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ROCK MECHANICS PROJECT PROGRESS AND RESULTS: ROCK FRACTURE AND PORE COLLAPSE

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CONTENTS

Abstract	1
Introduction	1
Effect of Confining Stress on Explosive-Induced Fracture	3
Effect of Simultaneous and Sequential Detonation on Explosive-Induced Fracture	7
Effect of Intermediate Strain Rate on Strength	13
Plasticity Model for Porous Rock	18
Summary	43
References	45
Appendix: Review Panel Discussion	47

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ROCK MECHANICS PROJECT PROGRESS AND RESULTS:
ROCK FRACTURE AND PORE COLLAPSE

ABSTRACT

We of the Rock Mechanics Project are studying problems in rock fracture and pore collapse with the intent of understanding mechanisms of and developing models for rubble formation, permeability enhancement, and wave propagation. The progress and results of this project are outlined for the following studies:

1. Explosive-induced fracture and fluid flow in a gas-bearing sandstone at confining pressures to simulate *in situ* conditions,
2. Explosive-induced fracture in a brittle plastic with multiple charges fired both simultaneously and sequentially,
3. The effect of several intermediate-strain rates on strength and fracture in limestone, and
4. Development and testing of a theoretical model for yielding and pore collapse in porous rocks.

INTRODUCTION

The Rock Mechanics Project of the Earth Sciences Division of LLL¹ has four primary functions:

1. to coordinate experimental and theoretical work in basic rock mechanics,
2. to combine the needs and support in rock mechanics science and technology so that we may begin new work in areas that benefit LLL programs;
3. to organize, review, and report on specific goal-oriented subprojects lasting about six months to one year, and
4. to establish and maintain contact with members of the scientific community working in the field of rock mechanics.

The specific technical objectives of the project are as follows: First, to understand the physical mechanisms and develop models for the creation of fractured material or rubble and the enhancement of permeability by underground high-explosive detonations. This understanding will benefit *in situ* energy and resource technologies that use high explosives to achieve or enhance recovery of underground resources.

Our second objective is to understand the physical mechanisms and develop models that describe the response of rock materials along various loading and unloading paths at various strain rates. The loading and unloading paths and strain rates of greatest interest are those that occur near underground explosions. An improved understanding of rock responses in this regime will benefit both *in situ* energy and resource technologies, and the study of seismic wave propagation from underground detonations.

To achieve these objectives, both experimental and theoretical investigations are necessary. Our current areas of interest include: (1) the influence of external confining stress, single and multiple detonations, and simultaneous and sequential detonations on explosive-induced fracture (experimental), (2) the influence of strain rate on rock strength (experimental and theoretical), and (3) the modeling of compaction of porous rock along various loading and unloading paths (theoretical). To advance knowledge in all of these areas, the Rock Mechanics Project is pursuing four major sub-projects:

1. the effect of confining stress on explosive-induced fracture (A. Kusubov),
2. the effect of simultaneous and sequential detonation on explosive-induced fracture (J. Hearst, J. Schatz),
3. the effect of intermediate strain rate on rock strength (A. Abey), and
4. a plasticity model for porous rock (J. Schatz, L. Thigpen, C. Snell)

In the following sections, we will review the progress, results, and plans for the future in the Rock Mechanics Project. The outside academic community has been involved in many phases of this work; we now have a formal association (consulting agreements or subcontracts) with the Massachusetts Institute of Technology (Prof. W. Brace), the California Institute of Technology (Prof. T. Ahrens), U.C. Berkeley (Prof. M. Carroll), and U. C. Davis (Prof. J. Cheney). Informal contacts are also maintained with others.

A review panel will consider and advise on Rock Mechanics Project work at approximately six-month intervals. The panel first met on March 17, 1977. The current panel consists of W. Brace (Earth Sciences, MIT), I. Borg (Energy Resource Planning, LLL), and D. Stephens (*In Situ* Coal Gasification, LLL). The people asked to serve on this panel will change as time goes on. A summary of the conclusions of the first panel is given in the Appendix of this report.

EFFECT OF CONFINING STRESS ON EXPLOSIVE-INDUCED FRACTURE

The purpose of this subproject is to understand the explosive-induced fracture and resultant enhancement of fluid flow in rocks containing hydrocarbon, geothermal and other potential energy-resource materials. From this work we should eventually be able to evaluate the technical and economic feasibility of conducting large-scale rubblizing or permeability-enhancing operations beneath the earth's surface. To simulate *in situ* pressure conditions, we will carry out explosive fracturing tests in rock samples at confining pressures up to about 0.2 GPa, corresponding to depths of about 10 km. We will measure the relevant physical properties of the rocks both before and after explosions to understand the basic mechanisms of fracture and fracture-related enhancement of fluid flow. In addition to careful experimental measurements, we will use existing computer models^{2,3} to predict the extent of fracture. Our results will then be used to improve models for predicting the extent of fracture and enhancement of permeability.

Sample Acquisition

Our first selected test material is a natural gas-bearing sandstone similar to that found at the Rio Blanco test site.⁴ We will study this rock because it is a typical porous (~10%) material that has too low a permeability *in situ* for gas production, and is therefore suitable for studies of permeability enhancement. As the Rio Blanco nuclear stimulation experiment was not completely successful, a study of confined explosive fracturing and permeability in this sandstone may also help us to better understand the Rio Blanco results.

After examining outcrops and similarity of formations, we decided to obtain two 0.305 m diam by 12.2 m length cores (1 by 40 ft) from the Mesa Verde Sego formation. Adjacent holes were drilled at the B well location, Dragon trail site, near Rangely, Colorado. The sample was taken at some distance from the Rio Blanco site on the surface, but should represent a section of similar material,⁵ as shown in Fig. 1. After redesign of our core drill to account for the large diameter of the core and the friability of the formation, samples were successfully obtained and delivered to LLL early in 1977.

Fig. 1. Colorado's Piceance Basin. Schematic cross section showing sandstone sample location and Rio Blanco site.

High Explosive Development

To fracture rock with explosives in the laboratory, it is necessary to use a small, but safe and repeatable high explosive. Such explosive assemblies were developed by scientists at the LLL Chemistry Department; they provide a variable rate of energy output and are compatible with laboratory safety regulations.⁶ The explosives consist of 50%/50% PETN/Cab-O-Sil mixture or low-density PETN with a modified bridgewire detonator. We tested the explosives in 178-mm (7-in.) diam polystyrene and epoxy cylinders for their reproducibility and ability to cause rock fracture. The final configuration meets the preliminary objectives of size, energy release and reproducibility. These explosives may be fired down to 25 mg PETN for small experiments at low or zero confining pressure. For experiments at high confining pressure or with strong rocks where more energy is needed, we will use up to several hundred mg of pure low-density PETN. These larger PETN charges have been successfully tested in 305-mm (12-in) diam cylinders of epoxy-plastic.

Fracture Experiments

The preliminary design of our experimental apparatus is shown in Fig. 2. The experiment will consist of hydrostatically pressurizing either a 178-mm (7-in.) or 305-mm (12-in.) diam sandstone sample in a high-pressure vessel and detonating a high explosive charge with a constant confining pressure. We plan to make suitable measurements before, during, and after the detonation to characterize crack formation and fracture-induced permeability. To account for stress-closure effects, we will measure permeability after the shot while confining pressure on the sample is maintained. We will also attempt to decorate cracks caused by the explosive for post-shot optical, SEM, and x-ray analysis.

To achieve these results, the experiments will be conducted in three stages, each consisting of different tests. A pressure vessel is necessary for all experiments conducted at confining pressures above atmospheric pressure. We have thought of using either the 2100-psi, 13-in. (0.0145 GPa, 330-mm) vessel at Stanford Research Institute (SRI), or the 50,000-psi, 14-in. (0.345-GPa, 356-mm) vessel and the 30,000-psi, 8-in. (0.207-GPa, 203-mm) vessel at Lawrence Livermore Laboratory (LLL). Due to scheduling and design requirements, it is uncertain which of these facilities will be used first.

Fig. 2. Confining pressure-explosive experiment. Schematic drawing showing explosive, containing vessel, and permeability apparatus.

Stage 1

Post-shot qualitative analysis will be made of fractures generated under confining pressures up to 2100 psi (0.0145 GPa) for the 12-in. (305-mm) and 7-in. (178-mm) samples.

Stage 2

Permeability or flow rate will be measured using one re-entry port into the high-pressure vessel. In this experiment we will first introduce a known volume of gas under pressure into the sandstone while maintaining the outside confining pressure, and second, allow the gas to escape into a vessel of known volume while recording the pressure drop and the gas flow as functions of time. At this stage, we may use either the SRI or LLL high-pressure facilities.

Stage 3

Using either the SRI or LLL high-pressure vessels, we will directly measure flow rates of pressurized gas circulating through the fractured rock. Two re-entry ports into the pressure vessel are required for this measurement. These final experiments will be made at several confining pressures. We will also characterize the fracture pattern by extensive pre- and post-shot analysis of the rock, and will attempt to correlate the fracturing effects with the creation of permeability in the fractured samples.

EFFECT OF SIMULTANEOUS AND SEQUENTIAL DETONATION ON EXPLOSIVE-INDUCED FRACTURE

In this subproject, we plan to examine the effects of multiple charge detonations for rock fracturing in the absence of a free surface. This topic is important because we must sometimes blast in a contained underground configuration that has no nearby free surfaces to create tensile reflections and enhance fracturing effects. The permeability enhancement from fractures will be studied at a later stage in this project. In this multiple experiment, we will also investigate the influence of detonating charges simultaneously, at short sequential delays, and at long delays (much longer than the wave travel time between the charges). We would like to study the effect of delays on the fracture pattern, and determine which scheme is better at connecting charge holes with fractures, at creating large fracture surface area, or at creating large volumes of rubble.

There is no consensus in the literature⁷ whether simultaneous or sequential explosions are more effective at fracture production; all the work we found was concerned with blasting in proximity to a free surface. However, we are specifically interested in the situation for which a free surface is absent. A one-dimensional, spherical computer-code study⁸ has indicated that sequential shots with long delays would be better for producing damage in a large volume of rock. In the one-dimensional study, the enhancement occurs because the first shot is assumed to create a damaged zone with degraded wave propagation properties, which then produces a partial tensile (damage-producing) reflection of the stress wave from the second shot. These results are shown in Fig. 3. A two-dimensional study⁹ with short delays (~ms) has shown little effect. In this study, it is uncertain whether the two-dimensionality or the short delays inhibit the enhancement effect. In either case, the code calculations have questionable application to the laboratory experiments because they use only a continuum damage model that does not predict the extent or effect of large fractures. This limitation would make an experimental study desirable since we can use the results to check the existing models, and those models now under development that attempt to include improved descriptions of fracturing effects.

Material and High Explosive

Since we are primarily interested in fracturing, we decided to work initially with a transparent brittle plastic. PMMA (plexiglass) was considered first, but we are now using polystyrene cast at LLL. We use this material because it can be cast in the desired size of 254-mm diam by 254-mm high cylinders, and because it is probably the most brittle of the common cast plastics. The high explosive charge was that previously discussed, 50%/50% PETN/Cab-O-Sil mixture in a cylinder 7-mm diam by 11-mm length. We fired a number of preliminary shots. From the fracture patterns of these shots, it was evident that the fracturing depended more on the locked-in stress in the plastic (the plastic is rapidly cooled after casting) than on the detonation pattern or timing. Decreasing the cooling rate to 2.5°F/h (1.39 K/h) greatly reduces the locked-in stress according to photoelastic test results. It now appears that the detonation pattern exerts the dominant effect on the fracture pattern. Nevertheless, it will be necessary to quantitatively interpret the photoelastic pictures to ascertain the intensity of the static stress/strain field before we can conclude that this effect is negligible.

Fig. 3. SOC code calculations. Inelastic shear strain ϵ_f (damage predictor) vs radial distance R from spherical charge in coal.

Fracture Experiments

The cast polystyrene blocks are immersed in a liquid of matching impedance, sodium silicate, to minimize surface reflections. This is a simple precaution, though perhaps not necessary, since a test shot without the liquid did not noticeably change results.

Aside from our preliminary work described above, six experiments were completed by April, 1977. We used shot spacings of 25, 37.5, and 75 mm, and fired shots simultaneously and with a delay of several minutes. We made the following preliminary observations as shown in Fig. 4(a) and (b):

1. In the plastic, simultaneous detonation is more effective than sequential detonation in linking the two emplacement holes with fractures at charge spacings of 25 and 37.5 mm. At 75 mm, the charges do not interact. There is essentially no permanent cavity growth, so in terms of the 7-mm cavity diam, connection is achieved at intercharge spacings of 3.6 and 5.4 cavity diameters, but not at a spacing of 10.7 cavity diameters.
2. The direction of large fractures is strongly dependent on the orientation of the emplacement hole. All large fractures are nearly parallel to the hole axis.
3. Fracturing consists of a few long fractures and several much shorter ones.

From these preliminary experiments in plastic, we conclude that two charges fired simultaneously create a pattern of large fractures similar to the pattern created by cavities that are simultaneously and statically pressurized. That is, maximum tensile stress is generated in a plane parallel to the holes and directly between them; thus, the largest fracture propagates along this plane. For sequential shots, the large fractures caused by the first shot are only in the plane of the axis of that hole; they do not necessarily "see" the other hole. With the second shot, fractures produced by the first shot mask the presence of the first hole from the second. Thus, the second shot fractures do not necessarily connect the two holes. In other words, the stress concentration in simultaneous shots occurs in the plane of two holes so that the fracture pattern responds to the double hole geometry; in sequential shots, an extreme stress concentration from the explosion only occurs at one location.

Fig. 4. (a) Polystyrene simultaneous shot. Note complete splitting between holes. (b) Polystyrene sequential shot. Note lack of charge interaction. Hole with long fracture was shot first.

These conclusions do not support the code calculations for fracture damage. This is not surprising since the experiments create a few large running cracks, while the existing codes calculate stress, strain, and damage in a continuum sense, which precludes the calculation of large fractures. However, the disagreement between our experiment and calculations is not complete. Code calculations¹⁰ of the radius of effect in rocks as compared to the spherical explosion cavity radius indicate intense fracture to ~2 cavity radii, and some damage to ~4 radii. This is consistent with the fracture patterns for single shots in plastic, a result indicating that only the interaction, which is dominated by large cracks, is incorrectly calculated for plastic.

The experiments described above have not yet been carefully repeated, nor have the effects been quantitatively measured. Furthermore, the plastic used is very homogeneous in comparison to typical rock. Rock is not homogeneous since it has numerous compositional changes and inherent microcracks on the scale of the fractures that are created. If these small-scale inhomogeneities inhibit explosive-induced fracture size, and large fractures are only a coalescing of small ones, then enhanced damage could indeed be produced by sequential firing. Further investigations are necessary before final conclusions about this can be drawn. In particular, rock permeability, not only its fracturing, needs to be studied. Nevertheless, if more careful experiments confirm these results, and if the results also apply to rock, we may draw the following tentative conclusions:

1. For interconnecting cavities, it is better to fire explosives simultaneously than sequentially, provided the cavities are close enough together to permit connection.
2. Far from the charge, all fracturing is confined to a few large and long radial cracks. If this also happens for field shots in coal and rock, then the permeability may be strongly dependent on the exact measurement location, whether near or far from one of the few large fractures. Thus, permeability would not be uniform about the charge and could not be scaled with any simple distance scaling method. This type of effect could explain the apparently inconsistent results of the Kemmerer and Hoe Creek field fracturing experiments in coal.¹¹ The observed permeability enhancement and fluid flow rates after the shot were much greater at Kemmerer than at Hoe Creek; perhaps at Kemmerer we could see the fluid flow in the few large fractures, whereas at Hoe Creek we could not.¹¹

To complete the experimental work in plastic, further tests are necessary. We now plan twelve more shots; each will be in an identical, slow-cooled plastic block. For each sample, we will determine photoelastically the built-in strain both before and after shot. We will also take photographic and crack count records of the samples. Fluid flow measurements will be made to determine crack interconnection. The twelve shots will consist of six each at 25-mm and 38-mm spacing. The first two shots in each group of six will be at zero delay (simultaneous) and large delay (several minutes). These will be careful repeats of the existing shots. Of the remaining four shots, two each will be at 10 and 20 μ s delays. These delays are comparable to crack transit times between holes, and could cause significant changes. This sequence will complete this study. In our future work, we will use rock instead of plastic.

EFFECT OF INTERMEDIATE STRAIN RATE ON STRENGTH

The purpose of this subproject is to investigate the effects of intermediate strain rate ($\dot{\epsilon}$, to $10^2/s$) on rock strength and fracture. These results will eventually be incorporated into computer models to better predict the influence of high-rate and short-duration loading upon fracture. The maximum strain rate of these experiments is called intermediate because it is less than that caused by intense shock wave loading (10^4 to $10^6/s$) but more than that normally produced in the quasi-static laboratory tests ($10^{-4}/s$). However, this intermediate rate regime is important to the loading and unloading effects associated with the production of explosive fracture. The apparatus for these experiments is shown in Fig. 5, and it is described in more detail elsewhere.¹² It is a cam-loaded, axial-piston, radial confining pressure vessel. Although problems with reverberations may present difficulty at higher strain rates, this apparatus allows routine measurements of both axial and radial strains at intermediate strain rates, something not easily done previously.

The first material we studied with this apparatus for the Rock Mechanics Project was Indiana limestone. It is a homogeneous, relatively strong, somewhat porous (~14%) rock that is easy to work with. Runs were made in the

Fig. 5. The intermediate strain rate apparatus.

intermediate strain rate apparatus at confining pressures of 0.0001 and 0.012 GPa. We completed 18 tests using strain rates of 10^{-2} , 1, and 10^2 /s. Samples are right circular cylinders, 25.4-mm in diam and length, and are sealed (jacketed) by several thin coats of epoxy. Microscopic inspection indicated that penetration of the epoxy into the limestone is small. Longitudinal and radial strain gauges are attached to the epoxy jacket; sample ends are cemented to the pistons. This design constrains the samples so there is no motion at the piston-sample interface during deformation.

Test results using the intermediate strain rate apparatus showed that failure at all pressures and strain rates was brittle, with cones of material (with included angle ~ 2.1 rad) remaining attached to the pistons. Material not part of the attached cones was broken into relatively small fragments.

Typical plots of axial stress σ_1 vs. time are shown in Fig. 6 and 7 for each combination of pressure and strain rate $\dot{\epsilon}$. The stress-time data taken at strain rates of 10^{-2} /s and 1/s show a steady increase in stress with time to some maximum stress, followed by a sudden drop in stress as the sample fails. At a strain rate of 10^2 /s when a confining pressure of 0.012 GPa is applied, the stress increases step-wise. These steps are approximately 1.5×10^{-4} s apart. This is about 20 times greater than the stress-wave transit time through the sample.

Fig. 8 is a plot of axial stress at failure vs. strain rate $\dot{\epsilon}$ for confining pressures of 0.0001 and 0.012 GPa. There is relatively little variation in these data. The axial failure stress of 0.05 GPa at strain-rate of 10^{-2} /s and a confining pressure of 0.0001 GPa is in reasonable agreement with the quasi-static value of 0.06 GPa obtained by Heard et al.¹³ We calculated $d\sigma_1/d\dot{\epsilon}$ to be 0.0054 GPa at a confining pressure of 0.0001 GPa, and to be 0.0027 GPa at a confining pressure of 0.012 GPa. That $d\sigma_1/d\dot{\epsilon}$ is two-fold smaller at a confining pressure of 0.012 GPa than at 0.0001 GPa is somewhat surprising. However, if we use only the data at $\dot{\epsilon} = 1$ /s and at 10^2 /s, along with Heard's value of $\sigma_1 = 0.06$ GPa, we obtain a $d\sigma_1/d\dot{\epsilon}$ close to that obtained at a confining pressure of 0.012 GPa. Therefore, the observed change of $d\sigma_1/d\dot{\epsilon}$ with confining pressure may not be significant; additional measurements are needed.

Fig. 6. Intermediate strain rate experiments on Indiana limestone. Stress as a function of time at three strain rates ($\dot{\epsilon}$) at 0.0001 GPa confining stress.

Fig. 7. Intermediate strain rate experiments on Indiana limestone. Stress as a function of time at three strain rates ($\dot{\epsilon}$) at 0.012 GPa confining stress.

Fig. 8. Intermediate strain rate experiments on Indiana limestone. Axial stress at failure for three strain rates and two confining stresses. Axial stress at failure increases with strain rate.

The longitudinal strain calculated from piston travel at maximum σ_1 is plotted vs $\dot{\epsilon}$ for both confining pressures in Fig. 9. This strain increases both with strain rate $\dot{\epsilon}$, and with confining pressure. The increase with pressure was expected, but the increase with strain rate was not. It appears that the increase in maximum σ_1 with strain rate and pressure is due mainly to this increase in strain; that is, the effective longitudinal deformation reaches higher values at higher strain rates before gross failure of the sample occurs. The longitudinal strains at failure indicated by the longitudinal strain gauges are also shown in Fig. 9; these strains indicated by the strain gauges are only about 10% of the corresponding strains at maximum σ_1 , as calculated from the piston displacements. This large difference is present even though both strains occur at the same time. This result is consistent with the observed failure mode in the porous limestone. As the sample center is wedged out, as illustrated in Fig. 10, the piston has a large displacement, which is accounted for by slip on the angled shear cones in the sample. The strain gauges do not sense the strongly inhomogeneous and inelastic strain in the sample.

An example of the radial and longitudinal strain gauge traces is shown in Fig. 11 for $\dot{\epsilon} = 10^2/\text{s}$ and a confining pressure of 0.0001 GPa. It is not possible to tell from the data in Fig. 11 exactly when the radial gauge fails, but the sample fails at 3.25×10^{-4} s. In all tests, a Poisson's ratio of ~ 0.3 is calculated from the strain gauge outputs, with strain readings taken at a time equal to 1/2 the time at failure.

We will extend our work to materials of different porosities and strengths, using higher confining pressure. We expect that we can correlate the results of this study with those of our study of the effects of confining pressure on explosive fracturing.

PLASTICITY MODEL FOR POROUS ROCK

The purpose of this subproject is to improve our capability for the computer modeling of pore collapse as a response of porous rock to static stress and stress waves. This section is longer than the others in this report because it represents completed work rather than work in progress.

Fig. 9. Intermediate strain rate experiments on Indiana limestone. Strain at maximum axial stress vs strain rate.

Fig. 10. Failure mechanism for Indiana limestone. Deformation occurs in three stages: (a) homogeneous, (b) shear failure cones begin to form, (c) sample shatters. Piston senses displacement of failure cone while gauge does not.

Fig. 11. Strain gauge output as a function of time in Indiana limestone.

Upon application of "hydrostatic" stress (all principal stresses equal, no shear stresses present), porous rock deforms non-linearly. It is first stiff, then when the pores begin to collapse it becomes soft, only to become stiff again when most of the pores are removed.^{14,15} Generally, this behavior is irreversible. When shear stresses are present in the loading, collapse is enhanced; i.e., the onset of collapse occurs at lower stress, and the initial pore collapse occurs on an even softer trajectory than the hydrostatic. This behavior is illustrated in Fig. 12. When shear stresses become comparable to the mean stress, most rocks will begin to exhibit dilatant and tensile failure behavior. This behavior tends to increase porosity rather than decrease it. The model presented here does not attempt to accurately represent anisotropic dilatancy and tensile failure by itself. This will be done in a future study.

Compaction modeling is a part of a larger overall study designed to improve computer modeling of rock. Included in the overall study are:

- pore compaction
- dilatancy — tensile failure
- strain rate
- discontinuous media
- properties (permeability, electrical conductivity, etc.)
- scale effects (field vs laboratory correlations)

This progress report and articles in preparation essentially conclude the pore compaction phase of the study. Work in the immediate future will concentrate on dilatancy, tensile phenomena, and strain rate.

Background

Current models for computer codes at LLL^{2,3} assume non-linear irreversible mean stress vs volume loading and unloading. For shear stress vs shear strain we use an elastic, modified Mohr-Coulomb plasticity model. Tensile failure is treated separately by a stress-limiting scheme. There is no coupling between inelastic volume and inelastic shear behavior except for the crack opening due to tensile failure. The material parameters or parameter curves used are:

- initial density (1 parameter)
- elastic Poisson ratio or shear modulus (1)
- loading hydrostatic curve (2)
- unloading hydrostatic curve (1)
- failure curve (2)
- failure rate constant (1)

Fig. 12. Representation of pressure vs volume strain in a porous rock, showing hydrostatic compaction and the enhancement effect in a triaxial test.

This list is simplified to give the essentials of the model. The number of material parameters necessary depends upon the complexity of the curves desired, but the smallest conceivable number of independent parameters that could represent a porous material is about eight. These are the numbers shown in parentheses above. Of these, all can be easily measured in the laboratory, although to accurately represent porous materials, it may be necessary to adjust the parameters. Two exceptions are (1) the Mohr-Coulomb type failure curve is difficult to define for a porous rock undergoing pore collapse; it is best defined for shear-dominated behavior and (2) the failure rate constant must be found by curve-fitting techniques. Even with their limitations, the existing models used at LLL probably represent, though not uniquely, the state-of-the-art for non-coupled continuum models of rocks.

A new model for enhanced rock compaction must (1) better represent behavior during pore collapse, (2) do so without adding many new parameters, (3) contain parameters that are easily measured and (4) not require extensive parameter adjustment to fit other data. The new model we present here has achieved objectives (1), (2), and (3), and in part, (4). It was developed from plasticity model concepts that were first introduced to earth scientists through work in soil mechanics. The best example of such work and the source of many of our ideas is the Cam-Clay model, as proposed by Schofield and Wroth,¹⁶ which accounts for volumetric compaction and strain hardening in porous soils. This model is based on an energy balance flow rule that equates the irreversible (plastic) work of deformation (including both volumetric compaction and shear deformation) to a frictional loss mechanism with a constant friction parameter.

The Cam-Clay model has been successful in describing shear behavior for many types of soils. However, it omits certain features needed for higher-strength rocks. Specifically, the model assumes that the material has no cohesion, and it ignores elastic shear strain. These assumptions are probably reasonable for noncohesive weak soils in which the plastic shear strains are typically far greater than any elastic component. In addition, the energy-absorption term in the Cam-Clay flow rule applies only to shear loss; it does not account for irreversible work due to hydrostatic deformation. This leads to an overprediction of plastic shear strains when shear stresses are small but compaction is occurring, a common situation in the loading of porous rock.

Previous efforts have been made to develop generalized plasticity models applicable to a variety of geologic materials. A widely-used approach is the "Cap Model,"¹⁷ which can also be traced back to the work of Schofield and Wroth. Cap models are quite versatile, but in their development, the original concept of relating flow to frictional work was lost. Versatility in the models has been achieved at the expense of simplicity. More than twenty parameters are usually required for a Cap Model fit, and there is no simple way to obtain these parameters.

New Model "Putah Petra"

A theory proposed by Cheney and Schatz¹⁸ attempts to overcome most of the disadvantages of the Cam-Clay and Cap models. The basic formulation stems from and is similar to the Cam-Clay model, but cohesion, elastic shear strain, and energy absorption terms associated with hydrostatic compaction are added. As shown in Fig. 13, a yield surface is defined in shear stress Y , mean normal stress P , and specific volume V space. The yield surface is given an initial position. If loading begins from a point inside that surface, behavior is elastic. If the yield surface is struck, behavior becomes elastic-plastic, and the Y - P - V point may move, but it must remain on the yield surface unless unloading occurs. A line drawn through the points of horizontal tangency to all yield curves in the Y - P plane ($\frac{dY}{dP} = 0$) has slope μ and is called the critical state line, as shown in Fig. 14. To the left of critical state, volume deformation is dilatant or strain-softening, and to the right, it is compactive or strain-hardening. At and to the left of the critical state line, deformation is unstable for stress control. That is, maintenance of shear stress leads to unstable shear "failure." Unlike the Cam-Clay and Cap models, flow normality is not assumed. Typical loading in the Y - P and P - V planes is shown in Fig. 14. Quantitatively, the new model allows a better fit to static test data than the previous models. In Fig. 15, we show data, a normal SOC model fit, and Putah Petra (PP) model fits to static triaxial data for a porous tuff. The SOC model has no enhanced compaction, so it cannot correctly describe any P - V curve except a hydrostat. Furthermore, for the static triaxial test, the SOC model can only be elastic and then fail completely, while the Putah Petra model can strain harden. Clearly, for static modeling of porous material, Putah Petra provides a significant improvement over the SOC model.

Fig. 13. Yield surface in the Putah Petra model. Inside the yield surface, behavior is elastic; on the yield surface, behavior is plastic with pore collapse or dilation and inelastic shear strains.

Fig. 14. Putah Petra model. Shear stress vs mean stress and volume vs mean stress behavior before and during yield for typical hydrostatic and triaxial tests,

Fig. 15. Putah Petra and SOC model fits to triaxial test data for Mt. Helen tuff: pressure vs volume and shear stress vs shear strain. Only PP matches enhanced compaction and strain-hardening effects. Confining stress is 0.25 GPa.

The material parameters for Putah Petra are:

- initial density (1)
- elastic Poisson ratio or shear modulus (1)
- virgin hydrostatic curve (2)
- elastic hydrostatic curve (1)
- critical state curve (2)
- rate constant (1)

As before, numbers in parentheses indicate the number of parameters needed for the simplest interpretation. Like the simplest SOC model, the Putah-Petra model requires eight parameters. Furthermore, it is easier to define the measurement of the yield parameters for Putah Petra than for the SOC model. The Putah Petra model can also be made to merge with conventional SOC-TENSOR behavior as the material porosity approaches zero. Two questions are now considered: Can the model be coded so that dynamic calculations can be made easily and efficiently, and do the results of these calculations fit data better than previous models?

Coding the Model

The incremental equations of the model for behavior during yielding are:

$$\frac{\Delta V}{V} = \left(\frac{\Delta V}{V}\right)^P - \frac{\Delta P}{K} ,$$

$$\Delta \epsilon = (\Delta \epsilon)^P - \frac{2\Delta Y}{G} ,$$

$$-P \left(\frac{\Delta V}{V}\right)^P - \frac{2Y}{3} (\Delta \epsilon)^P = \frac{1}{3} \mu P |(\Delta \epsilon)^P| + \alpha P \left(\frac{\Delta V}{V}\right)^P ,$$

$$\text{where } \alpha = 1 - \frac{4Y}{\mu P} + \left(\frac{2Y}{P}\right)^2, \frac{2Y}{\mu P} < 1$$

$$\alpha = 0, \frac{2Y}{\mu P} > 1$$

$$\left(\frac{\Delta V}{V}\right)^P = - \left(\frac{1}{K^*} - \frac{1}{K}\right) \Delta P_o ,$$

$$\frac{2Y}{\mu P} = \pm \ln \left(\frac{P_o}{P}\right) , \frac{2Y}{\mu P} \geq 0 ,$$

where $\frac{\Delta V}{V}$ and $\Delta \epsilon$ are volume and shear strain increments, P and Y are mean stress and shear stress, and P_0 is the intercept of the yield surface with the P -axis. The superscript p indicates plastic increments. K and G are elastic bulk and shear moduli that may be pressure dependent, and K^* is the equivalent modulus of the virgin hydrostatic loading curve. To account for cohesion, we simply substitute $P^* = P + \frac{C}{\mu}$ for P above, where C is the cohesion. These equations apply for small departures from equilibrium in a quasi-static sense. For dynamic yielding, where strain rate dependence is taken into account, we use a simple viscous model:

$$P' = P - \frac{4}{3} \frac{\tau}{\Delta \tau} \left| \frac{\Delta V}{V} \right| P ,$$

$$Y' = Y - \frac{\tau}{\Delta \tau} (\Delta \epsilon) P ,$$

where P' and Y' are the total (dynamic) stresses to be used in the code, τ is the rate time constant, and $\Delta \tau$ is the finite-difference time increment.

These equations are a set of non-linear, coupled equations. To incorporate them into the SOC code, we treat them as a set of algebraic equations for calculation of stress increments in each time increment. The strain increments and the previous stress state are assumed to be known. The algorithm chosen is based on a generalized Newton method for solving simultaneous equations. In our tests, problems that arose during code development were associated with transitions between material modeling regimes, such as the boundaries between elastic and plastic behavior and between porous and non-porous behavior. For large strain increment solutions, growing errors may develop due to the influence of second-order terms which are ignored in the incremental solution. To avoid this, we limited the size of the time-step that the code may take. In some cases, to further minimize instabilities caused by this kind of error, a large artificial viscosity was imposed on the problem. The effect of this will be discussed later. If this model is to be used extensively in the future, improved numerical techniques such as sub-cycling may be required to eliminate the need for small time steps and excessive artificial damping.

Choosing Model Parameters

The following is a somewhat simplified account of the method we used to choose the parameters for the Putah Petra model. Significant differences from the existing SOC model are mentioned in this discussion.

Density ρ

Initial density is a standard quantity, easily measured.

Poisson's Ratio ν (or Shear Modulus G)

This is a material elastic constant. When yielding occurs, it affects only the elastic strains. It is best measured by a test that introduces shear and compressive stresses, and extends into the collapse regime. We suggest a uniaxial strain test, where the ratio (or modulus) is determined by the initial part of the unloading path. This avoids problems in measurements at low stress.

Bulk Modulus K

This parameter represents the bulk elastic modulus of the material. Bulk modulus is best measured by an unloading hydrostatic test, and is found from $K = -V \frac{dP}{dV}$. Since there may be elastic pore recovery, K is not necessarily the same as the solid material modulus. We ignore reverse yielding effects that usually occur at the low end of an unloading test. K may be pressure-dependent.

Plastic Loading Modulus K^*

This parameter represents the equivalent modulus of the hydrostatic loading curve during pore collapse. This curve is the "virgin loading curve" of soil models. The observed loading curve for a hydrostatic test begins as an elastic curve with modulus K , and then intersects the K^* curve at the onset of pore collapse. As with K , K^* is generally pressure-dependent.

Pore Collapse Onset P_{o_i}

This is the position on the pressure axis at the onset of pore collapse in a hydrostatic test. P_{o_i} is a measure of the position of the initial Y-P yield surface in the Putah Petra model.

Friction and Cohesion Parameters μ , c

These parameters represent the critical state line for shear failure in the model. In the existing SOC model, the equivalent of these is the shear failure curve. However, in standard SOC, for a strain-hardening porous material, ultimate shear failure is difficult to define. To obtain μ and c for the Putah Petra model, for a shear test where yielding is occurring (triaxial stress or uniaxial strain test), one plots the quantity Y/P vs $V-V^*$, where V^* is the volume the material would have if the test were hydrostatic. From the equations, it can be shown that this plot, during yielding, must have the slope

$$\frac{\mu}{2P\left(\frac{V^*}{K^*} - \frac{V}{K}\right)},$$

where V^*/K^* and V/K are found from the loading and unloading curves, respectively, at pressure P . From this equation, μ may be found. Cohesion c is found by comparing yield onset for two tests, to determine the yield surface offset; it may also be estimated since it is usually not an important parameter except in the low-pressure brittle and tensile regime, where this model is not in itself accurate.

Rate Constant τ

This constant is chosen by a trial and error fit to gas gun data.

In conclusion, the method of parameter choice for Putah Petra is not greatly different from that for the current SOC.

Results

We chose two materials for our calculational tests of the model; both are dry, porous rocks. Our first choice, Mt. Helen Nevada tuff,¹⁹ is perhaps the only high-porosity rock with fully comprehensive suites of both static and dynamic data. Mt. Helen is a fine-grained zeolitic tuff with a dry density of about 1.48 g/cm^3 and a porosity of approximately 0.362. Three-dimensional stress-strain measurements were obtained for Mt. Helen Nevada tuff in triaxial loading at confining pressures to 0.25 GPa. Measurements of the uniaxial strain loading and unloading paths were also taken. Our second choice, Indiana limestone,¹³ is a spargenite with a total calcium carbonate content of 90 to 95%. The dry density is about 2.29 g/cm^3 , and the porosity is estimated to be about 0.16. The loading hydrostat, some unloading data, and ultimate failure envelopes were measured for Indiana limestone. No triaxial or uniaxial strain loading paths were measured.

Basic model parameters adopted for Mt. Helen tuff and Indiana limestone are listed in Table 1. Curves for K and K* are shown in Fig. 16. Since no definitive shear loading path measurements are available for Indiana limestone, the critical-state parameters were estimated. The estimates are based on a qualitative comparison of the low-pressure strength measurements for limestone and tuff, which indicates that the limestone is somewhat stronger and that its critical state line is significantly steeper than for the tuff. The elastic Poisson's ratio for the limestone is derived from low-pressure ultrasonic data.

Table 1. Parameters for tuff and limestone

Material	Density, g/cm ³	Poisson's ratio ν	μ	C, GPa	P_{o_i} , GPa	τ , μ s
Tuff	1.48	0.25	0.90	0.005	0.15	65
Limestone	2.29	0.30	1.16	0.010	0.20	—

Two kinds of experimental data will be compared with calculated results: gas gun planar plate impact tests, and spherical-charge high-explosive shots. In both cases, velocity waveforms were measured using in-material electromagnetic velocity gauges. All test data were obtained at LLL by Larson and his coworkers.²⁰ As described above, the model parameters were determined only from static laboratory testing, with the exception of the rate constant, which was fit to the data obtained from the gas gun experiments. The range of strain rates and sample sizes covered here constitutes a severe test of the code models.

All of the measured waveforms to which we will compare code output are for particle velocity vs time at a gauge position. The gas gun shots cause the sample to load and unload in one-dimensional strain, so the strain geometry is simple; in the high explosive shots, however, a spherical, expanding wave is created. Not only does the amplitude decrease with distance from the source, but the loading geometry changes with time and position. The spherical high-explosive tests are probably the best currently available high-quality data for checking a one-dimensional code.

Fig. 16. Pressure vs volume strain for Indiana limestone and Mt. Helen tuff. K and K^* are effective bulk moduli obtained from slope of loading and unloading curves, respectively.

Mt. Helen Tuff

We chose a single gauge measurement from one typical planar plate impact test for careful study. Measurements from the test are typical of high-quality data. In this test, the impact projectile is PMMA at a velocity of 0.634 cm/ μ s, producing a peak stress of ~ 0.9 GPa in the tuff. Figure 17 shows velocity vs time data compared to the calculations for SOC and Putah Petra (PP) models. The Putah Petra calculation included a rate constant of 65 μ s (Table 1). The PP model clearly fits the data better than SOC. However, without rate dependence (not shown), the PP result is not significantly different from SOC. This is not surprising because the stresses in this experiment quickly get out of the range of collapse onset, where SOC and PP models differ significantly. It has been previously shown that the type of rate dependence used in PP can also be used in SOC to fit these data.²¹ Thus, gas gun data in the loading regime do not provide a suitable test of the pore collapse model; however, it does demonstrate the rate dependence.

As shown in Fig. 17 (upper right), the arrival of the relief wave is late for both SOC and PP models. This is because both SOC and PP models are represented by a single curve to describe elastic and unloading behavior at all strain rates. A data-adjusted stiffer unloading curve (closer to grain-density stiffness) could provide a fit, but that was not our objective. A rate-dependent unloading model is probably needed.

Spherical high-explosive experiments tell us much more about pore collapse. In Fig. 18 we show data, SOC, and PP calculations for peak velocity vs distance. Figures 19(a) and (b) present the corresponding velocity vs time waveforms at five gauge locations. Rate dependence was included in our calculations, but it does not have a noticeable effect at these rates. Close to the source, there is not a significant difference between SOC and PP. The PP velocity vs time waveforms are spread out because of the unusually large artificial viscosity; by making comparison runs, we have determined that this effect reduces calculated peak heights by 10% in the region shown here. The PP model calculates peak heights very well, considering the 10% artificial viscosity loss. This seems to indicate the presence of a physical dissipative effect.

Fig. 17. Velocity vs time for Mt. Helen tuff gas gun shot experiment. Rate dependence is necessary to fit data.

Fig. 18. Peak particle velocity vs distance (normalized radius) for spherical high-explosive shots in Mt. Helen tuff. Data compared with SOC and PP models.

Fig. 19 (a), (b). Particle velocity vs time for five different gauge locations in Mt. Helen tuff. Figure shows change of wave shape and compares SOC and PP model calculations with data.

At intermediate and long ranges, differences between the SOC and PP models become obvious (Fig. 18). The SOC calculations become elastic by a radius ratio of about eight ($R/R_0 \sim 8$). This is a much closer range for the beginning of elastic attenuation (proportional to R^{-1}) than the data would indicate. PP closely resembles the data by not becoming elastic until much later. This difference is caused by the onset of compaction at lower pressure (shear-enhanced compaction) in the PP model. The data clearly support the presence of the enhanced compaction effect. Furthermore, the velocity data show clear evidence for an elastic-compaction precursor that is emerging and changing out to the distance of pore collapse onset at a pressure of about 0.03 GPa; this pressure is much less than the hydrostatic tests would indicate, but it is consistent with the shear-enhancement predicted by the PP model. However, the fit of PP is still not perfect. Most of the remaining problems are probably associated with ignoring crack closure at low pressures, with the uncertain quality of static data in the collapse onset regime, and with yet-unmodeled rate effects. More work is needed in these areas.

Indiana Limestone

Results for limestone are shown in Figs. 20 and 21(a) and (b). The results we obtained for the gas gun shot experiments are similar to those for tuff. For spherical high-explosive shot tests, the PP model shows good wave shapes, but too high an attenuation rate; the SOC model calculation using the measured hydrostat shows a better attenuation rate but grossly distorted wave shapes. We varied the parameters to achieve a better fit and found that the main difficulty for all models is related first, to the interpretation of quasi-static low-stress loading path measurements, and second, to the existence of a rate effect not presently accounted for by either model. An example of the measurement problem is shown in Fig. 22, a magnified view of the low-pressure hydrostatic measurements from a typical experiment. The cylindrical sample has four strain gauges attached to it, two that measure radial strain and two that read the tangential or circumferential strain. For pure hydrostatic loading, these strains should all be equal; but the individual gauges behave differently at low stresses, and a systematic difference exists between the radial and tangential gauges. There are no such inaccuracies at higher pressures after pore collapse begins and the gauges are fully seated. As with tuff, and more so with limestone, a major limitation is in our measurement of material behavior at low stresses (up to about 0.15 GPa) at quasi-static and intermediate strain rates.

Fig. 20. Peak particle velocity vs distance (normalized radius) for spherical high-explosive shots in Indiana limestone. Data compared with SOC and PP models.

Fig. 21. Particle velocity vs time for five different gauge locations in Indiana limestone. Figure shows change of wave shape and compares SOC and PP model calculations with data.

Fig. 22. Strain gauge output at low stress levels in hydrostatic test on Indiana limestone. Figure illustrates problem of measurements at low stress; material anisotropy is not large enough to cause this.

Conclusions

From this work, we conclude the following about porous rock plasticity modeling:

1. For problems where pore collapse onset is important, the Putah Petra model is more accurate than the SOC model now used.
2. The parameters for the Putah Petra model are easily chosen from data, or estimated when data are not available.
3. The Putah Petra model produces improvement primarily in the compaction-loading response of the material. Other responses such as unloading, dilatancy, tensional and extensional failure, and creep are not yet intended to be modeled by Putah Petra, although they are conceivably within the capacity of the model.
4. Code calculations with the Putah Petra model have so far used short time steps and large artificial damping. Revised numerical techniques are needed to correct this problem.
5. A major problem lies in the interpretation of low-stress data for choice of model parameters. This problem affects *any* computer model.

This work concludes the main part of plastic pore collapse modeling effort by the Rock Mechanics Project. On demand, some additional work may be done to improve the computation technique, but only minor work is now planned to:

- incorporate the effects of water
- investigate the possibility of producing a simple but equivalent model to account for the most important part of Putah Petra — the effect of shear stress on the onset of compaction.

SUMMARY

Work in the Rock Mechanics Project is proceeding in two areas: explosive fracture effects, and continuum wave propagation effects. In studies of fracturing, we have concluded that a system dominated by large fractures cannot be modeled by current continuum models; how extensively we can use continuum models, and how they can be modified to account for fracture are problems still needing work. In wave propagation, enhanced compaction effects are observable and capable of being modeled by a generalized plasticity model. However, rate effects and problems with low-pressure data remain very important.

It is important for the future to improve our knowledge of fracturing and the properties associated with it, both experimentally and theoretically, and to consider how the continuum codes and models of finite discontinuities can be combined to provide more generalized descriptions of material behavior.

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APPENDIX: REVIEW PANEL DISCUSSION

On March 17, 1977, the review panel met. The presentations covered much of the work presented in this report. Considerable discussion took place both during and after the presentations. Most of the specific suggestions that arose during the discussion have now been incorporated into the work reported here; however, some general comments and suggestions arose:

- The stresses, strain rates, and boundary conditions of each experiment need to be measured and carefully recorded so that we can make good assessments of the causes of phenomena, errors, and goodness of fit of proposed models.

- For this project, work needs to be goal-oriented if it is to assist programs at LLL. This does not mean that it needs to be specific work for a project, but only that general investigations with no time limit should be avoided.

- A general policy should be established to do as much micro and macro characterization of rock structure as possible since it is now evident that rock properties are determined largely by their structure.

- The effect of long, macroscopic fractures as compared to continuum fracturing effects must be investigated, particularly with regard to its relation to fluid flow properties.

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