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GEOHYDROLOGICAL ENVIRONMENTAL EFFECTS OF GEOTHERMAL POWER PRODUCTION PHASE IIA

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Final Report for Phase IIA

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FOREWORD

This interim technical report entitled, "Geohydrological Environmental Effects of Geothermal Power Production - Phase IIA", is submitted by Systems, Science and Software (S³) to the National Science Foundation (NSF). The report presents the results of the second year of a research effort to develop and validate a computational capability whereby field information for a given geothermal reservoir system can be used to predict the behavior of the reservoir and associated subsurface environmental effects due to production and reinjection of geothermal fluids. This work, in support of the NSF/RANN Geothermal Research Program, was accomplished under Grant No. AER 75-14492. Dr. Ralph N. Perhac was the cognizant NSF Program Manager for the second year of the research reported here. Mr. John W. Pritchett was the S³ Principal Investigator for this study.

Dr. Patrick R. L. Browne of the New Zealand Department of Scientific and Industrial Research and the University of California at Riverside served as consultant on the Wairakei geothermal field. Terra Tek, Inc. of Salt Lake City, Utah performed rock properties tests on Wairakei core samples under subcontract to S³.

During the course of the second year of this research program two papers were presented at the NSF sponsored workshop on Geothermal Reservoir Engineering (Garg [1975], Pritchett [1975]). Results of the Wairakei reservoir's simulation calculation have been presented both at the 17th U. S. Symposium on Rock Mechanics in Utah [Garg, et al., 1976] and at the International Conference on the Geothermal Reservoir in New Zealand (September, 1976).

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I. INTRODUCTION AND BACKGROUND

The goal of this research is to develop and validate a computational capability whereby field information for a given geothermal reservoir system can be used to reliably predict the behavior of the reservoir under specific conditions of fluid withdrawal and injection. The computer programs developed may be applied to predict the consequences of alternate production/injection strategies and to evaluate three potential environmental hazards: land surface subsidence, induced seismicity, and groundwater pollution resulting from reinjection of spent geothermal brines. The computer programs are time dependent, three-dimensional, and treat the transport of mass and energy by single or two-phase geothermal fluids being driven through a heterogeneous reservoir. Both thermal and mechanical interactions are treated in the models.

The research has been underway since May 15, 1974. The work accomplished during the first year (Phase I), was reported earlier [Pritchett, et al., 1975]. The work accomplished in the second year (Phase IIA) is documented in this report.

1.1 PREVIOUS WORK (PHASE I)

During the first year of this research program, the following was accomplished under NSF Grant No. GI-43885:

1. Development of basic energy-mass transport equations to be applied to a geothermal system.
2. Development of a numerical scheme and a basic one-dimensional test-bed computer program to solve the governing equations under the simplifying assumption that the rock is immobile.

3. Development of a computer program (QUAGMR) for solving the governing equations in one, two or three dimensions under the assumption that the rock is "quasi-active", i.e., the rock cannot move as a whole but its porosity, permeability and other properties can depend upon the instantaneous state of the interstitial fluids as well as location within the reservoir.
4. Incorporation of a comprehensive water (liquid & steam) equation of state computer routine into the quasi-active geothermal reservoir model computer programs.
5. Development of a separate three-dimensional computer program (STAGR) to treat the deformation of the rock.
6. Compilation of seismic data from Imperial Valley for use in Phase IIB (third year currently in progress).

Basically, the computer programs for treating the uncoupled fluid mass/energy transport and the rock stress-strain response were developed during the first year. The codes were validated against laboratory data and applied to idealized reservoirs. A complete description of the research results was documented in the report issued at the end of Phase I [Pritchett, et al., 1975]. The principal results of the first year were also published in the technical literature [Brownell, et al., 1975; Garg, et al., 1975a; Garg, et al., 1975b].

1.2 WORK ACCOMPLISHED IN PHASE IIA

This report describes the work accomplished during the second year of the research under NSF Grant No. AER75-14492:

1. The QUAGMR simulator has been applied to a five-spot geothermal reservoir production/injection pattern to demonstrate that the numerical scheme employed is free of grid orientation problems reported for other techniques.
2. The multidimensional two-phase (liquid water & steam) computer program for simulating heat and mass transport in a quasi-active geothermal reservoir (QUAGMR) has been combined with the corresponding multidimensional rock deformation program (STAGR) to create an interactive program. This fully active geothermal reservoir simulator (AGRESS) permits continuous iteration of the independent programs under changing fluid and rock conditions.
3. A series of reservoir calculations simulating pressure drawdown and buildup well tests were performed to examine the effects of two-phase flow. The two-phase simulator is required since classical single-phase analytical models are shown to lead to large errors in the permeability values inferred for the producing formation.
4. The effect of in-situ flashing near a wellbore has been examined in a series of reservoir calculations. Severe reduction of well deliverability results because of relative permeability effects even when no salt precipitation occurs. If the salinity is sufficient for precipitation, the problem is aggravated without significantly alleviating the corrosion-related problems associated with the produced brine.

5. The effect of in-formation flashing above the producing horizon has also been studied. The deliverability of the reservoir is improved by sustaining the reservoir pressure through the creation of a steam cap. This beneficial result is not materially affected by the salinity of the reservoir fluid.
6. A tabular equation of state for water-sodium chloride solutions is being constructed for a pressure range from one bar to two kilobars. It models the thermodynamic behavior of geothermal brines in the range of practical geothermal interest. The basic function of the package is to return values for the pressure, temperature, viscosity, and thermal conductivity when called with the arguments used in reservoir simulators (density, specific internal energy, and salinity).
7. A two-dimensional vertical model of the Wairakei geothermal system has been developed based on available geologic data, material properties tests conducted on rock core samples obtained from the New Zealand field, and the observed production and ground surface subsidence history of the field.
8. Application of the reservoir simulators to the Wairakei model has successfully reproduced the total production history over the time interval for which data are readily available (1953-1967). The calculations show that early in-formation flashing created a steam cap which maintained the reservoir-pressure and production rate until the cap expanded to the depth of the production zone.

9. The ground surface subsidence history of the Wairakei field has also been examined in terms of the calculated reservoir pressure history. Nonlinear ground movement processes clearly have been operating since 1963. The observed occurrence of maximum subsidence outside the production area of the field appears to be the combined result of the local geology and the fluid production history.

In Section II the numerical techniques embodied in the fully interactive reservoir simulator (AGRESS) are described; the results of the calculation for the five-spot production/injection pattern are also presented. The analysis of the pressure drawdown and buildup tests for geothermal reservoirs is presented in Section III. Section IV describes the effects of in-formation flashing of brines both near a wellbore and at levels above the producing horizon. The thermodynamic equation of state package for sodium chloride/water solutions is described in Section V. Section VI presents the calculations simulating the production and subsidence history of the Wairakei field. Finally, future research plans are outlined in Section VII.

II. RESERVOIR/SUBSIDENCE SIMULATORS

One objective of this three-year research program is to develop reliable computational tools whereby field information for a specific liquid- or vapor-dominated geothermal system can be used to predict reservoir performance and, in addition, subsurface environmental effects associated with fluid production/injection. These latter effects include potential land surface subsidence, induced seismic activity, and pollution of fresh water aquifers by geothermal brines. The approach is first to develop the necessarily complex computer codes and then to validate them using both laboratory measurements and field data.

All of these geohydrological effects involve thermo-mechanical interactions between the rock and fluid (water and/or steam) components of the system. These interactions include mass, momentum and energy transfer, variable porosity and permeability. A "fully interactive" theoretical model, constructed within the framework of the Theory of Interacting Continua, was previously developed to describe the thermo-mechanical response of the rock and fluid composite in terms of the isolated components (see Phase I report, Pritchett, et al. [1975]). The stress-strain equations for the rock matrix are coupled with the diffusion equations for the fluid in the fully interactive model. The microscale details of the pore/fracture network of the rock are ignored, but the fluid pressures and the stress field in the rock matrix are permitted to assume distinct values within each computational region for the composite.

During Phase I, separate computer codes were also developed for describing the multidimensional multiphase unsteady transport of heat and fluid mass in a heterogeneous geologic setting in the absence of rock shear deformation, and for calculating the stress-strain response of a

multidimensional rock matrix to prescribed pore pressure and temperature changes without specific consideration of fluid flow. The separate codes for describing the fluid flow (QUAGMR) and the general rock deformation (STAGR) have been described elsewhere [Pritchett, et al., 1975]. These codes have now been combined (in Phase IIA) to produce a single fully-interactive fluid flow/rock deformation geothermal reservoir simulator (AGRESS). The separate codes in themselves are of interest in geothermal investigations, however. QUAGMR has, in fact, been used for all of the applications to be presented.

In Section 2.1, for completeness of this report, a description of the quasi-active geothermal reservoir model incorporated in the QUAGMR code is presented. Section 2.2 describes the results of a 2-D areal simulation of a five-spot production/injection pattern which demonstrates that QUAGMR is free of any numerical difficulties associated with the orientation of its finite-difference grid. Section 2.3 contains a description of the rock-deformation model incorporated in the STAGR finite element code. Finally, the procedure used in coupling the STAGR and QUAGMR codes to form the fully active geothermal reservoir simulator (AGRESS) is described in Section 2.4.

2.1 QUASI-ACTIVE GEOTHERMAL RESERVOIR MODEL

QUAGMR describes the unsteady flow of heat and fluid mass in a "quasi-active" geothermal reservoir in one, two or three dimensions. The method properly treats the effects of fluid phase change (liquid $\rightleftharpoons$ vapor) within the pore space of the reservoir rock. The quasi-active case is midway in sophistication between the "rigid-matrix" model in which all rock properties (porosity, permeability, thermal conductivity, etc.) are treated as functions of position only and the "fully-interactive" model (in which the entire rock stress

and deformation fields are calculated along with the heat and fluid flow). In the quasi-active model, the rock thermal properties (heat capacity and thermal conductivity) may be functions of temperature as well as position; the permeability may change with porosity, and the porosity may also vary with position and time, but at a particular point, the local porosity is assumed to depend only on the local pore pressure. The isotropic deformation of the rock is included but not its distortional deformation under a shear stress.

The equations governing the quasi-active reservoir model may be summarized as follows [Pritchett, et al., 1975]:

Fluid Mass Conservation

$$\frac{\partial [\phi \rho]}{\partial t} = \dot{m} + \nabla \cdot \left[k ([\alpha_L \rho] [\nabla P - \rho_L \vec{g}] + [\alpha_V \rho] [\nabla P - \rho_V \vec{g}]) \right] \quad (2.1)$$

Energy Conservation

$$\frac{\partial}{\partial t} [E_s + \phi E] = \dot{e} + \nabla \cdot \left[k ([\beta_L E] [\nabla P - \rho_L \vec{g}] + [\beta_V E] [\nabla P - \rho_V \vec{g}]) + \bar{\kappa} \nabla T \right] \quad (2.2)$$

Local Thermal Equilibrium

$$T_s = T \quad (2.3)$$

where

	<u>S = 0</u> <u>(all liquid)</u>	<u>0 < S < 1</u> <u>(multiphase)</u>	<u>S = 1</u> <u>(all vapor)</u>
α_L	$1/\mu$	$\frac{1-Q}{1-S} \frac{R_L}{\mu_L}$	0
α_V	0	$\frac{Q}{S} \frac{R_V}{\mu_V}$	$1/\mu$
β_L	$1/\mu$	$\alpha_L [1 - QE_{\text{vap}}/E]$	0
β_V	0	$\alpha_V [1 + (1-Q)E_{\text{vap}}/E]$	$1/\mu$

and

E = Bulk fluid internal energy per unit fluid volume.

E_{vap} = Latent heat of vaporization per unit fluid volume.

E_s = Solid internal energy per unit total volume.

ρ = Bulk fluid density.

ρ_V = Vapor phase density = $\rho Q/S$.

ρ_L = Liquid phase density = $\rho(1-Q)/(1-S)$.

Q = Steam quality.

S = Steam saturation.

$\mu(\mu_L; \mu_V)$ = Bulk fluid (liquid; vapor phase) viscosity.

$R_V(R_L)$ = Relative vapor (liquid) permeability.

k = Absolute solid permeability.

$\bar{k}$ = Mixture (rock-liquid-vapor) heat conductivity.

ϕ = Porosity.

P	= Pressure.
T	= Fluid temperature.
T_s	= Solid temperature.
$\vec{g}$	= Acceleration of gravity.
$\dot{m}$	= Local fluid mass source/sink rate.
$\dot{e}$	= Local heat source/sink rate.

These balance laws are to be solved subject to appropriate initial and boundary conditions. Furthermore, constitutive relations must be prescribed both for the rock matrix and for the interstitial fluid. For the rock, the density, porosity, directional absolute permeabilities, relative permeability functions, heat capacity and thermal conductivity must be supplied at each point in the system. Also, the dependence of local porosity and permeability on pore pressure (if any) must be provided. For the fluid, a large number of properties must be known as functions of water density (ρ) and internal energy (E). These include pressure (P), temperature (T), steam quality (Q), vapor saturation (S), latent heat of vaporization (E_{vap}), and separate viscosities (μ_l , μ_v) and thermal conductivities (κ_l , κ_v) for liquid and vapor. For this purpose, a rather elaborate system of subroutines, describing the thermodynamic behavior of pure H_2O , was developed which use large data tables and various interpolation schemes valid up to ultra-high pressures (several megabars) and temperatures to 3000°C [Pritchett, et al., 1975].

2.2 QUAGMR TEST PROBLEMS

The system of balance equations governing the quasi-active geothermal reservoir model is solved by a finite difference technique embodied in QUAGMR, which has been described elsewhere [Pritchett, et al., 1975]. Essentially, an implicit-time, first-order (upstream) space representation of the equations is employed; the iterative Alternating-Direction-Implicit

(ADI) technique is used to reduce a single multidimensional problem to an equivalent sequence of one-dimensional problems. These one-dimensional problems are, of course, nonlinear in themselves - these nonlinearities are removed by iteration within the one-dimensional "module".

The QUAGMR code possesses considerable flexibility. Several geometries can be considered: (1) 1-D slab, (2) 1-D cylindrical, (3) 1-D spherical, (4) 2-D planar or areal, (5) 2-D axisymmetric, or (6) 3-D Cartesian. Each computational zone may contain a different rock type, and any face of any zone may be a boundary. Provision is made for all practical boundary condition options: (1) impermeable, insulated, (2) impermeable, prescribed heat flux, (3) impermeable, prescribed temperature, (4) prescribed mass flux, insulated, (5) prescribed mass and heat flux, (6) prescribed mass flux and temperature, and (7) prescribed pressure and fluid heat content. Boundary condition parameters may be functions of time.

QUAGMR has been extensively tested, using both simplified analytic problems with known solutions and bench-scale experimental results. Pritchett, et al. [1975] presented some of the test results against laboratory data. Briefly, one-dimensional simulations were performed on laboratory experiments carried out by Kruger and Ramey [1974] and Arihara [1974] at Stanford. These experiments involved flow in a narrow 60 cm long tube packed with sandstone. In these experiments, non-isothermal and multiphase flow occurred. Results computed by the simulator included pressure and temperature distributions within the tube as functions of time - agreement was generally within experimental scatter for all cases considered.

The QUAGMR reservoir simulator possesses several desirable numerical features. Mass and energy are conserved exactly, since the numerical scheme is based squarely upon density and internal energy rather than other auxiliary

quantities. Proper treatment of flow-type (i.e., prescribed-pressure) boundaries eliminates artificial computational "energy sources" at these boundaries, even under conditions of flow reversal. The use of the implicit upstream difference technique suppresses the computational "jitter" produced by many other simulators - artificial oscillations of this sort occasionally cause computational catastrophes in single-phase regions near the saturation line.

A recent paper by Coates, et al. [1973] describes a serious computational difficulty they encountered when performing a 2-D areal simulation of a five-spot steamflood of an oilfield. The "five-spot" pattern is a checkerboard-like system with alternating injection and production wells. Coates found that if he treated this problem with a grid oriented such that a line connecting adjacent production and injection wells lies at 45° with respect to the axes the computed water interface expands outward in a roughly circular manner, whereas if the grid is oriented with coordinate lines connecting adjacent production and injection wells, thin "fingers" of injected fluid penetrate outward rapidly. Times of water breakthrough at the production well differed by a factor of three for these calculations.

To investigate this problem the QUAGMR simulator has been used to calculate a five-spot cold water injection into a producing hot water field. These areal calculations were made using the 2-D Cartesian mode of QUAGMR for both grid orientations. Figure 2.1 shows the areal representation of the reservoir production/injection pattern along with the two grid orientations considered. The 7×7 grid at 45° and the 10×10 grid at 0° results in essentially the same zone dimensions for the two cases. By symmetry there is no flow across the boundaries of either grid. The thickness of the reservoir is 100 m and it initially contains pressurized water at $P = 100$ bars and $T = 300^\circ\text{C}$. The reservoir rock is assumed to

Figure 2.1. Five-spot pattern areal configuration used for QUAGMR grid orientation test problem.

be a "typical sandstone" with the following properties:

$$\rho_s \text{ (grain density)} = 2.65 \text{ g/cm}^3$$

$$\phi \text{ (porosity)} = \phi_0 = 0.2$$

$$k \text{ (permeability)} = k_0 = 50 \text{ md} = 5 \times 10^{-10} \text{ cm}^2$$

$$\kappa_s \text{ (thermal conductivity)} = 2.1 \times 10^5 \text{ ergs/sec-cm-}^\circ\text{C}$$

$$C_{vs} \text{ (specific heat)} = 10^7 \text{ ergs/g-}^\circ\text{C}$$

Note that in this case the porosity and permeability were fixed so that QUAGMR is reduced to a rigid-matrix model. The system remains single-phase throughout the calculation. Cold water injection is assumed to be at $T = 100^\circ\text{C}$; 57.4 kg/sec/well is the assigned injection rate.

Snapshots showing the evolution of the temperature field with time for both of the grid orientations are depicted in Figure 2.2. The contours for the two calculations are in close agreement, well within the resolution of the finite-difference grid employed.

Figure 2.3 shows (for both grid orientations) the variation with time of the fluid temperature delivered by each production well in the five-spot configuration. Times of cold water breakthrough in these two calculations are seen to agree to within a few percent. Therefore, it is believed that the numerical method used in the QUAGMR code is not subject to the difficulty experienced by Coats, et al. [1973], at least for problems of geothermal interest.

In Figure 2.4 a comparison for the two calculations is made of the gross thermal power delivered as a function of time. Also shown for each calculation is the fraction of the total available thermal energy that has been delivered at each point in time. The calculated power and depletion curves for the two orientations are within the resolution of the grid employed.

Figure 2.2. Evolution of the temperature field with time for the two grid orientations used. The solid contours refer to the 10×10 grid oriented at 0° ; the dashed contours correspond to the 7×7 grid oriented at 45° .

Figure 2.3. Grid orientation test problem - temperature of produced fluid.

Figure 2.4. Grid orientation test problem - power production history.

2.3 THE STAGR NONLINEAR FINITE ELEMENT CODE

The module used for calculating the structural equilibrium of the rock-fluid system in a geothermal reservoir, at the end of a QUAGMR time step, is the STAGR nonlinear finite element code. STAGR is used, in the present context, to calculate the equilibrium of the rock matrix using a prescribed rock-fluid interaction, keeping the fluid pressure constant.

The calculation of structural equilibrium, using constant pore pressures, is shown in Figure 2.5. Each step, except for the evaluation of the element "secant" moduli, has been described earlier [Pritchett, et al., 1975]. The theory of STAGR secant moduli is as follows:

For a linear, isotropic elastic material, the stress-strain relation may be written

$$\{\sigma\} = [C] \{\epsilon\} \quad (2.4)$$

where the stress vector is

$$\{\sigma\} = \begin{Bmatrix} \sigma_x \\ \sigma_y \\ \sigma_z \\ \sigma_{yz} \\ \sigma_{xz} \\ \sigma_{xy} \end{Bmatrix} \quad (2.5)$$

Figure 2.5. Computational procedure for STAGR finite element static equilibrium solution using constant pore pressure.

the strain vector is

$$\{\epsilon\} = \begin{Bmatrix} \epsilon_x \\ \epsilon_y \\ \epsilon_z \\ \epsilon_{yz} \\ \epsilon_{xz} \\ \epsilon_{xy} \end{Bmatrix} \quad (2.6)$$

and the stress-strain matrix is

$$[C] = \begin{bmatrix} \lambda+2\mu & \lambda & \lambda & 0 & 0 & 0 \\ \lambda & \lambda+2\mu & \lambda & 0 & 0 & 0 \\ \lambda & \lambda & \lambda+2\mu & 0 & 0 & 0 \\ 0 & 0 & 0 & 2\mu & 0 & 0 \\ 0 & 0 & 0 & 0 & 2\mu & 0 \\ 0 & 0 & 0 & 0 & 0 & 2\mu \end{bmatrix} \quad (2.7)$$

where λ and μ are the Lamé constants.

In one-dimensional problems, one has simply

$$\sigma = C\epsilon \quad (2.8)$$

which relation may be solved for the "secant" modulus, C , if $\epsilon \neq 0$.

For multidimensional problems, we postulate a relation of the form:

$$[C] = \begin{bmatrix} \lambda_x + 2\mu_x & \frac{1}{2}(\lambda_x + \lambda_y) & \frac{1}{2}(\lambda_x + \lambda_z) & 0 & 0 & 0 \\ \frac{1}{2}(\lambda_x + \lambda_y) & \lambda_y + 2\mu_y & \frac{1}{2}(\lambda_y + \lambda_z) & 0 & 0 & 0 \\ \frac{1}{2}(\lambda_x + \lambda_z) & \frac{1}{2}(\lambda_y + \lambda_z) & \lambda_z + 2\mu_z & 0 & 0 & 0 \\ 0 & 0 & 0 & 2\mu_x & 0 & 0 \\ 0 & 0 & 0 & 0 & 2\mu_y & 0 \\ 0 & 0 & 0 & 0 & 0 & 2\mu_z \end{bmatrix} \quad (2.9)$$

We note that the stress-strain matrix in Eq. (2.9) is symmetric, so that this choice of "secant" moduli will lead to a symmetric stiffness matrix, which greatly reduces the computational difficulty of the linear equation solution phase of the structural equilibrium calculation.

If the relation in Eq. (2.9) is inserted into Eq. (2.4), then, given $\{\sigma\}$ and $\{\epsilon\}$, we obtain a set of six linear equations in six unknowns; $\lambda_x, \lambda_y, \lambda_z, \mu_x, \mu_y, \mu_z$. The solution is easily accomplished; we first solve for the shear moduli:

$$\begin{aligned} \mu_x &= \frac{\sigma_{yz}}{2\epsilon_{yz}} \\ \mu_y &= \frac{\sigma_{xz}}{2\epsilon_{xz}} \\ \mu_z &= \frac{\sigma_{xy}}{2\epsilon_{xy}} \end{aligned} \quad (2.10)$$

If any of the ϵ_{ij} in Eq. (2.10) are zero (or very small), we use the linear isotropic value for the corresponding μ . Then,

given the μ_i , there results the following relation for the λ_i :

$$\frac{1}{2} \begin{bmatrix} \epsilon + \epsilon_x & \epsilon_y & \epsilon_z \\ \epsilon_x & \epsilon + \epsilon_y & \epsilon_z \\ \epsilon_x & \epsilon_y & \epsilon + \epsilon_z \end{bmatrix} \begin{Bmatrix} \lambda_x \\ \lambda_y \\ \lambda_z \end{Bmatrix} \equiv [D] \{\lambda\}$$

$$= \begin{Bmatrix} \sigma_x - 2\mu_x \epsilon_x \\ \sigma_y - 2\mu_y \epsilon_y \\ \sigma_z - 2\mu_z \epsilon_z \end{Bmatrix} \quad (2.11)$$

where ϵ , the volumetric strain, is

$$\epsilon = \epsilon_x + \epsilon_y + \epsilon_z \quad (2.12)$$

Relation (2.11) is invertible if (and only if) $\epsilon \neq 0$, since $\det [D] = \frac{1}{4} \epsilon^3$. If ϵ is zero, or very small, we use the linear isotropic values for λ_x , λ_y and λ_z .

2.4 THE AGRESS FULLY INTERACTIVE RESERVOIR SIMULATOR

A schematic outline of the computational procedure is shown in Figure 2.6. Each time step consists of an iteration for the update of fluid variables (e.g., density, temperature, pressure) and structural variables (e.g., nodal displacements, stresses). Each iteration consists of two parts: a QUAGMR (fluid variable) portion and a STAGR (static equilibrium) portion.

The QUAGMR loop shown in Figure 2.6 is not a fluid variable equilibrium iteration, but rather a complete sequence of one-dimensional equilibrium iterations over each "line"

Figure 2.6. Computational procedures for AGRESS coupled fluid/structure calculations.

in the grid. Such a sequence of one-dimensional passes yields a new set of thermodynamic variables, in particular, a new state of pore pressure in each cell. These cell-centered pore pressures are then fed into the STAGR finite element module, which calculates a new state of nodal displacements and cell-centered stresses and porosities. These new porosities are fed back into the QUAGMR module, completing one fluid/structure equilibrium iteration. This procedure continues until convergence is attained, at which point the time-step is complete.

III. PRESSURE DRAWDOWN AND BUILDUP ANALYSES

Pressure drawdown and buildup experiments are well-test tests which are performed to obtain in situ permeability data for the reservoir formation. For a drawdown test, a well, initially at equilibrium conditions, is produced at a constant flow rate while the bottom-hole pressure history is recorded. After the well has been produced in this manner, a pressure buildup test is often performed by shutting-in the well (i.e., by discontinuing any production from the well) and again monitoring the bottom hole pressure as the reservoir equilibrates.

Classical techniques for well-test interpretation assume single-phase flow in the reservoir. Using this classical interpretation, the drawdown and buildup pressure histories are used to "back-out" the effective permeability of the reservoir. It will be shown in this section that this assumption can lead to dramatic errors in the inferred permeability values if, in fact, multiphase flow takes place in the formation during the test. A suite of four calculations using the QUAGMR geothermal reservoir simulator will be presented to demonstrate the need for a model capable of handling multiphase (liquid & vapor) systems for correct interpretation of well test data if information flashing occurs. The geothermal fluid is treated as pure H_2O in these calculations.

Section 3.1 presents the classical theory for interpreting single-phase well-test data. In Section 3.2 the QUAGMR code is applied in its 1-D cylindrical mode to analyze pressure drawdown and buildup for two problems. In the first, the flow remains single-phase and the inferred permeability for both the drawdown and buildup tests agrees closely with the actual input permeability. In the second problem, flashing occurs and classical interpretation is found to lead to large errors in the inferred permeability both for the

drawdown and buildup tests. In Section 3.3, QUAGMR is applied in 2-D axisymmetric mode to examine a case in which the well penetrates only 1/5 of the producing formation. If the flow remains single-phase, a modification of the classical interpretation due to Nisle [1958] is found to result in inferred permeabilities in close agreement with the actual input value. If flashing occurs, however, it is again demonstrated that a simulator capable of handling a multiphase system is required for reliable interpretation of the pumping tests. Finally, Section 3.4 examines the buoyancy of the steam bubble during pumping tests in which in-formation flashing occurs.

3.1 CLASSICAL WELL-TEST INTERPRETATION

The analytic solution for single-phase radial flow in a homogeneous, isotropic and infinite (laterally) confined (vertically) reservoir is given by

$$p(r,t) = p_i + \frac{q\mu}{4\pi h k} \ln \left(\frac{\gamma \phi \mu c r^2}{4kt} \right) \quad (3.1)$$

where the units are Darcy units, i.e.,

$p(r,t)$ = Pressure at time t and radius r , in atm.

p_i = Initial pressure of the reservoir, in atm.

q = Flow rate in cc/sec.

μ = Viscosity in cp.

h = Reservoir thickness in cm.

k = Permeability in darcies.

γ = Euler's constant, 1.78.

ϕ = Porosity of the reservoir.

c = Compressibility in cc/cc/atm.

r = Radius in cm.

t = Time in sec.

Differentiating the above expression for $p(r,t)$ with respect to $[\ln t]$ we get

$$\frac{\partial p}{\partial [\ln t]} = - \frac{qu}{4\pi kh} \quad (3.2)$$

Converting this expression to the units described below and $\log_{10} t$, we get

$$\frac{\partial p}{\partial [\log_{10} t]} = - \frac{(2.30)(1.01325)(1000)qu}{4\pi kh} \quad (3.3)$$

where

p = Pressure in bars.

t = Time in sec.

q = Flow rate in g/sec.

μ = Viscosity in cp.

k = Permeability in md.

h = Reservoir thickness in cm.

ρ = Density in g/cc.

The constants 2.30, 1.01325, and 1000 are conversion factors from Darcy units to the above unit system.

The above expression for $\partial p / \partial (\log_{10} t)$ tells us that the graph of pressure versus $\log_{10} t$ should be linear. If we plot the drawdown pressure history versus $\log_{10} t$, find the slope of the best straight-line approximation to the plotted curve, and then equate the value of the slope to the right-hand side of Eq. (3.3), we can "back-out" the permeability of the reservoir since we know all the other values on the right-hand side of the equation.

For a pressure buildup analysis, the following equation is used to determine the effective permeability

$$\frac{\partial p}{\partial [\log_{10} t_s]} \approx \frac{(2.30)(1.01325)(1000)q\mu}{4\pi kh} \quad (3.4)$$

where t_s is the shut-in time in seconds, i.e., time after production was terminated.

The following examples illustrate these techniques.

3.2 ONE-DIMENSIONAL EXAMPLES

The following properties are assumed for the reservoir rock:

$$\rho_s \text{ (grain density)} = 2.65 \text{ g/cm}^3$$

$$\phi \text{ (porosity)} = \phi_0 = 0.1$$

$$k \text{ (permeability)} = k_0 = 3 \text{ md} = 3 \times 10^{-11} \text{ cm}^2$$

$$\kappa_s \text{ (thermal conductivity)} = 2.1 \times 10^5 \text{ ergs/sec-cm-}^\circ\text{C}$$

$$C_{vs} \text{ (specific heat)} = 10^7 \text{ ergs/g-}^\circ\text{C}$$

$$S_{lr} \text{ (irreducible liquid saturation)} = 0.3$$

$$S_{vr} \text{ (irreducible vapor saturation)} = 0.5$$

The parameters S_{lr} and S_{vr} define the relative permeabilities using the Corey formulation [Pritchett, et al., 1975].

The QUAGMR code was used in its 1-D cylindrical mode to analyze pressure drawdown and buildup tests for two sets of initial conditions for the reservoir fluid. In one case the flow is single phase and in the other case it is two phase. Figure 3.1 shows the computational grid which employed 25 uniform-sized zones. Pressure and internal energy are prescribed at the outer boundary. The zone nearest the axis of symmetry is a mass sink during the pressure drawdown simulation. It is shut off during the subsequent pressure buildup simulation.

Figure 3.1. Geometry for 1-D cylindrical QUAGMR well-test calculations. Zone 1 is a mass sink. At the outer boundary, pressure and fluid internal energy are specified. There is no conduction or convection at the other boundaries.

3.2.1 1-D Single Phase Flow

The initial state of the reservoir fluid is assumed to be

$$P_0 = 124.98 \text{ bars}$$

$$T_0 = 313^\circ\text{C}$$

The production rate is (mass sink in zone 1) first set at the value of 0.414 g/sec-cm and maintained constant for 6×10^3 sec. The well was then shut down. The flow remained single phase throughout both the pressure drawdown and buildup phases of the simulated pumping tests.

Figure 3.2 is the plot of the drawdown pressure computed by the QUAGMR code in the sink zone versus $\log_{10} t$. The transient part of the curve is seen to be approximated very well by a straight line. Using the slope of the line, Eq. (3.3) and the techniques described above, we calculate the inferred effective permeability of the formation to be 3.0 md.

After producing the reservoir for 6×10^3 sec, the well was shut-in. The pressure buildup history in the first zone was recorded and plotted versus $\log_{10}$ (shut-in time). This graph is presented in Figure 3.3. Using theory analogous to the drawdown theory above, Eq. (3.4), we find that the inferred permeability from the drawdown curve is about 3.3 md.

In both the simulated drawdown and buildup analyses the value of the permeability inferred from classical well-test theory agrees very closely with the actual value of 3 md.

3.2.2 1-D Two-Phase Flow

In this QUAGMR calculation the initial state of the reservoir fluid is assumed to be

Figure 3.2. Simulated 1-D single-phase pressure drawdown test.

Figure 3.3. Simulated 1-D single-phase pressure buildup test.

$$P_0 = 124.98 \text{ bars}$$

$$T_0 = 326^\circ\text{C}$$

The production rate is again initially set at 0.414 g/sec-cm and maintained constant for 4.28×10^7 sec. The well was then shut down, but the flow became two-phase during the pressure drawdown phase of the simulated test.

The calculated drawdown pressure history of the sink zone versus $\log_{10} t$ is displayed in Figure 3.4. The point in time at which in-formation flashing occurs is also depicted. The best straight line approximation for early time behavior infers an effective permeability of 95.5 md. This is in dramatic disagreement with the actual value of 3.0 md.

The production was stopped at 4.28×10^7 sec. The pressure buildup curve for the sink zone is plotted versus $\log_{10}$ (shut-in time) in Figure 3.5. From the graph, the permeability inferred from the application of conventional well-test analysis is 6 md, a factor of 2 from the actual value.

3.3 TWO-DIMENSIONAL EXAMPLES

The QUAGMR code was used in its 2-D axisymmetric mode to analyze simulated pumping tests in which the well only partially penetrates the thickness of the reservoir. Figure 3.6 shows the computational grid which employs 25 uniform horizontal zones and 5 uniform vertical zones. Pressure and energy fluxes are prescribed at the outer boundary and the effect of gravity is included. Zone 1, nearest the axis of symmetry and the top of the reservoir, is a mass sink during the pressure drawdown; it is shut off during the pressure buildup phase. This simulation corresponds to a 1/5 penetration of the formation by the well being tested.

Figure 3.4. Simulated 1-D two-phase pressure drawdown test.

Figure 3.5. Simulated 1-D two-phase pressure buildup test.

Figure 3.6. Geometry for 2-D axisymmetric QUAGMR well-test calculations. Zone 1 is a mass sink. At the outer boundary, pressure and energy fluxes are specified. There is no conduction or convection at the other boundaries.

The reservoir rock properties are assumed to be the same as listed in Section 3.2 for the one-dimensional simulations. Since the formation is only partially penetrated, a modification in the application of the classical well-test data interpretation is necessary, as discussed by Nisle [1958]. He indicated that the straight line used in inferring formation permeability should approximate late-time transient behavior on the drawdown and buildup curves.

3.3.1 2-D Single Phase Flow

The initial state of the fluid is assumed to be

$$P_0 = 124.98 \text{ bars}$$

$$T_0 = 313^\circ\text{C}$$

During the simulated drawdown test over a period of 1.33×10^8 secs, the production rate is (mass sink in zone 1, Figure 3.6) held constant at 18,663 g/sec. The flow is single phase in both the drawdown and buildup phases of the test.

Figure 3.7 shows the drawdown pressure calculated by the QUAGMR code in the mass sink zone versus $\log_{10} t$. The slope of the line which approximates the curve in the region suggested by Nisle infers a permeability of 2.96 md.

The well is shut-in at 1.33×10^8 sec in the QUAGMR simulation. The pressure buildup history in the mass sink zone is depicted in Figure 3.8. The straight line approximation to the curve again infers a formation permeability of 2.96 md. Thus, the classical method of well-test data interpretation, modified for partial penetration, gives good agreement with the actual permeability of 3.0 provided the flow remains single phase.

Figure 3.7. Simulated 2-D single-phase pressure drawdown test (1/5 penetration.

Figure 3.8. Simulated 2-D single-phase pressure buildup test (1/5 penetration).

3.3.2 2-D Two-Phase Flow

The initial state of the fluid is assumed to be

$$P_0 = 124.98 \text{ bars}$$

$$T_0 = 326^\circ\text{C}$$

The production rate is again initially set at 18,663 g/sec, but the higher temperature in this case causes flashing to occur in the mass sink zone (zone 1 in Figure 3.6). The simulated pressure drawdown curve, pressure in zone 1 versus $\log_{10} t$, is shown in Figure 3.9. The classical well-test data interpretation, as modified for partial penetration by Nisile, would use the straight line approximation to the curve (shown in Figure 3.9) to infer the formation permeability. This region is controlled by the two-phase flow and the inferred value is 19 md rather than the true value of 3.0 md.

The well is shut-in at 1.62×10^7 sec. The pressure history in the sink zone is given by Figure 3.10. The slope of the best straight line for late buildup when two phases are still present implies a permeability of 5.2 md. Again, we see that this classical interpretation is inaccurate when there is two-phase flow in the reservoir.

3.4 VAPOR BUOYANCY

During pressure buildup in a two-phase multidimensional problem, we are also interested in determining if the vapor phase demonstrates buoyancy. Due to the coarse grid used in the calculation in Section 3.3.2, no buoyancy effect is seen since vapor exists in only one zone. In order to demonstrate the buoyancy of the vapor, a similar 2-D problem was formulated in which a finer grid was used in the calculations. Figure 3.11 shows the refined grid. Note that there are only 14 horizontal grid blocks and 10 vertical blocks and that each

Figure 3.9. Simulated 2-D two-phase pressure drawdown test (1/5 penetration).

Figure 3.10. Simulated 2-D two-phase pressure" buildup test (1/5 penetration).

Figure 3.11. Refined grid used to demonstrate vapor buoyancy during buildup. Heavy lines are "old" grid. Fine lines are "new" grid. Shaded area is the mass sink. At the outer boundary pressure and energy fluxes are specified. There is no convection or conduction at the other boundaries.

of these "new" blocks is $1/16$ the size of the "old" calculational zones. The system is initially in hydrostatic equilibrium with a pressure of 121.89 bars in the top row of cells. The initial temperature of the system is 322°C . The shaded region on Figure 3.11 is the mass sink which represents production at the rate of 18,633 g/sec. Drawdown of this system was continued until $t = 4.32 \times 10^7$ sec (500 days). At that time, the production for the sink was terminated and the pressure was allowed to build up for $t = 2.592 \times 10^6$ sec (30 days). In order to show the buoyancy of the vapor phase, contour plots of the vapor saturations are presented in Figures 3.12a through 3.12f. The region shown is only the portion of the system where flashing occurred. The shaded area corresponds to the shaded mass sink of Figure 3.11. Figure 3.12 shows a series of snapshots of vapor saturations at shut-in time and at five day intervals during the subsequent pressure buildup. At 30 days, the system has returned to all liquid phase. As is seen from these figures, the vapor phase does float to the upper layers of the reservoir. This simulation describes the behavior we would expect to occur during an actual pressure buildup test under the assumed conditions.

(a) Time of shut-in.

(b) 5 days after shut-in.

Figure 3.12. Snapshots of calculated vapor saturation in simulated pressure buildup test at time of shut-in and at 5 day intervals subsequent to the shut-in.

(c) 10 Days after shut-in.

(d) 15 Days after shut-in.

Figure 3.12. (continued)

(e) 20 Days after shut-in.

(f) 25 Days after shut-in.

Figure 3.12. (concluded)

IV. EVALUATION OF INFORMATIONAL FLASHING OF GEOTHERMAL BRINES

When the pressure in a liquid-phase geothermal reservoir drops to the phase line (the vapor pressure of the fluid at the temperature in question), the fluid within the pores will begin to boil and vapor will be evolved. If the geothermal waters contain dissolved salt, the concentration of these salts in the liquid phase will begin to increase since their solubility in water vapor is much less than in liquid water. If the initial concentrations are high enough, or if a sufficient amount of the liquid is boiled off, the salt concentration in the liquid phase will reach saturation and, thereafter, salt will begin to precipitate, in solid form, within the pores of the rock matrix.

This possibility raises a number of questions concerning optimum production strategy in a highly saline liquid-dominated geothermal system such as the Salton Sea field. For example, it has been suggested that production wells be deliberately designed so that a flash front will propagate outward from the bore wall, thereby causing salt to precipitate within the formation and reducing the salinity of the output flow. If the salinity in the produced fluid were reduced substantially in this way, corrosion damage to piping and surface equipment would be correspondingly ameliorated. On the other hand, it seems clear that the solid salt left behind in the reservoir rock would tend to reduce the permeability in the vicinity of the wellbore and therefore adversely influence production rates.

A second question is related to the creation of a large-scale steam cap after heavy production of the field. In the Wairakei geothermal field the transition from a liquid-dominated to a two-phase system has resulted in the creation of such a steam cap which has served to maintain the reservoir

pressure and sustained productivity rates (see Section VI). Would this enhanced productivity be precluded by salt precipitation in the case of a high salinity brine such as occurs in the Salton Sea field?

To investigate these possibilities, a series of calculations were performed using the QUAGMR simulator, modified to monitor salt deposition and to approximate associated decreases in porosity and permeability. In Section 4.1 the fluid and reservoir properties employed (typical of the Salton Sea reservoir) are given as well as the simplifying approximations that were made in the calculations. Section 4.2 uses the 1-D cylindrical mode of the modified QUAGMR code to examine the effects of information flashing in the immediate vicinity of a producing wellbore. Section 4.3 presents results obtained using the modified QUAGMR code in 1-D slab mode to evaluate the case of a steam cap being formed at the top of the geothermal reservoir during large-scale production of the brine from the field. Briefly, the analyses show that near wellbore flashing is disastrous but no salt deposition problems are to be anticipated from the creation of a steam cap provided the wells are deep enough.

4.1 SIMPLIFYING ASSUMPTIONS

A number of simplifying approximations were introduced, since at the time the calculations were performed the fully-integrated salt transport and brine equation of state version of the code was incomplete. These approximations are as follows:

- The thermodynamic properties of the multiphase fluid (water/steam) are insensitive to salt concentration.
- The solubility of salt in the vapor phase is negligible.

- The solubility of salt in the liquid phase is a function of temperature only.
- Precipitated salt takes its ordinary crystalline form.
- The principal effect of salt precipitation within the rock upon the flow is to reduce permeability.
- The permeability of the rock varies in proportion to the cube of the remaining pore space (simplified Carman-Kozeny law, $k = k_0 (\phi/\phi_0)^3$).
- Rates of dissolution and/or precipitation are rapid compared to fluid-flow rates.
- The brine consists of pure sodium chloride solution.

While none of these assumptions is strictly true, it is believed that a more sophisticated treatment would not change the overall conclusions reached, even though the detailed output numbers from the code would be altered somewhat. The QUAGMR code was accordingly modified and extended so that both precipitated and dissolved salt contents and their effects could be followed as a function of time along with the rest of the fluid mechanics, and a number of specific problems were run to investigate the phenomenon.

The reservoir rock properties were taken to be representative of the fractured sandstones in the Salton Sea field. Values selected were as follows:

- ρ_s (grain density) = 2.65 g/cm³
- ϕ_0 (initial porosity) = 0.2
- k_0 (initial permeability) = 100 md
- κ_s (rock thermal conductivity) = 2.1×10^5 ergs/sec-cm-°C

C_{vs} (rock specific heat) = 10^7 ergs/g-°C

S_{lr} (irreducible liquid saturation) = 0.3

S_{vr} (irreducible vapor saturation) = 0.05

In all cases, the reservoir's ambient temperature was taken to be 300°C.

According to Helgeson [1968], the in situ composition of the Salton Sea brines is as follows:

<u>Species</u>	<u>Mass Fraction</u>	<u>Mole Fraction</u>
Na ⁺	5.03%	4.70%
K ⁺	1.65%	0.86%
Ca ⁺⁺	2.88%	1.46%
Cl ⁻	15.50%	8.91%
Other ionic species	0.64%	0.35%
H ₂ O	74.03%	83.72%
TOTAL	100.00%	100.00%

The above data suggest that regarding the brine as a sodium chloride solution is a reasonable first approximation:

<u>Species</u>	<u>Mass Fraction</u>	<u>Mole Fraction</u>
Na ⁺	9.43%	8.14%
Cl ⁻	14.55%	8.14%
Other	0	0
H ₂ O	76.02%	83.72%
TOTAL	100.00%	100.00%

The solubility of sodium chloride in water (Section 5.4) may be approximated as

$$s_{\max} = (26.218 + 7.20089 \times 10^{-3}T + 1.05998 \times 10^{-4}T^2)\% \quad (4.1)$$

where $s_{\max}$ is the solubility (weight percent) at temperature $T^\circ\text{C}$ over the range $20^\circ\text{C} \leq T \leq 325^\circ\text{C}$. This means that the Salton Sea brines are undersaturated at 300°C at the concentrations reported by Helgeson; he found a total dissolved solid mass fraction (salinity) of $s = 25.92$ percent whereas the brine could be concentrated to $s = 37.92$ percent (if the sodium chloride analogy holds) before precipitation occurs. The precipitate may be regarded as ordinary NaCl crystals, of which the density is 2.165 grams/cm^3 . Of course, values of dissolved salt content both higher and lower than those given by Helgeson have been reported for Salton Sea brines. It seems likely, however, that over substantial parts of the field the brine is considerably undersaturated at reservoir conditions. This possibility is rather important, as will be seen later.

4.2 NEAR WELLBORE FLASHING

Consider a production well of diameter 40 cm (15-3/4 in.) penetrating the reservoir which is perforated for a vertical extent of 30 meters. At zero time, fluid begins to flow from the reservoir into the borehole. This problem is computed with the QUAGMR simulator in the 1-D cylindrical mode by imposing initial conditions:

$$\left. \begin{aligned} P(r, t=0) &= P_0 \\ T(r, t=0) &= T_0 \\ s(r, t=0) &= s_0 \\ m(r, t=0) &= 0 \end{aligned} \right\} \quad r_0 \leq r \leq R \quad (4.2)$$

and boundary conditions:

$$\left. \begin{aligned} P(r = r_0, t) &= P_0 - \Delta P \\ P(r = R, t) &= P_0 \\ T(r = R, t) &= T_0 \\ s(r = R, t) &= s_0 \end{aligned} \right\} \quad t > 0 \quad (4.3)$$

In the above, P_0 and T_0 are the ambient reservoir pressure and temperature (the latter is always 300°C); s_0 is the ambient salt concentration; m is the precipitated salt mass per unit pore volume; ΔP is the pressure drawdown imposed at the well wall; and r_0 and R are, respectively, 20 cm and 60 meters. Very fine resolution was employed near the wellbore, but at larger distances the mesh was made increasingly coarse.

At 300°C, the vapor pressure of water (P^*) is 86 bars. Accordingly, if $P_0 \geq 86$ bars, no vapor will be present in the system initially (this was the case for all problems run). Furthermore, if also $P_0 - \Delta P \geq 86$ bars, the system will remain single-phase liquid for all times. On the other hand, if $P_0 - \Delta P < 86$ bars, vapor will form within the rock. The overall situation is illustrated in Figure 4.1, which depicts the asymptotic (or steady) state attained for one of the multiphase cases treated in the series of QUAGMR calculations. As $r \rightarrow R$, the pressure approaches the ambient pressure P_0 . As r decreases, however, the pressure declines monotonically until, at the point where P reaches P^* (the vapor pressure) boiling begins. Finally, at $r = r_0$, $P = P_0 - \Delta P$ and the steam saturation attains its maximum value (22 percent in the case illustrated).

To describe the results for the full series of QUAGMR calculations, it is convenient at this point to define:

Figure 4.1. Representative QUAGMR calculation of steady-state pressure, temperature, and steam saturation distributions around the wellbore in the multiphase regime. In this case $P_0 = 96$ bars (ambient pressure), $P^* = 86$ bars (vapor pressure), $\Delta P = 20$ bars (drawdown), $T_0 = 300^{\circ}\text{C}$ (ambient temperature), $s = 70$ percent (ambient salt concentration) and $\eta = (P_0 - P^*)/\Delta P = 0.5$.

$$\eta = \frac{P_0 - P^*}{\Delta P} \quad (4.4)$$

At $\eta = 0$, the ambient reservoir pressure itself lies on the phase line. For $\eta \geq 1$, the pressure drop imposed is inadequate to cause flashing. For $0 < \eta < 1$, however, a two-phase region will form around the wellbore which will in turn be surrounded by a single-phase liquid region. The extent of the boiling region would, furthermore, be expected to increase with decreasing values of η .

Near-wellbore flashing analysis, therefore, involves a three-parameter problem: that is, we can vary:

$$(1) \quad \eta = \frac{P_0 - P^*}{\Delta P}$$

which measures the "strength" of the two-phase behavior,

(2) ΔP ; the pressure drop, and

(3) s_0 ; the ambient brine concentration.

Two values of ambient salinity (brine concentration by mass fraction) were considered in the series of QUAGMR calculations: $s_0 = 26.544$ percent (corresponding roughly to Helgeson's measurements and equal to 70 percent saturated brine) and $s_0 = 37.920$ percent (corresponding to fully saturated brine). Three values of pressure drop were considered: $\Delta P = 10, 20$ and 30 bars. Several values of η were also treated. The cases run may be summarized as follows:

70% Saturated Brine				100% Saturated Brine			
ΔP (bars):				ΔP (bars):			
η	10	20	30	η	10	20	30
0		x		0		x	
0.1		x		0.1		x	
0.25	x	x	x	0.25	x	x	x
0.5	x	(x)	x	0.5	x	x	x
0.75	x	x	x	0.75	x	x	x
1.00	x	x	x	1.00		x	

The notation () in the 70 percent saturated table indicates the representative case depicted in Figure 4.1.

4.2.1 70 Percent Salt-Saturated Brine

We will first consider the results of the QUAGMR calculations for the cases in which the brine was 70 percent saturated. In none of these cases did salt precipitate in the pores at any time. Consequently, the salinity (salt mass/total fluid mass) of the produced fluid was identical to the ambient salinity. All except the $\Delta P = 20$ bar, $\eta = 0$ run were carried out far enough in time that a steady-state was reached. Even that case was settling down reasonably well by the end of the run.

At $\eta = 1$ or greater (single-phase throughout), both the computed results and a linearized analytic solution show that the total production rate from the well at steady-state may be expressed as:

$$\dot{m} = (2.5644 \Delta P) \text{ kg/sec} \quad (4.5)$$

where ΔP is expressed in bars. In other words, the steady-state well-production rates calculated by QUAGMR in the single-phase, undersaturated cases were 25.64, 51.29 and 76.93 kg/sec

for the 10, 20 and 30 bar drawdown cases, respectively. The effect of two-phase behavior ($\eta < 1$) on these production rates is shown in Figure 4.2. The computed points do not fall on the same line - the dependence upon pressure drop is because of the relatively greater importance of heat conduction for smaller (ΔP) drawdown flows. Clearly, even in the absence of pore blockage by precipitated salt, the effect of two-phase behavior is to substantially reduce production rate. In Figure 4.3, the steady-state distribution of steam saturation (vapor volume/pore volume) is shown as a function of radius for the various 20 bar drawdown cases run - the profiles for the other drawdowns are similar. As η declines, the size of the steam annulus increased rapidly. The presence of this vapor region inhibits the total flow rate because of the relative-permeability effect.

In these cases, of course, the concentration of the salt within the liquid phase increases from its ambient value as the wellbore is approached, reaching a peak at the wellbore face. This increase in concentration was in no case sufficient to cause precipitation, however. Figure 4.4 shows the liquid-phase salinity at the well face at steady-state normalized to the ambient salinity. Again, the greatest concentration increases are found for small values of η and large drawdowns (ΔP). These data furthermore suggest that no salt precipitation should be expected unless salt concentrations are in excess of ~90 percent of saturation, or even higher for high values of η .

4.2.2 100 Percent Salt-Saturated Brine

We now turn to the case in which the ambient fluid is entirely salt-saturated at reservoir conditions. In all cases (except, of course, for $\eta \geq 1$), salt will accumulate in the pores as time goes on, reducing porosity and permeability, and the salinity of the fluids entering the wellbore will consequently be less than ambient. In Figure 4.5, the

Figure 4.2. Effect of multiphase (flashing) behavior upon steady-state production rate from wells (70 percent salt saturated cases). Decline is due to relative permeability effect alone since there is no salt deposition for these cases.

Figure 4.3. Steam saturation profiles at steady-state for 20 bar drawdown (ΔP) calculations with undersaturated (70 percent) brine.

Figure 4.4. Liquid-phase salt concentration at the wellbore face at steady-state normalized to ambient salt concentration for unsaturated cases (70 percent salt saturation).

Figure 4.5. Well output salinity (normalized to ambient salinity) as a function of time for 100 per cent salt saturated cases treated in the QUAGMR calculations.

well outlet flow salinity (normalized to ambient) is shown as a function of time for all cases considered. QUAGMR was modified for treatment of reduced porosity and permeability due to salt deposition for these calculations. These results indicate that the improvement in outlet salinity is exceedingly slight and decreases with time.

Furthermore, this slight improvement is exceedingly costly, as shown in Figure 4.6. This figure shows the well outlet mass flow rate as a function of time for all cases, normalized to the steady-state flow rate for $\eta = 1$ at the same drawdown. These results indicate that production under conditions of in-formation flashing for saturated brine systems has catastrophic effects upon production rate - in all cases, the precipitated salt adjacent to the borehole virtually shuts off the flow within a matter of hours or (at most) days.

It seems, therefore, that whether or not the brine is saturated with dissolved salts, there are no advantages to be gained by deliberate flashing within the formation around the borehole. If the brine is significantly below saturation conditions, no reduction in output salinity takes place, and the steady-state production rates are substantially reduced. If the brine is saturated (or nearly saturated), the reduction in output salinity is slight and the steady-state production rate is zero - the region adjacent to the hole will become plugged with precipitated salts within a very short time. Therefore, in either case, it is recommended that well perforation depths be sufficiently great so that the higher hydrostatic pressures will preclude such near-wellbore flashing under production conditions, if at all possible.

Figure 4.6. Decay with time of well outlet flowrate (compared to steady-state non-flashing flowrate at same drawdown) for 100 percent salt saturated cases treated with QUAGMR.

4.3 CREATION OF STEAM CAP ABOVE PRODUCTION ZONE

One nagging question remains, however. In liquid-dominated systems, in the absence of adequate natural recharge or reinjection, it is to be expected that vapor will begin to form sooner or later at the top of the reservoir, and that subsequently the size of this steam bubble will increase with time, penetrating deeper and deeper into the reservoir. This is known to have occurred at the Wairakei field in New Zealand, for example; prior to production, that system was essentially all-liquid, but at present, after 23 years of production, it has become largely vapor-dominated, at least in its upper portion. The question is: even if wellbore perforation depths are sufficiently great to avoid flashing around the borehole, could large-scale steam formation above the production horizon due to general decline in reservoir pressures cause pore plugging by precipitated salts and thereby damage the overall reservoir?

To investigate this question, a pair of problems (one with 70 percent ambient brine saturation, one with 100 percent saturation) were run using the modified QUAGMR code in one-dimensional slab (vertical) geometry. In order to create a "worst case" situation, no recharge or reinjection was considered, but the general geometry and material properties used are reminiscent of the Salton Sea field.

Consider a vertically stratified geothermal reservoir of which the uppermost 1 km consist of impermeable cap rock. The next 4 km are a permeable, porous sandstone containing the geothermal brine. Below this level lies impermeable basement rock. For the purposes of these calculations, the reservoir rock was assigned the same properties as given earlier (Section 4.1) and used for the near-borehole calculations. Also, for simplicity, the temperature in the permeable region was again taken as 300°C everywhere initially, and the initial pore pressure distribution within the permeable (sandstone) layer was

hydrostatic, varying from 89.14 bars at the top (depth = 1 km) to 376.91 bars at the bottom (depth = 5 km). The fluid was initially entirely in the liquid state but, at the top of the reservoir, the initial pressure was only slightly (~ 3 bars) above vapor pressure, so that flashing would be expected there shortly after production begins.

The production strategy is as follows. A large number of wells are drilled into the reservoir and perforated at the 3 km level, at the midpoint of the permeable formation. These wells are arranged in a square-grid fashion with 0.5 km between adjacent wells (see Figure 4.7). At $t = 0$, all the wells begin producing brine at a constant rate of 30 kg/sec. For the one-dimensional (vertical) calculations, the close well spacing and the high permeability permit this problem to be simulated by imposing a fluid-mass sink of strength 1.2×10^{-5} grams/cm²-sec at the 3 km level.

Both calculations were carried out to approximately the same point in time. For the 100 percent salt-saturated case, the calculation ran to 31.453 years; the 70 percent saturation case was calculated to 31.395 years. A uniform grid spacing of 50 meters of depth per zone was used. In both cases, after initial transient behavior damps out, a vapor cap forms at the top of the reservoir and grows downward. Simultaneously, the upper portions of the reservoir begin to cool slightly and the pressure in the reservoir begins to drop. Figure 4.8 shows the state of the system in the upper 2 km of the permeable layer for the salt-saturated case at the end of the calculation ($t = 31.453$ years). By this time, the vapor cap (illustrated by the dotted "vapor saturation" curve) has penetrated downward some 1.425 km; with continued production, within a few more years, it would engulf the production horizon. The pressure at the production level has, by this time, dropped from its initial value of 231 bars to 125.5 bars. Temperatures at the top of the reservoir have dropped

Figure 4.7. Definition sketch of hypothetical liquid brine-dominated geothermal reservoir.

Figure 4.8. State of the system at $T = 31.453$ years for 100 percent salt saturated case. Calculation made with modified QUAGMR code using 1-D slab mode.

about 5.4°C, but at the production depth the temperature drop is negligible (0.34°C).

The results for the two cases (70 percent and 100 percent saturation) were virtually identical. After the first few years, the pressure at the production level declines at a constant rate. For the 70 percent saturation case, this rate was 2.979 bars/year and for the 100 percent saturation case, 2.969 bars/year; a difference of only 0.34 percent. In the 70 percent saturated case, no salt is ever precipitated in the pores. For the fully saturated case, precipitated salt does appear throughout the steam cap but the porosity is thereby reduced only slightly (from 20.00 percent to about 19.90 percent - the exact amount is slightly dependent on position and time, the larger values of blockage tending to be at the top of the system).

The inevitable conclusion is that pore blockage by precipitated salts in the upper portion of the reservoir need be of little concern. If, for example, fresh-water recharge occurred through the surface layer, this water would tend to dissolve away even the small amount of salt left behind in the fully saturated case. On the other hand, it is apparently exceedingly desirable (if not mandatory) to locate well perforation depths so as to avoid production-induced flashing near the wellbore. This is particularly true if the reservoir brines are salt-saturated or nearly so.

V. EQUATION OF STATE FOR SALT SOLUTIONS

An equation of state package in the form of a computer subroutine for use in the reservoir simulators is being developed for water-sodium chloride solutions. As discussed in Section 4.1, this treatment should be adequate for describing the thermodynamic behavior of geothermal brines such as are found in the Salton Sea area. It describes the behavior of water-sodium chloride solutions of any salinity as well as pure water. The complete package, which is designed for a pressure range from one bar to two kbar, will use as independent variables, or calling arguments, the density, specific internal energy, and salinity. It will return values for the pressure, temperature, viscosity, thermal conductivity, mass fraction of precipitated salt and vaporized water, and various thermodynamic derivatives.

The computational scheme divides the pressure-volume plane into five regions, as indicated schematically in Figure 5.1. Tentative coding for pressure as a function of density, energy and salinity for initially unsaturated solutions has been completed for Regions I, II, III and V. A description of the progress so far is given below.

5.1 REGION I

Region I consists of compressed states, i.e., states with a density greater than that of the solution at 25°C and 1 bar. In each of the Regions I through III, the pressure is computed from

$$P = P_{\text{ref}} + \bar{\rho}G (E - E_{\text{ref}}), \quad (5.1)$$

where P_{ref} is the pressure as a function of density on the appropriate reference curve, E_{ref} is the corresponding specific

Figure 5.1. Division of the P-V plane into computational regions as used in the equation of state package for aqueous sodium chloride solutions.

internal energy, and $\overline{\rho G}$ is the pressure-energy coupling coefficient,

$$\overline{\rho G} = \left(\frac{\partial P}{\partial E} \right)_{\rho} = \frac{1}{E - E_{\text{ref}}} \int_{E_{\text{ref}}}^E \left(\frac{\partial P}{\partial E} \right)_{\rho} dE. \quad (5.2)$$

In Region I, the reference curve is the Hugoniot centered at 25°C and 1 bar, at which point the specific internal energy for any salinity is assigned the value zero.

The principal sources of data used in constructing the equation of state package are a compilation of volumetric properties by Potter and Brown [1975], and a compilation of phase-line data by Haas [1975]. A fit to the data reported by Potter and Brown gives for the density (g/cm³) at 25°C and 1 bar,

$$\rho_0 = 0.99721 + (6.92687 \times 10^{-3})s + (2.3544 \times 10^{-5}) s^2, \quad (5.3)$$

where s is the salinity in percent salt by weight.

The representation of the Hugoniot is based on the experimental data for pure water as given by Rice and Walsh [1957] and on the volumetric data for sodium chloride solutions to 2 kbar as given by Potter and Brown [1975]. Data given by the latter authors were used to calculate the shock velocity as a function of salinity for a 2 kbar shock. A plot of these calculated points is given in Figure 5.2, along with a value for zero salinity as calculated from the shock-particle velocity fit given by Rice and Walsh. As can be seen from the figure, the agreement of the zero salinity point with the other points is excellent. It was then assumed that at low pressures the slope of the shock velocity versus particle velocity curve for any salinity is the same

Figure 5.2. Shock velocity as a function of salinity at 2 kbar and 1 bar.

as that for pure water,

$$U_s = C + 2.119 U_p. \quad (5.4)$$

The above expression was used to calculate the sound speed C at one bar from the shock velocities at 2 kbar,

$$C = U_s - \frac{2.118}{\rho_0 U_s} (P - P_0). \quad (5.5)$$

These points are also plotted in Figure 5.2. The resulting fit for the sound speed at 1 bar and 25°C as a function of salinity is

$$C(\text{cm/sec}) = 1.48216 \times 10^5 + (822.632)s + (8.38298)s^2, \quad (5.6)$$

where s is the salinity in weight percent. The use of Eq. (5.4) then gives for the Hugoniot pressure,

$$P_H - P_0 = \rho_0 C^2 \frac{\epsilon}{(1 - 2.118\epsilon)^2}, \quad (5.7)$$

where $\epsilon \equiv 1 - V/V_0$, and $P_0 = 1$ bar.

The pressure-energy coupling coefficient used to complete the P, V, E description in Region I will be discussed in Section 5.5.

5.2 REGION II

Region II consists of states with a density less than that at 25°C and 1 bar but greater than that at the boiling point at 1 bar. A fit to the data as reported by Haas [1975] gives for the density at the boiling point at 1 bar,

$$\rho_b \text{ (g/cm}^3\text{)} = 0.958793 + (6.63869 \times 10^{-3}) s + (2.36244 \times 10^{-5}) s^2, \quad (5.8)$$

where s is the salinity in weight percent.

Specific heat data [Silvester and Pitzer, 1976] were used to calculate the specific internal energy at the boiling point. The resulting fit is

$$E_b \text{ (joule/g)} = 312.42 - (3.074095)s + (0.0494964)s^2, \quad (5.9)$$

for $s \leq 25$ weight percent. For $s > 25$ weight percent, a straight line extrapolation of the above expression is used.

The reference pressure, Eq. (5.1), in Region II is the constant value 1.0×10^6 dyne/cm². The reference energy is obtained from a quadratic volume interpolation between the value zero at 25°C and the value at the boiling point as obtained from Eq. (5.9). The pressure energy coupling coefficient is discussed in Section 5.5.

5.3 REGION III

Region III consists of states with densities less than that at the boiling point (1 bar) but greater than the density at the critical point for the solution, and with pressures above the steam dome.

In Region III the reference pressure is the pressure on the liquid phase line as a function of volume. Pressures and energies for pure water as a function of volume, as taken from the ASME Steam Tables [1967], are entered in tabular form. In locating the phase line for salinities other than zero, it is assumed that the critical pressure

remains constant at the value for pure water, i.e., 221.2 bar. An examination of the phase line data is presented by Haas [1975] for pressures up to about 100 bar leads to the following fit for the density as a function of salinity and pressure on the liquid phase line,

$$\begin{aligned} \rho_{ph}(s,P)/\rho_{ph}(0,P) = & 1.0 + (6.92401 \times 10^{-3})s \\ & + (2.46397 \times 10^{-5})s^2 \\ & + (7.74932 \times 10^{-5})(P-1)s, \end{aligned} \quad (5.10)$$

with s in weight percent and P in bars.

The specific internal energy on the liquid phase line is calculated from the fit

$$E_{ph}(s,P)/E_{ph}(0,P) = 1 + As + Bs^2 \quad (5.11)$$

for $s \leq 25$ weight percent. For $s > 25$ weight percent, a straight line extrapolation of the above expression is used. The coefficients A and B are given by

$$A = -9.83962 \times 10^{-3} - (2.16748 \times 10^{-3})(P-1)^{1/3} \quad (5.12)$$

$$\begin{aligned} B = & 1.58429 \times 10^{-4} - (3.60158 \times 10^{-7})(P-1) \\ & + (3.71579 \times 10^{-8})(P-1)^2 \end{aligned} \quad (5.13)$$

for $P \leq 40$ bar. For $P > 40$ bar, both A and B are constant as evaluated at 40 bar.

5.4 REGION V

A state in Region V consists of either a mixture of unsaturated solution and steam or a mixture of saturated

solution, precipitated salt, and steam. In determining the pressure, the equation of state subroutine calculates, for the given input volume, the specific energy at each of the tabulated pressures on the phase line. A linear interpolation is then used between the two values which bound the input value of specific internal energy. Details of the method of solution are outlined below.

In the first part of the calculation the specific internal energy at each of the tabulated pressures is determined using the assumption that there is no precipitated salt. When the solution is found, the salinity of the liquid is compared with the saturation value at the pressure (or temperature). If the calculated salinity of the liquid is less than the saturation value, the calculation is finished. If the computed value for the liquid salinity is greater than the saturation value, the pressure and other variables are recalculated using the equations appropriate for a salt-saturated liquid.

If one assumes that there is no precipitated salt, then the specific volume and net salinity of the liquid-vapor mixture are given by

$$V = \alpha_l V_l (s_l, P) + \alpha_g V_g (s_l, P) \quad (5.14)$$

$$s = s_l \alpha_l + s_g \alpha_g \quad (5.15)$$

$$\alpha_l + \alpha_g = 1 \quad (5.16)$$

where α_l and α_g are the mass fractions of liquid and vapor respectively, s_l and s_g are the salinities (mass fraction) of the liquid and vapor components of the mixture, s is the net salinity (input value) of the mixture, and V_l and V_g are the specific volumes of the liquid and vapor, respectively. A relation between s_g and s_l is necessary in order

to solve the above equations. The assumed relation is that the salinity of the gas is related to that of the liquid by

$$s_g = \frac{V_l}{V_g} s_l \quad (5.17)$$

The above relation assures continuity of solubility at the critical point. With the use of Eq. (5.17), one can write Eqs. (5.14) through (5.16) in the form,

$$\alpha_l = \frac{V - V_g}{V_l - V_g} \quad (5.18)$$

$$s_l = \frac{s}{\alpha_l + \frac{V_l}{V_g} (1 - \alpha_l)} \quad (5.19)$$

Since both V_l and V_g depend upon the liquid salinity s_l , the above equations are solved for α_l and s_l by an iteration procedure: In the first pass, the net salinity s is used to determine provisional values for V_l and V_g , which are used in Eq. (5.18) to determine a provisional value of α_l . Equation (5.19) is then used to determine the first approximation to s_l , which in turn is used in Eq. (5.18) to re-evaluate α_l . The process is continued until the difference between successive values of s_l satisfies the convergence criterion. In the above procedure, the specific volume of the liquid as a function of the liquid salinity is evaluated with the use of Eq. (5.10). The specific volume of the vapor is scaled from that for pure water,

$$V_g(s_l, P) = V_{crit}(s_l) + \frac{V_g(0, P) - V_{crit}(0)}{V_g(0, 1) - V_{crit}(0)} (V_g(s_l, 1) - V_{crit}(s_l)), \quad (5.20)$$

where $V_g(0, 1)$ is the specific volume of the vapor for zero

salinity at 1 bar, and $V_g(s_\ell, 1)$ is that for salinity s_ℓ at 1 bar. From a fit to the data of Haas [1975],

$$V_g(s_\ell, 1) = V_g(0, 1) + 1.5257 s_\ell, \quad (5.21)$$

with s_ℓ in weight percent.

The saturation salinity on the liquid phase line as given by a fit to Haas' data is

$$s_{\text{sat}} = 26.218 + (7.20089 \times 10^{-3})T + (1.05998 \times 10^{-4})T^2 \quad (5.22)$$

with s_{sat} in weight percent and T in °C. In terms of pressure, the saturation salinity is given by

$$s_{\text{sat}} = 26.08 + \left[680.176 - \frac{294.528 - P}{0.435137} \right]^{1/2}, \quad (5.23)$$

with P in bars.

If the liquid salinity as calculated from Eqs. (5.18) and (5.19) is less than the saturation value as given by Eq. (5.23), the specific internal energy for the mixture is calculated from

$$E = \alpha_\ell E_\ell + \alpha_g E_g, \quad (5.24)$$

where the specific energy of the liquid, E_ℓ , is calculated with Eq. (5.11) and that of the vapor, E_g , is calculated with the scaling relation Eq. (5.20), with volumes replaced with the equivalent energies. The energy of the vapor in joule/g as a function of liquid salinity at 1 bar is calculated from

$$E_g(s_\ell, 1) = 2400.35 + 0.4892 s_\ell, \quad (5.25)$$

with s_ℓ in weight percent.

If the liquid salinity as calculated above using the assumption of no precipitated salt turns out to be greater than the saturation value, then the calculation is repeated with the liquid and vapor salinities set equal to the saturation values. In this case, the mass fractions of the liquid, solid, and vapor are obtained from a solution of the three equations,

$$V_l \alpha_l + V_s \alpha_s + V_g \alpha_g = V \quad (5.26)$$

$$\alpha_l + \alpha_s + \alpha_g = 1 \quad (5.27)$$

$$s_l^* \alpha_l + \alpha_s + s_g^* \alpha_g = s \quad (5.28)$$

where α_s is the mass fraction of precipitated salt, and s , s_l^* , s_g^* are the net salinity, the saturation salinity of the liquid, and the saturation salinity of the vapor (mass fractions). For the pressure range of interest here, the specific volume of precipitated salt is assumed to depend upon temperature only. It is calculated with the expression

$$V = V_0 \exp \left[\int_{T_0}^T \alpha(T) dT \right], \quad (5.29)$$

where $V_0 = 0.46189 \text{ cm}^3/\text{g}$, $T_0 = 25^\circ\text{C}$, and $\alpha(T)$ is the volumetric coefficient of thermal expansion. A fit to the data as given in the American Institute of Physics Handbook [1972] is

$$\alpha \times 10^6 = A + BT + CT^2. \quad (5.30)$$

With T in $^\circ\text{C}$, the coefficients are

$$A = 119.6$$

$$B = 0.04693$$

$$C = 1.0571 \times 10^{-4}.$$

The specific internal energy is calculated from

$$E = \alpha_l E_l + \alpha_s E_s + \alpha_g E_g, \quad (5.31)$$

where E , E_s and E_g are the specific energies of the liquid, solid and vapor, respectively. The specific internal energy of the solid is assumed to be a function of temperature only and is calculated with the expression

$$\begin{aligned} E_s (\text{erg/g}) &= 8.535 \times 10^6 (T - 25) \\ &+ 2719.6 (T^2 - 625), \quad (T \leq 100^\circ\text{C}) \\ &= 6.6562 \times 10^8 + 9.079 \times 10^6 (T - 100), \\ &\quad (T > 100^\circ\text{C}) \end{aligned} \quad (5.32)$$

which is based on specific heat data as given in the Handbook of Chemistry and Physics [1949].

As mentioned earlier, for a given input volume, the specific internal energies are calculated at each of the tabulated pressures on the phase line until two values are found which bound the input value of the energy. Then a linear interpolation is used to determine the final value of the desired variables.

5.5 PRESSURE-ENERGY COUPLING COEFFICIENT

The pressure-energy coupling coefficient as defined by Eq. (5.2) is based on a fit to the P , V , E data for pure water to 1 kbar as given in the ASME Steam Tables [1967]. A plot of the points used in the fit is given in Figure 5.3.

Figure 5.3. Pressure versus energy for pure water.

For points to 2 kbar which lie outside the range as plotted in Figure 5.3, the curves are extrapolated as straight lines. The pressure-energy coupling coefficient, which is the slope of the chord connecting the point in question and that on the phase line at the same specific volume, is computed by means of tabular and analytic fits in a separate function sub-program. For salinities other than zero, the coupling coefficient is tentatively taken to be the same as for pure water at the scaled volume

$$V_{\text{scale}} = 1.0028 \rho_0 V, \quad (5.33)$$

where ρ_0 is given by Eq. (5.3). This assumption will be examined further to make sure that it is compatible with the volumetric data as given in the compilation of Potter and Brown [1975].

5.6 FURTHER WORK

The principal task remaining is the development and coding of the P, V, E description for Region IV. In this region the general state will consist either of unsaturated vapor or a mixture of precipitated salt and salt-saturated vapor in thermal and pressure equilibrium. Tentatively, the method of solution will be the same as for Region V: At the given net specific volume, V, the specific internal energy will be calculated at a series of pressures until the two values are found which bound the input value E. For the mixture state the governing equations are

$$s = \alpha_s + (1 - \alpha_s) s_g^*(V_g, P) \quad (5.34)$$

$$V = \alpha_s V_s + (1 - \alpha_s) V_g \quad (5.35)$$

$$E = \alpha_s E_s + (1 - \alpha_s) E_g \quad (5.36)$$

The first two of the above equations will be solved by an iteration procedure for mass fraction of the solid precipitate α_s and for the specific volumes of the vapor and solid, V_g and V_s . These values together with the equations of state will then yield specific internal energies for use in the third equation.

Following completion of the P, V, E description for Region IV, expressions for various thermodynamic derivatives and for viscosity and thermal conductivity will be incorporated into the equation of state subroutine.

It is noted that the equation of state subroutine will be used to calculate the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s , and the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s .

The equation of state subroutine will be used to calculate the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s , and the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s . The equation of state subroutine will be used to calculate the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s , and the specific internal energy, E , and the specific volume, V , and the mass fraction of the solid precipitate, α_s .

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VI. ANALYSIS OF THE WAIRAKEI GEOTHERMAL FIELD

The reservoir simulators developed in this program have been previously validated against measurements of non-isothermal single-phase (liquid water) and boiling two-phase (water+steam) flow through linear bench-scale laboratory models [Pritchett, et al., 1975]. In Section 2.2, calculations were presented to demonstrate that the numerical method employed in the simulators is free of computational difficulties encountered by other multidimensional reservoir simulators. To complete the validation of the reservoir simulators, it is desirable to successfully reproduce the observed performance of a geothermal field under full scale production.

Within the United States the only geothermal field with sufficient production history to be considered for verification of computer simulators is the Geysers. The data, however, are closely held by the commercial operators and are not readily available to outsiders. The Geysers field is vapor dominated and thus atypical since liquid dominated systems represent by far the greatest geothermal resource within the United States. One of the areas in the Imperial Valley is likely to be the first U.S. liquid-dominated field to reach large scale production, but this will not occur for at least one to two years. Since land surface subsidence is a major environmental concern associated with large scale geothermal fluid production in the Imperial Valley, the verification of a reservoir model should also include treatment of this effect.

The Cerro Prieto field in Mexico is just across the border from the Imperial Valley and may one day offer the production and subsidence histories needed for a detailed study. However, these data are not presently available and its history is presently too brief for the purposes of this study.

The Wairakei geothermal field in New Zealand is well-suited for study of the production and subsidence histories of an exploited geothermal system. It offers the opportunity for field validation of both the reservoir fluid flow and subsidence models. Since the first wells were discharged in the early 1950's, a comprehensive data bank has been established which records pertinent production and subsidence information. This wealth of data is undoubtedly the most complete for a liquid-dominated geothermal system. Its availability permits a comprehensive study of the field. The low salinity of Wairakei fluid permits its thermodynamic behavior to be adequately treated by the equation of state of pure H_2O .

An early attempt to predict the long range response of the Wairakei field to production was made by Whiting and Ramey [1969]. These authors used concepts generally applied in petroleum engineering to model the field as a lumped parameter system. This approach is useful in analyzing the general trend of the reservoir's performance but does not include the spatial dependence of the system. Recently, Mercer, et al. [1975] have performed a two-dimensional areal single-phase simulation of the hydrothermal system at Wairakei. Their calculated temperature and pressure histories match the observed histories until approximately 1963, i.e., the period 1953-1963. At that time, they found that the increasing steam saturation invalidated their single-phase assumption. They are presently expanding their study to perform a two-phase areal simulation of the field.

In the present study, the geothermal reservoir simulator QUAGMR has been applied, in its 2-D planar mode, to analyze a vertical cross-section of the Wairakei field over the period 1953-1967. This representation permits treatment of information flashing, fluid recharge, geologic

structure and ground surface subsidence within the limited scope (2-D analysis) of the current investigation. Although a detailed history match would require a detailed 3-D model of the field, the principal features of the observed production and subsidence histories of the Wairakei field can be analyzed with the 2-D model considered in the present calculations.

In Section 6.1 the Wairakei geothermal system is described and the material properties, initial conditions and boundary conditions assumed for the QUAGMR calculations are presented. The relevance of thermomechanical data obtained by tests performed on core samples to in situ field conditions is also examined. Section 6.2 describes the results of the QUAGMR calculations reproducing the total fluid production history of the field. It is found that in-formation flashing of the fluid dominates the response of the Wairakei field from its very early history. Section 6.3 examines the observed ground surface subsidence history of Wairakei in light of the QUAGMR calculations and laboratory data on Wairakei core samples. The laboratory thermal properties data adequately represents the reservoir thermal properties, but the mechanical response of the reservoir rock is not represented by the behavior of small scale specimens. The subsidence pattern observed at Wairakei appears to be the combined result of the local geology and the fluid production history.

6.1 WAIRAKEI GEOTHERMAL SYSTEM

The Wairakei geothermal system is located north of Lake Taupo and west of the Waikato River (Figure 6.1); it occupies a surface area of approximately 15 km² [Grindley, 1965], and extends westward from the river approximately 5 km. In order to simulate the behavior of the field, we

Figure 6.1. Map showing location of cross-section A-B, of length 5 km, of Wairakei field.

consider a two-dimensional vertical cross-section (line AB in Figure 6.1) which extends through the main production area and the region of large surface subsidence. The geologic stratification, as determined from wellbore logs [Grindley, 1965; Grange, 1955] is shown in Figure 6.2. The numerical grid is shown in Figure 6.3. Most of the fluid production comes from the Waiora formation (see Figure 6.3). The Waiora formation dips steeply in the east (Figure 6.2); the exact depth is, however, unknown and, therefore, the indicated depth in Figure 6.2 may be in substantial error. To the west, the Waiora formation is cut by the much less permeable rhyolites. There are indications that the reservoir extends somewhat beyond A in the west [Bolton, 1970]. The observed pressure changes beyond A (e.g., in borehole 224) are, however, small (indicating a region of very low permeability); we will, therefore, for purposes of the present study assume the Western reservoir boundary to be located at A. The reservoir is assumed to be 3 km thick (in the direction transverse to AB); this yields a surface area of 15 km² for the reservoir.

6.1.1 Thermomechanical Properties of Wairakei Rocks

In order to utilize the QUAGMR simulator to study the production history of the Wairakei geothermal field, it is necessary to specify the following thermomechanical properties of various rock formations encountered at Wairakei:

Grain density, ρ_s

Effective porosity, ϕ

Horizontal and vertical permeabilities, k (millidarcies)

Thermal conductivity, κ_s

Specific heat, C_{vs}

Relative permeabilities, R_l and R_v

Figure 6.2. Cross-section of Wairakei field.

Figure 6.3. Computational grid for two-dimensional vertical cross-section of Wairakei. X - production region through 1967.

For a simulation using the AGRESS code the stress-strain response of the rock mass is also treated so that the following additional properties are required:

Bulk modulus of drained rock, K

Bulk modulus of rock grain, K_g

Poisson's ratio of drained rock, ν

Coefficient of linear thermal expansion of drained rock, η

Coefficient of linear thermal expansion for rock grain, η_g

In an attempt to determine some of these properties, Terra Tek, Inc., acting as subcontractor to S³, performed laboratory measurements on selected Wairakei cores. Although the complete report by Terra Tek is appended to this report, some of these laboratory results are summarized in Table 6.1. The thermal properties, given in Table 6.1, generally lie close to previous estimates of these properties (see, e.g., Mercer, et al. [1975]). The situation is quite different however, in regard to the mechanical properties (grain density, porosity, elastic moduli and permeability). As an example, the porosity values given in Table 6.1 are almost twice those used by Mercer, et al. [1975]. In our initial attempts to simulate Wairakei production history, we employed the porosity values given in Table 6.1; it was found that this gave too high a value for storage. We, therefore, arbitrarily reduced porosity values of Table 6.1 by a factor of two. A partial justification for this procedure may be sought in the inhomogeneous nature of Wairakei formations. The presence of interspersed shales would tend to reduce the effective porosity; this, however, would not show up in the laboratory measurements. The laboratory measurements of permeability show that the matrix permeability is practically nonexistent. Therefore, practically all of the permeability at

TABLE 6.1

THERMOMECHANICAL PROPERTIES OF WAIRAKEI CORES
(Laboratory Tests Performed by Terra Tek under Subcontract to S³
are Described in Detail in Appendix A of this Report)

ROCK TYPE(S)	ROCK GRAIN DENSITY (g/cm ³)	EFFECTIVE POROSITY	SPECIFIC HEATS (cal/g°C)	THERMAL CONDUCTIVITY (watt/m°C)	LINEAR THERMAL EXPANSION COEFFICIENT (cm/cm/°C)	PERMEABILITIES (μdarcies)	ELASTIC MODULI FROM HYDROSTATIC & TRIAXIAL TESTS (kbar)		ELASTIC MODULI FROM ULTRASONIC TESTS (kbar)	
							Bulk Modulus	Poisson's Ratio	Bulk Modulus	Poisson's Ratio
Pumice/Breccia	2.71	0.488	0.191	1.03*	.	.	8.7 (Effective Pressure=10 bars)	0.05	11.3* (Effective Pressure=6.9 bars)	0.132*
Huka Falls	2.70	0.3986	0.175	1.28*	.	0.32 (Effective Pressure=16 bars)	1.9 (Effective Pressure=30 bars)	0.17	19.3* (Effective Pressure=68.9 bars)	0.07*
Malora	2.72	0.3870	0.177	1.56*	8.2 10 ⁻⁶	50** (Effective Pressure=50 bars)	20 (Effective Pressure Between 35 and 100 bars)	0.18	90.6* (Effective Pressure=103.4 bars)	0.124*
Ignimbrite	2.69	0.144	0.194	2.11*	.	.	.	.	232.0* (Effective Pressure=137.9 bars)	0.222*
				*(All Specimens Saturated)	*Not Measured	*Not Measured	*Data not available		*Data available at other effective pressures	
						**Values also available for P _{eff} > 50 bars	DRY SPECIMENS (Pore Pressure=0)		SATURATED SPECIMENS	

Wairakei arises from the presence of fractures. It is well known that the Wairakei field is interlaced with faults. Since no well-test data are available, it was found necessary to numerically experiment with the permeability values. Our final permeability values are in general agreement with those of Mercer, et al. [1975]. Table 6.2 gives the grain density, porosity, thermal conductivity, specific heat and permeability values used in the final simulation.

It should be noted that the grain density, thermal conductivity and specific heat values of Table 6.2 were obtained from the corresponding values of Table 6.1 by assuming the classical law-of-mixtures formulae. Thus, for example, grain density ρ_s of Table 6.2 is given by:

$$\rho_s = \frac{V_R \rho_R + V_f \rho_f}{V_R + V_f} \quad (6.1)$$

where

ρ_R = grain density of Table 6.1

V_R = 1 - porosity of Table 6.1

V_f = 0.5 × porosity of Table 6.1

ρ_f = fluid density at appropriate pressure and temperature.

Rock types in Table 6.2 refer to the formation types assigned to various computational zones (see Table 6.3). Properties listed for the mixed rock types (types 8 and 10) were calculated by simple averaging. In addition to the rock properties shown in Table 6.2, we need to specify the elastic moduli, coefficient of linear thermal expansion, and the relative permeabilities. The specification of the first two properties (i.e., elastic moduli, and coefficient of linear thermal expansion) will be discussed in Section 6.3.

TABLE 6.2
ROCK PROPERTIES EMPLOYED IN THE SIMULATION OF THE WAIRAKEI
PRODUCTION HISTORY

Rock Type(s)	Geologic Formation	Rock Grain Density (g/cm ³)	Porosity	Thermal Conductivity (10 ⁵ ergs/sec cm°C)	Specific Heat (10 ⁷ ergs/g°C)	Permeability Millidarcies	
						Horizontal	Vertical
1	Pumice/Breccia	2.15	0.244	1.225	0.939	100	100
2,3,4	Huka Falls	2.26	0.1993	1.55	0.8757	10	10
5	Waiora (Production Region)	2.27	0.1935	1.976	0.8475	100	100
6	Rhyolite and Ignimbrites (Unfaulted Region)	2.54	0.072	2.305	0.7666	0.03	0.03
7	Ignimbrites and Rhyolites (Faulted)	2.54	0.072	2.305	0.7666	0.3	3
8	Andesites/Waiora	2.27	0.1935	1.976	0.8475	110	100
9	Waiora (Eastern Region)	2.27	0.1935	1.976	0.8475	10	10
10	Rhyolites/Waiora	2.42	0.133	2.148	0.8046	50	50

TABLE 6.3

MAP RELATING THE COMPUTATIONAL ZONES OF FIGURE 6.3
TO THE ROCK TYPES LISTED IN TABLE 6.2

1 - Pumice/Breccia, 2, 3, 4 - Huka formation, 5 - Waiora (production region), 6 - rhyolite and ignimbrites (unfaulted), 7 - rhyolites and ignimbrites (faulted), 8 - andesites/Waiora (mixed cells), 9 - Waiora (eastern region), 10 - Wairoa/rhyolites (mixed cells), 0 - void.

1	1	1	1	0	0	0	0	0	0	0	0	0	0	0	0	0	0
1	1	1	1	1	0	0	0	0	0	0	0	0	0	0	0	0	0
1	1	1	1	1	0	0	0	0	0	0	0	0	0	0	0	0	0
2	2	3	2	1	1	1	1	1	1	1	1	1	0	0	0	0	1
2	2	3	2	2	1	1	1	1	1	1	1	1	1	1	1	1	2
5	2	3	2	2	3	3	3	3	3	3	3	3	1	1	1	4	2
5	5	5	5	2	2	3	3	3	3	3	3	3	2	2	2	4	2
5	5	5	5	5	5	5	5	5	5	5	5	5	3	2	2	2	4
5	10	10	10	10	5	5	5	5	5	5	5	5	5	5	5	9	9
6	6	7	5	5	8	8	8	8	8	5	5	5	5	5	5	9	9
6	6	5	5	6	6	7	7	7	6	6	6	5	5	5	5	9	9
5	5	5	6	6	6	7	7	7	6	6	6	6	5	5	5	9	9
5	5	7	6	6	6	7	7	7	6	6	6	6	5	5	5	9	9

Little or no experimental data are available on relative permeability for the Wairakei rocks. For present purposes, we will assume that R_ℓ and R_v are given

$$R_\ell = \left(\frac{S_\ell - S_{\ell r}}{1 - S_{\ell r}} \right)^4 \quad (6.2)$$

$$\begin{aligned} R_v &= S^2/0.64 && \text{for } S \leq 0.8 \\ &= 1 && \text{for } S > 0.8 \end{aligned} \quad (6.3)$$

where

$$S_\ell = 1 - S$$

S = vapor saturation

and $S_{\ell r}$ = residual liquid saturation = 0.2.

The form for R_v has been suggested by Ian Donaldson of New Zealand in a private communication; the relation for R_ℓ is a slightly modified form of the Corey equations (see, e.g., Pritchett, et al. [1975]). It is worthwhile to point out that we have found that the exact numerical values for R_ℓ and R_v are not very important for the purposes of this history match as long as R_ℓ and R_v exhibit the general trends assumed in this study.

6.1.2 Initial and Boundary Conditions

It is assumed that the Wairakei system was in a quasi-steady state prior to exploitation. Therefore, the initial conditions for the simulation are those determined from the temperature logs acquired from the wells drilled in the early 1950's [Grindley, 1965; Grange, 1955]. Shallow temperature measurements at Wairakei are not precise [Browne, 1976]; in the present work it was found necessary to somewhat lower

(from the ones reported by Grindley) the shallow temperatures to ensure no vaporization in the pre-production phase. The upper portions of Waiora (above the production region) are, however, close to flashing.

It is perhaps worthwhile to briefly review here the results of our numerical experimentation regarding the specification of initial temperatures. As will be seen in Section 6.2, the temperatures in the upper portions of Wairoa must be high enough to ensure that either the reservoir is flashing or is close to flashing in the pre-production phase; this is necessary to explain the initial portion of the fluid pressure drop curve. The temperature distribution (and hence presence or absence of flashing) in the Huka and Pumice/Breccia formations does not, however, appear to have a major impact on the field pressure drop history. The specification of temperatures in the Waiora formation at the production horizon (and somewhat above and below it) is of critical importance; the difference of a few degrees centigrade in initial temperatures in this region can profoundly alter the calculated pressure drop history. As will be discussed in Section 6.2, this sensitivity to temperature is due to the relative permeability effect in two-phase flow.

Table 6.4 shows the initial temperature distribution corresponding to our best simulation (designated as Wairakei 10) of the Wairakei pressure drop history. The initial pressure distribution (corresponding to assumed initial temperatures) is calculated by running the QUAGMR code (with no production, and infinite specific heats for all rock types to ensure constant temperature) until the pressure distribution is essentially steady. Boundary conditions for the pressure initialization are no mass flux along side and bottom boundaries, and $p = 1$ bar and $T = 20^{\circ}\text{C}$ (for mass influx only) along the top boundary.

TABLE 6.4

CELL BY CELL INITIAL TEMPERATURE MAP (°C). ZEROES INDICATE VOID CELLS. PRODUCTION
HORIZON - 4th LAYER FROM THE BOTTOM (WAIRAKEI 10)

33.0	33.0	33.0	33.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
65.0	60.0	57.0	55.0	34.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
100.0	85.0	83.0	80.0	60.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
130.0	110.0	110.0	110.0	70.0	39.0	38.0	38.0	38.0	38.0	38.0	38.0	38.0	37.0	40.0	40.0	.0	.0	38.0
160.0	140.0	135.0	130.0	110.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0	80.0	60.0	100.0	90.0	40.0	40.0	65.0
190.0	165.0	155.0	150.0	150.0	140.0	140.0	140.0	140.0	140.0	140.0	130.0	130.0	110.0	140.0	140.0	90.0	95.0	120.0
210.0	207.0	204.0	200.0	190.0	180.0	180.0	180.0	180.0	180.0	180.0	170.0	160.0	150.0	200.0	190.0	140.0	145.0	150.0
227.0	227.0	227.0	227.0	226.0	225.0	225.0	224.0	223.0	221.5	220.5	220.0	218.0	210.0	237.0	236.0	185.0	180.0	175.0
244.0	244.0	244.0	244.0	244.0	243.5	243.0	242.5	241.5	240.5	240.0	240.0	239.0	238.0	251.0	251.0	235.5	234.0	232.5
255.0	255.0	255.0	255.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0
265.0	265.0	265.0	260.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0
270.0	270.0	270.0	260.0	255.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0
270.0	270.0	260.0	260.0	255.0	255.0	255.0	255.0	255.0	255.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0

For the production phase of the calculation, the specific heats for various rock types are set equal to the values listed in Table 6.2. The boundary conditions for the production phase are no mass flux and prescribed temperature (= initial temperature) along side and bottom (except faulted regions) boundaries, and $P = 1$ bar and $T = 20^{\circ}\text{C}$ (for recharge fluid only) along the top boundary. Along the faulted portion of the bottom boundary, pressure and temperature (as given by the initialization run) are prescribed. It is obvious from the above description of boundary conditions that we allow recharge to the reservoir from both the upper and bottom boundaries; this is in conformity with the views expressed by several New Zealand researchers but is at variance with the view of Mercer, *et al.* [1975] who allow no recharge from the bottom boundary.

6.2 PRODUCTION HISTORY AT WAIRAKEI

The field behavior is simulated from 1953 to 1967 since the subsidence is well documented (for 1967) and the data readily accessible through 1967 [Bolton, 1970; Hatton, 1970]. The data bank provides the total-field fluid production for each of the years 1953-1967, as well as the number and location of the wells producing each year. For each year, the locations of the producing wells are projected onto the vertical cross-section which is divided into computational zones (see Figure 6.3). The locations of these projections determine which zones are to be considered as computational mass sinks; i.e., as production zones. Each mass sink flow rate is determined by the number of wells projected into that zone and the total annual production of the field. This procedure for specifying mass sink flow rate is open to objection insofar as it is assumed that all wells at any given instant of time produce identical amounts of fluid. With the passage of time and inhomogeneous (spatially) drop in reservoir pressures, some wells will be

unable to maintain a specified production rate. This will be especially true in the period after 1963-1964 when large scale vapor formation occurs in the eastern part of the field. However, for purposes of this preliminary study, the present procedure for specifying mass sink flow rates should suffice. It should be borne in mind that the purpose of this study is to understand the gross features of the Wairakei geothermal system, and is not to do a detailed history matching.

Figure 6.4 shows a comparison of calculated pressure-drop history (simulation Wairakei 10) for the field with the data. In general, the agreement is extremely good. Borehole 36 is of special interest insofar as it lies towards the eastern end of the field where subsidence is maximum; observed pressure drops in borehole 36 are generally lower than those observed elsewhere in the field. This suggests that the Waiora formation in the east has a lower permeability than that obtaining in the rest of the geothermal field [c.f., Mercer, et al., 1975]. The computed pressures for borehole 36 are also in good agreement with the data (see Figure 6.4).

The behavior of the Wairakei field, under exploitation, is primarily governed by the saturation temperature-pressure relation for water [Bolton, 1970]. The upper portions of the reservoir start flashing soon after production commences (see Figure 6.5a); this helps to maintain the reservoir pressures in the early years (Figures 6.4, 6.6a). The two-phase boiling region keeps on growing with continued production; in the years 1959-1960, the two-phase region begins to invade the production horizon (Figure 6.5b). Field pressures now (1959-1960) begin to drop rapidly (see Figures 6.4 and 6.6b) due to the relative permeability effect in two-phase flow. Eventually (around 1964) the entire production region starts to boil (see Figure 6.5c); this marks the onset

Figure 6.4. Comparison of calculated pressure drop (field and borehole 36) histories with data.

(a)

(b)

(c)

Figure 6.5. Vapor saturation contours at beginning of 1955 (a), 1960 (b) and 1968 (c).

Figure 6.6. Pressure drop (in bars) contours at the beginning of 1955 (a), 1960 (b) and 1968 (c).

of the relative flattening of the pressure drop curve (Figure 6.4). The above discussion illustrates the dominating influence exercised by boiling on the reservoir pressure response; as a matter of fact, all the important stages (initial flat portion, middle large pressure drop region, the final relatively flat part, Figure 6.4) in the reservoir pressure history can be traced to boiling in one or another part of the reservoir.

To further illustrate the major influence exercised by boiling (and hence initial temperature distribution) on the pressure response, initial temperatures were lowered by a few degrees in the eastern part of the production horizon (see Table 6.5). Figure 6.7 shows a comparison of field pressure drop history as given by this simulation (Wairakei 11) with the results of Wairakei 10 and the field data. It is clear from Figure 6.7 that a change of even a few degrees produces a dramatic variation in the calculated pressure drop values; this is especially true in the years between 1959 and 1962 (i.e., when the two phase flow first starts to invade the production horizon). This once again reinforces our earlier remark regarding the importance of correctly specifying initial temperature in the production region of the reservoir.

6.3 WAIRAKEI SUBSIDENCE DATA

Ground subsidence at Wairakei was first measured in 1956 when benchmark levels were compared with those established in 1950; periodic measurements have indicated that the area affected by subsidence probably exceeds 25 square miles [Hatton, 1970]. The area of maximum subsidence (subsidence ≥ 0.5 m), however, lies outside the main production region. Cross-section AB (Figures 6.1-6.3) passes through the large subsidence region; the intersection of the subsidence region with AB is indicated in Figure 6.3. The maximum subsidence

TABLE 6.5
CELL BY CELL INITIAL TEMPERATURE MAP (WAIRAKEI 11). ZEROES INDICATE EMPTY CELLS.
NOTE THAT THE INITIAL TEMPERATURES ARE THE SAME AS IN WAIRAKEI 10 EXCEPT IN THE
REGION (EASTERN PART OF PRODUCTION HORIZON) ENCLOSED IN A BOX

33.0	33.0	33.0	33.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
65.0	60.0	57.0	55.0	34.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
100.0	85.0	83.0	80.0	60.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0	.0
130.0	110.0	110.0	110.0	70.0	39.0	38.0	38.0	38.0	38.0	38.0	38.0	37.0	.0	.0	.0	.0	38.0	39.0
160.0	140.0	135.0	130.0	110.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0	80.0	60.0	40.0	40.0	40.0	40.0	65.0
190.0	165.0	155.0	150.0	150.0	140.0	140.0	140.0	140.0	140.0	140.0	130.0	130.0	110.0	100.0	90.0	90.0	95.0	120.0
210.0	207.0	204.0	200.0	190.0	180.0	180.0	180.0	180.0	180.0	180.0	170.0	160.0	150.0	140.0	140.0	140.0	145.0	150.0
227.0	227.0	227.0	227.0	226.0	225.0	225.0	224.0	223.0	221.5	220.5	220.0	218.0	210.0	200.0	190.0	185.0	180.0	175.0
244.0	244.0	244.0	244.0	244.0	243.5	243.0	242.5	241.5	240.5	240.0	240.0	239.0	238.0	237.0	236.0	235.5	234.0	232.5
255.0	255.0	255.0	255.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	249.0	247.0
265.0	265.0	265.0	260.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0
270.0	270.0	270.0	260.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0
270.0	270.0	260.0	260.0	255.0	255.0	255.0	255.0	253.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0	251.0

Figure 6.7. Comparison of computed field pressure drop histories (Wairakei 10 and 11) with observed values.

region, in the shape of an elliptical bowl, overlies the thicker part of the Waiora formation (Figure 6.3). Maximum subsidence at Wairakei (1964-1974) is of the order of 4.5 m; this has been accompanied by horizontal movements of the order of 0.5 m [Stilwell, et al., 1975]. Calculated pressure profiles for the period 1953-1967 (Figure 6.6) show that the region of largest pressure drop lies directly below the maximum subsidence area, at least during the years 1964-1967; furthermore, the region of large pressure drop to the west of the subsidence region (i.e., in the thinner part of Waiora) is relatively small. This strongly suggests that the subsidence pattern observed at Wairakei is the combined result of the local geology and the fluid production history.

Ground subsidence is primarily the result of pore collapse and crack closure (i.e., a reduction of porosity ϕ) due to a drop in pore fluid pressure (and possibly temperature). For small deformations, changes in porosity ϕ are governed by the relation [Garg, 1976]:

$$\Delta\phi = \left[\frac{1}{K_s} - \frac{1-\phi}{K} \right] \Delta(P_c - P_f) + 3(1-\phi)(\eta - \eta_s) \Delta T, \quad (6.4)$$

where

$K(K_s)$ = Bulk modulus of porous rock (rock grain)

$\eta(\eta_s)$ = Coefficient of linear thermal expansion of porous rock (rock grain)

$P_c = (1-\phi) P_s + \phi P_f$ = total (or confining) pressure

P_s = Rock grain pressure

P_f = Fluid pressure

ΔT = Change in fluid/rock temperature

At Wairakei, the temperature change is relatively small ($< 20^\circ\text{C}$), and we can, therefore, in a first order theory ignore the second term in the relation for $\Delta\phi$. This still leaves us with the problem of specifying K and K_s for each rock type

encountered at Wairakei. Table 6.1 gives the elastic moduli for dry (hydrostatic and triaxial tests) and fluid-saturated (ultrasonic tests) rocks. No direct measurements of K_s are available; K_s may be calculated by using the following result of Garg and Nur [1973]:

$$K_c = K + \frac{\left[1 - K/K_s\right]^2}{\frac{\phi}{K_f} - \frac{K}{K_s^2} + \frac{1-\phi}{K_s}} \quad (6.5)$$

where

$K_c(K)$ = Bulk modulus for fluid-saturated (dry) rock

K_f = Bulk modulus for fluid.

Substituting for K_c and K from Table 6.1 and for ϕ from Table 6.2 into the above equation, and taking $K_f = 22$ kbar (water at room temperature), we obtain the following values for the rock grain bulk modulus (K_s):

<u>Formation</u>	<u>K_s (kb)</u>
Pumice/Breccia	9.7
Huka Falls	18.7
Waiora	235
Ignimbrites (and Rhyolites)	483 (obtained by assuming $K=100$ kb)

The drained elastic moduli given in Table 6.1 were obtained from hydrostatic and triaxial tests in the laboratory. There is ample evidence (e.g., calculations for production history discussed in Section 6.2) that the reservoir behavior at Wairakei is governed by fractures, formation inhomogeneities, and other large scale features such as faults. For such a system, it is reasonable to assume that the compaction (subsidence) behavior will be profoundly influenced by the

presence of fractured rock masses. It, therefore, becomes important to supplement the laboratory measurements by suitable field data (e.g., seismic measurements). In the absence of the latter measurements, the applicability of laboratory measurements to in situ rock masses may be checked by back-calculating in situ elastic moduli using observed subsidence rates together with the calculated pressure drop rates. In the following discussion, we will of necessity employ this indirect procedure.

Assuming that the fluid mass withdrawal at Wairakei is small so that the overburden remains essentially constant, and that $K \ll K_s$, we have for uniaxial compaction [Garg, 1976]:

$$\frac{\Delta h}{h} = \frac{\Delta p}{K + 4/3\mu} \quad (6.6)$$

where μ is the shear modulus of the porous rock and

Δh = Compaction rate

h = Formation thickness

Δp = Pressure drop rate

The thickness and the calculated pressure drop (averaged through thickness) in the years 1964-1967 for the various formations in the area of maximum subsidence are:

<u>Geologic Formation</u>	<u>Thickness, h (m)</u>	<u>Pressure Drop, Δp (bars/year)</u>
Pumice/Breccia	100	0.34
Huka	150	0.68
Waiora	950	1.77

Assuming that the laboratory values for the drained elastic moduli of the Pumice/Breccia and Huka formations are representative of in situ values, we obtain:

$$(\Delta h)_{\text{Pumice/Breccia}} \approx 0.02 \text{ m/year}$$

$$(\Delta h)_{\text{Huka}} \approx 0.02 \text{ m/year}$$

Since the observed subsidence rate at Wairakei during 1967 is 0.4 m/year, we have:

$$\begin{aligned} (\Delta h)_{\text{Waiora}} &= 0.4 - (\Delta h)_{\text{Pumice/Breccia}} - (\Delta h)_{\text{Huka}} \\ &\approx 0.36 \text{ m/year} \end{aligned}$$

Substituting these values for $(\Delta h)_{\text{Waiora}}$, h , and Δp into Eq. (6.6), we obtain:

$$(K + 4/3\mu)_{\text{Waiora}} \approx 4.67 \text{ kb.} \quad (6.7)$$

Assuming Poisson's ratio is 0.18 (see Table 6.1), this gives

$$K_{\text{Waiora}} = 2.24 \text{ kb} \quad \mu_{\text{Waiora}} = 1.82 \text{ kb.} \quad (6.8)$$

The above calculated value of K is smaller by a factor of nine than the laboratory value (Table 6.1). This large discrepancy implies that either the Waiora formation in the region of maximum subsidence is much thicker than that assumed in the present simulation or that the Waiora formation is intensely fractured. In view of our analysis of the Wairakei production data and also of available geologic data, we lean towards the second of these explanations.

In the above discussion of subsidence at Wairakei (for the years 1964-1967) it was assumed that the rock matrix responds to changes in pore pressure as if it were a linear-elastic material (constant elastic moduli K , μ). There exists substantial evidence which suggests that this assumption is rather poor. Figure 6.8 (from Stillwell, et al. [1975]) is a map of the Wairakei field showing both the

Figure 6.8. Total subsidence in meters at Wairakei during period 1964-1974 [Stillwell, et al., 1975].

areas of principal production and principal subsidence. Within the subsidence area and somewhat to the south of the center of the region is "Benchmark A-97", located at about the one-third maximum (subsidence) amplitude contour near the main highway. Figure 6.9 shows, as a function of total fluid mass produced, the fluid pressure in the reservoir at 150 m below sea level; Figure 6.10 shows, on the same scale, the total vertical surface subsidence at Benchmark A-97. At early times, the pressure in the reservoir drops rapidly, but later on the pressure drops more and more slowly. This, as we know, is because as time goes on the two-phase region becomes more and more extensive. On the other hand, note that the slope of the Benchmark A-97 subsidence versus total production curve actually increases somewhat with time. At present, and for the past several years, the subsidence rate at Wairakei has essentially remained constant whereas the rate of pressure drop has declined continuously.

In Figure 6.11, the reservoir pressure drop (in bars-ordinate) is plotted as a function of the downward movement of Benchmark A-97 (in meters-abscissa) as deduced from the data presented in Figures 6.9 and 6.10. The "dots" denote time - 1 January of the year indicated in each case. This plot strongly suggests that nonlinear ground movement processes are operating at Wairakei. At early times, the slope of this (psuedo) stress-strain curve is 36 bars/meter of subsidence - at present, the slope is 2.4 bars/meter, lower by a factor of 15.

On the basis of the subsidence data taken over the interval 1 January 1964 - 1 January 1968 (1964-1967), and treating the various formations to be homogeneous and linear elastic, we obtained a mean value of $(K + 4/3\mu)_{\text{Waiora}} \approx 4.67 \text{ kb}$, Eq. (6.7). If we make the assumption that the

Figure 6.9. Pressure versus mass of fluid produced at Wairakei [Stillwell, et al., 1975].

Figure 6.10. Subsidence at Benchmark A-97 versus mass of fluid produced at Wairakei [Stillwell, et al., 1975].

Figure 6.11. Wairakei subsidence data - reservoir pressure drop versus subsidence at Benchmark A-97.

general trend throughout the area of surface subsidence is qualitatively similar to the behavior shown in Figure 6.11, we can make more definite statements about the behavior of the reservoir rocks. Over the time interval 1964-1967, Figure 6.11 shows that the average slope of the pressure drop-subsidence curve at Benchmark A-97 was 12 bars/meter: a factor of three lower than the initial slope but a factor of five greater than the current slope. This suggests that, at early times (1953),

$$(K + 4/3\mu)_{\text{Waiora}} \approx 14 \text{ kilobars}$$

and that, at late times (1975),

$$(K + 4/3\mu)_{\text{Waiora}} \approx 0.9 \text{ kilobars.}$$

It is comforting to note that the value of 14 kilobars at early times is substantially closer to the laboratory value than the value of 4.67 kb for the period 1964-1967 - it is low by only a factor of three. The difficulty is, of course, to account for the spectacular decrease in apparent elastic moduli with time.

The apparent increase in rock compressibility at Wairakei with time is typical of many reservoirs (for a case study of an oil/gas reservoir see Merle, et al. [1976]). A nonlinearity in the mechanical response of the rock may be ascribed to (1) structural failure at late times and/or (2) decrease in bulk modulus with an increase in $\Delta(P_c - P_f)$. Initially, the reservoir rock behaves in a linear-elastic manner with $K + 4/3\mu \approx 14 \text{ kbars}$. From the Benchmark A-97 data, we know that this model is probably adequate up to about 1963. At about that time, however, failure must have begun. Hence, we should be able to estimate, based upon elastically-calculated 1963 shear stresses, the yield strength of the rock. Rock which has yielded should thereafter

be assigned an effective incremental shear modulus of zero. The elastically-calculated 1963 response would also enable us to estimate the threshold value of $\Delta(P_c - P_f)$ at which the bulk modulus K starts to decrease with increasing $\Delta(P_c - P_f)$. The functional dependence of K on $\Delta(P_c - P_f)$ would be, of course, determined by history-matching (see also Merle, et al., [1976] in this connection).

The foregoing discussion illustrates the difficulties associated with matching (and predicting for the future) the subsidence history at Wairakei. The amount of work required to accomplish this task (requiring application of the AGRESS fully coupled simulator) is beyond the scope of the present grant; and, therefore, no numerical results are presented here. It is, nevertheless, interesting to point out the implications of our analysis of the Wairakei subsidence data for predicting subsidence in a virgin geothermal field. If for example, we attempt to predict subsidence in the Salton Sea field due to some specified production/injection strategy, we would necessarily have to use elastic moduli based on measurements of the early-state moduli (derived from seismic measurements, for instance). If, however, in reality the effective moduli were to decline by a factor on the order of 15 during production as they did at Wairakei, we would thereby drastically underestimate the subsidence hazard. Clearly, it would be desirable to determine the appropriate long-term nonlinear stress-strain relations prior to making such theoretical predictions.

At this time, it is not clear how these material parameters can be measured. Neither laboratory tests on small core samples nor pre-production seismic measurements are liable to be of much help. It may be possible to obtain some guidance from the analysis of geological, subsidence, and production data for geothermal and oil/gas reservoirs with well-documented production and subsidence histories. Such

an analysis may help in identifying the mechanisms which cause the nonlinear behavior. Some examples of such mechanisms are:

1. Geological history of the field.
2. Dewatering of interspersed shales.
3. Thermal effects on the mechanical properties of the rock.
4. Chemical dissolution of intergranular cementing minerals by fresh water recharge.
5. Mechanical scouring and weakening of the matrix by fluid motion.

An understanding of these mechanisms, to the extent necessary to assess their relative magnitudes, appears to be required before devising experimental procedures for characterizing rock response and making subsidence predictions at a virgin geothermal field.

VII. FUTURE PLANS

Work is presently proceeding (under Phase IIB of this program) to complete the water-sodium chloride solution equation-of-state package described in Section V. A computer subroutine is being written which will allow these brine thermodynamic calculations to be made with the speed and flexibility required in geothermal reservoir simulations. The QUAGMR and AGRESS simulators will be modified to calculate the salt concentration at each point in space and time in a geothermal reservoir calculation, and the brine equation of state package will be incorporated. The completion of these tasks will finish the development of the computer techniques originally planned in this three-year project.

A significant part of the work planned in Phase IIB will involve the application of the modeling capabilities developed during the course of this research project. The reservoir which will be studied is the Buttes field, located on the southeastern flank of the Salton Sea. This field, also known as the Salton Sea Geothermal Field (SSGF), represents a high-salinity, high-temperature resource under active development. The field is located in a tectonically active region in which both ground surface subsidence and seismicity are potential environmental hazards. One site, operated by New Albion Resources Company (a subsidiary of the San Diego Gas and Electric Company) and Magma Power Company, is located near the center of this region. A second site, located near the southeast edge of the high heat flow region is operated by Southern California Edison Company and its associates. A series of fluid production, pressure drawdown and buildup tests have been recently performed at the SDG&E/Magma site. The resulting data for porosity, permeability and inter-well interferences are expected to be available for our studies. ERDA's Lawrence Livermore Laboratory (LLL)

and Lawrence Berkeley Laboratory (LBL) are currently correlating the geologic data, fluid salinity and thermal data available from the various wells in the SSGF (Palmer, et al. [1975]; Towse [1975]; Towse and Palmer [1976], Palmer [1975]).

The numerical simulations which we will perform for the SSGF will address several problems -- the productivity of the region, vertical (subsidence) and horizontal ground-surface motions induced by alternate production/injection strategies and an assessment of seismic hazards. We plan to use the LLL/LBL data base in defining the geology and reservoir characteristics to be used in the simulation calculations. The overall objective of these studies, however, is to validate methodology developed in this research project, rather than detailed predictions for a specific site.

For a selected production/injection strategy, the fully coupled reservoir simulator will be used to describe the response of the geothermal system. A fair amount of non-environmental information will be obtained in this way, such as the evolution with time of the bottomhole pressures and temperatures in both production and injection wells, the overall performance decline of the system, the onset of information flashing (if it occurs), and the total energy depletion and gross thermal power output from the reservoir.

At the same time, the simulator will calculate the deformation of the reservoir rock itself and the surrounding impermeable strata. Numerous large faults are known to be present in the vicinity of the SSGF. In Phase A of the program, we surveyed the literature to determine the distribution of the stress-release due to earthquakes in the Imperial Valley area [Pritchett, et al., 1975]. These data will be updated and used to estimate the pre-production tectonic stress state, and the critical stress necessary to cause slippage along major faults in the neighborhood of the field.

These will then be compared with stress changes calculated for the production/injection strategies treated in an attempt to estimate the associated seismic hazard.

The fully active simulator also provides, as direct output, the deformations of all portions of the rock matrix, including the surface of the ground itself. Both horizontal and vertical motions are calculated. The potential surface displacements will be calculated for the production/injection strategies considered. For example, if all production occurred in one portion of the field, and all injection took place in another to avoid premature cooling of the produced fluid, large amounts of downward motion in the production region might be accompanied by surface swelling above the injection region even though the average deformation is near zero. In view of our discussion of the nonlinear subsidence measured at Wairakei and the lack of data on the mechanisms that might contribute to subsidence at the Salton Sea Geothermal Field, however, the calculated ground surface motions will necessarily be only preliminary estimates.

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APPENDIX A

FINAL REPORT

use as part of title

Subtitle

04103

TESTS ON CORES FROM THE WAIRAKEI
GEOTHERMAL PROJECT, WAIRAKEI, NEW ZEALAND

by

R. R. Hendrickson

Submitted to

Systems, Science and Software, Inc.
Box 1620
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Systems, Science and Software, Inc.
Contract #S-4959 Under National Science Foundation Grant
AER75-14492

Submitted by

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TR 75-63
July 1976

FOREWORD

This report describes testing performed by Terra Tek, Inc., for Systems, Science, and Software, Inc., of La Jolla, California under Purchase Order #S-4959, "Testing, Wairakei Cores".

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ABSTRACT

Physical, thermophysical and mechanical properties for the cap rocks (Huka Falls mudstone, Pumice), the reservoir formation (Waiora tuff) and the lower barrier formation (Wairakei Ignimbrite) have been determined for the Wairakei Geothermal Site. Cores were supplied by the Ministry of Works and Development (N.Z.) from bore holes #24, 34 and 37 for depths varying from 134 ft. to 2618 ft.

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INTRODUCTION

Terra Tek has conducted a series of tests on cores taken from the Wairakei geothermal site in New Zealand. The cores tested are representative of each of the four basic rock types significant to the functioning of the geothermal site. The underlying rock layer is a welded tuff known as Wairakei Ignimbrite.¹ It is the most dense of the four layers, with a saturated density of 2.36 Mg/m^3 , and is relatively impermeable, thus forming a lower barrier for the aquifer. The aquifer is a tuff which is referred to as the Waiora Formation.¹ Permeability of this rock is surprisingly low, less than 50 microdarcies and it is believed that water travels primarily through fractures in the stratum. Above the aquifer is an impervious layer of mudstone known as Huka Falls Formation¹ (saturated density 1.99 Mg/m^3). The surface layer is Pumice¹ (1.88 Mg/m^3 , saturated) which may serve as a thermal insulator, but is probably not necessary to the functioning of the geothermal site.

All samples are from two shipments of cores shipped directly from Wairakei under the direction of Mr. W. B. Stilwell and Mr. E. L. D. Fooks, Ministry of Works and Development, Wairakei, N.Z. The following specific bores and depths were selected for testing, based on decisions made during the meeting between Dr. Sabod Garg of S³, and Terra Tek personnel in September, 1975.

- A. Pumice, Bore hole #34, depth 134 ft.
- B. Huka Formation, Bore hole #37, 700 ft. to 720 ft.
- C. Waiora Formation, Bore hole #37, 1130 ft. to 1135 ft., hereafter referred to as "Waiora 1130 ft."

D. Waioara Formation, Bore hole #37, 2296 ft. to 2618 ft., hereafter referred to as "Waioara 2618 ft."

E. Wairakei Ignimbrite, Bore hole #24, 2482 ft. depth.

It should be noted that all samples were received dry and have been out of the ground for about ten years. The effects of such a drying and resaturating cycle should be considered by the reader. In addition, the Huka Falls and Pumice cores were difficult to prepare and test, especially when saturated, as they tended to slake. However, Terra Tek was able to complete all tests as described.

MINERAL ANALYSIS

Mineral composition was determined by microscopic examination of standard .03 mm thin sections of the sample materials. This thickness is standard in the geological field since the constituent minerals are more readily differentiated. The first step in the analysis was the identification of as many mineral types as possible. Next, a count of the major constituents was made using a microscope reticle as a guide; two hundred points were used in the count. Both plane-polarized light and cross-polarized light* were used; in both cases the light was transmitted through the thin sections.

Pumice

The pumice tested, Figure 1, is a porous, medium-grained grayish-brown rock showing banding of light and dark minerals and/or rock fragments.

Composition

	<u>% by volume</u>
Matrix material & voids	43
Rock fragments	24
Feldspars	17
Altered Mafic minerals	6
Pyroxenes	3
Opakes	3
Quartz	2
Amphiboles	2

*In Cross-Polarized illumination, plane-polarized light passes through the sample then through a second polarizer at right-angles to the first. Thus the only visible light is that which has been "rotated" by minerals in the sample.

Figure 1. Pumice, Bore 34, 134', standard thin section photo, 31.25 X magnification using plane-polarized light.

Huka Falls Formation

The Huka Falls Formation Sample, Figure 2, is a fine-grained light grayish-brown mudstone. No bedding or fissility is noted. The mineral analysis revealed fine-grained clay and silt.

Composition

	<u>% by volume</u>
Clay .005 mm or less	86
Silt .05 mm to .005 mm	14

Waioara Formation, 1130'

This sample of the Waioara Formation is a whitish-green altered tuff with a dominant, fine-grained glassy matrix and phenocrysts of feldspar. The photograph, Figure 3, shows that both the matrix and feldspars have been extensively altered. The hand specimen is chalky in appearance.

Figure 2. Huka Falls (Mudstone), Bore 37, 700', magnified X 125, plane-polarized light.

Figure 3. Waioara Formation, Bore 37, 1130', magnification 31.25 X, plane-polarized light.

Composition

	<u>% by volume</u>
Matrix Material	63
Feldspars	35
Opakes	2
Zeolites	Trace

Waiora Formation, 2618'

This sample of the Waiora Formation, Figure 4 is a light-brown altered tuff with a fine-grained matrix consisting of glass, quartz, feldspar and large fragments of altered mafic material. The feldspar fragments have been at least partially altered to a clay.

Composition

	<u>% by volume</u>
Fine-grain glassy matrix	82
Voids	14
Feldspars	2
Quartz	2
Opakes	Trace

Wairakei Ignimbrite

This Ignimbrite, Figure 5, is a grayish-green welded tuff with a fine-grained matrix of glass, quartz, feldspar and phenocrysts of glassy quartz. Some minor alteration has occurred.

Composition

	<u>% by volume</u>
Fine-grain matrix (glass dust)	50
Feldspar	32
Sericite	9
Quartz	5
Calcite	2
Fibrous zeolites & chlorites	2
Opakes	Trace

Figure 4. Waiora Formation, Bore 37, 2618', standard thin-section magnified 31.25 X, cross-polarized light.

Figure 5. Wairakei Ignimbrite, Bore 24, 2482', standard thin-section photo, magnification 31.25 X, plane-polarized light.

Comments on the Lithology

The Wairakei geothermal region was created by active volcanism and rock strata are highly transitional both vertically and horizontally. This is confirmed by the Drill Log for the area.¹

Although a complete geotechnical analysis of the field is beyond the scope of this report, it is safe to assume that transitions between strata are generally abrupt.

PHYSICAL PROPERTIES

Dry and Saturated Bulk Densities

The bulk volume of the sample was determined by the Archimedean principle. The sample was first saturated in distilled water and weighed in the air. Next, the sample was weighed while submerged in distilled water; the difference in measured weights represents the weight of water displaced. The water temperature and pressure were measured and the corresponding water density was found from a handbook. The weight difference was divided by the water density to yield the sample bulk volume. The sample was then dried for 24 hours at 105°C (ASTM standard procedure), allowed to cool in a desiccator and weighed. This dry weight was divided by the sample volume to obtain the dry bulk density. Similarly, the weight of the saturated sample in air divided by the sample bulk volume yielded the saturated bulk density. Results are listed in Table 1.

Effective Porosity

Effective porosity is the percentage of the sample volume occupied by connected pores. The volume of connected pores was determined by dividing the difference between the sample's saturated weight and its dry weight by the density of the distilled water. The volume of connected pores was divided by sample volume to yield effective porosity. Table 1 lists the effective porosities.

Grain Density

The dried sample mentioned above was pulverized to a 100 mesh[†] particle size for determination of grain density. The volume of the pul-

[†] U.S. Standard sieve series, 0.15 mm openings, Tyler equivalent 100 mesh.

verized rock was measured using a Beckman Gas Pycnometer; grain density is the weight of the sample material divided by the measured grain volume, and is listed in Table 1.

Total Porosity and Occluded Porosity

The total porosity is the total of the connected pores and the occluded pores. Total porosity can be calculated from the grain density and dry bulk density data as follows:

$$\eta_p = 1 - \eta_s$$

where

η_p = volume fraction of all pores, i.e., total porosity = $\eta_p \times 100\%$

η_s = volume fraction of solids

and

$$\eta_s = \frac{\text{Dry Bulk Density}}{\text{Grain Density}}$$

The occluded porosity can then be calculated by subtracting the effective porosity from the total porosity.

TABLE 1

Saturated and Dry Bulk Densities, Effective Porosity, Total Porosity, Occluded Porosity and Grain Density

ROCK TYPE	BULK DENSITY+ SATURATED DRY Mg/m ³ Mg/m ³		EFFECTIVE POROSITY PERCENT BY VOLUME	TOTAL POROSITY PERCENT BY VOLUME	OCCCLUDED POROSITY PERCENT BY VOLUME	GRAIN DENSITY+ Mg/m ³
PUMICE	1.88	1.39	48.8 ± 0.5	48.7 ± 0.3	0 ± 0.8	2.71
HUKA FALLS	1.97	1.56	40.7 ± 0.5	42.2 ± 0.3	1.5 ± 0.8	2.70
HUKA FALLS*	2.01	1.62	39.0 ± 0.5			--
WAIORA 1130	2.01	1.59	41.6 ± 0.5	42.4 ± 0.3	0.8 ± 0.8	2.76
WAIORA 1130*	2.04	1.65	39.1 ± 0.5			--
WAIORA 1130*	2.07	1.69	37.9 ± 0.5			--
WAIORA 2618*	1.99	1.64	35.6 ± 0.5	38.8 ± 0.3	3.2 ± 0.8	2.68
WAIORA 2618*	1.99	1.60	39.1 ± 0.5			--
IGNIMBRITE	2.34	2.20	13.8 ± 0.5	18.2 ± 0.3	4.4 ± 0.8	2.69
IGNIMBRITE*	2.38	2.23	15.0 ± 0.5			--

* Indicates different samples from same footage.

+ Bulk density and grain density measurements are accurate to within ±0.005 Mg/m³.

ULTRASONIC MEASUREMENTS

The technique used to measure the ultrasonic velocities is the "Through Transmission System" shown in block diagram form in Figure 6. This is an adaptation of the technique introduced by Mattahoni and Schreiber², and is capable of measuring small elapsed times to a high degree of accuracy. The time measurement is derived from the frequency of a very stable frequency synthesizer (stability ± 1 part in 10^7 /month, accuracy ± 0.001 percent).

Figure 6. Through-Transmission System

The signal passing through the specimen is viewed on an oscilloscope alternately with the signal from the variable frequency synthesizer (comparison wave). The latter is modified by a pulse shaper to exactly match the wave which has passed through the specimen. Next, the pulse which excites the transmitting transducer is viewed, and its shape matched to that of the comparison wave. Once the pulse shapes are matched, they can be

made to coincide on the oscilloscope to a high degree of precision. The frequency of the synthesizer is then adjusted for an exact number of cycles between the transmitted signal and the signal through the specimen. The transit time of the ultrasonic wave through the material is obtained by dividing the number of cycles by the frequency. The binary divider is required in order to operate the pulse generator at a repetition rate that allows all of the ultrasonic wave energy to dissipate between pulses. This requires that the oscilloscope be triggered from the pulse generator in order to maintain the proper display.

Referring to Figure 7, the upper traces show the initial transmitted signal matched to the comparison wave. The lower traces (via a double-exposure) show the same comparison wave matched to the signal that passed through the specimen. In this example there were twelve cycles of comparison wave between the initial signal and the signal through the specimen. The

Figure 7. Oscilloscope display showing the comparison wave and the signal through a specimen in the lower trace and the comparison wave and the initial signal in the upper trace.

frequency synthesizer setting was 0.3542 MHz, the elapsed time was therefore 33.88 μsec . Because of the difference in the inherent delay in the electrical and acoustic paths, it is necessary to determine a time correction using a reference sample. The velocity of the acoustic wave in the specimen is obtained by dividing the path length by the corrected elapsed time.

The overall accuracy of this technique is limited by the accuracy with which the wave forms can be matched on the oscilloscope.

Derivation of Moduli from Ultrasonic Velocity Measurements

The following classic relationships were used to calculate moduli from ultrasonic velocity measurements.

V_s = Shear-wave velocity, km/sec ($\pm 1\%$ measurement accuracy)

V_p = Plane-wave velocity, km/sec ($\pm 1\%$ measurement accuracy)

ρ = Density, Mg/m^3 ($\pm 0.005 \text{ Mg/m}^3$ measurement accuracy)

A. Young's Modulus, E

$$E = \frac{3\rho V_s^2 (V_p^2 - 4/3 V_s^2)}{V_p^2 - V_s^2}, \text{ GPa}$$

B. Bulk Modulus, K

$$K = \rho (V_p^2 - 4/3 V_s^2), \text{ GPa}$$

C. Shear Modulus, G

$$G = \rho V_s^2, \text{ GPa}$$

D. Poisson's Ratio, ν

$$\nu = \frac{E - 2G}{2G} = \frac{1/2 V_p^2 - V_s^2}{V_p^2 - V_s^2}$$

E. Constrained Modulus, B

$$B = \rho V_p^2, \text{ GPa}$$

Ultrasonic Data and Calculated Moduli

The following are the ultrasonically determined velocities and calculated elastic moduli. The designation "P-Wave" represents the longitudinal wave, while "S-Wave" stands for the shear wave. Sample density is found in the "Physical Properties" section. Each test was performed at hydrostatic confining pressures selected to simulate the range of *in situ* stress.

Pumice, 134'

Density: $1.360 \pm .005$, Mg/m³, dry

Figure 8. Longitudinal and Shear-wave velocities vs. Confining pressure in Pumice, 134'.

TABLE 2

Pumice, 134', Velocities and Moduli at Five Confining Pressures

CONFINING PRESSURE MPa	VELOCITIES		POISSON'S RATIO	MODULI			
	P-WAVE Km/sec	S-WAVE Km/sec		CONSTR. GPa	SHEAR GPa	BULK GPa	YOUNG'S GPa
1.35	1.05	.532	.328	1.50	.385	.989	1.02
.69	1.17	.560	.332	1.70	.427	1.13	1.14
3.45	1.39	.827	.225	2.62	.930	1.38	2.28
5.52	1.51	.910	.215	3.10	1.13	1.60	2.74
6.89	1.58	.942	.224	3.40	1.21	1.79	2.96

Huka Falls Formation, 700'Density: $1.605 \pm .005 \text{ Mg/m}^3$, dry

Figure 9. Longitudinal and Shear-wave velocities vs. confining pressure in Huka Falls Formation, 700'.

TABLE 3

Huka Falls Formation, 700', Velocities and Moduli at Six Confining Pressures

CONFINING PRESSURE MPa	VELOCITIES		POISSON'S RATIO	MODULI			
	P-WAVE Km/sec	S-WAVE Km/sec		CONSTR. GPa	SHEAR GPa	BULK GPa	YOUNG'S GPa
0	1.64	1.17	≈0	4.31	2.21	1.36	4.30
3.45	1.73	1.18	.062	4.81	2.25	1.81	4.77
6.89	1.77	1.20	.070	5.02	2.32	1.93	4.96
10.3	1.77	1.19	.081	5.02	2.29	1.97	4.95
13.8	1.80	1.20	.104	5.19	2.29	2.13	5.05
17.2	1.88	1.25	.107	5.66	2.49	2.34	5.52

The increase in wave velocity above about 14 MPa is considered to be due to pore-collapse; this material is a mudstone and is substantially weaker than the other rocks tested.

Waiora Formation, 2618'

Density: $2.000 \pm .005 \text{ Mg/m}^3$, water saturated

TABLE 4

Waiora Formation, 2618', Velocities and Moduli at Seven Confining Pressures

CONFINING PRESSURE MPa	VELOCITIES		POISSON'S RATIO	MODULI			
	P-WAVE Km/sec	S-WAVE Km/sec		CONSTR. GPa	SHEAR GPa	BULK GPa	YOUNG'S GPa
0	2.22	1.08	.344	9.85	2.34	6.73	6.29
3.45	2.46	1.23	.333	12.1	3.02	8.05	8.06
6.89	2.59	1.31	.329	13.4	3.41	8.82	9.07
10.3	2.63	1.34	.324	13.9	3.60	9.06	9.54
13.8	2.66	1.37	.321	14.1	3.73	9.16	9.85
17.2	2.69	1.38	.322	14.5	3.79	9.40	10.0
21.0	2.71	1.39	.322	14.6	3.84	9.52	10.1

Figure 10. Longitudinal and Shear-Wave Velocities vs. confining pressure in Waiora Formation, 2618'.

Wairakei Ignimbrite, 2482'

Density: $2.330 \pm .005 \text{ Mg/m}^3$, water saturated

TABLE 5

Wairakei Ignimbrite, 2482', Velocities and Moduli at Six Confining Pressures

CONFINING PRESSURE MPa	VELOCITIES		POISSON'S RATIO	MODULI			
	P-WAVE Km/sec	S-WAVE Km/sec		CONSTR. GPa	SHEAR GPa	BULK GPa	YOUNG'S GPa
0	4.22	2.52	.223	41.6	14.8	21.8	36.2
3.45	4.25	2.54	.221	42.1	15.1	22.0	36.8
6.89	4.30	2.57	.223	43.1	15.4	22.6	37.6
10.3	4.33	2.59	.222	43.8	15.6	22.9	38.2
13.8	4.36	2.61	.222	44.3	15.8	23.2	38.6
17.2	4.38	2.62	.222	44.6	16.0	23.4	39.0

Figure 11. Longitudinal and shear-wave velocities vs. confining pressure in Wairakei Ignimbrite, 2482'.

PERMEABILITY MEASUREMENT

Permeability can be measured or estimated by any one of several different methods. Typical laboratory determinations of permeability, however, are usually made using either the constant flow method or the transient method. Each method has advantages and disadvantages depending on the conditions required for the test and the permeability of the sample in question. The constant flow method is well documented and is widely used for porous media having permeabilities greater than 100 μ d. The second method is more suitable for low porosity materials such as those encountered in the Wairakei field, where the permeabilities are in the microdarcy region.³

Figure 12 illustrates the apparatus used by Terra Tek for making permeability tests with the transient method. The sample is placed in a pressure vessel and pore pressure inlet and outlet lines are connected to the external hardware. The sample can be subjected to hydrostatic pressure and/or axial loading during the permeability test. Pore pressure can be regulated to any value less than the confining pressure. As can be seen from this figure, there are volumes of fluid on either side of the sample which can be hydraulically connected to allow the pore pressure to equalize. When the sample has reached equilibrium, the volumes are disconnected by closing a valve. Referring to Figure 13, the pressure in Volume #1 is raised slightly and then applied to the sample suddenly. This pressure step is normally only a few percent of the absolute pressure in the reservoir volume. The pressure step decay is monitored accurately through the use of a differential pressure transducer. Sample length and reservoir volume can be changed to allow the test to be completed in a convenient length of time.

Figure 12. A sketch of the test apparatus used to measure permeability.

Figure 13. A schematic of a permeability test.

A brief outline of the theory involved in measuring permeabilities using the transient technique is given below. A detailed treatment of this analysis is presented in Reference 4.

Permeability k is given as:

$$k = \frac{\alpha \beta \mu l}{A(1/V_1 - 1/V_2)}$$

where

α = the slope of the semi-log plot of the natural log of the decaying pressure versus time.

β = fluid compressibility

μ = fluid viscosity

l = the sample length

A = the sample area

V_1 = volume at top of sample

V_2 = volume at bottom of sample

With the following assumptions made concerning fluid flow characteristics,

1. Darcy's law is valid.

2. The fluid flow is laminar.

3. The change in fluid volume in the pores in the rock, due to the step pressure change, is negligible compared to the amount of fluid flowing through the sample during a test.

4. The pressure step is small compared to the absolute pore pressure so that the physical constants of the fluid (viscosity and compressibility) can be considered constant in all parts of the sample.

Thus, the permeability can be determined with no direct measurement of the flow rate. Clearly this is a major advantage for making:

1. Accurate measurements on low permeability samples.

2. Using the system at high pore pressures.

Data, Huka Falls Mudstone

Table 6 lists the details of a single-point permeability test on Huka Falls Mudstone.

TABLE 6
Huka Mudstone Permeability

Confining Pressure	0.69 MPa
Pore Pressure	0.0 MPa
Saturation	water, 100%
Temperature	24°C
Permeability.	63 Microdarcies

Permeability vs. Effective Stress for Saturated Waiora Altered Tuff

Figure 14 is a plot of permeability in microdarcies vs. effective stress, i.e., confining pressure minus the pore pressure. The pore fluid was water. Pore pressure was constant at 10 MPa throughout the test while confining pressure was increased from 15 MPa to 25 MPa, then decreased back to 15 MPa. As the effective stress was increased, permeability decreased at a constant rate initially, but with a change in slope above 8 MPa effective pressure, probably indicating pore collapse. As the effective stress was decreased, the sample exhibited substantially lowered permeability. This is typical of many rocks, and permeability will usually return to the original (unconfined) value after being left unconfined for several days. Repeatability of the permeability measurements is $\pm 5\%$ or better.

Figure 14. Saturated Waiora Formation, 1130', Permeability in Microdarcies vs. Effective Stress.

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HYDROSTATIC & TRIAXIAL TESTS

Waioara Formation

The bulk modulus (K) and shear modulus (G) were determined with hydrostatic and triaxial loading tests. Prepared samples were 5.08 cm diameter x 6.35 cm long; ends were ground parallel to within ± 0.025 mm. Where possible, a length-to-diameter ratio of at least 2.5 should be used in order to reduce "end effect" errors. Unfortunately, none of the Wairakei cores had a length of more than about 8 cm as received. The alternative of reducing sample diameter to about 2.5 cm would have resulted in breakage problems due to the poor competency of the rock. Baligh⁽⁵⁾ has shown that the worst-case failure stress error would be about 10% for an L/D ratio of 1.25 and a Poisson's ratio of 0.4. Since the Poisson's ratio for this material is closer

Figure 15. Hydrostatic and Triaxial Test Apparatus

to 0.2, the error is certainly less than 10%. At higher confining pressures, the error becomes less significant.

Saturated samples were sealed in urethane jackets and mounted between steel end caps. The lower end cap permits drainage to a 50 cc pore fluid chamber within the end cap; this insures that pore pressure within the sample is zero. Figure 15 schematically depicts the test hardware. Confining pressure (p) was monitored with both a diaphragm pressure transducer and a Heise Pressure gauge (bourdon tube mechanism), having a resolution of ± 70 kPa.

The axial load (L) was measured with a load cell located within the pressure vessel. Axial strain is measured via four cantilever arms mounted on a steel ring, and spaced 90° apart. These cantilevers slide on a 30° cone mounted to the other end of the sample. The strain-gauge bridge is wired such that effects of sample tilting, if any exists, will not affect the measurement. Resolution of this system is $\pm 0.025\%$ strain. Transverse strain was measured using another strain-gauge bridge consisting of four cantilevers mounted to a free-floating ring. The cantilevers contact the urethane jacket at the sample midpoint. Deformation of the urethane due to confining pressure is extremely small but is well known and has been taken out of the data. Similarly, the effect of hydraulic pressure on the strain gauges used in the load cell and cantilevers is removed during the data reduction process. Resolution of the transverse strain cantilever system is $\pm 0.02\%$ strain. Both cantilever systems were calibrated prior to use.

The first step in the experimental procedure was the application of confining pressure (3.5 MPa) with axial and lateral strains being recorded. Next, an axial load was applied with confining pressure constant. The axial load was limited to about 75 percent of the material's ultimate strength; this value was estimated from the shape of the resultant stress-strain plot.

The axial load was then removed and the confining pressure increased to 10.4 MPa, followed by a second axial loading and unloading. Finally, the confining pressure was increased to 17.2 MPa, followed by axial loading up to the yield point. Figure 16 illustrates the stress paths described. Confining pressure, lateral strain, axial strain, and axial load were recorded on XY plotters.

Figure 16. Triaxial Loading Schedule.

Bulk Modulus: The bulk modulus is defined as

$$K = \frac{\Delta P \cdot V}{\Delta V} = \frac{\Delta P}{\epsilon_1 + \epsilon_2 + \epsilon_3}, \text{ GPa}$$

where

V = sample volume

ϵ_1 = axial strain due to confining pressure (cantilever & cone system)

$\epsilon_2 = \epsilon_3$ = average lateral strain due to confining pressure (floating cantilevers)

ΔP = increase in confining pressure, GPa

Poisson's Ratio, ν : The Poisson's ratio is derived from the ratio of average lateral strain to the axial strain during differential axial loading.

$$\nu = \frac{-\epsilon_{2,3}}{\epsilon_1}$$

Young's Modulus, E: E is derived directly from the initial slope of the plot of differential stress vs. axial strain, with the confining pressure held constant. Table 8 lists the moduli measured for each confining pressure; see Figure 18 and 19.

Shear Modulus, G: G is derived from previously calculated functions:

$$G = \frac{E}{2(1+\nu)}$$

Results of Hydrostatic and Triaxial Tests: Table 7 presents the hydrostatic test results, Figure 17 is the plot of mean stress vs. volume strain from which the bulk moduli of Table 7 were derived.

TABLE 7

Bulk Modulus Measurements for Three Saturated Samples of Waiora Formation Over Three Regions of Confining Pressure

SAMPLE IDENTIFICATION	CONFINING PRESSURE, MPa	BULK MODULUS GPa ± 0.13 GPa
WAIORA, 1130'	0 to 3.5	0.41
	3.5 to 10.4	2.03
	10.4 to 17.2	1.70
WAIORA 2618', #1	0 to 3.5	0.69
WAIORA 2618', #2	0 to 3.5	0.73
	3.5 to 10.4	1.98
	10.4 to 17.2	2.86

Figure 17. Mean Stress, MPa, vs. Volume Strain, Percent.

Figure 18 shows plots of differential stress vs. axial strain (quadrant I) and vs. lateral strain (quadrant II) for a saturated sample of the Waiora Formation from the 1130-foot level. Figure 19 is a similar plot for two samples from the 2618-foot level of the Waiora Formation. Table 8 is a presentation of the moduli derived from the graphs shown in Figures 18 and 19.

TABLE 8

Young's Modulus, Poisson's Ratio, and Shear Modulus for Three Saturated Samples of Waiora Formation at Three Confining Pressures

SAMPLE IDENTIFICATION	CONFINING PRESSURE MPa	YOUNG'S MODULUS GPa, $\pm .18$ GPa	POISSON'S RATIO ± 0.04	SHEAR MODULUS GPa ± 0.15 GPa
WAIORA, 1130'	3.5	0.91	0.21	0.38
	10.4	1.48	0.24	0.60
	17.2	1.75	0.22	0.72
WAIORA, 2618' #1	3.5	2.65	0.11	1.19
WAIORA, 2618' #2	3.5	2.90	0.12	1.30
	10.4	4.20	0.15	1.82
	17.2	4.94	0.23	1.99

Figure 18. Differential Stress vs. Axial Strain and vs. Lateral Strain in Waiora Formation, 1130'.

Figure 19. Differential Stress vs. Axial Strain and vs. Lateral Strain in Two Samples of Waiora Formation, 2618'.

Special Stress-Path Test for Waiora 2618': The following special test was run on the saturated tuff from the Waiora Formation, depth 2618'. The sample measured 1.90 cm in diameter by 3.8 cm long. Strain-gauge cantilever systems similar to those described previously were used to monitor axial strain and two orthogonal transverse strains. The sample was jacketed in urethane and fitted with a special permeable end cap to facilitate distribution of the pore fluid, water.

The stress path consisted of first increasing the confining pressure and pore pressure simultaneously, with the confining pressure always 2 MPa greater than the pore pressure; the two were stabilized at 20 MPa and 18 MPa, respectively. Next, the pore pressure was slowly reduced to zero in small steps; this procedure was done over a period of about 45 minutes to insure that the pore pressure had time to equalize throughout the sample prior to the taking of each data point. Finally, this process was reversed, and the pore pressure was increased from zero to 18 MPa in small steps over a 45 minute time span. Throughout the above, the confining pressure was held at 20 MPa.

Results: The recorded strains are shown in Figure 20. The discrepancy between the two transverse strains is to be expected for materials containing fragments which are large relative to the sample size (see Mineral Analy-

Figure 20. Special Stress-Path Test for Waiora.

sis section and Figure 4). Some seating effect was observed in the axial strain measurement.

This test can be considered as a special type of bulk modulus measurement. Using the entire region from 18 MPa down to 0 MPa pore pressure, a mean bulk modulus of 2.40 GPa can be calculated. The region from 0 MPa back up to 18 MPa yielded a mean bulk modulus of 3.25 MPa.

The sample experienced a permanent volume compaction of 0.2% due to the excursions mentioned above.

Pumice and Huka Falls Formation

Combination hydrostatic and triaxial tests were run on saturated samples of the Pumice and the Huka Falls mudstone. Details of the tests are similar to the hydrostatic and triaxial tests previously described for the Waiora Formation, except that the triaxial test was run at only one confining pressure. Sample size was 5.97 cm diameter by 6.30 long for the Huka, and 5.25 cm diameter by 6.26 cm long for the Pumice. This length-to-diameter ratio was used for

reasons given previously. The failure stress and modulus of elasticity will appear to be high by about 10 percent or less. Confining pressures were selected to simulate the *in situ* stress; 1.0 MPa for the Pumice and 5.0 MPa for the Huka. In both tests, the samples were vented to the 50 cc chamber within the lower end cap in order to maintain zero pore pressure.

Test Results: Figure 21 shows the results of the hydrostatic test on the Pumice sample and Figure 22 shows the hydrostat of the Huka mudstone. The results of the Huka hydrostat are difficult to assess because the sample exhibited a softening with between 5 and 10 cc's of water forced into the end cap chamber. The softening caused an excessive axial displacement during hydrostatic loading. The graph includes this axial strain without

modification. It should be noted that the Huka formation is essentially clay, and liquefaction such as seen here is to be expected. It is questionable as to whether a successful vented hydrostat could have been performed, even if other cores had been available.

The Pumice hydrostatic yielded low bulk moduli, consistent with ultrasonic measurements and physical properties tests showing it to be a weak, porous material. Table 9 below tabulates the moduli calculated for various segments of each hydrostatic.

TABLE 9

Bulk Modulus Measurements for Pumice and Huka Formation

Sample Identification	Confining Pressure MPa	Bulk Modulus GPa $\pm 10\%$
Pumice, B34, 134'	0 to 0.2	0.032
	0.2 to 0.4	0.057
	0.4 to 0.8	0.096
	0.8 to 1.1	0.29
Huka, B37, 700'-725'	0 to 1.0	0.89
	1.0 to 5.0	2.06

Figure 23 shows plots of differential stress vs. axial strain (quadrant I) and vs. lateral strain (quadrant II) for both the Pumice and the Huka Formations. Transverse strains were nearly identical on the Pumice, but slightly different on the Huka test, and are shown individually. The softening of the Huka during the hydrostatic loading appears not to affect the triaxial loading response. This is known because the axial strain plot exhibits a linear departure from the origin; any further deformation of the plastic region would have produced a pronounced "footing effect". Note also that, due to equipment malfunction, the Huka was not completely failed and no other sample was available to rerun the test. However, the test was complete enough to allow calculation of the elastic moduli, and these are listed in Table 10.

Figure 21. Mean Stress, MPa vs. Volume Strain, Percent for Pumice.

Figure 22. Mean Stress, MPa vs. Volume Strain, Percent for Huka Mudstone.

Figure 23. Differential Stress vs. Axial Strain and vs. Lateral Strain in Pumice and Huka.

TABLE 10

Young's Modulus, Poisson's Ratio and
Shear Modulus for Saturated Pumice and Huka

Sample Identification	Confining Pressure, MPa	Young's Modulus MPa $\pm$ MPa	Poisson's Ratio	Shear Modulus MPa $\pm$ MPa
Pumice B34, 134'	1.0 MPa	176 $\pm$ 2 MPa	.07 $\pm$.003	82 $\pm$.7 MPa
Huka, B37, 700'-725'	5.0 MPa	154 $\pm$ 2 MPa	.37 $\pm$.008	56 $\pm$ 1 MPa

Comparisons With Ultrasonic Measurements

The four elastic moduli found above by deformation measurements on samples of Waioara, Pumice, and Huka were also determined via ultrasonic velocity measurements on other samples from the same footage. The ultrasonic data yields stiffer moduli than those found by deformation tests. While this is atypical for competent materials, it is typical for porous or less competent rocks. Discrepancies of this sort normally show ultrasonic moduli from two to four times greater than those found by deformation tests. Terra Tek is presently investigating the basis of these discrepancies.

LINEAR COEFFICIENT OF THERMAL EXPANSION, α

Figure 24 illustrates the equipment used to measure the coefficient of linear expansion. Two linear variable differential transformers (LVDT's) are used to monitor expansion of the sample. The sample is 2.5 cm diameter by 5.1 cm long, and has a thermocouple bonded to it for accurate monitoring of sample temperature. The slight error introduced by expansion of the fused quartz rods running the length of the sample is calculated out of the data. The remainder of each quartz rod is enclosed in a quartz tube, and expansion

Figure 24. Measurement of Linear Coefficient of Thermal Expansion

effects are cancelled. The experiment is allowed to stabilize for an hour after the furnace reaches the desired temperature; this is several times the thermal time-constant of any component in the system.

Data: Linear efficient of Thermal Expansion

Sample: Waiora 1130', dry

length: 5.1 cm

dia: 2.5 cm

cold temp: 22°C

hot temp: 238°C

ΔT : 216°C

$$\alpha = 8.2 \times 10^{-6} \text{ m/m/K} \pm 5\%$$

SPECIFIC HEAT MEASUREMENT

Figure 25 depicts the apparatus used to determine the specific heat of a sample. An aluminum calorimeter of known heat capacity is embedded in rigid polyurethane foam insulation at least 10 cm thick in all directions. The calorimeter separates into upper and lower halves for loading of the sample. The calorimeter temperature is monitored via thermocouples.

Figure 25. Calorimeter System

Calibration

Calibration of the calorimeter was accomplished by means of a built-in electric heater. A DC voltage was applied to the heater, and the current, voltage, and duration of the heating provided the caloric input figure. During calibration the temperature of the calorimeter was periodically measured by a built-in X4 thermocouple cascade. The thermocouple signal is amplified X1000; thus temperature rises can easily be measured with a resolution of $\pm 0.001^{\circ}\text{C}$.

Since rocks are relatively poor heat conductors, a test takes appreciable time, during which a certain amount of heat escapes into the insulation. This loss was corrected for during calibration by tailoring the electrical heat input vs. time to be similar to the calorimeter heating caused by a sample during a test. Thus, insulation losses were nearly identical in both the calibration and actual testing, and tended to cancel.

The temperature rise of the calorimeter was measured at a fixed time after start of the test. This temperature reading, when divided by the caloric input to the heater, yielded the total heat capacity of the calorimeter: $870 \text{ cal}/^{\circ}\text{C}$.

Testing

All samples were placed in a low-mass cylindrical container to facilitate handling. Core samples were sized to 5.1 cm diameter x 5.1 cm long, and the ends ground flat to within $\pm 0.0025 \text{ mm}$ to facilitate heat transfer to the carrier and hence to the calorimeter. Fragmented samples may also be tested by pulverizing the material and compacting the powder into the container. In both cases the sample must be thoroughly dried prior to

weighing. The weight and heat capacity of the container are known and their effects are analytically removed from the data.

The sample and container were next placed in a stable oven and brought up to the desired temperature. A thermocouple was inserted through a small hole in the container to monitor the sample temperature. The sample carrier was situated within a 1 kg iron vessel in the oven during the heating process. The oven, iron vessel, sample carrier and sample were held at temperature for four hours to insure a uniform temperature throughout the sample. The iron vessel was next used to transfer the sample to the calorimeter without heat loss. The temperature rise of the calorimeter was measured after a time interval identical to that used during calibration.

Calculation of Specific Heat

By definition,⁶ the average specific heat between two temperatures is:

where

$$c = \frac{Q}{m(T_1 - T_2)} = \frac{Q}{m\Delta T}$$

Q	=	heat gained or lost by the sample
T ₁	=	initial temperature
T ₂	=	final temperature
m	=	mass of sample

The average specific heat of the sample, c_s , is therefore:

$$c_s = \frac{Q_c}{m_s \Delta T_s}$$

where

Q_c = heat delivered to calorimeter by the sample

ΔT_s = temperature drop of sample

m_s = sample mass

and

$$Q_c = C_c \Delta T_c - C_a \Delta T_s$$

C_c = total heat capacity of calorimeter

ΔT_c = temperature rise of calorimeter

C_a = total heat capacity of (aluminum) sample carrier

Table 11 below lists the specific heats measured for the Pumice, Huka mudstone, two depths of the Waiora altered tuff, and the Wairakei Ignimbrite.

TABLE 11
Specific Heat Data for Dry Samples

Rock Type	Specific Heat cal/g °C	T_1 °C	T_2 °C	ΔT °C
Pumice	$0.191 \pm 5\%$	240	36	204
Huka	$0.175 \pm 5\%$	241	33	208
Waiora 1130'	$0.174 \pm 5\%$	232	33	199
Waiora 2618'	$0.180 \pm 5\%$	232	34	198
Ignimbrite	$0.194 \pm 5\%$	243	38	205

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1. *Polypodium* *polypodioides* (L.) Presl
 2. *Polypodium* *polypodioides* (L.) Presl

loosely packed glass wool.

Heat flows out of the experiment to a water-cooled heat sink. The heat sink can be moved up and down the "thermal gradient rod" to regulate the temperature drop across the sample. Water cooling and electronic control of the guard-cup heater insure rapid thermal stabilization throughout the experiment.

In order to insure that there is no heat transfer through the sides of the sample, it is surrounded with insulation contained by the "thermal gradient ring". Since this insulation sees an identical vertical temperature gradient on the edge of the sample and on the inside surface of the "thermal gradient ring", there will be no lateral heat flow.

Temperatures of the sample top and sample bottom are determined by thermocouples embedded in the guard-cup and in the sample heat sink. Both probes are within 2.5 mm of the constant-temperature planes. This measurement system is acceptable because the thermal conductivity of copper is about 250 times that of rock. In addition, heat transfer into and out of the sample must be optimized. Both the sample heater and sample heat sink have been lapped and polished to be flat at the heat transfer surfaces. The flat faces of the sample are ground flat to within ± 0.0025 mm.

Calculation of k

$$k = \frac{P \cdot \ell}{A \cdot \Delta T} = \frac{\text{watts}}{\text{meter-kelvin}}$$

P = power, sample heater current x voltage (watts)

ΔT = top temperature - bottom temperature, K

A = sample cross-section, square meters

ℓ = sample thickness, meters

Test Procedure, Wairakei cores (saturated)

The Wairakei samples were vacuum saturated and then sealed in an aluminum foil jacket. A thin layer of thermal compound was used on both the inside and outside surfaces of the foil covering the top and bottom of the sample. Heat flow through the foil on the side of the sample was calculated and removed from the data (a correction of about 20%). Samples were weighed before and after testing to insure that the sample had not leaked; water loss was between 0.1 and 0.8 gram maximum. Data were taken when all readings had stabilized (about 35 minutes).

Table 9 lists the Thermal Conductivities Measured on the four basic rock types. All samples were saturated.

TABLE 12
Thermal Conductivity for Saturated Pumice 134',
Huka 700', Waiora 2618' and Ignimbrite 2482'

ROCK TYPE	CONDUCTIVITY WATT/m-K	HOT FACE °C	COOL FACE °C
Pumice 134'	1.03 ±10%	81.5	46.0
Huka 700'	1.28 ±10%	82.6	49.5
Waiora 2618'	1.56 ±10%	82.5	48.8
Ignimbrite 2482'	2.11 ±10%	82.8	51.1

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