator stress, thermal effects were much more pronounced in comparison to confining pressure effects.

LeCompte attempted models using both empirical ($\epsilon = at^n$) and dislocation rate theories while Heard investigated flow mechanisms and used only dislocation models. LeCompte using a reaction rate model $\dot{\epsilon} = A \exp(-E/RT)$ found the activation energy for rock salt to be 30,000 cal/mole at 300°C and decreasing to 12,500 cal/mole at 29°C.

LeCompte also investigated a dislocation model based on dislocation intersections and recovery by dislocation climb as presented by Mott. Upon integration of Mott's dislocation model the general term $\dot{\epsilon} = at^n$ was obtained which indicated that the empirical equation was a simplified form of the dislocation rate equation presented by Mott.

Heard's work with polycrystalline rock salt found two dominant mechanisms to appear. The first occurred at differential stresses greater than 100 bars at 2 kilobars confining pressure and consisted of intracrystalline glide while the second occurred at differential stresses below 100 bars and consisted of polygonization glide and climb. Provided that a steady-state condition had been reached, the intracrystalline glide was modeled using the Eyring equation, $\dot{\epsilon} = A \exp(-E/RT) \sin(B\sigma)$, giving non-newtonian fluid flow while polygonization was best fit using the Weertman equation, $\dot{\epsilon} = A \exp(-E/RT) \sigma^n$, with fair agreement attained by both. It should be noted that in the strictest sense, a steady state was not obtained by Heard since small stress rate variations could be observed in nearly all of the testing stages.

Modeling of Rheological Behavior

Many authors have gone into rather in-depth discussions concerning general modeling theory and principles, for example Flugge (1967) and Nadai (1963); while for rock creep only, Cruden (1970a) and Wawersik (1972) are examples. Therefore, complete synopsis of modeling shall not be given here. However, in order to substantiate later arguments concerning power law modeling, a few remarks are warranted concerning empirical, physiological and viscoelastic modeling.

Cruden discussed at length the possibility of rejecting an exponential model compared to a power law with time fit for "hard" rock data. As a basis for rejection, a statistical goodness of fit test using residuals was conducted. From a goodness of fit criteria based on the accumulation of residuals, at least half of the creep data used for analysis could not be modeled using an exponential law with a significance level of 1%, while all power law fits with time were not rejected. Following a regression analysis, Wawersik used a number of empirical fits to uniaxial creep data of limestone, sandstone and granite. When using a correlation coefficient analysis, Wawersik was unable to reject any one fit in preference to another. Thus, for empirical modeling, it appears that a power law fit is reasonable.

As for modeling the material on a physiological basis of dislocation glide and polygonization, Weertman (1957, 1971), Ree (1960), and others have proposed rate models composed of either a power stress or a hyperbolic function of stress, both combined with the exponential term $\exp(-E/RT)$. Use of a physiological model requires determination of the activation energy, E . If a material exhibits steady-state creep at a given deviatoric stress state and temperature, then a least square fit analysis to the modeling equations may be conducted to determine the activation energy and coefficient terms. For example, Heard (1972) used the Weertman and Eyring equations, $\ln \dot{\epsilon} = \ln A - E/RT + N \ln \sigma$ and $\ln \dot{\epsilon} = \ln A - E/RT + \ln \sinh(B\sigma)$, to describe polygonization by dislocation climb and intracrystalline slip respectively. Since no steady-state behavior was observed in the present study, direct application of activation energy principles was not possible. Even when considering strain rather than strain rate and introducing a time

function into the equations, insufficient data was available to determine the constants for a given time, temperature and stress state.

Finally, for the development of a complete theoretical viscoelastic model, Lai (1973, 1974) and Hardy (1959) indicated that for even a linearly viscoelastic material, modeling requires at least fifteen to twenty samples for various uniaxial stress states and reproducibility requirements. When elevated temperatures, confining pressures, and non-linearity are included into the material model, 30 to 40 samples are required for testing, thus involving many months for long-term creep analysis. Obviously the simplest means of modeling involves the empirical approach when considering the time involved for long-term creep analysis, material complexity and available samples.

SPECIMENS

The rock salt used for testing was produced by the Morton Salt Co. using either flash pan or solar distillation techniques. The rock salt was formed in a confining vessel 8" x 8" x 13" using an axial load of approximately 17,000 psi. The salt was of high purity with subsequent grain density tests agreeing to the density of pure halite to within 99.8%. Two inch diameter samples were cored using a saturated salt water solution as the coolant. Specimen ends were machined parallel to within $\pm .001$ in. using a lathe.

Cored specimens were received from RE/SPEC, Inc. in four shipments. Table I gives sample dimensions and physical properties for each lot. Selected samples from each shipment received from RE/SPEC, Inc. were tested for porosity, bulk density, moisture content and grain size so as to check for uniformity of lot. In addition, these same physical properties were determined for all specimens after testing. The permeability of selected specimens was also checked to determine the possibility of creating pore pressures during tests. Appendix I states the techniques used in determining the physical properties.

Observable physical property trends are apparent from Table I. The moisture content for all samples was very low ($< 1\%$) or at least could not be driven off at 100°C . These trends were dependent upon type of test, lot type and whether temperature tests had or had not been run. Creep tests gave lower porosity values than quasi-static tests. Further observation yields that the untested samples from each

TABLE I
Physical Property Data

Sample No.	Lot	Initial Dimensions (in.)		Sample Test Conditions	Moisture (%)	Porosity (%)	Bulk Density (gm/cc)	Permeability of selected samples (10^{-3} darcies)
		Length	Diameter					
S-3A	0	---	---	Creep (23°C)	.184	6.48	2.009	---
S1	1	4.110	1.985	Quasi-static	.119	7.79	1.991	---
S2	1	4.107	1.983	Quasi-static	--	--	---	---
S3	1	4.109	1.984	Quasi-static	.260	7.21	2.004	---
S4	1	3.947	1.980	Creep (23°C)	.13	3.62	2.081	10.63
S5	1	4.109	1.991	Creep (23°C)	.08	2.96	2.036	---
S6	1	4.037	1.983	Quasi-static	.039	2.71	2.101	---
S7	1	3.966	1.981	Quasi-static	.10	5.19	2.047	---
S8	1	4.112	1.987	Quasi-static	.030	3.75	2.079	---
S9	1	4.101	1.984	Untested	.75	10.05	1.943	113.4
S10	1	4.031	1.981	Creep (23°C)	.048	3.57	2.082	---
S11	2	4.003	1.975	Creep (23°C)	.107	2.14	2.108	---
S12	2	3.884	1.982	Untested	.100	6.06	2.028	6.88
S13	2	3.939	1.981	Creep (23°C)	.047	1.87	2.119	9.61
S14	3	4.004	1.942	Untested	.068	4.97	2.052	9.82
S15	3	4.003	1.963	Creep (23°C)	.044	1.55	2.126	---
S16	3	4.008	1.976	Creep (23°C)	.117	5.95	2.031	---
S17	3	4.031	1.964	Creep (23°C)	.029	1.42	2.129	6.76
S18	4	4.040	2.012	Aborted Creep Test (12 hrs)	.128	6.04	2.020	---
S19	4	4.030	2.015	Untested	.473	7.57	1.981	13.8
S20	4	4.042	2.010	Creep (66°C)	.317	4.33	2.062	---
S21	4	4.020	2.013	Creep (97°C)	.289	3.92	2.075	3.12
S22	4	4.050	2.016	Creep (66°C)	.229	4.12	2.067	---
S23	4	4.029	2.011	Creep (128.5°C)	.102	2.80	2.099	7.91

lot were generally higher in porosity than the tested samples, which indicates that some consolidation did occur in the specimens during testing. Table I also indicates that when the untested and tested porosities are compared, the lots varied in initial porosity. Lot 1 tested and untested porosities are, on the average, higher than the tested and untested porosities of lots 2, 3 and 4. This difference in initial porosity would depend upon the method used in forming the salt crystals, the pressure of formation used to create the samples as well as the location within the salt block in which the sample was cored.

In all but one instance, the permeability of tested samples was less than in untested samples, with permeabilities ranging from 3.12×10^{-3} darcies (S21) to 113.4×10^{-3} darcies (S9). The lowest permeabilities occurred in the temperature creep tests, even though the smallest porosities were observed for the room temperature tests. Since the permeabilities were in the millidarcy range, build-up of pore pressure during testing was deemed unlikely since adequate permeability and fluid ventilation was present.

Grain size analysis was used to evaluate differences in initial sample properties and to classify the material grains (coarse, medium, fine, etc.) using methods described by Richardson (1971). The dye used for grain size analysis was suggested by Freedman (Texas A & M) and consisted of using toluidine blue crystals in dimethylsulfoxide fluid. For grain size determination it was necessary to first polish the sample end and then lightly etch the polished surface using distilled water. After etching with water for about 15 seconds, the etched surface was coated with the dyeing compound and allowed to stand for about five minutes. The excess dyeing compound was then wiped off and the prepared surface

observed under the monocular to judge whether the process needed repeating. Pre-test grain boundaries were easily observed using this technique and gave a mean grain size of 0.7 mm for selected samples from each lot. This would then be defined as a small grained material according to Dreyer (1972). No statistically significant difference in mean grain size existed among the tested samples.

Post creep grain size analysis was not successful except for the outer 1/16 inch layer where surface fracture occurred. The dye was not able to penetrate the polished sample surface even when using a partial vacuum. Dye penetration of the post-creep surface fractures was partially successful using a vacuum technique. Figure 1 shows one of the dyed samples with fracture lines. In some instances these lines were observed to start 1/2 inch from the sample bottom and traverse diagonally at angles of $\sim 65^\circ$ and 115° (with respect to horizontal) through and along the sides of salt grains to within about 1/2 inch of the top of the sample. Thus, it would appear that at least on or near the sample surface considerable fracture was occurring thus increasing the surface porosity while a simultaneous reduction in bulk sample porosity was occurring.

Figure 1. Surface fractures on sample S21 using dye impregnation.

LOADING APPARATUS, INSTRUMENTATION AND CALIBRATIONS

Two similar loading systems were developed for this study based on the design of W. Wawersik (1973). The difference between them was in the sensitivity to which volume changes of the specimens were measured. System A was able to handle larger differential stresses. Consequently this system had a larger axial loading ram and also a vessel volume which allowed for additional lateral creep. As a result it was capable of measuring large creep ($>1\%$ strain) while being less sensitive to small creep. System B, due to its smaller vessel volume, was sensitive to small creep ($<1\%$), yet unable to handle massive creep. The two systems' combined measuring capabilities spanned the spectrum of desired tests. Figure 2 shows a photograph of the testing system.

Except for the previously mentioned differences, the two systems were identical. Axial loading was accomplished using double acting hydraulic cylinders contained within a loading frame of end platens connected by four tie rods. The loading frames were capable of 30,000 pounds force. Proper alignment of the end platens was achieved by precision machining of the tie rods in conjunction with shimming of the rods under axial load. Lateral loads were maintained through the use of a servo-controlled pressure intensifier. In turn, the servo mechanism was regulated by a specially designed differential pressure gauge. Both lateral systems had a design capacity of 10,000

Figure 2. Creep testing system showing loading components and deformation sensing equipment.

psi. Figure 3 shows a schematic of the system. Axial loading was accomplished by using a gas driven "Sprague" hydraulic pump and a 3000 psi capacity gas accumulator connected in series to the pressure vessel.

To achieve sample isolation from the confining fluid, samples were sealed between two loading pistons (Figure 4) using either silicone rubber or polyurethane for jackets. Jacket thicknesses were approximately .04 inches. The loading pistons extended outside of the vessels with small capillaries contained within each piston. Thus pore pressure build-up was prevented by venting the sample to the atmosphere.

For the constant stress creep experiments, both the axial and lateral stresses had to be maintained to within ± 5 psi. Constant axial stresses were accomplished using a 3000 psi capacity gas accumulator identical to that used for loading. The accumulator was connected in series with the loading ram. The lateral stresses were held constant through the action of the servo-controlled pressure intensifier (Figure 5). The intensifier was actuated by operation of a differential pressure gauge. The differential gauge was essentially a cylinder where one side was separated from the other using a metal bellows free to extend or distend. To one side of the bellows an LVDT (linear variable differential transformer) core was mounted. About the core was the LVDT windings. For operation, one side of the differential pressure gauge was isolated at the desired pressure level with the LVDT millivolt output reading zero ohms. The open side was then equal to the vessel pressure. As the sample increased or decreased in diameter, the vessel pressure increased or decreased

Figure 3. Schematic diagram of axial and lateral loading systems.

Figure 4. Salt sample sealed between the loading pistons using a polyurethane jacket.

Figure 5. Servo-controlled lateral pressure intensifier and lateral strain indicator.

as well. This would correspond to the bellows moving toward or away from the referent side of the differential gauge, causing the LVDT core to move in or out of the winding, therefore generating a millivolt output. The millivolt output was transferred to an external motor control which had a preset activation sensitivity of about ± 100 millivolts. When the output exceeded the triggering sensitivity the motor control activated the pressure intensifier causing it to either raise or lower the pressure depending upon whether the output was positive or negative. Corrective action ceased upon return of the output reading to zero millivolts. Stresses were held constant to within ± 5 psi for the axial system.

Emergency shut-off units were installed as safety features for both the axial and lateral systems. To prevent further loading ram travel after sample rupture, a microswitch was placed in the line of ram movement. Once activated, the switch would trigger the closing of a solenoid valve. The valve was placed between the loading ram and the accumulator and when activated would dump all pressure in the ram while isolating the loading accumulator. The lateral emergency shut-off unit was similar in that a microswitch was placed in line with the intensifier piston line of travel. Its operation was simply to shut off the servo system in the event that the intensifier piston had traveled too far in either direction. As an additional safety feature the differential pressure gauge had been designed for a 3500 psi overload.

Axial force was measured by either a 5000 psi capacity Heise pressure gauge or a load cell. Four 500 ohm strain gauges connected to a full Wheatstone bridge formed the load cell. The pressure

gauge was used to monitor the force during creep tests while the load cells were mainly used for recording force during quasi-static experiments. Lateral pressure was measured by means of a 10,000 psi capacity Heise pressure gauge for both creep and quasi-static tests.

Temperatures above ambient were maintained by using an API phase fired triac temperature controller. The heating elements with asbestos insulation were placed in an annulus of sheet metal with the heating elements wound about the inner rings and the asbestos wound about the elements. The pressure vessel fit tightly in the center annulus. For controlling feedback, a thermocouple was placed at the mid-height of the pressure vessel extending into the vessel fluid. With this system, temperature was maintained to within $\pm 1^\circ \text{C}$ after two hours were allowed for a steady state condition to be reached.

Both axial and lateral strains were measured by indirect means. Because rock salt was known to exhibit extensive creep, it was decided that strain gauges would not function due to the possible microfracturing and anticipated large strains. In addition, strain gauge bonding would also become a problem because of the poor surface qualities of rock salt. Further strain gauging complications would arise upon the escape of moisture from the specimens during testing since degeneration of both the bonding agent and gauges could be expected. Consequently axial deformations were measured by means of two externally mounted DCDT (direct current differential transformer) transducers. When axially mounted, the transducers were diametrically opposed. The DCDT's were mounted on phenolic rod cantilevers connected to the base plate of the loading frame. A

similar cantilever system was attached to the upper loading piston. This cantilever held the push rods to which the DCDT plungers were attached by magnets. Thus as the upper piston traveled with axial creep of the sample, the DCDT plunger moved a corresponding distance. The cantilevers isolated the DCDT's from the pressure vessel to reduce temperature effects. To eliminate eccentric loading effects the DCDT's were connected in series, thus summing their outputs. By calibration of the DCDT's with metal samples, the summed output was related to axial strain. DCDT calibration with and without the holding magnets caused no change in output.

Lateral strains were found according to the volume of lateral confining fluid displaced during a test. An equation relating volumetric displacement to the lateral strain was derived by W. R. Wawersik (1973) specifically for the type of lateral servo system which was used. Validity of the volumetric equation was determined through calibration tests on metal samples. The derivation of the volumetric equation may be found in Appendix II. A ± 6 volt range potentiometer was placed in contact with the intensifier. Thus as the intensifier turned to adjust the pressure in the lateral system, the potentiometer also turned. Through calibration of the intensifier-potentiometer unit, the cubic centimeters displaced per millivolt output was obtained.

The axial strain (DCDT) sensitivity was determined to be $\pm 15 \times 10^{-6}$ in/in for a 4 inch sample when considering data acquisition sensitivity and thermal drift of the DCDT's. The lateral strain sensitivity was evaluated as $\pm 14 \times 10^{-6}$ in/in for a 2 inch diameter sample. Lateral strain sensitivity was dependent upon the

differential pressure gauge gain. If greater sensitivity had been desired, the gain could have been increased at least tenfold.

Calibration Tests

Both axial and lateral strain calibration tests were required prior to experimentation. Incorporated in the strain calibrations were error analyses of thermal and end effects. Because axial strain measurement included the loading piston deformation, calibrations were performed to eliminate its axial strain contribution from the total strain response. Lateral strain calibrations were used to evaluate the machine constants contained in equation 4 below which was developed by Wawersik (1973). Appendix II gives the derivation of equation 4 which relates volume displacement to lateral strain for a cylinder deforming to a cylinder:

$$\epsilon_2 = \epsilon_3 = \frac{1}{(1-\epsilon_1)} \left[\frac{\Delta V}{C_1'} - (C_3 + C_2\epsilon_1)F \right] \quad (4)$$

where ϵ_2 and ϵ_3 are the principal lateral strains, ΔV is the displaced volume while C_1' , C_2 and C_3 are machine constants, ϵ_1 is the principal axial strain and F is the applied force. Use of this equation depends upon predicting sample shape during deformation. For example, if the sample deformed from an initial cylinder to a parabolic shape, equation 4 would be modified by a factor of 3/2 where the lateral strain would represent the mid-sample (maximum) strain. As to whether a cylindrical or parabolic approximation is best for describing sample deformation shall be discussed later in this section.

Axial and Lateral Strain Calibration Tests (at Room Temperature)

Cylinders of mild steel, aluminum and brass having similar

diameters (1.998 in $\pm$.002 in) and lengths (3.945 in $\pm$.010 in), were used to calibrate for axial and lateral strains. Young's Modulus of elasticity and Poisson's ratio were determined for each metal sample from previous tests conducted with strain gauges. A series of nine quasi-static triaxial tests were done at room temperature for each of the two loading systems with three tests being run for each of the metal samples. The steel, aluminum and brass quasi-static lateral deformation curves were analyzed using a first order linear least square fit. Wawersik (1973) showed that for metal calibration specimens, the lateral strain equation 4 reduced to

$$\epsilon_2 = \epsilon_3 = \frac{\Delta V}{C_1} - C_3 F \quad (5)$$

due to the small axial strain ($\epsilon_1 < .02\%$ strain). Consequently, the slopes of the calibration curves were used to validate the machine constants, C_1' , C_3 and L_p as well as to determine the axial strain contribution of the loading piston. Table II summarizes the experimental and theoretical equations describing the terms C_1' , C_3 and L_p . Derivations for the theoretical equations are in Appendix II.

The terms A_0 and L_R represent the initial sample area and length, respectively, γ_p is Poisson's ratio for the loading piston, E_p is Young's Modulus of elasticity for the loading piston and L_p is the effective length of the test column inside the pressure vessel. In the experimental formulations the ΔV_{Al} and ΔV_{st} terms are the volume changes per unit force for the aluminum and steel samples as determined from the slopes of the calibration curves. Similarly, $\bar{\epsilon}_2^{Al}$ and $\bar{\epsilon}_2^{st}$ are the experimental lateral strains per unit force obtained from the strain gauge determined elastic moduli for the

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TABLE III
Axial and Lateral Strain Calibration Values

Calibration Constant	System A			System B		
	Theoretical	Experimental	% difference	Theoretical	Experimental	% difference
Axial Transducer Output (millivolt / 10^{-3} in.)	---	14.21	---	---	12.91	---
C_1' (in. ³)	24.79	25.98	4.8 %	24.79	23.39	6.0 %
C_3 (in/in/lb _f)	2.538×10^{-9}	3.084×10^{-9}	21.5 %	3.094×10^{-9}	3.300×10^{-9}	6.7 %
L_p (in.)	7.375	8.099	9.8 %	8.125	8.391	3.3 %

The constant C_1' was determined from the sample dimensions using the theoretical formulation given in Table II.

TABLE IV
Brass Elastic Moduli

Elastic Moduli	System A		System B		Strain Gauged Values
	Value	% Difference	Value	% Difference	
Young's Modulus (10^6 psi)	13.69	1.63 %	13.61	1.04 %	13.47
Poisson's Ratio	0.370	1.65 %	0.346	-4.94 %	0.364

Table IV shows that system A moduli agree to within 1.65 percent of the strain gauged values. System B shows Young's Modulus to agree to within 1.04 percent while giving a difference of -4.94 percent for Poisson's ratio. To achieve maximum accuracy only system A was used for quasi-static testing of the salt. This suggests an accuracy for sample strains of better than 2% for room temperature tests. Both systems A and B were used for creep testing under the consideration that the -4.94% error in Poisson's ratio was tolerable.

Because volume displacement is used to determine lateral strain, the errors in volume measurement caused by thermal and end effects must also be considered. Knowledge of the magnitude of thermal and end effects may give an upper bound accuracy to axial and lateral strains.

Thermal Effects

When running a quasi-static or creep test involving a temperature differential between the pressure vessel and the pressure lines feeding the vessel, thermal effects on volume displacement must be

considered. The factors influencing salt tests were sixfold:

- a) The magnitude of the coefficient of thermal expansion of the confining fluid,
- b) The amount of pressure tubing increase or decrease as heated confining fluid was expelled or pushed into the vessel,
- c) The thermal gradient within the sample,
- d) The rate at which thermal equilibrium was reached in the sample, pressure vessel and pressure lines,
- e) The rate at which the sample was loaded during the test, and
- f) The amount of drift present in the temperature controller.

Each of these six factors were analyzed as to their effect upon the lateral strain.

The coefficient of thermal expansion for Dow Corning Dielectric 200 silicone fluid at ambient pressure was determined to be $1.04 \times 10^{-3} \text{ in}^3/\text{in}^3 \text{ } ^\circ\text{C}$. Tests conducted to 100°C showed the expansion coefficient to be constant within this range. The pressure effect on the expansion coefficient was not directly determined. However, an indirect measure of the pressure effect of the expansion coefficient was ascertained by simultaneously monitoring volume drift and thermal drift. For example, the thermocouple in the system B pressure vessel gave an average output variation from six tests of $\pm 0.0234 \text{ mv}$ at 3000 psi confining pressure. This corresponded to a temperature variation of $\pm 0.468^\circ\text{C}$. Similarly the average variation in vessel volume was simultaneously recorded as $\pm 4.64 \times 10^{-3} \text{ in}^3$ which indicated a temperature variation of $\pm 0.472^\circ\text{C}$ using $\beta = 1.04 \times$

$10^{-3}/^{\circ}\text{C}$. Although representing only a point value, this suggests that at the pressures and temperatures being used, the thermal expansion coefficient may be considered a constant.

Thermal equilibrium in the salt sample and pressure tubing must also be considered. The salt reached thermal equilibrium about 2 hours after the initial temperature had been set with a resulting thermal gradient of $\pm 2^{\circ}\text{C}$ occurring throughout its length. The effect of pressure tubing expansion or contraction in a quasi-static test was calculated to be on the order of $1.8 \times 10^{-5} \text{ in}^3/100^{\circ}\text{C}$ or approximately 10^{-6} in/in strain. This is well below the sensitivity of the intensifier.

The effect of thermal drift of the pressure vessel temperature upon lateral strain data depends upon the type of test and loading rate. The temperature variation of $\pm 0.47^{\circ}\text{C}$ occurred within a 24 hour cycle. Consequently, during creep tests the temperature variation would show as a cyclic effect in the lateral component of strain. However these cyclic trends were either negligible or could be subtracted from the creep test results. When running a fast quasi-static test on the order of 7 psi - 10 psi/sec loading rate the temperature effects were again minimal because of the short duration of the test ($t < 10 \text{ min.}$). The test most effected by thermal drift of $\pm 0.47^{\circ}\text{C}$ was a slow quasi-static test with a loading rate on the order of 0.5 psi/sec and a test duration time greater than 120 minutes. For a slow loading rate quasi-static test the load vs. deformation slope increased or decreased depending upon whether the vessel temperature increased or decreased. Figure 6 shows 66°C quasi-static test runs on an aluminum sample at 10 psi/sec

Figure 6. Elevated temperature calibration curves.

loading rates with the result that the millivolt output for fast loading tests are in error by a maximum of 4% while the error in slope is about 17% for the slow loading rate tests.

With the above data, thermal consideration and correction for various loading states could be made. Because the quasi-static tests were loaded at rates greater than 8 psi/sec, "fast" loading conditions existed thus eliminating thermal correction for the 66°C temperature tests. Elevated temperature creep tests were corrected for thermal contraction of existing fluid from the vessel by including a correction term involving the thermal expansion coefficient in the lateral strain equation as shown in Appendix II, equation n.

End Effects

Error in volume displacement was also caused by end effects. The end effects caused the samples to deform parabolically (figure 7) thus deviating from the fundamental assumption that right cylinders deform to right cylinders. Post-test sample shapes were plotted with first, second and third order least square fit analyses being conducted. Figures 8, 9 and 10 show radial deformations as measured with a micrometer for tested samples S6, S13 and S15. The plots showed that as the percent axial strain increased, the sample progressively deviated from the theoretical cylinder to a parabolic shape. Thus, from the experimental data it appears that the actual and theoretical deformations agree for axial strains of 0 to 4%. From 4% axial strain to higher values, substantial deviation from the theoretical right circular cylinder is observed. The line drawn in the curves labeled R_{avg} is the surface of a right circular cylinder needed to give the same volume displace-

Figure 7. Salt samples showing the barreling effect of room temperature creep tests, samples S13, S17 and A3-S as compared to an untested sample S14.

RADIAL DEFORMATION CURVE FOR COMMERCIAL SALT BLOCKS

Figure 8. Sample S6 post test radial deformation.

Figure 9. Sample S13 post test radial deformation.

Figure 10. Sample S15 post test radial deformation.

ment in the system. This average radial strain is less than the maximum mid-sample strain by approximately 23.9% and 28% for samples S13 and S15, respectively. The R_{avg} value corresponds to the radius calculated using the lateral strain equation 2. As to which value of strain is appropriate in describing sample properties, the mid-sample strain or the average radial strain, depends upon whether the average strain represents the lateral strain that would occur if no end effects were present, i.e., if a frictionless end cap were used.

End effects caused by the sample-end cap interface have been investigated by numerous authors: Baligh (1973), Brady and Blake (1972) and Al-Chalabi, et al. (1974) to name a few. Baligh's work was the most applicable for studying the salt in that it involved the numerical solution of samples deformed to .08 in/in axial strain using finite element methods. Parameters used in his study of end effects of standard cylindrical triaxial configurations included the slenderness ratio, the friction at the steel-specimen interface, the thickness and stiffness of the steel platens, anisotropy, nonlinearity and plastic flow, and pre-existing joints and cracks in the specimen. By independently varying the parameters, changes in the uniformity of the stress field within the sample were observable. The parameters for the triaxial systems A and B of this study were determined and matched to the appropriate conditions in Baligh's investigation. It was found that the stress field was uniform to within 5.2% error or less with the resulting axial and lateral strains determined to within 2% and 6.3% error respectively when compared to the perfectly smooth end cap-specimen stresses and strains. The lateral strain error of 6.3% was derived from the

cylinder deforming to a cylinder assumption. Hence, for tests in which axial strains were less than .08 in/in equation 4 was used to calculate lateral strains. The resulting error in Poisson's ratio was calculated to be at worst 7%.

A basic and important result from Baligh's work was that in the middle 2/3 of the sample in a "real" test the lateral strains for the no slip interface analysis was 5% higher than the smooth interface results. This indicates that using the parabolic formulation for lateral strain determination would not be correct since it calculates for mid-sample strain only. For axial strains greater than .08 in/in an even larger percentage error of mid-sample lateral strain would occur when compared to the true strain. This suggests that using the average lateral strain as determined from the deformed cylinder approximation is more representative of sample deformation which would occur if no end cap friction were present. Thus, equation 4 was also used for the large strain creep tests.

Sum of Errors

In light of the previously discussed errors in strain measurements due to metal sample calibration, thermal and end effects, it is appropriate to summarize the total strain error. Axial and lateral strains for quasi-static room temperature tests were accurate to within 2% (system A) while for elevated temperatures an axial strain accuracy to within 2% was indicated, with lateral strains accurate to within 4%. For axial strains less than .08 in/in, the strain accuracy for creep tests was predicted to be within 2%, with lateral strains accurate to within 6.3%.

EXPERIMENTAL PROCEDURES AND DATA ACQUISITION

As stated previously, both quasi-static and creep tests were conducted using commercial rock salt cylinders prepared by RE/SPEC Inc. All specimens were tested in triaxial compression.

The rock salt specimens were first inserted between two loading pistons inside polyurethane or temperature resistant silicone rubber jackets. This three-piece unit was put into the pressure vessel. After filling the vessel with Dow Corning Diaelectric 200 silicone fluid and securing the upper vessel seals (viton), it was placed into the loading frame. Next a small axial load of less than 50 psi was applied to the loading pistons to steady the vessel for connection of the lateral confining pressure line. Before loading the sample, both the axial DCDT's and the lateral potentiometer outputs were adjusted to the desired initial values.

All samples were subjected to 500 psi hydrostatic pressure. This pressure was maintained for 24 hours for sample conditioning, i.e., to impose a common loading history. After 24 hours the pressure was raised to the desired operating pressure at which point quasi-static or creep tests were conducted. The loading rate for quasi-static tests was about 10 psi/sec which induced strain rates between 10^{-6} to 10^{-5} in/in/sec. During creep testing, differential axial loading was halted once a predetermined stress state was achieved at which time a precharged accumulator was opened into the system to maintain the load.

Quasi-static tests were conducted at lateral confining pressures from 500 psi to 2500 psi at 500 psi intervals. At each confining pressure, tests were conducted at 21.5°C and 66°C. Three quasi-static tests were performed on each sample. Each of these tests will be denoted as an "experiment" in the following discussion. For example, a specimen would first be tested quasi-statically at a confining pressure of 1000 psi and 21.5°C. Then the confining pressure would be raised to 1500 psi and, after 24 hours, another quasi-static test would be run. Finally, the temperature would be elevated to 66°C and, after another 24 hour period, a final quasi-static test conducted.

Creep tests were conducted at confining pressures from 500 psi to 2500 psi at 500 psi intervals. Two or three creep tests were conducted on each specimen so that possible model representation might be more easily defined. Each of these tests will be designated as an "experimental stage" in the following discussion. The first ten experiments tested for reproducibility of results at room temperature, and the dependency of creep on confining pressure; while the remaining four experiments focused upon the influence of elevated temperature on creep.

Experimental data (DCDT, potentiometer and load cell output in millivolts) was recorded differently depending upon whether the test was a quasi-static or a creep test. All quasi-static tests were instantaneously logged on a Hewlet-Packard X-Y recorder. All creep tests were recorded on paper tape using a Honeywell digital data acquisition system (Figure 11). Subsequent data reduction consisted of converting force and total displacement to stresses and strains.

Figure 11. Honeywell data acquisition with paper tape millivolt print-out.

For quasi-static data this was accomplished by manual conversion to stress-strain diagrams whereas a computer program was used to convert creep test output to stresses and strains. The Honeywell digital system could be either manually triggered at intervals greater than 10 seconds, or automatically triggered at 1 hour intervals for recording data. During the first hour of creep, the digital system was manually triggered, after which the system was set to automatically record data at one hour intervals. The raw data thus obtained was analyzed using a Hewlett-Packard 9100 A computer with plotter attachment. For example, both linear strain vs. linear time and log strain vs. log time plots were generated for subsequent analysis.

EXPERIMENTAL RESULTS

The testing program was divided into four phases. The first phase concentrated on the stress-strain response of the commercial salt during hydrostatic loading. Phase II studied the response of the salt during quasi-static triaxial compression tests. The information gained from the tests was used to select differential stress levels for subsequent creep testing. Phases III and IV consisted of salt creep tests at room temperature and elevated temperatures, respectively. Tables VI and VII give the testing sequence for the quasi-static and creep tests. Note the positive stress and strain denote compression while positive strain causes an element to shorten.

All samples were examined after testing. The porosity, void ratio, particle density and, for selected samples, the permeability and grain size distribution were determined for tested samples as discussed previously.

Hydrostatic Consolidation

Attention was directed toward deformation under hydrostatic loading primarily because of consolidation. That appeared to occur because of the considerable and variable porosity of the commercial salt studies. The samples were hydrostatically loaded to the desired confining pressures and allowed to consolidate for 24 hours. Figure 12 shows typical 24 hour consolidation curves

Figure 12. Sample S8 axial and lateral strain hydrostatic consolidation curves.

as observed for the salt sample S8 at 1500 psi hydrostatic pressure.

Table V shows the hydrostatic consolidation stresses and strains for the first loading stage quasi-static samples while Figure 13 shows the respective volume strain (consolidation) plots. Loading stage two of sample S3 was used for the 1000 psi consolidation curve instead of sample S6, stage 1, due to a drop in load during the testing of sample S6. Accurate measurement of the instantaneous volume shrinkage during loading was not possible using the present loading apparatus. However, it was determined that the maximum 24 hour volume strain was at least on an order of magnitude greater than the instantaneous volume strain through measurement of the axial strain component.

TABLE V
Hydrostatic Consolidation

Sample Number	Loading Stage	Hydrostatic Stress ($\sigma_1 = \sigma_2 = \sigma_3$)(psi)	Maximum Strain (24 hr)		Volume Strain $\epsilon_1 + 2\epsilon_2$ (10^{-3})
			Axial (10^{-3})	Lateral (10^{-3})	
S3	1	500	+ 3.57	+ 3.41	+ 10.39
S3	2	1000	+ 6.85	+ 8.70	+ 24.25
S8	1	1500	+11.96	+10.60	+ 33.16
S5	1	2000	+12.40	+18.24	+ 48.88

Although hydrostatic creep tests were conducted prior to each first stage quasi-static test, the initial creep tests on the virgin samples are of most interest. Considerable volume shrinkage is noted from Table V and Figure 13 ($\sim 5\%$ maximum volume strain at $\sigma_3 = 2000$ psi). The axial strain was approximately equal to the lateral strain which is an indication that the salt may be considered isotropic. The volume change corresponds to a reduction of porosity and, in fact, it is the porosity of the material which induces the

Figure 13. Hydrostatic creep volume strain curves showing sample consolidation with time.

hydrostatic consolidation. This consolidation may be related to both the creep tests and quasi-static tests to help explain differences in strain characteristics.

Quasi-Static Tests

Nine multi-stage quasi-static tests were conducted at room temperature and 66°C, with confining pressures ranging from 500 psi to 2500 psi. Tests were conducted to about 1% axial strain. The results of the tests are found in Table VI. All indicated stresses and strains are nominal values referencing the original sample dimensions. Although failure was not observed in any of the quasi-static tests, three distinct regions were evident. The three regions, as seen in the idealized Figure 14, may be defined as being 1) pseudo-elastic, 2) transitional, and 3) strain hardening. In the pseudo-elastic region, cyclic loading showed an initial "elastic" slope, while once past the transitional phase "yield point", unloading-loading cycles again approximated the "elastic" slope with a continuance of the strain hardening curve as axial stress increased. Both axial and lateral strains exhibited this general loading behavior. Loading rates for the tests ranged from 7.1 to 12.2 psi/sec in the pseudo-elastic region resulting in a strain rate of $5 \times 10^{-6} \text{ sec}^{-1}$. Strain rates were $1 \times 10^{-5} \text{ sec}^{-1}$ in the transitional region and $3 \times 10^{-5} \text{ sec}^{-1}$ during work hardening.

Many methods have been proposed for defining the elastic modulus of salt. Secant, tangent, 2% offset and unloading curve tangent are but a few of the elastic modulus definitions that have been used. A tangent to the initial loading curve was used to describe the

TABLE VI
Quasi-Static Triaxial Compression Test Data

Sample	Loading stage	Confining Pressure (psi)	Temperature, °C	Initial Elastic Modulus (10 ⁶ psi)	Initial Elastic Poisson's Ratio, ν_e	Bisector Yield Stress, $\sigma_1 - \sigma_3$, psi	Axial strain at Yield, %	Work Hardening Tangent Modulus, (10 ⁵ psi)	Principle Strain Ratio in Work Hardening Region	Loading Rate (psi/sec)	Total Axial Strain (%)	Total Lateral Strain (%)
S2	1	1000	21.5	2.63	.061	2608	0.234	2.02	0.345	10.0	0.83	0.306
S2	2	1000	66.0	2.85	.094	3557	0.184	0.500	0.338	10.0	0.808	0.239
S3	1	500	21.5	1.74	.072	1076	0.235	0.961	0.293	0.4	1.08	0.309
S3	2	1000	21.5	1.92	.163	2055	0.147	0.838	0.245	7.1	0.586	0.170
S6	1	1000	21.5	1.79	.147	2505	0.243	1.50	0.370	9.1	1.01	0.313
S5	2	1500	21.5	3.88	.123	4110	0.313	0.785	0.148	7.1	0.993	0.367
S6	3	1500	66.0	2.67	.144	3776	0.298	0.281	0.231	8.5	0.982	0.362
S8	1	1500	21.5	1.75	.102	2414	0.221	1.61	0.340	9.6	1.16	0.332
S8	2	2000	21.5	2.45	.130	3344	0.246	1.26	0.325	10.0	1.13	0.345
S8	3	2000	66.0	3.15	.065	3119	0.231	0.880	0.210	12.2	0.960	0.338
S5	1	2000	21.5	2.45	.118	2876	0.257	1.47	0.253	10.6	1.12	0.295
S5	2	2500	21.5	2.87	.083	4317	0.256	0.856	0.220	10.0	1.01	0.423
S5	3	2500	66.0	2.92	.097	4037	0.301	0.915	0.215	8.6	1.28	0.437
S1	1	1000	21.5	1.69	.170	--	--	1.96	0.390	--	0.82	0.108
A1-S	1	1000	21.5	--	--	1800	0.22	--	--	10	3.4	--
A2-S	1	1000	21.5	--	--	1910	0.44	--	--	10	7.0	--
A4-S	1	3000	21.5	--	--	2650	0.33	--	--	10	9.3	--

IDEALIZED QUASI-STATIC TRIAXIAL COMPRESSION TEST CURVE

Figure 14. Idealized quasi-static loading behavior.

initial elastic modulus (Young's Modulus) and Poisson's Ratio in the pseudo-elastic region. A tangent was also used to describe the work hardening modulus and a principal strain ratio. The bisector of the two tangents was used to give the yield point as shown in Figure 14. This point approximately corresponds to the maximum rate of change of the loading curve.

Figure 15 shows the selected first stage quasi-static tests, S3, S6 and S5. From these and other tests the first and second loading cycle yield stresses were plotted as a function of the first and second stress tensor invariants, J_1 and J_2 respectively as shown in Figure 16 with $J_1 \equiv \sigma_1 + \sigma_2 + \sigma_3$ and $J_2 \equiv 1/6 [(\sigma_{11}-\sigma_{22})^2 + (\sigma_{33}-\sigma_{11})^2 + (\sigma_{33}-\sigma_{11})^2] + \sigma_{12}^2 + \sigma_{23}^2 + \sigma_{31}^2$. The first cycle yield point data shows that as the mean stress increased from 750 psi to 2000 psi the yield stress increased from 1000 psi to 2500 psi. At higher mean stresses the rate of increase of the yield stress decreased. Work hardening caused yield stresses to increase as shown for the second loading cycle. Figure 17 shows typical axial and lateral stress-strain curves for a three-stage loading test as obtained from sample S8. As seen from Figure 17 and Table VI, the yield stresses increased as work hardening progressed. The effect of temperature was to decrease the yield point even after considerable work hardening had already occurred from loading stages 1 and 2. Furthermore, Table VI shows that an increase in temperature from 21.5°C to 66°C caused the work hardening slope to decrease (1.50×10^5 to $.281 \times 10^5$ psi for sample S6). The amount of decrease is a function of the stress levels and previous work hardening. Increasing confining pressure for first stage loading appeared to increase the

Figure 15. Selected quasi-static compression test results for commercial salt samples S3, S5, and S6.

Figure 16. Quasi-static compression yield stress data from first and second loading cycles indicating an increase in the "yield" point with mean stress and work hardening.

Figure 17. Three stage loading quasi-static test comparison for sample S8. Note the increase in yield stress as work hardening progresses and the decrease in yield stress with temperature application.

work hardening slope from 0.961×10^5 psi at $\sigma_3 = 500$ psi. For sample S3 to 1.47×10^5 psi at $\sigma_3 = 2000$ psi for sample S5. Table VI also shows that the initial elastic modulus was, in general, about an order of magnitude greater than the tangent in the work hardening range (2×10^6 psi vs. 1×10^5 psi) with the initial elastic Poisson's ratio being about 0.11 as compared to the principle strain ratio of about 0.28 in the work hardening range.

It is believed that because of prior consolidation and subsequent pore collapse, the initial elastic moduli determined for the salt samples S1, S2, S3, S5, S6 and S8, are higher than the values found for samples A1-S, A2-S and A4-S which had no prior consolidation before quasi-static testing. The moduli for samples A1-S, A2-S and A4-S agree within the scatter of data to values determined by RE/SPEC, Inc. for unconsolidated salt samples obtained from the same source ($\sim 5 \times 10^5$ psi). Thus, the samples S1, S2, S3, S5, S6 and S8 would approximate *in situ* salt samples due to the allowed consolidation, i.e., the allowed hydrostatic consolidation would approximate the geostatic loading effect. Published values for *in situ* polycrystalline salt elastic moduli appear to vary about a mean value of 1×10^6 psi, Dreyer (1972). This agrees with the tangent modulus obtained for the consolidated salt.

In order to understand the strain history of the salt, plots of the principle strain ratio versus the differential stress ($\sigma_1 - \sigma_3$) were made for all of the quasi-static tests. Figure 18 shows an idealized principle strain ratio versus differential stress plot while Figure 19 shows typical salt plots as obtained for sample S5, stages 1, 2 and 3, respectively. The idealized curve shows the

IDEALIZED PRINCIPLE STRAIN RATIO VARIATION DURING QUASISTATIC TESTING

Figure 18. Idealized principle strain ratio variation with differential stress loading during quasi-static tests.

Figure 19. Principle strain ratio variation for sample S5, loading stages 1, 2, and 3.

the general regions in which the pseudo-elastic, transition and work hardening phases occurred. During the pseudo-elastic region the principle strain ratio increased until the transition region was reached. In the transition zone a plateau occurred or at the least an inflection in the curve was observed. Finally, the principle strain ratio would again increase as work hardening progressed. During first stage loading often little or no change in principle strain ratio was noted. As work hardening occurred from preceeding stages of loading, the principle strain ratio curves approached the idealized curve. This is shown in Figure 19. Serata (1968) observed a plateau to occur at yield whereas in the tests reported herein an increase in principle strain ratio occurred after yield.

Creep Tests

Fourteen multistage creep tests were run at temperatures ranging from 21.5°C to 128.5°C and at confining pressures ranging from 500 psi to 3000 psi. Except for samples A3-S, A4-S and S1, all samples were given an initial 24 hour 500 psi hydrostatic load. Samples A3-S and A4-S were given no preload while sample S1 was given a 500 psi hydrostatic preload for six hours. The samples were then brought to their respective confining pressure and loaded deviatorically at a rate of 10 psi/sec until the desired differential load had been reached. Due to slow jacket leaks, lateral creep data was not obtainable for samples S4, S7 and S11. Table VII lists the pertinent creep data while Figure 20 shows strain versus time and log strain versus log time creep curves as recorded for lateral strain sample S13 stage 2. The elastic response caused by initial deviatoric loading is not shown in the creep curves. Creep

TABLE VII
Triaxial Compression Creep Data

SAMPLE NO.	LOADING STAGE	TEMPERATURE (°C)	CONFINING PRESSURE (psi)	DIFFERENTIAL STRESS, $\sigma_1 - \sigma_3$ (psi)	TEST DURATION (hrs.)	MAX. AXIAL STRAIN, ϵ_1	MAX. LATERAL STRAIN, ϵ_3	MIN. AXIAL STRAIN RATE, $\dot{\epsilon}_1$ ($10^{-5}/hr$) (at end of test)	MIN. LATERAL STRAIN RATE, $\dot{\epsilon}_3$ ($10^{-5}/hr$) (at end of test)	Power Law Equation $\epsilon = at^n$ (t in hours)			
										Axial Strain, ϵ_1		Lateral Strain, ϵ_3	
										n	a (10^{-3})	n	a (10^{-3})
A3-S	1	21.5	1000	3820	75	4.1	-2.1	---	---	.50	4.7	---	---
	2	21.5	1000	6370	24	8.1	-5.1	---	---	.53	16	---	---
A4-S	1	21.5	3000	3830	48	3.1	-1.2	---	---	.44	5.4	---	---
	2	21.5	3000	6440	21	6.8	-3.8	---	---	.44	20	---	---
S4	1	21.5	1500	2285	100	0.20	---	7.55	---	.395	0.329	---	---
	2	21.5	1500	4575	73	1.20	---	0.73	---	.611	0.891	---	---
S7	1	21.5	500	1470	89	0.953	---	4.95	---	.581	0.736	---	---
S10	1	21.5	500	730	126	0.300	---	1.43	---	.594	0.171	---	---
	2	21.5	500	1470	99	0.387	---	3.10	---	.815	0.0973	---	---
	3	21.5	500	2705	76	0.551	---	6.10	---	.762	0.200	---	---
S11	1	21.5	1500	2290	127	3.01	---	9.76	---	.388	4.63	---	---
	2	21.5	1500	4580	122	11.1	---	64.8	---	.858*	1.85*	---	---
S13	1	21.5	1500	2285	120	2.36	-0.174	9.95	-0.744	.40	3.55	.390	0.266
	2	21.5	1500	4575	94	5.00	-2.03	29.1	-11.5	.634	2.92	.625	1.189
S15	1	21.5	2500	2400	140	3.85	-0.115	8.82	-1.25	.429	4.93	.800**	0.0335**
	2	21.5	2500	4800	111	10.1	-5.15	9.41	-54.3	.977*	1.00*	1.128*	0.240*
S16	1	21.5	500	730	131	1.30	-0.025	5.51	0.0	.633*	0.531*	---	---
	2	21.5	500	1470	141	1.56	-0.390	9.68	-2.4	.789	0.324	.813	0.0708
	3	21.5	500	2705	296	3.96	-1.70	9.80	-4.52	.716	0.681	.815	0.174
S17	1	21.5	1500	2290	159	5.86	-1.10	11.76	-2.97	.392	8.26	.593	0.437
	2	21.5	1500	4580	36	8.78	-4.28	197.6	-108	.750	5.73	.828	2.24
S20	1	66.0	500	700	131	2.66	-0.185	4.85	-1.16	.507	2.37	.692**	0.063**
	2	66.0	500	1435	131	3.60	-1.50	18.9	-10.3	.817	0.656	.868	0.211
	3	66.0	500	2640	64	8.25	-4.90	124	-76.5	.878*	2.11*	.939	0.955
S21	1	97.0	1500	1780	95	4.37	-0.58	39.7	-7.04	1.135	0.269	1.057*	0.0501*
	2	97.0	1500	3690	27	10.3	-4.40	265	-126	.665	11.48	.712	4.17
S22	1	66.0	1500	1685	105	4.56	-0.63	17.9	-4.59	---	---	1.032	0.0546
	2	66.0	1500	3460	36	5.44	-2.49	120	-55.1	.717	3.83	.757	1.38
S23	1	128.5	1500	1780	106	6.26	-1.31	43.2	-9.37	.471	8.13	.625	0.750
	2	128.5	1500	2810	37	11.9	-4.95	221	-86.5	.703	9.18	.785	2.880

- * Curves became linear with log vs. log plotting after ~ 10 hr. test duration.
 * Did not reach a linear state with log vs. log plotting.
 ** Considerable data scatter (approximately linear with log vs. log plot after 30 hours).

Figure 20. Linear axes and log versus log plots of the lateral creep strain response for sample S13.

test duration ranged from 21 hours for test A4-S stage 2 to 296 hours for test S16 stage 3.

Table VII lists the test temperature, stress levels, test duration and end-of-test strains and strain rates as well as the constants for the power law fit which was used to describe the material response. A power law fit was applicable since log vs. log plots of strain vs. time were linear for most of the tests in both axial and lateral strains. In a few loading stages, log vs. log linearity was not achieved until ten to thirty hours of creep testing had elapsed. Loading stage 1 of sample S22 axial strain and loading stage 1 of sample S16 lateral strain showed no linearity upon log vs. log plotting.

An empirical approach to data modeling was decided upon after considering two factors: a) because of the expected nonlinearity, an inadequate number of samples would be available for complete modeling using a theoretical viscoelastic model, and b) because of the low pressure and temperature states being used, a phenomenological approach using rate controlled processes such as dislocation glide would not be reasonable since steady state creep did not occur from the commercial salt, thus complicating the modeling equations. The $\log \epsilon$ vs. $\log t$ model was a reasonable fit as exhibited by Figure 21 which shows the power law fit for the axial strain of salt sample S11, one of the worst cases. It is readily apparent from Figure 21 that the model gives adequate results for even the most difficult fits if linearity is obtained in the later portions of creep. Using the power law equations in Table VII, obvious comparisons and trends may be observed.

Figure 21. Linear axes and log versus log plots of the axial creep strain response for sample S11 (a worst case fit).

Table VIII was constructed so that possible correlations of the axial strain power law terms n and a could be observed with respect to mean stress ($J_1/3$), octahedral shearing stress $(2/3 J_2)^{1/2}$ and temperature. Due to the small amount of data, correlation of lateral strains was not possible. From the table, data set 1 lists the first stages of loading at room temperature. It may be observed from data set 1 that as mean stress and octahedral shearing stress increases, the exponent n decreases from about .6 to .4 while the parameter, a , shows an increasing trend from .17 for sample S10 to 4.9 for sample S15. Table VIII also lists the second and third stage loadings at room temperature in data set 2. The results of data set 2 suggest the effect of strain history: as mean stress and octahedral shearing stress increase, the exponential term n remains constant at an average value of .77 or perhaps drops slightly from .815 to .611 as obtained for samples S10 and S4, respectively. Simultaneously an increase in the constant term, a , is noted from .097 for sample S10 to 5.73 for sample S17. Figures 22 and 23 show trends in n and a when plotted with octahedral shear stress and mean stress variations. More tests would be necessary to separate the mean stress effect from the octahedral shearing stress effect.

No correlatable tendencies were observed for the elevated temperature creep tests using the power law fit constants. However, as will be shown in the discussion section by comparing strain and strain rate at various time intervals in conjunction with the bulk comparison of the strain versus time plots, basic tendencies produced by elevated temperatures may be observed.

TABLE VIII
Power Law Fit Correlation With J_1 and J_2

DATA SET NO.	SAMPLE NO.	LOADING STAGE	TEMPERATURE	MEAN STRESS $J_1/3$, psi	OCTAHEDRAL SHEARING STRESS, $(2/3 J_2)^{1/2}$, psi	AXIAL STRAIN, ϵ_1	
						n	$a(10^{-3})$
1	S10	1	21.5	743	344	.594	.171
	S16	1	21.5	743	344	.633	.531
	S 7	1	21.5	990	693	.581	.736
	S 4	1	21.5	2,270	1,080	.395	.329
	S11	1	21.5	2,270	1,080	.388	4.63
	S13	1	21.5	2,270	1,080	.4	3.55
	S17	1	21.5	2,270	1,080	.392	8.26
	S15	1	21.5	3,030	1,130	.429	4.93
2	S10	2	21.5	990	693	.815	.097
	S16	2	21.5	990	693	.789	.324
	S10	3	21.5	1,400	1,275	.762	.200
	S16	3	21.5	1,400	1,275	.716	.681
	S 4	2	21.5	3,030	2,157	.611	.891
	S11	2	21.5	3,030	2,157	.858	1.85
	S13	2	21.5	3,030	2,157	.634	2.92
	S17	2	21.5	3,030	2,157	.750	5.73
	S15	2	21.5	4,100	2,263	.977	1.00

Figure 22. Power term, η , variation with J_2 and J_1 indicating a decrease in η as J_2 and J_1 increase.

Figure 23. Constant term, a , variation with J_2 and J_1 indicating an increase in a as J_2 and J_1 increase.

Recovery Measurements

Table IX shows the recovery of salt after one year. The samples were given no special storage between measurement, but were simply allowed to sit in laboratory storage open to ambient atmospheric conditions. In five of the eight samples the axial length increased while in seven out of eight tests the diameters decreased. The axial changes averaged $-.001$ inch while the average lateral change was $-.012$ inch. It appears that little change in axial length occurred while the average diameter change was an order of magnitude greater than the axial change. The average axial change was approximately $.025\%$ strain, i.e., a decrease. Dreyer (1972) observed axial strain recovery of 0.6% . The lateral recovery of the commercial rock salt was 0.6% .

TABLE IX
Recovery Data

Sample No.	Dimensions (in)			
	After 1 hour		After 1 year	
	Length	Diameter	Length	Diameter
S3	4.021	1.985	4.023	1.978
S4	3.885	1.990	3.880	1.986
S5	3.865	1.978	3.857	1.964
S6	3.901	1.988	3.895	1.978
S8	3.890	1.990	3.892	1.960
S13	3.627	2.024	3.621	2.019
S15	3.495	2.063	3.505	2.037
S16	3.738	2.005	3.739	2.006

DISCUSSION

It is obvious that the artificial salt showed appreciable volume shrinkage (consolidation) with time under constant hydrostatic loading ($\Delta V/V_0 \approx 1\%$ to 5%). The consolidation is due mainly to the large initial porosity which is not usually found in natural salt deposits (7 to 10 percent artificial salt porosities as compared to 1 percent or less *in situ* salt porosities). Because the consolidation is probably an inherent property of only the artificial salt, the following discussion will limit itself exclusively to the artificial salt experiments. Figure 13 shows that considerable volume strain could continue to occur past the 24 hour testing period as seen by the end of test volume strain rates in Table X. Consequently consolidation and its effect on the stress and temperature variables must be evaluated for both the quasi-static and creep tests. Because of the few number of tests, complete analytical formulations were not possible.

Hydrostatic Creep (Consolidation)

A three dimensional consolidation problem is involved during hydrostatic creep. Although traditional soil mechanics defines the consolidation problem as the release of excess pore fluid pressures by fluid flow, the consolidation experienced with the salt would be based on a completely different mechanism. Attempts were made to empirically model the hydrostatic creep using various laws. Plots involving $\Delta V/V$ vs. $\log$

TABLE X

Volume Strain Power Law Fit

Sample	Loading Stage	Hydrostatic Pressure, psi	Temperature °C	Power law fit $\epsilon_v = at^n$, t=hours		24 hour creep rate $\dot{\epsilon}_v$ in ³ /in ³ /hr, 10^{-7} (from raw data)
				n	a(10^{-3})	
S3	1	500	21.5	0.567	1.688	0.671
S3	2	1000	21.5	0.685	2.972	2.078
S8	1	1500	21.5	0.491	7.334	1.388
S5	1	2000	21.5	0.475	11.977	2.541

t, strain vs. log t, strain vs. $\sqrt{t}$, $\Delta V/V$ and e (void ratio) vs. $\sqrt{t}$ were made. The only plot to be linear was the log $\Delta V/V$ vs. log t plot, and this was only strictly true at confining pressures less than or equal to 1000 psi. At the confining pressures greater than 1000 psi the log-log plots progressively deviated from the linear condition. The dotted curves in Figure 13 show the derived power law curves vs. the raw data showing the deviance of the power law as confining pressure increased. Nevertheless, for a first approximation, the log-log fits were used for discussion and analysis. Table X gives the power law fit exponents and coefficients as determined from a first order least square fit analysis as well as the 24 hour creep rate.

Clearly, the effect of increasing the confining pressure ($1/3 J_1$) was to increase the consolidation as Figure 13 shows. However, the volume strain consolidation was found to be viscoelastically nonlinear at least for the first 24 hours since the first law of linearity failed, i.e., doubling the stress caused the strains to more than double.

The porosity of natural rock salt is small ($n < 1\%$, Dreyer (1972)) in comparison to the commercial salt tested herein. It thus appeared

advisable to cast the data in such a form as to examine volume and shear strains separately.

If an isotropic material is assumed then

$$1/3 \epsilon_H(J_1) = \epsilon_1(J_1) = \epsilon_2(J_1) = \epsilon_3(J_1). \quad (6)$$

An isotropic material assumption appears valid since axial and lateral strain components were on the same order of magnitude and approximately equal as shown in Table IX. Let the total creep or quasi-static strain response be denoted as $\epsilon_1(J_1, J_2)$ for axial strain and $\epsilon_2(J_1, J_2)$ for lateral strain giving the axial and lateral strain after subtraction of the hydrostatic strain as

$$\epsilon_1' = \epsilon_1(J_1, J_2) - 1/3 \epsilon_H(J_1) \quad (7)$$

$$\epsilon_2' = \epsilon_2(J_1, J_2) - 1/3 \epsilon_H(J_1) \quad (8)$$

Therefore the maximum shear strain produced after subtraction of the mean strain component is

$$\epsilon_1' - \epsilon_2' = \epsilon_1(J_1, J_2) - \epsilon_2(J_1, J_2) \quad (9)$$

which simply states that the shear strain does not involve the mean strain component ϵ_H (an obvious result assuming an isotropic material).

Thus shear strain was used to evaluate the creep tests. Furthermore, since shear strain does not include the hydrostatic component, it may be a better approximation to *in situ* salt behavior which exhibits little volume strain due to hydrostatic loading at room temperature.

Quasi-Static Triaxial Compression Tests

Figure 24 shows a plot of volume strain versus differential stress, $\sqrt{3}J_2$. The quasi-static volume strain response includes the hydrostatic and deviatoric stress effects. Only first loading stage responses are shown in Figure 24. The basic feature of the quasi-static volume strain curves is that sample consolidation accelerates during increased differential load except for sample A2-S. Three quasi-static tests, A2-S, S1 and S6, are shown at 1000 psi in which the pretest consolidation times were 0, 6 and 24 hours, respectively. Clearly, the effect of preloading was to decrease the volume strain during deviatoric loading. It also appears that preconsolidation changes the general shape of the volume strain curve. Sample A2-S shows initial volume consolidation then, as differential stress is applied, volume consolidation ends and a reversal toward volume expansion begins. In contrast, samples S1 and S6 show increasing volume strain (consolidation) with differential stress. Further examination of Figure 24 shows a general decrease in volume strain with increasing mean stress as shown in Table XI at the differential stress level of 3000 psi with sample S3 indica-

TABLE XI

Quasi-Static Volume Strain at 3000 PSI Differential Stress

Sample	Loading Stage	Differential Stress Level $\sigma_1 - \sigma_3$, psi	Confining Pressure σ_3 , psi	$\Delta V/V$, %
S3	1	3000	500	0.19
S2	1	3000	1000	0.045
S6	1	3000	1000	0.070
S8	1	3000	1500	0.033
S5	1	3000	2000	0.032

Figure 24. Volume strain response for selected tests during quasi-static differential stress loading.

Figure 25. Maximum shear strain response during quasi-static differential stress loading.

ting 0.19% $\Delta V/V$ and S5 indicating 0.032% $\Delta V/V$ at 500 and 2000 psi confining pressure, respectively.

The differential strain response during quasi-static loading is shown in Figure 25 for samples S2, S3, S6, S8 and S5. Both the differential strain and volume strain curves show little difference in general behavior at confining pressures at or above 1000 psi at least to the 1% axial strain obtained here.

Triaxial Compression Creep Tests

In order to compare and analyze multiply loaded creep tests it was necessary to gain an initial understanding of the history dependency. Both viscoelastic linearity and creep rate comparison of multiply loaded samples were used to investigate history effects, the results of which were used to compare multiply loaded tests.

Viscoelastic Nonlinearity

Viscoelastic linearity must satisfy two conditions. Firstly, a linear viscoelastic material must have a constant creep compliance, $J_c(t)$.

$$\epsilon(t)/\sigma = J_c(t) \quad (10)$$

Secondly, a linear material must obey Boltzman's superposition principle which states that the strain at a given time is the sum of the strains caused by the loads applied independently for their respective durations of time as given in equation 11.

$$\epsilon(t) = \sigma_0 J_c(t) + \sigma_1 J_c(t-t_1) + \sigma_2 J_c(t-t_2) + \dots \quad (11)$$

Viscoelastic linearity for the commercial salt may be best ana-

lyzed by using the power law equations in conjunction with Boltzman's superposition principle (since multiple loading tests were conducted). Boltzman's equation for a two stage loading test would be

$$\epsilon(t) = \epsilon_0(t) + \epsilon_1(t-t_1) = \sigma_0 J_c(t) + \sigma_1 J_c(t-t_1) \quad (12)$$

If $\sigma_1 = 2\sigma_0$ then

$$\epsilon(t) = \sigma_0 J_c(t) + 2\sigma_0 J_c(t-t_1) \quad (13)$$

Since a linear material implies a stress independent creep compliance, the effect of doubling the stress is to double the strain at a given time. Hence

$$\frac{\epsilon_1(t-t_1)}{\epsilon_0(t)} = \frac{2\sigma_0 J_c(t-t_1)}{\sigma_0 J_c(t)} = 2 \quad (14)$$

Writing this in terms of the power law equation would give

$$\frac{\epsilon_1}{\epsilon_0} = \frac{a_1}{a_0} t^{n_1-n_0} = c \quad (15)$$

Since this ratio should be independent of time, n_1-n_0 should be zero and the ratio of a_1/a_0 should equal the ratio of stresses σ_1/σ_0 . When examining the power law equations in Table VII it is clear that the exponents from the multiple loading tests are equal in only a few instances (sample A3-S, A4-S, S16 and S20). Even when the exponential terms were equal making the strain ratios time independent, the coefficient ratios (a_1/a_0) were not equal to the stress ratios (σ_1/σ_0). This then indicates a nonlinear material response. Failure to satisfy visco-elastic linearity implies that the material is history dependent during multiple loading tests.

Multiple Loading Behavior

Multiple loading stages were used during creep testing in order to observe loading history effects and (because of the limited number of available samples) to maximize the data obtained from each sample. In order to gain an initial understanding of the multiple loading material behavior the axial strains from tests S7 loading stage 1 and S10 loading stages 1 and 2 were compared. The confining pressure was 500 psi while the differential load was 730 psi for S10 stage 1 and 1470 psi for S7 and S10 stages 1 and 2, respectively. Figure 26 shows the axial strain creep curves for samples S7 and S10. The figure suggests that the salt may follow a current time response, i.e., the creep strains in segment C-D of sample S7 (Figure 26) would be equal to the creep strains in segment E-F of sample S10 loading stage 2. Similarly, the strain rates would be equal. For a current strain response to be valid, the creep strain in segment A-B would have to equal the creep strain in segment E-F. Defining the coefficient C_1 as the ratio of strain rates of segment C-D to E-F at selected times, a coefficient plot is obtained as shown in Figure 27. Also plotted in Figure 27 is the ratio of strain rates of segment A-B to E-F shown as the coefficient C_2 . (Power law fits were used for strain rate calculations.) A coefficient of 1 should be obtained if ideal current time or current strain response were being observed. Figure 27 shows the current time coefficient C_1 to be nearest the ideal value of 1 with the coefficient ranging from 0.71 at 1 hour and becoming asymptotic to 1.3 at about 130 hours. Since current time response appeared to dominate multiple loading behavior, it was used as a basis for comparing different loading stages. Because of material

Figure 26. Multiple loading behavior creep test comparison using samples S7 and S10.

Figure 27. Strain rate coefficient plots for determining either current time or current strain multiple loading behavior for commercial salt creep tests. (An ideal coefficient fit would give a value of 1).

isotropy and homogeneity, it shall be assumed that the lateral strains followed similar lines of reasoning as the axial strains.*

Creep Test Comparisons

There are two components of creep for the commercial block salt: They are a hydrostatic component and a deviatoric component. As stated previously, the maximum shear strain is a function of both J_1 and J_2 minus the hydrostatic volume strain response. Thus the shear strain should be a better indication of the deviatoric *in situ* material behavior since *in situ* material exhibits little hydrostatic volume strain response in comparison to the large volume strain response of the commercial salt. Thus, maximum shear strain and volume strain were plotted vs. time for each creep test S13 through S23 as shown for a typical salt sample S13 in Figure 28. The maximum shear strain and volume strain plots in Figure 28 show that volume strain rate was significantly decreased by an order of magnitude from loading stage 1 to loading stage 2. This was typical of all of the creep tests, i.e., the significant portion of volume strain occurred only in the first stage of loading. In subsequent stages of loading the maximum shear strain was an order of magnitude greater than volume strain.

Figure 29 shows a plot of first and second stage loading volume strains at 24 hours vs. the 24 hour volume creep strains as obtained for the hydrostatic creep tests. Clearly the effect of applying the deviatoric stress is to decrease volume strain. A least square fit of the volume strain curves shows that the volume strain for the deviatorically

*At the time samples S4 and S7 were tested, the lateral strains were in error due to small leaks in the lateral confining fluid system.

Figure 28. Maximum shear strain and volume strain response for creep test S13. Note the order of magnitude difference in strain response during the second stage of loading.

Figure 29. Volume strain comparison at 24 hours for deviatorically loaded and hydrostatically loaded creep tests. The effect of deviatoric loading is to decrease volume strain (consolidation).

loaded samples begins to deviate from the hydrostatically loaded samples at a mean stress of about 500 psi.

Confining pressure and thermal effects were also observed for the commercial salt. Figure 30 shows the maximum shear strain plots for the room temperature tests S13, S15, S16 and S17. Note that loading stage 3 was used in the comparison and, if current time behavior exists for the salt, represents the creep rates that would have occurred for a virgin sample at $t = 272$ hours to $t = 392$ hours. Thus the creep tests were compared at 272 hours in order to correlate the first stage loading tests S13, S15 and S17 to the third stage loading test, S16. At approximately the same differential stress ($\sigma_1 - \sigma_3 = 2420$ psi "average") a tendency for increasing creep with decreasing confining pressure is observed in Figure 30. For example, curve 4, sample S16 stage 3 is at the lowest confining pressure of 500 psi and shows at least twice the creep strain of curve 2 sample S15 stage 1, which was at the highest confining pressure of 2500 psi.

If the confining and differential stresses were held constant an increase in temperature had the effect of increasing creep, as shown in Figure 31. Curve 1 in Figure 31 is the average maximum shear strain creep curve from samples S13 and S17 loading stage 1. In the same figure it is shown that at the highest test temperature, test sample S23 (128.5°C), the maximum shear strain creep is about twice that observed for the room temperature tests even though the differential stress for sample S23 was 500 psi less than the room temperature tests. The effect of temperature on creep may also be seen in Figure 32. Two samples, S16 and S20, were loaded to nearly identical stress states in three loading stages in which S16 remained at 21.5°C and S20 at 66.0°C . Instantaneous

Figure 30. Pressure effect creep plot showing an increase in creep as confining pressure decreased.

Figure 31. Thermal effect creep plots showing an increase in creep with temperature.

Figure 32. Thermal effect plots showing a three stage loading comparison for samples S16 and S20. The increase in temperature from 21.5° C to 66.0° C caused the maximum shear strain to more than double.

strain response during quasi-static loading is not shown. The creep strains and strain rates of higher temperature sample S20 were greater than the room temperature test S16. The deviation of the creep strains and strain rates increased at each successive loading stage as verified by the strains and strain rates given in Table XII. This is implied by Table 12 by determining the ratio of maximum shear strains of S20 and S16 at the end of each loading stage; they were 2.5, 2.8 and 7.4 for loading stages 1, 2 and 3, respectively.

An initial understanding of the confining pressure effect at elevated temperatures was observed when tests S22 stage 1 and S20 stage 2 were compared. Figure 33 shows the plots of the two tests starting at 131 hours at the initiation of loading stage 2 for sample S20. Again, the time adjustment is invoked to compensate for current time material response. The plot shows that an increase in confining pressure from 500 psi sample S20 to 1500 psi sample S22 lowers the creep strain regardless of time by about one-half at an average differential stress level of 1560 psi and $T = 66.0^{\circ}\text{C}$.

Some data scatter is apparent in view of the differences in creep strain for identical tests S13 and S17 in Figure 34. This scatter may be largely due to the initial differences in physical properties and sample origin. No control was possible over these parameters since all samples were supplied by another agency involved in the testing program. Table I may be used as a reference to indicate initial physical sample differences.

TABLE XII

Creep Strain, Strain Rate and Principle Strain Ratio Versus Time

SAMPLE NO.	LOADING STAGE	FIRST STRESS INVAR. $J_1/3$ (psi)	SECOND STRESS INVAR. $J_2/3$ (psi)	PERCENT AXIAL STRAIN AT SELECTED TIMES				AXIAL STRAIN RATE AT SELECTED TIMES (10^{-8} /sec)				PRINCIPLE STRAIN RATIO AT SELECTED TIMES ϵ_2/ϵ_1			
				5 hr	10 hr	100 hr	1 yr	5 hr	10 hr	100 hr	1 yr	5 hr	10 hr	100 hr	1 yr
A3-S	1	2,270	3,820	1.35	1.49	4.7	44.0	29.2	20.6	6.5	0.70	----	----	0.51	----
	2	3,120	6,370	3.76	5.42	18.4	197	111	79.8	27.1	3.31	----	0.64	----	----
A4-S	1	4,280	3,830	1.10	1.49	4.10	29.3	26.8	18.2	5.01	0.41	----	----	0.39	----
	2	5,150	6,440	4.06	5.51	15.2	109	226	67.3	18.5	1.51	----	0.56	----	----
S4	1	2,260	2,285	0.062	0.082	0.203	1.19	1.36	0.89	0.22	0.15	----	----	----	----
	2	3,030	4,575	0.238	0.364	1.49	22.8	8.09	6.17	2.52	0.44	----	----	----	----
S7	1	990	1,470	0.187	0.280	1.07	14.4	6.05	4.53	1.73	0.27	----	----	----	----
S10	1	743	730	0.045	0.067	1.02	3.76	1.47	1.11	0.435	0.071	----	----	----	----
	2	990	1,470	0.036	0.064	0.415	15.9	1.64	1.44	0.940	0.411	----	----	----	----
	3	1,400	2,705	0.068	0.116	0.668	20.2	2.89	2.45	1.42	0.488	----	----	----	----
S11	1	2,260	2,290	0.865	1.13	2.76	15.7	18.6	12.2	2.98	0.193	----	----	----	----
	2	3,030	4,580	0.744	1.33	9.62	4.46	35.1	31.8	22.9	12.15	----	----	----	----
S13	1	2,260	2,285	0.676	0.892	2.24	13.4	15.0	9.91	2.49	0.17	.073	.074	.071	.069
	2	3,030	4,575	0.810	1.26	5.41	92.2	28.5	22.1	9.53	1.85	.40	.398	.391	.375
S15	1	3,360	2,400	0.983	1.32	3.56	24.2	23.4	15.8	4.24	0.33	.012	.016	.037	.197
	2	4,100	4,800	0.482	0.948	8.99	711	26.2	25.7	24.4	22.0	.306	.340	.481	.945
S16	1	743	730	0.147	0.228	0.980	16.6	5.17	4.01	1.72	0.33	----	----	----	----
	2	990	1,470	0.115	0.200	1.23	41.8	5.06	4.37	2.69	1.05	.228	.230	.299	.272
	3	1,400	2,705	0.049	0.354	1.84	45.3	8.10	6.50	3.12	0.748	----	.321	.403	.627
S17	1	2,260	2,290	1.55	2.04	5.03	29.0	33.8	22.2	5.47	0.361	.073	.084	1.33	.328
	2	3,030	4,580	1.92	3.22	10.12	519	79.8	67.1	37.8	12.3	.442	.468	.560	.793
S20	1	733	700	0.54	0.76	2.45	23.6	15.1	10.7	3.45	0.380	.036	.041	.062	.143
	2	978	1,435	0.244	0.430	2.82	109	11.1	9.77	6.41	2.83	.350	.362	.407	.511
	3	1,380	2,640	0.867	1.59	12.0	611	42.3	38.9	29.3	17.0	.499	.522	.600	.787
S21	1	2,090	1,780	0.167	0.367	5.01	802	10.5	11.6	15.8	28.9	.164	.156	.130	.092
	2	2,730	3,690	3.35	5.31	24.5	481	124	98.0	45.3	10.1	.392	.405	.451	.556
S22	1*	2,060	1,685	.832	1.40	4.47	----	32.6	23.6	5.67	----	.005	.055	.135	----
	2	2,650	3,460	1.21	2.00	10.4	257	40.4	39.8	20.7	5.84	.386	.394	.433	.518
S23	1	2,090	1,780	1.74	2.41	7.11	58.5	45.4	31.5	9.31	0.874	.118	.131	.187	.373
	2	2,440	2,810	2.85	4.63	23.4	543	111	90.5	45.7	12.1	.357	.379	.457	.660

*Strains and strain rates obtained from raw data curves.

Figure 33. Pressure effect comparison at the elevated temperature of 66.0° C for samples S22 and S20. Increased confining pressure caused creep to decrease.

SUMMARY AND CONCLUSIONS

The principal aim of the investigation was to evaluate the lateral strain dilatometer for use in large strain - elevated temperature and pressure tests. From calibrations using steel, aluminum and brass specimens, it was determined that the dilatometer gave accurate results to within 2% for fast loading (10 psi/sec) quasi-static tests at ambient temperatures, and to within 4% for elevated temperature quasi-static tests conducted at 66°C. The lateral strain system was shown to be stable for long term creep tests using aluminum samples.

To evaluate the machine under actual working conditions, porous commercial block salt samples were used. Multistage triaxial compression, quasi-static, and creep tests were conducted at various temperatures and pressures from 21.5°C to 129°C and 500 psi to 3000 psi respectively. The results for the quasi-static tests showed an increase in yield with an increase in mean stress from 1080 psi at 500 psi confining pressure to 2880 psi at 2000 psi confining pressure. Most of the increase in yield occurred between 500 and 1000 psi confining pressure. A decrease in yield and work hardening was observed for the elevated temperature quasi-static tests. The quasi-static tests were allowed to consolidate for 24 hours prior to loading. The consolidation creep was recorded for each confining pressure with two immediate results: a) the lateral and axial strains were approximately equal indicating possible isotropic behavior, and b) as the confining pressure increased, so also did the hydrostatic creep. However, the hydrostatic creep (consolidation) was

viscoelastically nonlinear.

The ambient and elevated temperature creep tests showed that the confining pressure had little effect on the room temperature tests although considerable data scatter may be the cause of the discrepancy. The temperature effect was to increase strain rate for an increase in temperature at a given confining pressure and differential load. Because of material consolidation, maximum shear strain plots were used for comparison since the hydrostatic component of strain was eliminated in these plots.

Some consistent creep properties were apparent and may be assumed, in general, to hold true for the porous rock salt. A power law model of the form $\dot{\epsilon} = at^n$ clearly fits the data at nearly all creep stages, and the power of time increases for subsequent loading stages from about .5 for staged to about .8 for stages 2 and 3. Furthermore, the power is nearly always less than 1 except for the few tests which seemed to exhibit slightly accelerating creep rates (sample S15 stage 2 and samples S21 and 22 stage 1). The creep for the differentially loaded creep tests was nonlinear as seen from time comparison of strain as well as from multiple loading stage tests.

The data, in general, agrees with previous investigations. One of the main difficulties with the commercial salt was obtaining samples with initial physical properties (porosity, sp) which were similar. Care must be exercised in comparing various tests from different sample lots since small physical variations may induce large creep discrepancies. This may account for data scatter in the room temperature tests since samples from the same lots tend to show similar creep rates.

Upon considering the creep and quasi-static properties of the ma-

terial, the following may be concluded about the method of lateral deformation measurement:

1. For quasi-static tests loaded at rates of ~ 10 psi/sec thermal effects are minimized.
2. For long term creep studies, thermal cycles are either not observed or may be eliminated from the data.
3. For materials showing large plastic strain, the lateral strain determined from the theoretical equation gives an average response which describes the average lateral strain and is a lower bound to the mid-sample strain. The average strain appears to be in error by 6.3% (percent error not percent strain) at 8% axial strain.
4. For materials which do not allow direct mounting of lateral strain transducer elements (optics, cantilevers, strain gauges, etc.) the indirect measurement using the dilatometer appears to be the most viable solution.

APPENDIX I

Physical Property Techniques

The method used to determine the porosities of the salt specimens was based on Archimedes' principal that when an object is placed in a fluid, the bouyant force created by the fluid is equal to the volume of fluid displaced. Distilled water was used as the displaced fluid. To prohibit destruction of the salt during immersion, the salt was coated in latex. Consequently, the porosity test consisted of the following:

- a) Determine mass of latex jacketed salt sample in distilled water.
- b) Remove latex jacket and determine mass of jacket in distilled water.
- c) Weigh the salt sample immediately after removing the jacket.
- d) Place the salt sample in an oven at 100° C and after 24 hours find the mass of the sample
- e) Pulverize the oven dried salt sample and weigh out a 20 gm. sample into a calibrated flask.
- f) Place in hexane to calibrated line in flask and after weighing pull a vacuum for 24 hours to remove trapped air.
- g) Weigh the flask again and find the hexane temperature.

From the above procedure bulk sample volume, volume of solids, volume of voids, moisture content, and salt density may be determined. Furthermore, by knowing the bulk, solid and void volumes the porosity

and void ratio may be found.

Permeability of the rock salt was obtained using a transient flow equation. The method is described by Brace, et al. (1968) and was used to determine the permeability in granite. The equipment used during testing consisted of the jacketed salt sample with two porous end plates. The sample was confined with 100 psi lateral pressure while a pressure differential of approximately 10 psi was initiated across the salt sample. The pressure decay was then monitored versus time and the subsequent permeability found from a log pressure vs. time plot.

The equation used to calculate the permeability was

$$\alpha = \frac{KA}{LV_e \beta \mu} \quad \text{where } V_e = \frac{1}{V_1} + \frac{1}{V_2} \quad (a)$$

the term α is the slope of the $\ln (P_{\text{initial}} - P_{\text{time}}/P_{\text{initial}})$ vs. time plot and should be linear according to the theory. The terms A , L , β and μ describe the sample area, sample length, permeating fluid compressibility and permeating fluid viscosity. The upstream and downstream volumes are described by V_1 and V_2 , respectively. Since all is known in the equation except for the term K (permeability in darcies) the final answer is determined by solving for K in equation (a). Figures 2 and 3 show a typical transient pressure decay plot for the commercial salt and the resulting $\ln P$ vs. time plot, respectively. Note the linearity on the $\ln P$ vs. t plot. Note also that air was used as the permeating fluid.

Figure 34. Typical pressure decay curve for permeability tests with the commercial rock salt.

Figure 35. Log versus time permeability plot showing linearity as predicted by the theory.

APPENDIX II

Derivation of Lateral Strain Through Volume Displacement

Figure 36. Initial and final sample configurations for lateral strain derivation.

Variables

- L_P = effective vessel length
- D_P = initial loading piston diameter
- L_R = initial rock length
- D_R = initial rock diameter
- V = initial vessel fluid volume
- ΔV = change in vessel fluid volume
- D_{fP} = final piston diameter after deforming force applied
- D_{fR} = final rock diameter after deforming force applied
- ΔL_R = change in rock length due to deforming force

The derivation begins by assuming a deformation shape such as the cylinder deforming to a cylinder shown above. The resulting change in volume is then calculated.

$\Delta V = \text{final volumes} - \text{initial volumes:}$

$$\Delta V = \left[\frac{\pi D_{fR}^2}{4} (L_R - \Delta L_R) - \frac{D_o^2 \pi}{4} (L_R) \right] + \left\{ \left[\frac{\pi D_{fp}^2}{4} (L_p - L_R + \Delta L_R) \right] - \frac{D_o^2 \pi}{4} (L_p - L_R) \right\} \quad (a)$$

$$\Delta V = \frac{\pi}{4} \left\{ [D_{fR}^2 (L_R - \Delta L_R) - D_o^2 L_R] + [D_{fp}^2 (L_p - L_R + \Delta L_R) - D_o^2 (L_p - L_R)] \right\} \quad (b)$$

After combining terms and simplifying the change in volume becomes

$$\Delta V = \frac{\pi}{4} \left[D_{fR}^2 (L_R - \Delta L_R) + D_{fp}^2 (L_p - L_R + \Delta L_R) - D_o^2 L_p \right] \quad (c)$$

Introducing the diameter - lateral strain relationship as

$$D_f^2 = (1 + 2\epsilon_{\ell}) D_o^2 \quad (d)$$

$$\Delta V = \frac{\pi}{4} \left[(1 + 2\epsilon_{\ell R}) D_o^2 (L_R - \Delta L_R) + (1 + 2\epsilon_{\ell p}) D_o^2 (L_p - L_R + \Delta L_R) - D_o^2 L_p \right] \quad (e)$$

Simplifying equation (e) gives

$$\Delta V = 2A_o \left\{ \epsilon_{\ell R} (L_R - \Delta L_R) + \epsilon_{\ell p} (L_p - L_R + \Delta L_R) \right\} \quad (f)$$

or after isolating the lateral strains

$$\epsilon_{LR} = \left[\frac{\Delta V}{2A_o} - \epsilon_{lp} (L_p - L_R + \Delta L_R) \right] \frac{1}{L_R - \Delta L_R} \quad (g)$$

The loading piston lateral strain ϵ_{lp} may be related to the steel piston properties as

$$\epsilon_{lp} = \frac{\gamma_p F}{A_o E_p} \quad (h)$$

where γ_p and E_p are the steel Poisson's Ratio and Young's Modulus, respectively, A_o is the piston area and F the loading force. Using (h) in equation (g) results in

$$\epsilon_{LR} = \frac{1}{1-\epsilon_1} \left(\frac{\Delta V}{2A_o L_R} - \frac{\gamma_p F}{A_o E_p} \left\{ \frac{L_p - L_R}{L_R} + \epsilon_1 \right\} \right) \quad (i)$$

The following may be used as constants in the equation:

$$C_1 = 2A_o L_R \quad ; \quad C_2 = \frac{\gamma_p}{A_o E_p} \quad ; \quad C_3 = \left[\frac{L_p - L_R}{L_R} \right] C_2$$

giving

$$\epsilon_{LR} = \frac{1}{1-\epsilon_1} \left[\frac{\Delta V}{C_1} - F \{ C_3 + C_2 \epsilon_1 \} \right] \quad (j)$$

During temperature tests, a correction for fluid contraction must be included. Let β equal the fluid expansion coefficient, V equal the system fluid volume and ΔT equal the change in temperature. Assuming a linear expansion coefficient, the volume contraction becomes:

$$\Delta V_T = \beta - (\Delta T \cdot V). \quad (k)$$

If fluid is exiting from a chamber of elevated temperature, T_1 ($\Delta T = T_1 - T_0$), then the fluid volume shrinks by an amount ΔV_T . If the initial system

fluid volume is V_0 and the final system volume is V_f then

$$V_0 - \Delta V_T = V_f \quad (1)$$

or, substituting (k) into (1) gives

$$V_f = V_0 (1 - \beta \cdot \Delta T) \quad (m)$$

Since what is sought is the initial volume, V_0 , and the machine measures the final volume, V_f , thus

$$V_0 = \frac{V_f}{(1 - \beta \cdot \Delta T)}$$

Substituting into equation (j) gives the temperature corrected lateral strain as

$$\epsilon_{LR} = \frac{1}{1 - \epsilon_1} \left[\frac{\Delta V}{C_1 (1 - \beta \cdot \Delta T)} - F(C_3 + C_2 \epsilon_1) \right] \quad (n)$$

A parabolic deformation equation has been derived to relate volume change to mid-sample lateral strain. Assuming a frictionless end cap-sample interface and knowing the axial strain and volume change, the geometry of the parabola may then be used to give ϵ_{LR} . The volume change - lateral strain equation is identical to equation (j) except for a factor of 3/2, i.e.,

$$\epsilon_{LR} = \frac{3}{2} \frac{1}{1 + \epsilon_1} \left[\frac{\Delta V}{2A_0 L_R} - F(C_3 + C_2 \epsilon_1) \right] \quad (o)$$

The derivation is identical to the cylindrical deformation derivation except that the resulting parabolic sample volume is obtained using the parabolic area in conjunction with Pappus' theorem resulting in revolving the parabolic area about the sample center. If D_{fR} is the final mid-

sample diameter and D_o is the initial sample diameter, the centroid of the parabola is

$$\bar{C} = \frac{1}{10} (2 D_{fR} + 3 D_o) \quad (p)$$

and the area is

$$A_p = 1/3 (D_{fR} - D_o) (L_R - \Delta L_R) \quad (q)$$

where L_R is the rock length and ΔL_R is the change in rock length. The volume is then

$$V = 2\pi \bar{C} A_p$$

and the derivation proceeds as before.

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