EFFECTS OF TEMPERATURE AND PRESSURE ON FAILURE AND POST-FAILURE BEHAVIOR OF WESTERLY GRANITE

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Received 2 December 1980; revised 9 March 1981

Failure and post-failure behavior of Westerly granite were investigated at pressures up to 400 MPa and temperatures up to 700°C in an internally heated gas apparatus. 95 experiments were performed. For dry samples, the effects of temperature and strain rate on the failure stress are not significant relative to that of pressure. Some preliminary data indicate that the effect of water can be more pronounced at elevated temperature than at room temperature. At pressures above 80 MPa, crack morphology changes induced by thermal cracking have no effect on fracture strength.

Wawersik's manual control technique was adapted to high temperature, and a range of class I and II post-failure behavior was observed. Temperature was shown to be a stabilizing factor. Preliminary post-creep failure experiments show that the post-failure behavior depends on loading history. The behavior is more stable at a lower differential stress and a slower strain rate. The data are analyzed in terms of subcritical crack growth by stress corrosion. In light of previous room temperature post-failure and creep results and our analysis, we suggest that details of the post-failure curves are sensitive to statistical variation among samples.

1. Introduction

Brittle fracture of crustal rock is unquestionably one of the most studied processes in the laboratory. The failure stress or fracture strength is traditionally taken as the single parameter that is sufficient to characterize the fracture process; its dependence on confining pressure, strain rate, pore fluid, sample size and shape, and loading path have all been extensively investigated, and most of the results were comprehensively reviewed by Paterson (1978). Such studies, though essentially empirical in approach, provide much of the basis for applied rock mechanics in engineering and mining, as well as for the analysis of geologic faulting.

In order to have a better understanding of crustal processes, it is desirable to consider in greater detail the mechanics of brittle rock deformation. Even if one takes a phenomenological approach, to formulate a reasonable constitutive relation requires quite detailed knowledge of both pre- and post-failure behavior (Rudnicki and Rice, 1975) over the range of crustal pressure and temperature. In contrast to the vast number of studies on the effect of pressure carried out at room temperature, the combined effect of temperature and pressure on failure and post-failure behavior is relatively unknown. We decided therefore to investigate this problem in some detail for one of the most studied rocks, Westerly granite.

It is generally accepted that in the brittle regime, the failure stress is relatively insensitive to temperature. The classic work of Griggs et al. (1960) remains the major source of data, although the experiments were performed at only one pressure (500 MPa). With recent interests in rock mechanics application at high temperature environment for radioactive waste disposal and geothermal projects, it would be useful to be able to extrapolate to a lower pressure range. Studies on ceramics (Paterson and Weaver, 1974), sedimentary rocks (Handin and Hager, 1958), and serpentinite (Raleigh and Paterson, 1963) show that as long as one stays in the brittle field, the temperature dependence of the failure stress is minimal, and is about the same at different fixed pressures. Recent data show, however, that extensive thermal cracking can occur at low pressure (van der Molen, 1981; Wong and Brace, 1979). Would thermal cracking result in a stronger temperature dependence at the lower pressure range? The room temperature data suggest that effects of both strain rate and pore fluid are small (Brace and Jones, 1974). To what extent can we expect this to hold at high temperature?

Some post-failure studies have been done on silicate...
rocks under pressure (Wawersik and Brace, 1971; Hojem et al., 1975; Rummel et al., 1978), but to our knowledge the effect of temperature on post-failure behavior has not been investigated. Griggs et al.'s (1960) data indicate that the effect of temperature is to stabilize the post-failure response, and Wawersik and Brace (1971) show that for pressures up to 150 MPa at room temperature, both Westerly granite and Frederick diabase exhibit the unstable class II behavior. What are the role of temperature and pressure on the transition from class II to class I behavior?

In an attempt to clarify some of these questions, we carried out a total of 95 experiments on the failure and post-failure deformation of Westerly granite at temperatures up to 700°C and pressures up to 400 MPa. The effects of temperature and pressure, and in lesser detail, the effects of strain rate and pore fluid were examined, and the results are presented below.

2. Apparatus and procedure

The apparatus and sample configuration are similar to those used by Goetze (1971) and Stesky et al. (1974). Two sample sizes were used. The specimens used at higher pressures and temperatures where we expected the failure process to be unstable were 35 mm long and 19 mm in diameter, and were jacketed directly with 0.32 mm thick annealed copper tube (fig. 1). Spacers of tungsten carbide were placed adjoining the specimen to smooth out the temperature distribution: they were followed by polycrystalline alumina (Lucalox). A thermocouple reached the base of the sample through a hollow plug and an axial hole in the carbide and alumina spacers.

With the above set-up, the amount of fault displacement accompanying the instability may be sufficient to rupture the copper jacket if an unstable failure occurs: the pressure medium, which is argon at high pressure and temperature, will then rush in and blow the thermocouple out. Therefore, for pressure and temperature conditions at which such danger existed, the sample diameter was reduced to 16 mm, and a combination of graphite sleeve (1.3 mm thick) and copper jacket was used. As discussed by Brace and Byerlee (1970) and Stesky et al. (1974) the graphite "blunts" the edge of the fault, and allows a sliding displacement up to about 2.5 mm. The graphite sleeve can support a finite differential stress (Paterson and Edmond, 1972), and the copper and graphite together contribute an error of about 5 MPa in the confining pressure and a maximum systematic error of 50 MPa in the failure stress (Stesky, 1975).

The sample was heated by an internally wound furnace surrounding the copper jacket. To minimize convection in the argon pressure medium, powdered boron nitride was placed between the copper jacket and the furnace wall. The temperature profile along the axis of the specimen was described by Goetze (1971) and Stesky et al. (1974) for each of the sample configurations; the maximum difference in temperature is about 25°C at 700°C and less at lower temperatures. The temperature quoted for the experiments below are values at the bottom of the specimen recorded by the thermocouple.

Pressure was monitored with a Heise gauge, and maintained constant to within 1 MPa. All experiments were performed 'drained' with the bottom end vented to atmosphere. The axial load was measured with an external load cell with a probable error of about 2%. The displacement was measured outside the pressure vessel with a differential transformer (DCDT) mounted between the moving piston and the fixed lower platen. Measurement were accurate to within 1%. Elastic distortion of the loading system was 0.25 GNm⁻¹ (2.5 × 10⁵ kg/cm); this was subtracted from the apparent
displacement recorded from the DCDT for calculation of the axial strain.

The axial force was provided by a piston driven at a constant displacement rate by a ball-screw mechanism. For the fracture experiments, two displacement rates \(1.1 \times 10^{-2} \text{ mm s}^{-1}\) and \(9.5 \times 10^{-4} \text{ mm s}^{-1}\) were used. The actual values of the strain rates change as the axial load is increased. For example, at 400 MPa and 600°C initial Young's modulus of the sample was 50 GPa, and hence the initial strain rates for a graphite-sleeved sample were \(1.4 \times 10^{-4} \text{ s}^{-1}\) and \(1.3 \times 10^{-5} \text{ s}^{-1}\) respectively; as the stress increased, the sample became softer and took up a larger proportion of the imposed deformation, such that at peak stress effective stiffness was zero, and the corresponding strain rates had increased to \(3.1 \times 10^{-4} \text{ s}^{-1}\) and \(2.7 \times 10^{-5} \text{ s}^{-1}\). For convenience, we shall refer to these strain rates simply as \(10^{-4} \text{ s}^{-1}\) and \(10^{-5} \text{ s}^{-1}\) respectively.

For the post-failure studies, the technique developed earlier (Wawersik and Brace, 1971) was used. Before failure, the piston advanced with the constant displacement rate of \(9.5 \times 10^{-4} \text{ mm s}^{-1}\) (strain rate of about \(10^{-5} \text{ s}^{-1}\)). At and beyond the failure stress, the load was applied with the same rate with the operator closely watching the force-displacement display, and the load was quickly reduced manually as soon the strength showed sign of dropping. By repeating the process, one obtained from the envelope of these loading-unloading cycles a smooth curve in the post-failure region. Except for the experiments at high temperatures (above 420°C at 400 MPa, and 550°C at 250 MPa) for which the sample failed stably, the post-failure curves given here were from stress-strain envelopes obtained by this procedure.

3. Observation

3.1. Failure data

At a pressure up to 400 MPa and a temperature up to 700°C the differential stress supported by a sample always attained a maximum value, after which the sample either experienced a stable stress drop, or else fractured catastrophically. This maximum value, defined to be the failure stress, was determined for Westerly granite in three series of experiments at fixed pressures (400 MPa, 250 MPa, and 80 MPa). The samples deformed at 400 MPa were cored from the same block, but in an attempt to examine possible influence of moisture, two different procedures for sample preparation were used: the 'room dry' samples were left exposed to laboratory atmosphere after being ground to the desired dimensions, whereas the 'vacuum dry' samples (together with the graphite sleeves) were dried overnight in vacuo at about 80°C. Two series were deformed at the lower pressures of 250 MPa and 80 MPa. Two series were deformed at the lower pressures of 250 MPa and 80 MPa. The data are compiled in table 1 and plotted in fig. 2. To investigate the pressure dependence at a fixed temperature, additional runs were made at different pressures with temperature fixed at 150°C (table 2). The data are compared with room temperature fracture data of Byerlee (1967) in fig. 4. Note that the data compiled in table 2 are all for 'vacuum dry' sam...
Fig. 3. Comparison of data on failure stresses of Westerly granite at temperature and pressure.

Table 1

<table>
<thead>
<tr>
<th>Sample</th>
<th>(T^\circ C)</th>
<th>(\Delta\sigma_1 - \Delta\sigma_3) (GPa)</th>
<th>Jacket</th>
<th>Strain rate (log s(^{-1}))</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>HTW 54</td>
<td>20</td>
<td>1.66</td>
<td>G</td>
<td>-5</td>
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<tr>
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<td>154</td>
<td>1.62</td>
<td>G</td>
<td>-5</td>
<td></td>
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<tr>
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<td>1.53</td>
<td>G</td>
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<td></td>
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<tr>
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<td>1.54</td>
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<tr>
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<td>C</td>
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<tr>
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<td>C</td>
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<tr>
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<tr>
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<td>507</td>
<td>1.34</td>
<td>C</td>
<td>-5</td>
<td></td>
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<tr>
<td>HTW 0</td>
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<td>1.30</td>
<td>C</td>
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<tr>
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<td>1.26</td>
<td>G</td>
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<tr>
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<td>C</td>
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<td>C</td>
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<tr>
<td>HTW 28</td>
<td>680</td>
<td>1.03</td>
<td>C</td>
<td>-5</td>
<td></td>
</tr>
</tbody>
</table>

\(^a\) G: graphite sleeved and copper jacketed.
C: directly copper jacketed.
Table 2
Other failure stress data ('vacuum dry')

<table>
<thead>
<tr>
<th>Sample</th>
<th>Pressure $\sigma_1$ (MPa)</th>
<th>$T$ (°C)</th>
<th>Failure stress $\sigma_1 - \sigma_1$ (GPa)</th>
<th>Comments</th>
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<td>20</td>
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<td>20</td>
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<td>155</td>
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<tr>
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<td>250</td>
<td>302</td>
<td>1.20</td>
<td></td>
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<tr>
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<td>0.74</td>
<td></td>
</tr>
<tr>
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<td>668</td>
<td>0.62</td>
<td>Directly jacketed in copper</td>
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<td>20</td>
<td>0.79</td>
<td></td>
</tr>
<tr>
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<td>20</td>
<td>0.81</td>
<td></td>
</tr>
<tr>
<td>PFW 22</td>
<td>80</td>
<td>150</td>
<td>0.74</td>
<td></td>
</tr>
<tr>
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<td>350</td>
<td>0.70</td>
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</tr>
<tr>
<td>PFW 28</td>
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<td>551</td>
<td>0.66</td>
<td></td>
</tr>
<tr>
<td>PFW 30</td>
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<td>659</td>
<td>0.59</td>
<td></td>
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</tr>
<tr>
<td>HTW 51</td>
<td>150</td>
<td>153</td>
<td>1.15</td>
<td></td>
</tr>
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</table>

Note: All experiments were performed on graphite-sleeved samples at $10^{-5}$ s$^{-1}$ unless otherwise noted.

Fig. 4. Comparison of pressure dependence of failure stresses at 150°C and at room temperature. Typical error bars are shown.

To explore the effects of thermal cracking, we performed three fracture experiments on samples pre-heated overnight in a furnace at room pressure. One sample was heated to 300°C and deformed at 400 MPa and 422°C; two samples were heated to 400°C and then deformed at room temperature and pressures of 80 MPa and 250 MPa respectively. Strain rate was $10^{-5}$ s$^{-1}$. The data are included in tables 1 and 2, and fig. 2.

3.2. Post-failure data

Samples cored from a single block were used to study the post-failure behavior at two pressures (250 MPa and 80 MPa). All samples were pre-dried in vacuo. Data for four different temperatures at 250 MPa are shown in fig. 5. The two samples at 550°C and 668°C unloaded in a stable manner, but the manual control procedure discussed above had to be used at 150°C and 350°C to obtain the other two post-failure curves. Similar data at 80 MPa, all obtained by manual control, are shown in fig. 6. A room temperature curve at 80 MPa obtained by Wawersik and Brace (1971) is also included for comparison.

In a preliminary attempt to explore time-dependent effects, four creep tests were performed at differential...
stress at or above 90% of the failure stress. The samples were all pre-dried in vacuo. Loading was at a rate of $10^{-5}$ s$^{-1}$ up to a predetermined differential stress level, at which point the piston was stopped; the stress was maintained constant to within 1% as recorded on a chart recorder, and the piston reactivated whenever the stress had relaxed below this range. (Because of experimental error and sample variability, the ratio of the creep stress to the failure stress cannot be precisely determined. Some of the numbers quoted below were estimated by comparing the shape of the curves.)

The load-displacement curves of the creep tests are shown in figs. 8 and 9. The displacement shown is from the DCDT record directly, and it includes the elastic distortion of the testing machine. At 350°C and 250 MPa, a sample deformed at $10^{-5}$ s$^{-1}$ failed explosively; the post-failure curve obtained by manual control (fig. 8) was almost vertical, indicating that if the sample stiffness was slightly higher, the unloading would have been stable. The testing system itself therefore acted as a sensitive reference for slight increases in the post-failure softening slope, and it was for this advantage that we chose this temperature and pressure for most of the creep tests.

One sample (PFW17) was allowed to creep at about 97% of the failure stress. The creep curve showed the typical primary, secondary, and tertiary stages (fig. 7). At some point along the tertiary creep stage, the relaxation was so fast that even with piston activated, the stress could not be restored to the fixed value, and it dropped to the residual value stably at a strain rate of $10^{-5}$ s$^{-1}$ (fig. 8). No manual control was necessary to maintain stability during the post-creep failure deformation; the unloading slope was therefore clearly gentler than the previous post-failure curve (of PFW 16) obtained by manual control. A second creep test (PFW 27) was first performed at 93% of the failure stress; the steady state creep rate was too slow, so the stress was increased to about 96% and held fixed again. The steady state creep rate was slower ($2 \times 10^{-7}$ s$^{-1}$) than before ($3 \times 10^{-6}$ s$^{-1}$), and the sample unloaded with a post-creep failure slope even gentler than PFW17 (fig. 8).

Two other tests at 350°C and pressures of 80 MPa and 250 MPa respectively were terminated before the tertiary creep stage was reached; the load was applied again at a strain rate of $10^{-5}$ s$^{-1}$ and both the specimens...
Fig. 7. Creep curve of a sample at 250 MPa, 350°C and at a differential stress about 97% of the peak stress.

Fig. 8. Comparison of post-failure and post-creep failure behavior at 250 MPa, 350°C. Note what is shown is the apparent displacement which includes elastic distortion of the test machine.

Fig. 9. Behavior of samples first under creep and then rapid loading at 350°C, and at pressures of 80 MPa and 250 MPa.
failed explosively at a differential stress close to that in the constant displacements rate experiments (fig. 9).

4. Discussion

4.1. Effects of strain rate and moisture

Experiments were performed at different temperatures and a fixed pressure of 400 MPa to examine the possible influence of strain rate and moisture on the failure stress. The ‘vacuum dry’ samples were deformed at two different strain rates and with different jacketing arrangements (table I). We have three sets of duplicate experiments at 350, 420, and 550°C. If we consider all the \(10^{-5}\) s\(^{-1}\) data in fig. 2 there seems to be a trend for the ‘vacuum dry’ graphite-sleeved samples (dark squares) to be slightly stronger than those ‘vacuum dry’ samples jacketed directly with copper (dark circles). If true, this is consistent with our previous error analysis that the graphite sleeve is capable of supporting a finite differential stress and can result in a systematic error. If one examines the copper-jacketed sample data in fig. 2 closely, one can also argue for a tendency for the \(10^{-4}\) s\(^{-1}\) data (open circles) to be slightly higher than the \(10^{-5}\) s\(^{-1}\) data (dark circles). Nevertheless, both effect, if real, would be within the experimental error. We conclude, therefore, that any strain rate sensitivity of the failure stress is also within our experimental error which, according to our error analyses and reproducibility, should be about 4% of the peak stress.

Brace and Martin (1968) examined the variation of room temperature fracture strength of Westerly granite (at 150 MPa) with strain rate, and their ‘dry’ data show an increase of about 10% in fracture strength over the three orders of magnitude in strain rate. Our results here for ‘vacuum dry’ graphite-sleeved samples (dark squares) did not show any difference in strength between ‘room dry’ and ‘vacuum dry’ Westerly granite within experimental error, which should be about 3%. We have no means of quantitatively estimating the water content in our ‘room dry’ samples, but it is definitely less than that in water saturated samples, and yet we observed a substantial effect of up to 30% at elevated temperatures.

Our temperature and pressure range is probably too low for possible mechanisms such as ‘hydrolytic weakening’ (Griggs, 1967) to be operative. The experiments were done by applying the confining pressure first, and then the temperature; if water was trapped in pores closed by pressure, the subsequent increase in temperature might then induce a high pore pressure, and the corresponding effective pressure decrease would result in a lower failure stress. To achieve such a large pore pressure effect, the permeability had to be low up to the point of failure.

On the other hand the strength reduction is also consistent with a ‘chemical’ effect. Crack growth in quartz was shown to increase with partial pressure of water (Martin, 1972). Crack tips in ‘room dry’ samples have better access to water vapour, and therefore at a given differential stress and temperature, can extend at a faster rate. An unstable configuration can be reached at a lower stress, and hence the ‘room dry’ samples would have lower failure stresses, the decrease being more pronounced at elevated temperatures because stress corrosion crack growth also increases with temperature.

It is clear from our preliminary data that the effect of water on strength is more complicated at elevated
temperatures. However, lacking further experimental data, a more involved discussion of possible mechanisms would most probably be mere speculation. More experimental work under controlled conditions on the effects of water and pore pressure is necessary before we can clarify the roles played by water.

4.2. Comparison with previous high temperature fracture data

To our knowledge there were three previous studies on failure of Westerly granite at elevated temperatures, all at pressures higher than or equal to 400 MPa. Stesky et al. (1974) performed seven fracture runs at 400 MPa and 10^{-5} s^{-1}. They used a setup similar to ours, but their data at temperatures above 300°C consistently fell between our ‘vacuum dry’ and ‘room dry’ data. Their samples were ‘vacuum dry’, but the graphite sleeves were ‘room dry’. The discrepancy between the two sets of data can not be explained by variability from block to block: we performed two runs with ‘room dry’ graphite sleeves, and the measurements are closer to Stesky et al.’s data than to our other data. Our measurements (at 400 and 500°C) together with Stesky et al.’s data are plotted in fig. 3 as the ‘room dry graphite’ data. The graphite we used has a porosity of about 25% and probably the strength reduction has to do with water squeezed out of the pores at temperature and subsequent weakening of the rock due to a pore pressure or a chemical effect.

Griggs et al. (1960) reported ten experiments at 500 MPa (fig. 3). They used a gas apparatus, and the strain rate was 5 \times 10^{-4} s^{-1}, except for a sample at 500°C which was deformed at 2 \times 10^{-3} s^{-1}. The latter sample failed at 1.19 GPa, which fell between the two 10^{-4} s^{-1} data points at the same temperature (fig. 3), suggesting that the strain rate sensitivity was within experimental error.

One usually expects the failure stress to increase with confining pressure, and its pressure dependence to decrease with temperature. In this sense, Griggs et al.’s data do not agree with our ‘vacuum dry’ data since above 400°C, their data are consistently lower although the confining pressure was higher. Sample variability can not explain the lower strength in samples from Griggs et al.’s block, since their measurements at 25°C is on the high side of existing room temperature fracture data (Ohnaka, 1973). On the other hand, our ‘room dry’ data seem to agree well with the trend at 500 MPa indicated by Griggs et al.’s data. The latter work did not report the drying procedure, but we know that the samples were not vented to the atmosphere. If the samples were not pre-dried in vacuo, they are closer to our ‘room dry’ samples in water content, and the agreement is understandable. It should be reiterated, however, that the role of water needs to be explored more thoroughly before drawing further conclusions.

A third set of data were by Tullis and Yund (1977a, b) deformed at 500 MPa and 10^{-6} s^{-1}. The samples were dried by heating to 300°C at room pressure. As shown in fig. 3, their data are lower than all the rest at 400 MPa or 500 MPa. It was pointed out that at temperatures below 700°C, the solid medium apparatus cannot resolve the difference between the frictional sliding stress and the fracture stress (Tullis and Yund, 1977a). It is unclear which quantity the data represent, and therefore no meaningful comparison can be made.

4.3. Effects of temperature and pressure

To explore the effects of temperature and pressure without possible complications of water, we performed three series of experiments at fixed pressures on ‘vacuum dry’ samples (fig. 2). At a fixed confining pressure, the failure stress decreases with temperature. For the pressures we considered, the reduction in strength is relatively small, all within 20% at temperatures up to about 500°C beyond which, however, an accelerated downward trend was observed. Qualitatively, clear cut faults were observed in post-failure samples even at the higher temperatures, but the localized zones appeared to be wider at higher temperatures.

Temperature tends to stabilize post-failure behavior. At a fixed pressure, stable post-failure deformation without manual control is possible if temperature is increased to a point at which the softening slope equals the machine unloading stiffness. The transition temperatures for stable failure were above 660°C at 80 MPa, 450–550°C at 250 MPa, and 350–420°C at 400 MPa for our set-up, and 25–300°C at 500 MPa for Griggs et al.’s (1960) set-up. Comparing these transition temperature values, one would conclude that the effect of confining pressure is to stabilise the post-failure behavior. (Note that Griggs et al.’s results would not be relevant for this comparison if their testing system was much stiffer than ours.)

A similar indication of this influence of the pressure is observed by considering the temperatures at which sharp changes occur in slope of the strength versus temperature curves in fig. 2: these temperatures characterize the transition from a regime of strong pressure dependence of failure stress to a regime of reduced pressure dependence and enhanced temperature sensitivity. As is evident from fig. 2, the effect of an in-
creased pressure is to shift the transition to a lower temperature. Microscopic mechanisms responsible for such a transition from the brittle to 'semi-brittle' field were studied in detail by Paterson and Weaver (1970) for polycrystalline MgO, and a review of relevant work in metallurgy and ceramics science was recently given by Carter and Kirby (1978). Additional microstructural studies are needed to clarify the question of the extent to which some of these proposed 'semi-brittle' mechanisms are applicable to crustal rocks at the temperature and pressure conditions of interest.

The effect of pressure is customarily interpreted in terms of the Mohr–Coulomb criterion (Handin, 1969; Mogi, 1974; Paterson, 1978), and it is generally assumed that the criterion holds for brittle fracture in general. To test possible temperature dependence, we performed a series of runs on samples from a single block at a fixed temperature of 150°C. The variation with pressure at 150°C is compared with the corresponding pressure dependence at room temperature (Byerlee, 1967) in fig. 4. Owing to block to block variability, the strength of our samples were higher than Byerlee's by about 50 MPa, but the pressure dependence was almost identical for the two sets of data at different temperatures. Mohr–Coulomb criterion has been applied to the analysis of fresh geologic fault (e.g. McGarr and Pollard, 1979) and of bounds on in situ stresses (Zoback and Zoback, 1980); our data indicate that the extrapolation to elevated temperatures is justified. There are, however, several limiting factors rendering this criterion to be not very useful for placing bounds on crustal stresses in general; this question was recently discussed in some detail by Brace and Kohlstedt (1980).

The frictional strength data of Stesky et al. (1974) at 400 MPa and 250 MPa are also outlined in fig. 3 for comparison, and as expected the frictional strength is in general lower than the fracture strength at temperatures up to 600°C, the implications of which were discussed in detail by Stesky et al. (1974) and Stesky (1978). No attempt was made to deform the samples to high strain since quite extensive qualitative observations and microscopic work have been made on faulted Westerly granite (Tullis and Yund, 1977b; Stesky, 1978).

4.4. Effect of thermal cracking

Recent measurements of the thermal expansion of crystalline rocks under pressure (van der Molen, 1981; Wong and Brace, 1979) indicate that thermal cracking occurs when a rock is heated at sufficiently low pressures, and that the extent of thermal cracking increases with temperature at a given pressure. Can thermal cracking be responsible for the decrease in failure stress with temperature at a fixed confining pressure? The data shown in fig. 2 seem to argue against this: if thermal cracking is the mechanism responsible for strength reduction, we expect the failure stress to show a sharper decrease with temperature at lower pressures when the thermal cracking is more extensive. We do not detect this trend in our data; as a matter of fact, the 80 MPa data show a slightly smaller percentage decrease in strength with temperature.

As shown in fig. 2, the data of all three pre-heated samples agree with the 'vacuum dry' data within the experimental error. Judging from Simmons and Cooper's (1978) thermal cracking data, a large proportion of the thermally induced cracks in our pre-heated sample should still be open at a pressure of 80 MPa, and yet the failure stresses for the pre-heated and 'vacuum dry' samples are identical. Failure stress is evidently not affected by the difference in initial crack morphology. Similar effect of thermal cracking on fracture strength was recently reported by Bauer and Johnson (1979) and Kurita et al. (1980).

The experiment on a pre-heated sample at 422°C indicates that for 'dry' deformation at high temperature, if the failure strength is the only parameter of interest, then pre-heating to 300°C serves the same purpose as drying in vacuo. The microscopic observation of the stressed samples, however, is complicated by thermal cracks induced by the pre-heating at room pressure (Sprunt and Brace, 1974; Bauer and Johnson, 1979). Kurita et al. (1980) also reported that for a sample preheated to 200°C, although no reduction in strength was observed at 150 MPa, the dilatant strain at failure was significantly higher.

4.5. Post-failure behavior at elevated temperatures

The post-failure behavior of rock was quite actively investigated in the past decade, and a number of papers has been published on the various aspects from stiff machine design to complete stress–strain curves, an incisive review of which was recently given by Paterson (1978). Most of these studies interpreted the post-failure behavior in term of the classification proposed by Wawersik (Wawersik and Fairhurst, 1970) who showed that the post-failure response of certain rock types is so unstable that the load–displacement curves can actually turn over so far as to take on a positive slope. Wawersik categorized this type of behavior as class II, in contrast to the more stable class I behavior with persistently negative post-failure slope. Whereas class I behavior can be observed in a stable manner by using a sufficiently
The post-failure response was the most unstable: no general dependence on pressure. Wawersik and Brace (1971) observed. Similarly, Gummel et al.'s (1978) room temperature data on a class I granite over a comparable strain, but no systematic trend on stress drop was reported that at the highest pressure (150 MPa) the unstable class II (fig. 6).

Up to now most investigations are on class I rocks in uniaxial compression. To our knowledge three groups have published results on post-failure behavior of silicate rocks in triaxial compression. Hojem et al. (1975) studied the post-failure behavior of Witwatersrand quartzite at pressures up to 28 MPa by using an unusually stiff machine, and Rummel et al. (1978) studied Fichtelgebirge granite at pressures up to 300 MPa using servo-control feedback; both rocks belong to class I within the range of pressures they considered. Class II behavior of Westerly granite and Frederick diabase was studied by Wawersik and Brace (1971) at pressures up to 150 MPa using manual control. As discussed by Paterson (1978), it is desirable that the displacement signal for servo-control feedback changes monotonically throughout an experiment, and for class II behavior the choice of such an optimum signal is difficult. The lateral strain has been used by Hudson et al. (1971) and Sano (1978) as the feedback signal with variable degree of success in uniaxial compression of class II rocks. It is difficult to adapt similar techniques in triaxial compression at elevated temperatures. As discussed above, we therefore chose to consider the post-failure behavior by adapting Wawersik’s technique to high temperatures.

We attempted 19 post-failure experiments. 10 were successful; the rest ended in unstable failure. Several experiments were deliberately terminated before the complete post-failure curves were obtained so as to retrieve the stressed samples for scanning electron microscope observation (Wong, 1982a). The results in figs. 5 and 6 show that at a fixed pressure, an increase in temperature results in an increase of the post-failure strain and a decrease in stress drop. Therefore the overall post-failure slope is gentler at higher temperatures. Nevertheless, the initial unloading behavior at lower pressures and temperatures still belongs to the unstable class II (fig. 6).

The effect of pressure, however, can not be so simply generalized. There seems to be a trend for the higher pressure samples to experience slightly larger post-failure strain, but no systematic trend on stress drop was observed. Similarly, Rummel et al.’s (1978) room temperature data on a class I granite over a comparable range of pressure seem not to exhibit any systematic dependence on pressure. Wawersik and Brace (1971) reported that at the highest pressure (150 MPa) the post-failure response was the most unstable: no general conclusion can be made of this observation since it can be due to either a sharper stress drop or a smaller post-failure strain during initial unloading at the higher pressure.

4.6. Post-creep failure behavior and effect of loading history

We performed four creep tests, the data of which are shown in figs. 7, 8 and 9. Several aspects of the data are worth noting. First, they show that the observation by Wawersik (Wawersik and Brace, 1971; Wawersik, 1973) that the post-failure curve represents an approximate bounding curve for creep strain also applies at elevated temperatures. Some of the data (especially PFW 27) show larger creep strain at failure, but judging from similar creep strain at failure data at room temperature (Wawersik and Brown, 1973; Kranz, 1980), this type of scatter is to be expected. Second, the postfailure curve is dependent on the loading history. The behavior is more stable at a lower fixed differential stress and strain rate.

The experiments were all done at a temperature of 350°C. Judging from transmission electron microscope studies of Westerly granite (Tulis and Yund, 1977b; Stesky, 1978), dislocation activity should not be significant at the pressure, temperature, and strain rate of our experiments. It is reasonable to assume that the creep mechanisms operative are brittle and similar to those at room temperature (Wawersik, 1973; Wawersik and Brown, 1973; Kranz, 1980).

One mechanism commonly proposed to explain brittle creep is stress corrosion. A detailed review of possible microscopic processes responsible for the phenomenon was recently given by Wiederhorn (1978). The influence of temperature, stress, and moisture on corrosion rate was studied by Martin (1972), and data on explicit relationship between the stress intensity factor ($K_I$) and creep velocity ($v_e$) in regime I was given recently by Atkinson (1979). The effect of stress corrosion on the failure stress was qualitatively discussed in several articles (Martin, 1972; Rice, 1979; Scholz and Koczynski, 1979; Rudnicki, 1980). Rice’s analysis is particularly pertinent here since he shows how earlier work of Wawersik and Brace (1971) similar to that shown in fig. 9 can be interpreted within a stress corrosion framework. We shall not repeat the details, but discuss below how the analysis can be extended to interpret some of our new results.

If stress corrosion is a significant mechanism, then the critical stress intensity factor $K_I$ should increase with crack velocity $v_e$—except possibly in the so-called...
with crack velocity \( v \) — except possibly in the so-called 'regime II', where it remains constant (Wiederhorn, 1978). If we make the plausible assumption that local microcrack \( K_i \) values increase with the imposed differential stress, we can take that at rapid loading (say, at \( 10^{-5} \text{ s}^{-1} \)) a higher threshold differential stress for significant crack growth is required. Of course, in this case once the critical value is reached, the crack growth proceeds at a relatively fast velocity. At a slower loading (say, creep at \( 10^{-7} \text{ s}^{-1} \)) the reverse is true. A simplistic model can be formulated by assuming that a high critical stress intensity factor \( K_i \) for rapid crack growth applies in our experiments at \( 10^{-5} \text{ s}^{-1} \), whereas a smaller critical value \( K_i \) for slow crack growth applies for the creep tests.

Because the creep deformation involves extensive crack growth at the smaller \( K_i \), we can assume that relatively little crack growth is involved in constant displacement rate tests at fast loading until the failure stress is reached. Rice (1979a) suggested that the pre-failure sample in the latter case can, in this sense, be characterized as an 'iso-crack network': the crack configuration is relatively unchanged and the local \( K_i \) values increase mainly owing to the higher imposed stress, and no significant crack growth occurs until the high \( K_i \) value is reached close to failure. At the post-failure stage, each time the failure envelope is reached, rapid crack growth modifies the 'crack network', and subsequently a lower imposed stress is sufficient for the local stress intensity to reach \( K_i \). The unloading and reloading are both rapid and at stresses below this new threshold, hence no significant crack growth results from the stress cycle, except at the end of the cycle when the post-failure envelope is reached again. An 'iso-crack network' is therefore associated with each of the unloading-loading cycles.

Krauz and Scholz (1977) suggested that a 'critical dilatant volume' exists at failure; within Rice's framework, this implies that a specimen at the failure point can be characterized by an 'iso-crack network' independent of the loading history. If we accept this assumption, then the difference in post-failure response shown in fig. 8 is to be interpreted as a manifestation of the stress dependence: at the same unloading rate, a sample characterized by the same 'iso-crack network' fails more stably if the imposed stress level is lower. On the other hand, one can also argue that the slow creep deformation results in a crack configuration intrinsically different from that of fast loading. Paterson (1978), for example, suggested that the slower deformation rate may result in a 'more generalized proliferation of cracking': in which case, difference in post-failure behavior can also be attributed to changes in the 'crack network' variable.

To resolve the question, more systematic experimental and microscopic work are necessary. Rice (1980) has demonstrated how post-failure behavior may be interpreted by a slip-weakening model with two variables (shear stress and relative slip on the localized zone); our data here show that at a fixed temperature and pressure the post-failure behavior can not be uniquely characterized with such two variables if time-dependent effects are included. Various 'strain softening' models for earthquake instability have been proposed in recent years, as reviewed by Stuart (1980). What are the implications of the type of subtle changes in softening behavior we observed on these theoretical formulations? What are the microscopic mechanisms behind the observed dependence on loading history and to what degree does it extend to other rock types? These questions need to be explored in greater detail.

4.7. The question of reproducibility

The manual control technique used by Wawersik and by us in this study may be suspect because of the possible influence of artificial effects of the operator. Wawersik and Brace (1971) investigated the question of reproducibility by performing a total of 25 uniaxial experiments on Westerly granite, and the two limiting curves were shown in their fig. 3 to demonstrate the degree of reproducibility. Hudson et al. (1971) tried to obtain the complete uniaxial curve for a class II granite using servo-control feedback, but the radial strain gauge, the signal of which was used for feedback, broke halfway and hence no complete post-failure curve is available for comparison. Sano (1978) used a modified approach and succeeded in obtaining the complete curves for the class II Oshima granite. Judging from the three curves shown (Oshima 450, 451 and 452) the reproducibility in Sano's study was not better than that of Wawersik and Brace (1971).

Our technique involves a number of unloading-loading cycles. Cyclic fatigue studies show that very large number of fast (1 Hz) loading cycles (Haimson, 1978) or a relatively small number of stress cycles at slower loading rate (1 MPa s \(^{-1}\)) (Scholz and Koczynski, 1979) can have significant effect on the fracture strength. Haimson (1978) emphasized the similarity between dynamic fatigue and static creep, and from our discussion above, if stress corrosion is the mechanism, then each of our unloading-loading cycle will involve one 'iso-crack network' and should not cause undue influence on reproducibility as long as the unloading and loading...
rates are relatively fast and if the strength envelope is reached for each of the cycles. We performed an experiment to dramatize the effect when these are violated (fig. 10). The sample PFW15 was loaded and unloaded with a large number of cycles (about 50) along an irregular envelope below the failure envelope at 10^{-5} \text{s}^{-1}. As expected, the curve traced out is intermediate between the post-failure (at constant strain rate) and post-creep failure curves.

The idea of an 'iso-crack network' being preserved during an unloading-loading cycle is, of course, only an approximation. Each cycle involves hysteresis, possibly owing to energy dissipation mechanisms such as friction (Walsh, 1965; Scholz and Koczynski, 1979). We judge from our data, however, that the hysteresis was within measurement error. This contribution from hysteresis should increase with the number of cycles, and precaution still needs to be made to reduce the number of unnecessary cycles.

If the two types of mechanisms discussed above are the sole causes, one would probably expect the reproducibility to improve with pressure. Zoback and Byerlee (1975) and Hadley (1976) observed that at sufficiently high pressures, the hysteresis loops are stabilized after the first cycle. Haimson (1978) stressed the similarity between cyclic fatigue and static creep behavior; since the latter mechanism has a rate which decreases with pressure (Wawersik and Brown, 1973; Kranz, 1980), one expects the former to be less significant at high pressure. Even though Scholz and Koczynski (1979) emphasized the possible difference between the two, they also concluded that the effect of pressure is to reduce the fatigue strain.

In contrast to the expected improvement in reproducibility with pressure, Wawersik and Brace (1971) pointed that it actually became worse with pressure, suggesting that a third factor may be involved. One plausible candidate is simply the statistical variability among rock samples. Although Westerly granite shows an unusually small scatter in fracture strength of only a few percent, the statistical variation in the volumetric strain can be up to 20% (Costantino, 1978). To reduce the statistical variation, the precaution is usually taken to prepare samples with dimensions more than ten times the grain size. This may be sufficient for pre-failure processes, but in the post-failure stage most of the deformation occurs in a localized zone which, according to microscope observations, can have width as small as one grain size. Hence as a rule of thumb, the zone samples a volume consisting of only about one-tenth the total number of grains in the sample. The statistical variation is therefore expected to be wider. If we consider the post-failure process as an ensemble of discrete instability events (failure of individual grains along the localized zone), then at a high pressure each of the grains fails with a large stress drop and small displacement, thereby exaggerating the variability in load-displacement record. This is consistent with the observation that the gross post-failure slopes of duplicate curves are usually the same, but the qualitative features of discrete, irregular alternations between steeply dropping parts and much flatter parts are highly variable among experiments performed under identical conditions (Wawersik and Brace, 1971; Sano, 1978).

We do not at present have the capability to explore this question further by extending the post-failure experiments to a wider range of sample dimensions, except possibly in an uniaxial configuration. Rice (1980) has formulated a theory by which the shear fracture energy $G$ can be determined by an integral under the post-failure curve (Wong, 1982b). $G$ is a fundamental
fracture mechanics quantity widely used in theoretical fault models. The estimate of \( G \) obtained by Rice’s integration scheme is basically an average of the energy input over the entire localized zone: \( G \) should therefore be not very sensitive to statistical variation among samples. If further studies conclude that indeed both time-dependent effects and sample variability can in general have significant influence on details of the post-failure stress–strain relation, then a global quantity such as \( G \) may be the more appropriate parameter to use for characterization of post-failure behavior.

5. Conclusion

A number of general conclusions on the failure and post-failure behavior of Westerly granite can be made on the basis of the present study:

(1) In the brittle regime, the failure stress at elevated temperatures depend strongly on pressure. At 150°C, the pressure dependence is almost identical to that at room temperature. Comparatively the temperature dependence of the failure stress is small. For the pressure range (up to 400 MPa) we consider, the reduction of fracture strength due to temperature increase up to 500°C is less than 20% of the room temperature failure stress.

(2) At temperature up to 600°C, the strain rate sensitivity of the strength of dry samples is low. Our data indicate a reduction in strength of less than 4% per decade decrease of strain rate.

(3) The effect of water on strength reduction is more pronounced at elevated temperatures than at room temperature.

(4) At the pressures above or equal to 80 MPa, crack morphology change induced by thermal cracking has no consequence on the failure stress value. Furthermore, the slight temperature dependence of brittle fracture strength cannot be adequately explained by thermal cracking.

(5) The effect of temperature is to stabilize the post-failure behavior. At 80 MPa, 150°C the initial unloading behavior still belongs to the unstable class II: as temperature or pressure is increased, a transition to class I is observed.

(6) Post-failure behavior is dependent on loading history. The behavior is more stable at a lower fixed differential stress and a slower strain rate. Analysis of our data in light of published work on room temperature post-failure and creep behavior indicates that details of the post-failure curve are sensitive to statistical variation among samples.

Acknowledgement

I am grateful to W.F. Brace for his patient guidance and encouragement throughout the course of this work. Discussion with and comments by Y. Caristan, B. Evans, J.R. Rice, J.B. Walsh and W.R. Wawersik have been very helpful. This research was supported by the National Science Foundation under Grants No. EAR77-23158 and EAR79-10854.

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