T18/B5/FPR/97

ADVANCED GLAZING

and

ASSOCIATED MATERIALS FOR

SOLAR AND BUILDING APPLICATIONS

FINAL REPORT OF PROJECT B5: EVACUATED GLAZING

February 1997

Prepared by:

R E Collins, T M Simko, C Wilson, J-Z Tang: University of Sydney

H A L van Dijk, G P van der Linden and H Oversloot: TNO

H Elmahday: National Research Council, Canada

D Arasteh, B Griffith, F Beck: Lawrence Berkeley National Laboratory

M Brunotte, A Brunotte: Fraunhofer Institute for Solar Energy Research

ØAscherhoug: Norwegian University of Science and Technology

H Arnesen: SINTEF Architecture and Building Technology

B Norton, P C Eames, S N G Lo, P W Griffiths: University of Ulster

Abstract

This Report summarises the work undertaken in Project B5 - Evacuated Glazing of the International Energy Agency Solar Heating and Cooling Program Task 18 - Advanced Glazing Materials. The Report reviews the current state-of-the-art of the science and technology of vacuum glazing. The construction and method of manufacture of vacuum glazing is described. The overall rate of heat flow through vacuum glazing can be influenced by several different effects: thermal conductances for heat transfer at the external surfaces; gaseous conduction and radiation through the evacuated space; thermal conduction of the support pillars; and lateral heat flow along the glass sheets in the vicinity of the edge seal. Methods of measuring the heat flow in vacuum glazing due to each separate process are described, and experimental data on the magnitude of such flows are presented. The internal vacuum is shown to be high, and stable. Values of air-to-air, centre of glazing thermal conductance have been achieved ranging from 3 W m^{-2} K^{-1} (for vacuum glazing with no internal low emittance coating) to 0.8 W m^{-2} K^{-1} (for samples with two internal low emittance coatings). The overall heat transport rate through 1m x 1m samples of vacuum glazing has been measured in three guarded hot box instruments. The results obtained agree to within experimental error (between 2% and 6%) with those estimated on the basis of local measurements of heat transfer due to radiation, gas conduction (negligible), pillar conduction and lateral heat flow through the edge seal. Sources of mechanical tensile stress in vacuum glazing are identified. Stresses due to atmospheric pressure occur in the vicinity of the pillars, and (in poorly designed glazing) near the edge seal. Stresses due to temperature differences are influenced by many factors including external heat transfer coefficients, level of insulation of the glazing, edge insulation, and edge constraints. Methods of estimating these stresses are described. The design of vacuum glazing involves tradeoffs between stresses and heat flow. It is shown that a reasonable design compromise, with adequately low stresses, and low thermal conductance can be obtained. Initial estimates indicate that the costs of such glazing should be comparable with those for commercial insulating glazing. Vacuum glazing is now being manufactured in a pilot plant, and commercial sales have commenced.

1. Introduction

One of the possibilities for improving the thermal insulating performance of double glazing is to evacuate the space between the two glass sheets. The principles of operation of such a vacuum glazing are identical to those in a conventional Dewar flask: the vacuum eliminates heat transport between the glass sheets due to gaseous conduction and convection. In addition, radiative heat transport between the glass sheets can be reduced to a low level by the incorporation of a transparent, low emittance coating on the internal surfaces of one, or both of the glass sheets.

There are some obvious potential benefits from the successful development of vacuum glazing. Firstly, it would seem likely that very high levels of thermal insulation could be achieved from the combination of a vacuum and low emittance coatings. Secondly, since the insulating properties of an evacuated space are effectively independent of the width of that space, good thermal insulating properties should be achievable in a structure with a very narrow vacuum gap. The total thickness of such a glazing would therefore be not much greater than that of the two glass sheets.

The potential benefits of vacuum glazing are attached by several equally obvious challenges in its design and manufacture. Firstly, as is shown below, low gaseous thermal conduction is not achieved until the gas pressure is reduced to a very low level - typically below 10^{-6} of atmospheric pressure. In order to maintain such a vacuum over periods of decades, it is essential that the two glass sheets be sealed together in a way that completely eliminates any ingress of gas. Such a seal is termed hermetic. Materials which are used to seal conventional double glazings are far too permeable to be useful in a vacuum glazing. (It is noted, however, that should it be possible to make a hermetic edge seal successfully, vacuum glazing would be unlikely to suffer from long term degradation of performance due to gas permeation through the edge seal, as can occur in conventional double glazing.)

The requirement for a hermetic edge seal results in a second major challenge for the designer of vacuum glazing. The only techniques which have been successfully used so far to produce such a seal involve heating the entire structure to temperatures around 500°C. Any transparent, low emittance coating used to decrease radiative heat flow between the glass sheets must therefore be capable of surviving such temperatures without excessive degradation. Of course, a low temperature process to make a hermetic edge seal would overcome this difficulty; despite significant efforts, however, such a process has yet to be shown to be viable.

A third challenge associated with the construction of vacuum glazing relates to the effect of atmospheric pressure. In a conventional Dewar flask, the cylindrical shape of the glass vessel is intrinsically strong, and can withstand the large pressure differential (10 tonnes per square meter) associated with the internal vacuum. The plane glass sheets in a vacuum glazing structure are much less able to withstand the effects of this pressure difference. It is therefore necessary to incorporate into the structure an array of small, high strength support pillars in order to maintain the separation of the glass sheets. These support pillars result in stress concentrations, and also provide thermal contact between the glass sheets, thus decreasing the thermal insulation of the glazing. The design of vacuum glazing involves a complex set of trade-offs involving decreasing the mechanical stresses associated with the support pillars (which leads to more, larger pillars), and reducing the heat transport through the pillars (which requires fewer, smaller pillars). It is also highly desirable for the support pillars to be sufficiently small that they are visually unobtrusive.

A fourth challenge to the design of vacuum glazing relates to the effect of a temperature difference between the two glass sheets. Since the primary application of vacuum glazing is as a thermal insulator, it is essential that the structure be able to withstand the differential thermal expansion of the two glass sheets associated with such temperature differences. Two design strategies have been suggested to deal with this problem. One approach is to use a flexible metal edge seal which permits relative lateral movement of the two glass sheets. In such a construction, the glass sheets must slide over the support pillars, and it is necessary to produce glass-to-metal seals around the periphery of the glass sheets. The second method is to use an edge seal in which the two glass sheets are rigidly fused together. Such a glazing requires a peripheral glass-to-glass seal; in addition, there is very little lateral movement of the two glass sheets over the pillars under most conditions, including large temperature differentials. In this design, however, these temperature differentials result in significant mechanical tensile stresses. It is essential that the combination of these stresses, and those due to atmospheric pressure, be sufficiently low that the glass sheets do not fracture. To date, there is no record of the successful fabrication of a vacuum glazing structure with a flexible edge seal. In all of the work reported here, therefore, the vacuum glazings have a rigid, fused edge seal.

The potential benefits, and difficulties associated with the development of vacuum glazing have been known for many years. Indeed, the concept of vacuum glazing was first described in a patent which was filed in 1913 [1]. Since that time, many people have worked on this problem. Interestingly, until the 1980's, the only publications relating to vacuum glazing were in the patent literature [2-25]. In 1984 and 1985, Benson and his group at SERI, USA, published two conference papers [26, 27] and one patent on this topic [21], and also produced a series of internal reports [28-30]. This group did not succeed in making a vacuum glazing structure with good thermal insulation, however. The first published report of the successful fabrication of a vacuum glazing with low gas conduction was from the University of Sydney, in 1989 [31]. Since that time, this group has made substantial developments on the science and technology of vacuum glazing, and has published a series of papers [31-52] and theses [53-55], and submitted several patent applications on this topic [56-59]. The IEA Task 18 program has also resulted in several Working Documents on this topic [60-69].

The University of Sydney work on vacuum glazing has stimulated related research activities in several other countries. To date, however, very little published material has arisen from these activities [70]. There is significant current commercial interest in the concept.

The work reported in this document was undertaken in part as a contribution to the International Energy Agency Solar Heating and Cooling Program Task 18 - Advanced Glazing Materials. The early research and development work relating to vacuum glazing at the University of Sydney group occurred at the time when the IEA Task 18 was in its initial stages. At the suggestion of Professor M G Hutchins, Project B5 - Evacuated Glazing was included in the Task activities. In planning this Project, it was recognised that vacuum glazing was a very immature technology relative to most other insulating glazing concepts of interest to the Task. The objectives of the Evacuated Glazing Project of the Task therefore reflect the stage of development of the technology, and the understanding of the scientific concepts associated with it, in 1992. Specifically, the objectives of the project were:

1. To develop the technical basis/experience for the construction of evacuated glazings.

2. To obtain a thorough understanding, by theoretical and experimental means, of the heat transport mechanisms and the stresses in these devices.

The Evacuated Glazing Project in Task 18 had formal participation by Australia (The University of Sydney), UK (The University of Ulster) and Germany (The Fraunhofer Institute for Solar

Energy Systems). In addition, substantial interaction occurred with USA (Lawrence Berkeley National Laboratory) and Canada (NRC). This Project also aimed to produce samples of vacuum glazing of practical size (1 m x 1 m) and to supply those samples to other Projects in the Task (specifically in order that test results might be obtained for comparison with results from the Evacuated Glazing project, and with results obtained from measurements on other types of insulating glazing.) Samples of this size were supplied to the University of Ulster, NRC (Canada), TNO (Netherlands), SINTEF (Norway), and LBL (USA). Additional smaller samples were supplied to LBL, the Fraunhofer Institute, NRC and the University of Ulster.

The Objectives of the Evacuated Glazing Project within Task 18 can therefore be seen to be consistent with the overall Task Objectives in the sense that, in the context of vacuum glazing, the Project attempted:

"... to develop the scientific, engineering and architectural basis which will support the appropriate development and use of advanced glazings in buildings...".

It is particularly fortuitous that the research phase of Task 18 occurred at just that time when the technology of vacuum glazing was developing to the point where devices of practical size and good insulating performance were becoming available. It must be emphasised, however, that the samples of vacuum glazing which were built and tested within the Task 18 activities were prototype devices which were made in a university research laboratory. The samples were not in any sense production engineered and, in fact, had little benefit from the expertise and experience of commercial organisations which operate in the insulating glazing market. It was even necessary to learn (through the bitter and costly experience of failure) how to pack vacuum glazings in order that they could survive the rigors of international transport.

This discussion is not intended as an apology for any shortcomings in this performance of the samples of vacuum glazing as presented in this Report. It is important to keep in mind, however, that the technology of the vacuum glazing samples which were studied during the Task 18 work was still within the research laboratory. Unlike perhaps any other device studied in Task 18, vacuum glazing of any kind had never been commercially manufactured, nor indeed had never (until recently) been produced by a commercial organisation.

In this Report, we first present a brief summary of the methods of construction of vacuum glazing, as developed at the University of Sydney. The different processes which can lead to heat transport through vacuum glazing are then described, and the way in which these processes interact is discussed. For each process, modelling results are validated with experimental data. A method is presented for determining the overall, air-to-air thermal conductance of vacuum glazing from the individual processes which contribute to the heat transport. Values of thermal conductance, obtained in this way, are compared with experimental measurements of heat transport through 1m x 1m samples of vacuum glazing made in calibrated hot boxes at NRC, TNO and SINTEF. Mechanical stresses in vacuum glazing due to atmospheric pressure and temperature differentials are determined by analytic, and numerical modelling methods. This leads to the development of a design strategy for vacuum glazing which quantifies the trade-offs between heat transport, mechanical stresses, and fracture probability. Finally, comment is made on the likely cost of vacuum glazing, and on the current status of commercialisation initiatives in this area.

2. Construction and manufacture of vacuum glazing

Figure 1 shows a schematic diagram of the vacuum glazing developed at the University of Sydney, and studied in the IEA Task 18 project. The vacuum glazing is made from two sheets

of 4 mm thick soda lime glass. The inner surfaces of one or both sheets may be coated with a transparent low emittance coating. All coatings studied to date are pyrolitically deposited “hard” coatings, including K glass (Pilkington) [71], Energy Advantage (Libby Owens Ford) [72], Sungate 500 (PPG) [73], Comfort EM 5000 (Glaverbel) [74] and Comfort E^2 (AFG Industries) [75].

A detailed description of the manufacturing process for vacuum glazing is presented elsewhere [47]. Prior to the assembly of the structure, the mechanical support pillars are placed on one of the glass sheets. The pillars are made from Inconel 718 - a high strength, heat resistant, nickel-based alloy - and are typically ~ 0.25 mm in diameter and 0.15 mm high. The seals around the edges of the glazing, and around the pump and tube, are made with solder glass - a low melting point glass with a coefficient of thermal expansion which matches that of the soda lime glass. The edge seal is made by heating the entire structure to a temperature around 450°C for one hour, and subsequently cooling slowly to room temperature.

During the evacuation process, the structure is baked to a temperature between 100°C and 250°C in order to remove adsorbed and absorbed gases from the internal surfaces. After cooling, the end of the pump out tube is melted and fused to complete the sealing process.

Over 650 samples of vacuum glazing have been produced at the University of Sydney since 1989. The maximum size produced to date, 1 m x 1 m, is determined by the dimensions of the processing oven. At this time, there is no obvious upper limit to the size of vacuum glazing that can be produced using these techniques.

Figure 1 Schematic diagram of vacuum glazing, as produced at the University of Sydney.

3. Heat transport processes in vacuum glazing

In this section, the different processes which can cause heat to flow through vacuum glazing are discussed. The methods which have been used to measure and calculate such heat transport are also reviewed, and experimental and modelling heat transport data are presented. An analysis is made of the way in which heat flow due to these separate processes combine to determine the overall heat transport through vacuum glazing.

3.1 External heat transport processes

As in most insulating glazing designs, the thermally insulating part of the vacuum glazing structure (the internal evacuated space) is surrounded by material of relatively high thermal conductance (the glass sheets). The effect of this is to modify the heat flow *through* the structure by introducing significant heat flow *along* the glass sheets. In certain cases, this can

have a profound effect on the nature, and magnitude of the overall heat transport through, and the temperature distribution within, the vacuum glazing.

Heat transport to and from the external surfaces of the glazing can be exceedingly complex, involving a combination of conduction, forced and natural convection, and radiation. As in most analyses of such heat transport, the approach taken here is to simplify the treatment of the external processes by characterising them by a thermal conductance, C: The heat flow Q through area A for a temperature different ΔT is written:

$$Q = CA\ \Delta T. \tag{1}$$

Following one of the standards for determining heat flow through insulating glazing [76], the external conductance C is assumed to be constant and to have values of 8.3 W m^{-2} K^{-1} and 30 W m^{-2} K^{-1} on the hot and cold sides of the glazing respectively. As will be seen, such a simplistic approach leads to results which are in excellent agreement with experimental measurements.

The external heat transport processes result in significant temperature non-uniformities at various points on the outside surfaces of the glass sheets. It is convenient, however, to characterise heat transport processes within the glazing in terms of heat transfer coefficients. In effect, this is equivalent to assuming that the external temperature variations along the glass are relatively small compared with the temperature difference across the glazing. In vacuum glazing, this is always a reasonable approximation at points remote from the edges; heat flow near the edges can be treated separately from other heat flows, as discussed in Section 3.6 below.

3.2 Methods of measurement of heat transport

The existence of many small support pillars in vacuum glazing results in non-uniformities in the heat flow through the glazing. For typical array designs, the heat flow at any point on the external surface of the glazing always contains significant contributions from nearby pillars. It turns out, however, that for measurements of heat flow through a small area (~ 1 cm^2) under conditions where the external surfaces are maintained at constant temperature, this pillar contribution can be reduced to negligible levels relative to other heat flows if the pillars are slightly further apart than normal design criteria would permit [40]. Experimental samples of vacuum glazing were thus always built with a central pillar-free region about 50 mm in diameter in order to permit measurements of radiative and gaseous heat flow. The additional stresses associated with this pillar-free region appear to be tolerable in the sense that fracture of the samples as a result of these stresses did not occur.

In this section, we briefly discuss two instruments which were specifically developed for the accurate measurement of local heat flow through vacuum glazing. Later, we show how such instruments can be used to determine the relative contributions to the overall heat flow due to radiation, gas conduction, and the support pillars.

3.2.1 Guarded hot plate

Figure 2 shows a schematic diagram of a guarded hot plate which was developed specifically for measuring local heat flows through vacuum glazing [40]. This instrument operates on the same principle as a conventional guarded hot plate. A small thermal conductor, called the metering

piece, is placed in good thermal contact with one side of the vacuum glazing sample. The metering piece is surrounded by an isothermal hot guard which is also in good contact with the sample. The other side of the sample is maintained at a constant, lower temperature. As a result of heat flow through the sample, the temperature of the metering piece is slightly less than that of the guard by an amount which is proportional to the heat flow. If power is dissipated in the metering piece, its temperature increases relative to that of the guard. When the power is adjusted to make the temperatures of the metering piece and guard equal, all of this power flows through the sample, and the conductance is thus determined.

The application of this elegant principle to the measurement of heat flow through vacuum glazing requires very careful attention to parasitic heat flows from the metering piece. Such heat can flow laterally along the glass sheets of the sample, or along the connecting wires to the instrumentation in the metering piece. Methods of reducing such heat flows to negligible levels, and of demonstrating that this has been achieved, are discussed elsewhere [40].

Figure 2 Schematic diagram of guarded hot plate apparatus designed to measure local heat flow through vacuum glazing.

This guarded hot plate differs from conventional instruments of its type in that both metering piece and guard must be in good thermal contact with the (relatively) highly conducting glass sheet of the sample, and therefore in good thermal contact with each other. Combined with the small area metering piece, this means that for high accuracy measurements, it is necessary to measure very small temperature differences (10^{-4}K) between the metering piece and guard. This is achieved by the use of a sensitive thermistor bridge connected to a high stability amplifier in a temperature controlled environment.

A particularly critical issue is the calibration of the guarded hot plate. Very small departures from planarity of the front surfaces of the metering piece and guard can cause systematic errors in the readings. The instrument was therefore calibrated by measuring an accurately known heat flow. In this work, the calibration standard chosen was radiative heat transport through a highly evacuated space between two plane parallel uncoated soda lime glass surfaces. It was necessary to develop an innovative approach to calculate such heat flow from the optical constants of soda lime glass; in fact this work revealed an error of some 4% in the methods recommended in the standards for such a situation. Details of this calculation are presented in Section 3.4 below.

The guarded hot plate apparatus has been shown to give measurements of heat flow to an absolute accuracy of approximately 1%. The reproducibility of measurements is significantly better - about ± 10 μW, which corresponds to about 0.1% for radiative heat transfer between uncoated glass surfaces and a glass-to-glass temperature difference of 20°C.

3.2.2 Thermal transient apparatus

Whilst the guarded hot plate apparatus can give very precise measurements of the heat flow through vacuum glazing, its application is limited to low temperatures. It turns out that, in order to obtain a detailed understanding of the physical processes which can lead to vacuum degradation during high temperature ageing, it is necessary to measure the gas conductance of the glazing at elevated temperatures. It was thus necessary to develop a second apparatus [45, 52] with the capability to make in-situ measurements of glazing conductance at temperatures up to at least 150°C.

Figure 3a shows a schematic diagram of this apparatus. The measurement technique involves the application of a rapid, step increase of temperature to the glass sheet on one side of the glazing, followed by a precise measurement of the small and relatively slow temperature increase of the glass sheet on the other side. The rate of this second temperature increase is dependent on the thermal conductance of the glazing. Figure 3b shows experimental and finite element modelling data for a typical measurement. Good agreement is obtained between experimental and modelling data for a specific value of sample conductance.

Figure 3 (a) Schematic diagram of the thermal transient apparatus used to make measurements at elevated temperatures of the thermal conductance of vacuum glazing due to radiation and gas. (b) Typical data, showing the magnitude of the applied temperature step on one side of the sample, and the temperature increase on the other side. The points are experimental data, and the lines are the results of finite element modelling of the system.

As noted above, the purpose of this type of measurement is to obtain information about vacuum degradation at high temperatures. Whilst having reasonably good sensitivity, this measurement method is not absolute. The rate of slow temperature increase depends on the thermal mass of the glass sheet which is itself temperature dependent through the specific heat of glass. In addition, there are significant delays associated with the diffusion of the rapid temperature increase through the glass sheets. These, and other factors lead to a requirement for very careful calibration of the apparatus. This was achieved by measuring samples having thermal conductance which was accurately known. Such samples were themselves calibrated by measurements with the guarded hot plate apparatus, and through estimates of the temperature dependence of radiative heat flow from an accurate knowledge of the infrared optical properties of the internal surfaces [48, 52].

Full details of the design and calibration of this apparatus, and of the methods of separating radiative and gaseous heat transfer, appear elsewhere [52]. Experimental data obtained with this apparatus on the ageing of samples of vacuum glazing are presented in the next section.

3.3 Gaseous conduction

The thermal conductivity of gas is virtually independent of pressure over many orders of magnitude. This curious property is the result of the almost exact inverse relationship between pressure, and mean-free-path for molecule-molecule collisions. As the gas pressure is reduced, the number of gas molecules available to transport heat is reduced in proportion; however, the molecules travel correspondingly further between collisions. A significant reduction in heat transport due to gaseous thermal conduction only occurs when the pressure is low enough that the mean free path for molecule-molecule collisions is of the same order as the distance over which the heat is transported. For vacuum glazing with a gap of 0.2 mm, this corresponds to a pressure of about 30 Pa or 3 x 10^{-4} atmosphere. At lower pressures, the gaseous conductance is approximately proportional to pressure, and may be written [77]:

$$C_{gas} = a\left(\frac{g+1}{g-1}\right)\left(\frac{R}{8pMT}\right)^{1/2} PA(T_1 - T_2). \tag{2}$$

In this expression, α is the combined accommodation coefficient, given by:

$$a = a_1 a_2 / (a_1 + a_2 - a_1 a_2), \tag{3}$$

and α_1 and α_2 are the accommodation coefficients for the gas molecules at the two surfaces; γ is the specific heat ratio for the gas, M is the molar mass of the gas, T is a temperature intermediate between the temperatures of the surfaces, T_1 and T_2, and R is the gas constant. For water vapour, with a combined accommodation coefficient of 0.9, the gaseous conductance in the low pressure region is approximately equal to 0.8 P W m^{-2} K^{-1} where P is measured in pascals. In order to ensure that gaseous conduction is negligible, the pressure within the glazing should therefore be less than about 10^{-1} Pa.

In most as-produced samples of vacuum glazing, the internal pressure as determined from measurements of thermal conductance is immeasurably small. Furthermore, high accuracy guarded hot plate measurements of radiative and gaseous conductance indicate that, in most samples, no measurable change in gaseous conductance occurs over periods of many years at room temperature. Typical data of this kind are shown in Fig. 4. It is highly desirable to identify potential degradation mechanisms for the internal vacuum, and thus to estimate the likely lifetime of the device. It is therefore necessary to accelerate the processes which might lead to such degradation. In order to do this, periodic measurements of the thermal conductance of samples of vacuum glazing have been made during extended periods of storage at high temperatures. Initial measurements of this kind involved the removal of the samples from the high temperature storage oven for measurement in the guarded hot plate apparatus at room temperature. Such samples showed little degradation after several years of storage at 100°C. It was eventually realised, however, that substantial gas evolution was occurring at the high storage temperature, but that most of this evolved gas was adsorbed onto the internal surfaces of the glazing when it was cooled, and thus it did not contribute to the heat flow at room temperature.

Figure 4 Measurements of radiative and gas conductance for samples of vacuum glazing stored at room temperature over periods of several years. These samples were baked at approximately 130°C for 20 minutes during evacuation.

Measurements of gaseous conductance of vacuum glazing during ageing at elevated temperatures using the thermal transient apparatus described above are providing interesting and informative data about the degradation mechanisms of the internal vacuum. Typical data of this type are shown in Figs. 5 and 6. In these figures, the internal pressure in the glazing is shown, as calculated from eqn (2), assuming an accommodation coefficient, defined in eqn (3), of 0.5. At the present time, complete understanding has not been obtained of the physical processes which can cause vacuum degradation, and of the rates at which they occur. It is possible, however, to draw several conclusions of relevance to the stability of the vacuum under high temperature ageing. Firstly, the rate of vacuum degradation increases as the storage temperature is increased. Secondly, at any given storage temperature, samples which have been baked at higher temperatures during evacuation exhibit a lower rate of vacuum degradation. Thirdly, the internal pressure within a degraded sample of vacuum glazing is dependent on temperature, decreasing as the temperature is decreased. It is believed that this is due to adsorption of the gas onto the internal surfaces of the glazing. The temperature dependence of pressure is strongly dependent on the processing of the glazing during manufacture (specifically, the temperature of outgassing during evacuation), on the amount of degradation of the internal vacuum, and on the type of internal surfaces (K glass, or uncoated glass). It is noted that vacuum glazing made at the University of Sydney does not contain an internal getter.

Figure 5 (a) Measurements of internal pressure of samples of vacuum glazing at temperatures indicated in (b). The measurements were made in the transient apparatus, described in Section 3.2.2. A combined accommodation coefficient of 0.5 is assumed.

Although a comprehensive qualitative and quantitative understanding of the degradation of the internal vacuum in vacuum glazing remains to be developed, all of the data obtained to date indicate that the internal vacuum is extremely stable. It is also relevant to note that similar sealed devices (such as dewar flasks, television tubes and electron tubes) with a high and stable internal vacuum have been routinely produced for over a century. At this stage of the development of this technology, it is reasonable to conclude that gaseous thermal conduction can be ignored in the determination of the overall heat transport through vacuum glazing, and that degradation of the internal vacuum is unlikely to be a problem over the service life of these devices.

Figure 6 Temperature dependence of the internal pressure for a sample of vacuum glazing at various stages of the ageing process. The symbols correspond to the data of Fig. 5.

3.4 Radiative heat flow

As mentioned above, the calibration of both types of measuring equipment described in Section 3.2 requires precise estimates of the magnitude of radiative heat transfer between uncoated, and coated glass surfaces. In this Section, we discuss this issue; the material presented here is developed in more detail elsewhere[48, 62].

The rate of energy loss by thermal radiation from a surface of area A at temperature T (in kelvin) can be written:

$$Q_{rad} = e s A T^4, \tag{4}$$

where e is the emittance of the surface, and s is the Stefan Boltzmann constant (5.67 x 10^{-8} W m^{-2} K^{-4}). For two plane parallel surfaces, having temperatures T_1 and T_2 and hemispherical emittances e_1 and e_2, the nett rate of radiative heat flow can be written:

$$Q_{rad} = e_{eff} s A \left(T_1^4 - T_2^4\right) \tag{5}$$

where the effective emittance, e_{eff} is conventionally written:

$$\frac{1}{e_{eff}} = \frac{1}{e_1} + \frac{1}{e_2} - 1. \tag{6}$$

Equation (6) is strictly speaking only valid for surfaces for which the reflectance is independent of wavelength and angle of incidence of the radiation to the normal, q. No practical surface possesses such ideal properties. One approach to this difficulty is to define a directional total emittance $e(q)$ such that the hemispherical emittance e_{hemi} can be written [48]:

$$e_{hemi} = \int_o^{p/2} e(q) \sin 2q \; dq \,. \tag{7}$$

In this approximation, the nett rate of radiative heat transfer is given by eqn (5), with

$$e_{eff} = \int_o^{p/2} \frac{\sin 2q \; dq}{\dfrac{1}{e_1(q)} + \dfrac{1}{e_2(q)} - 1}. \tag{8}$$

This approach is justified on the basis that the angular dependence of the reflectance of most practical surfaces is more significant than the wavelength dependence of reflectance. Indeed, for "grey" surfaces, where the reflectance is independent of wavelength, eqn (8) is precisely correct. For many important combinations of surfaces, however, this is not so; indeed the errors which result from using either eqn (6) or eqn (8) can be comparable.

For specular surfaces having both wavelength and angular dependent reflectances, an exact expression can be written for the nett radiative heat transfer by integrating over wavelength l and angle q [48]:

$$Q_{rad} = \int_o^{p/2} \sin 2q \; dq \int_o^{\infty} dl \; \frac{E(l_1, T_1) - E(l_2, T_2)}{\dfrac{1}{e_1(l_1, q_1, T_1)} + \dfrac{1}{e_2(l_2, q_2, T_2)} - 1}. \tag{9}$$

In this equation, $e\,(l, q, T)$ is the directional, spectral emittance of either surface, at temperature T, and $E(l, T)$ is the black body radiation function at temperature T.

The determination of the radiative heat flow using eqn (9) requires a knowledge of the infrared optical properties of the surface as a function of wavelength and angle. For dielectrics, such as uncoated glass, such data can be obtained from measurements of the complex refractive index of the surfaces.

For transparent low emittance coatings, the situation is much more complex. In the absence of a suitable optical model, the only approach is to make detailed experimental measurements of these optical properties. In this work, a free-electron (Drude) model has been developed which accurately describes the infrared optical properties of pyrolytic transparent low emittance coatings, such as Pilkington K glass [48]. In this model, three parameters are adjusted to give good agreement with experimental near-normal wavelength dependent reflectance data. Figure 7 shows an example of such data. The full wavelength and angular dependence of the reflectance
of the surface can then be determined using these parameters. Figure 8 compares the results of such calculations with experimental data at large angles of incidence. Excellent agreement is obtained.

The validity of different methods of calculating radiative heat flow can be tested by comparing the results of such calculations with accurate measurements within a dynamically pumped sample. Table 1 shows the results of such a comparison for radiative heat flow between uncoated glass surfaces. The experimental data were obtained in a guarded hot plate for which the area was accurately known. Results are shown for the calculation of radiative heat flow by simply combining hemispherical emittances (Method 1 using eqns (5) and (6)), or by combining angular dependent total emittances (Method 2 using eqns (5) and (8)). In both cases there is an error of about 4%. In contrast, the use of the double integral over angle and wavelength in eqn (9) (Method 3) gives results that are in excellent agreement with the experimental data.

Figure 7 Comparison of near-normal experimental infrared reflectance data for K glass (solid line) with the reflectance obtained from a best fit of 3 parameters in the Drude model over the wavelength range 3 to 10 μm.

Table 2 shows the results of similar calculations and measurements for various combinations of uncoated glass and coated glass (K glass). In order to obtain these data, the small area guarded hot plate apparatus was calibrated against measurements of radiative heat flow between uncoated glass surfaces [48]. This has the effect of defining the measured and calculated radiative heat flow between uncoated glass surfaces to be precisely equal. The data in Table 2 show that eqn (9) is an accurate method of calculating radiative heat transport for all combinations of low, and high emittance surfaces. All three methods give good agreement for the radiation between a low emittance surface and uncoated glass; this is as expected, since the effective emittance of such a combination is close to the hemispherical emittance of the low emittance surface.

Equation (9) can also be used to calculate the radiative heat flow as a function of temperature difference between the two surfaces. The results of such calculations, and experimental data, are shown in Fig. 9 for two low emittance surfaces. As can be seen, the experimental data are in close agreement with the T_1^4-T_2^4 relationship given in eqn (5). There are, however, slight departures from this relationship, and these are also shown in Fig. 9 on an expanded scale. The calculated departures are in good agreement with experimental measurements.

Table 1 Heat transfer per unit area between uncoated glass surfaces for the temperatures indicated, calculated by different methods. For Method 1, the heat flow is calculated on the basis of a simple combination of the hemispherical total emittances (equations (5) and (6)). For Method 2, the heat flows are based upon an integration over angle of the directional total emittances (equations (5) and (8)). In Method 3, the heat flows are calculated by an integration over wavelength and angle of the directional spectral emittances and the spectral blackbody emissive powers (equation (9)). The experimental measurements were taken with the large area guarded hot plate.

Table 2 Heat transfer per unit area between various combinations of uncoated glass and K glass surfaces, for the indicated glass surface temperatures. The calculated results were determined by the same three methods described for Table 1. The experimental measurements were taken with the small area guarded hot plate. The effective and hemispherical emittances were determined by equations (5) and (6), respectively.

Figure 8 Comparison of experimental and modelling reflectance data for K glass at large angles of incidence.

Figure 9 Experimental measurements of radiative heat flow between two K glass surfaces, plotted against T_1-T_2 in (a), and against T_1^4-T_2^4 in (b). Also shown are departures from the linear relationship predicted by eqn (5), and a comparison with the expected departures as calculated with eqn (9).

High accuracy measurements such as in Figure 9 are particularly useful in identifying the physical mechanisms responsible for heat flow in vacuum glazing. As an example, Fig. 10 shows measurements of heat transport through a sample of vacuum glazing that has been degraded by storage at high temperatures. The departures from the T_1^4-T_2^4 relationship are still small, but are measurably, and qualitatively, different from those for radiative heat flow only. Such data are consistent with the existence of a contribution to heat flow by gaseous conduction. Indeed, it turns out that all of the degradation in insulating properties of such an aged sample is due to an increase in gas pressure; the emittance of the surface has never been observed to change under such ageing. Measurements such as shown in Fig. 10 provide a very convenient, non-destructive method of determining the relative contributions to heat flow in vacuum glazing from thermal radiation and gaseous conduction.

Figure 10 Experimental measurements of the radiative heat transfer in a degraded sample of vacuum glazing, plotted against T_1^4-T_2^4. Also shown on an expanded scale are the departures from the linear dependence. The concave upwards departures are characteristic of that expected for a sample containing significant gas conductance.

All transparent low emittance coatings used in vacuum glazing [71-75] are observed to degrade slightly during the high temperature process for forming the edge seal. Typically, this degradation amounts to an increase in emittance of about 10-15%. Most coatings tested had an effective hemispherical emittance in the glazing of approximately 0.24, as determined from measurements on a vacuum glazing sample with one low emittance coating, using eqn (6). Two coatings, however, performed significantly better than the others; the AFG coating [75] and the new LOF coating [72] both had effective emittances of about 0.21 and 0.20 respectively after

the formation of the edge seal. Work is in progress to determine the mechanisms responsible for the observed emittance degradation in these pyrolitically deposited coatings.

To summarise, a good understanding has been obtained of radiative heat transport through vacuum glazing. This component of heat flow can be calculated from infrared optical measurements of the internal surfaces of the glazing; the results of such calculations have been validated by accurate experimental measurements.

3.5 Pillar conduction

As noted above, mechanical support pillars are essential in vacuum glazing in order to maintain the separation of the two glass sheets under the influence of the forces due to atmospheric pressure. These pillars also provide thermal contact between the glass sheets, thus increasing the overall heat flow through the glazing. An understanding of the magnitude and spatial extent of the heat flow through the pillars is important in order to make accurate estimates of the overall heat flow through the glazing, and to interpret correctly experimental measurements on glazing samples, such as infrared thermograms.

An accurate estimate of the magnitude of the heat flow through an individual pillar can be obtained from the classical expression for the thermal resistance associated with a small circular contact between two otherwise isolated infinite half-planes [78]. The heat flow in this case is:

$$Q_{one\ pillar} = 2k_{glass}a(T_2 - T_1), \tag{10}$$

where k_{glass} is the thermal conductivity of the glass sheets, a is the radius of contact, and T_1 and T_2 are the temperatures of the half-planes at large distances from the contact.

In a vacuum glazing, the glass sheets are finite in extent, both in thickness, and laterally. Indeed, the symmetry of the glazing requires that there is no heat flow across planes which bisect the lines of pillars. In addition, the heat flow through each pillar can be slightly influenced by the finite thermal resistance of the pillar itself. Furthermore, the presence of a pillar results in a slight decrease of radiative heat flow in its immediate vicinity due to the local reduction in the temperature differences between the internal surfaces. However, finite element modelling [54] shows that eqn (10) provides an accurate estimate of the heat flow through a single pillar in a total array of pillars in a vacuum glazing. This modelling result is confirmed experimentally, as shown in Fig. 11. These data were obtained from measurements with a guarded hot plate on a sample of vacuum glazing in which the metering piece was located directly above an individual support pillar [40, 54]. The heat flow through the pillar was measured by subtracting the radiative heat flow in the vicinity of the pillar, and by accounting for the lateral spread in heat flow from the pillar. The measurements are very close to the $2k_{glass}a$ result of eqn (10).

The thermal conductance of the whole pillar array in vacuum glazing is obtained by multiplying the conductance of an individual pillar, $2\ k_{glass}a$, by the number of pillars per unit area, $1/\lambda^2$, where λ is the pillar separation:

$$C_{pillar\ array} = 2k_{glass}\, a/l^2\ . \tag{11}$$

For a specific value of the thermal conductance of the pillar array, equation (11) provides a relation between pillar separation and pillar radius. This relation can be used as a design criterion for the pillar array in vacuum glazing by restricting the choice of these quantities to values which give an array conductance below a specific limit.

Equations (10) and (11) are, strictly speaking, only valid when the external surfaces of each glass sheet are isothermal. In fact, in any practical application of vacuum glazing, this is not the case, and the temperatures of the external surfaces are influenced by the local heat transfer with the surroundings. This situation can be analysed by finite element modelling on the assumption of a constant heat transfer coefficient between the surroundings and the glass surface. Figure 12 shows the variations in local temperature near a single support pillar. It is of interest to note that this heat flow spreads out over a very large distance.

Figure 11 Measurements of inverse of thermal resistance for individual pillars, as a function of pillar radius. These data were obtained with the guarded hot plate apparatus, with a correction factor, based on finite element modelling, for the proportion of total heat flow through the pillar which originates from the metering piece.

Local temperature variations due to a pillar array can be calculated in two ways. One method involves superposition of the effects of many pillars; the basic assumption in this method is that the heat flow due to any pillar is essentially unaffected by the presence of all other pillars. Alternatively, local temperature variations can be calculated simply by modelling a single unit cell of the array, with a pillar at the centre, and adiabatic boundaries along planes bisecting the rows of pillars. This method exploits the symmetry of the pillar array. Both methods give essentially identical results, an example of which is shown in Fig. 13. It is noted that there is a local temperature depression of about 0.5°C above each support pillar for a typical design of vacuum glazing and the standard winter conditions [76]. Temperature non-uniformities of this magnitude have been observed in infrared thermogram measurements made at Lawrence Berkeley National Laboratory. The data obtained in this way are in excellent agreement with these calculations. Local condensation of moisture due to these small temperature non-uniformities would therefore be a rare occurrence and would, in any case, be preceded by much more significant condensation near the edges, as shown in the next section.

The data of Fig. 13 have important implications for the analysis of stresses in vacuum glazing due to a temperature differential. It can be seen that the temperature over most of the glazing surface is quite uniform, implying that the heat flux per unit area at the external surfaces is similarly fairly uniform. Indeed, over more than 90% of the area, the heat flux density is constant to within ±5%; only 10% of the total heat flow (that is very close to each pillar) varies by more than this, with the maximum heat flux density being about twice the average of the heat flow through the pillar. It is therefore a quite good approximation, for some purposes, to make the simplifying assumption that the heat flux due to the pillar array is uniform. The validity of this approach for estimating the magnitude of temperature induced stresses and bending is discussed in Section 4.3 below.

Figure 12 Temperature variation on the external surfaces of the glass sheets due to heat flow through a single support pillar. These data are for 4 mm thick glass sheets, and a pillar 0.25 mm in diameter. The external heat transfer coefficients on the hot side are as noted, and on the cold side are 30 W m^{-2} K^{-1}. The external air temperatures are +21.1°C and -17.8°C respectively

Figure 13 Local variations of the external surface temperature of the hot glass sheet due to a square periodic array of support pillars. Each pillar is 0.25 mm in diameter. The pillar separation is 25 mm. The external heat transfer coefficients are 8 W m^{-2} K^{-1} and 30 W m^{-2} K^{-1}, and the air temperatures are +21.1°C and -17.8°C on the hot, and cold sides, respectively.

3.6 Edge conduction

In all designs of insulating glazing, the heat flux density near the edges is larger than remote from the edges. This occurs because of lateral heat flow along the glass sheets and through the edge seal which is always more thermally conducting than the space between the glass sheets. In vacuum glazing with a fused edge seal, the bonded edge seal represents a thermal short circuit between the glass sheets. The option of using "warm edge" constructions with thermal breaks in the edge spacer, as commonly employed in highly insulating double glazing, is not available in vacuum glazing with a fused edge seal.

The heat flow near the edges of vacuum glazing has been modelled in two ways [49, 54]. Firstly, a simple, one-dimensional analytic model has been developed in which temperature variations through the thickness of each glass sheet are ignored. Secondly, a three dimensional finite element model of the heat flow was constructed. In both models, the pillar array was simulated by a uniform slab of material of appropriate thermal conductivity, and external heat transfer was approximated by uniform conductances. Figure 14 compares results obtained with each approach; excellent agreement is obtained.

The analytic model provides useful insights which lead to very simple methods of calculating the magnitude of the heat flow due to edge effects. In certain simple cases, an exact solution can be obtained. For highly insulating glazing, it is found that the temperature on either side approaches the centre-of-glazing value exponentially with a characteristic distance of $\sqrt{k_{glass}t/h}$, where k_{glass} is the thermal conductivity of glass, t is the thickness of either glass sheet, and h is the heat transfer coefficient at the external surface of the glass sheet. The total heat flow per unit length at the edge is equal to that which would flow laterally along a slab of glass of thickness t, and length equal to this characteristic distance, and the same temperature difference between the edge and points remote from the edge. This corresponds to a thermal conductance per unit length of edge of $\sqrt{k_{glass}th}$. The total heat flow through the edge from one side to the other is therefore equal to that through a slab of glass of thickness t, and length equal to the sum of the characteristic distances on each side.

This analysis can be extended to window frame designs in which insulation extends some distance over the surface of the glazing on either side. This is illustrated schematically in Fig. 15, where the distances are w_1 and w_2. In this case, the heat flow is that which would occur through a slab of glass of thickness t, and length equal to the sum of the characteristic distances, and the total insulated length, w_1+w_2. The total heat flow per unit length of the edge seal can then be written:

$$Q_{edge} = (T_{hot} - T_{cold})k_{glass}t \Big/ \left(\sqrt{\frac{k_{glass}t}{h_1}} + w_1 + w_2 + \sqrt{\frac{k_{glass}t}{h_2}} \right). \qquad (12)$$

This analytic result is strictly true only for negligible heat transfer through the glazing. It remains a very good approximation in the more general case, however, provided that the conductance of the glazing is small relative to the external conductances. For external heat transfer coefficients of 8.3 and 30 W m^{-2} K^{-1}, the analytic approach gives an edge conductance of

0.12 W m^{-1} K^{-1} for 4 mm thick glass, with uninsulated edges. For edges recessed only 5 mm into the window frame, this value drops to 0.10 W m^{-1} K^{-1}. The corresponding additional glazing conductance depends on the size of the glazing; for a 1 m x 1 m glazing having 4 m of edge, the additional conductances are approximately 0.5 W m^{-2} K^{-1} and 0.4 W m^{-2} K^{-1} respectively.

Figure 14 The temperatures of the external surfaces of each glass sheet near the edge seal, as a function of distance from the inner edge of the edge seal, as determined by finite element modelling (FEM), and a simple analytic approach using a finite difference solution (FDM).

Figure 15 Schematic diagram of the edge region of a vacuum glazing with insulation extending distances w_1 and w_2 beyond the edge seal on either side.

Experimental measurements of temperature variations near the edge seal have been made with the LBL infrared camera [49]. Figure 16 shows typical data obtained with this instrument. The experimental data are in excellent agreement with finite element modelling results for glazing with, and without edge insulation, as illustrated in Fig. 17.

Figure 16 Infrared thermogram of the external surface of the hot side of a sample of vacuum glazing. The small temperature depressions near each pillar can be clearly seen. There are large temperature gradients near the edges due to the short-circuiting effect of the edge seal.

3.7 Total heat transport in vacuum glazing

The total heat flow through vacuum glazing can be estimated by combining the heat flows due to gas conduction, radiation, and pillar conduction with the influence of external heat transfer processes, including heat flow near the edges. In all well-made samples of vacuum glazing, gas conduction is negligible, and can be ignored.

Vacuum glazing is a very complex structure thermally, and there are significant interactions between heat transport due to the pillars, and due to radiation between the internal surfaces. As

has been shown, however, the influence of these interactions on the total rate of heat transfer between the glass sheets is small and, to a good approximation, can be ignored. Similarly, the temperature non-uniformities on the external surfaces of the glass sheets are also quite small (less than ±5% of the total temperature difference between the glass sheets for the majority (90%) of the heat flow due to the pillars). Thus, again to a good approximation, the pillar array can be represented by a uniform internal slab of material of appropriate thermal conductivity. The total thermal conductance between the glass sheets of a vacuum glazing can therefore be written:

Figure 17 Comparison of the temperature of the external surface of the hot side of a vacuum glazing as obtained from infrared thermographic data and finite element modelling. The sample has insulation up to the sight line on (a) the cold side and (b) the warm side.

$$C_{g-g,total} = C_{g-g,gas} + C_{g-g,radiation} + C_{g-g,pillars}, \tag{13}$$

$$= 0.8P + 4e_{eff}\,s\,T^3 + 2k_{glass}a/l^2 \tag{14}$$

where P is the internal pressure, measured in pascals, and the other quantities are defined earlier.

The total, air-to-air thermal conductance of a vacuum glazing is obtained in two parts. Remote from the edges, the conductance is calculated simply by combining the internal conductance in series with the external heat transfer coefficients:

$$\frac{1}{C_{a-a,center-of-glazing}} = \frac{1}{C_{a-g}} + \frac{1}{C_{g-g}} + \frac{1}{C_{g-a}} \quad . \tag{15}$$

There is additional air-to-air heat flow associated with lateral thermal conduction near the edges. For a rectangular glazing of dimensions a,b, the edge conductance can be estimated from eqn (12) as:

$$C_{a-a,edge} = \frac{2(a+b)}{ab} k_{glass} t \Bigg/ \left(\sqrt{\frac{k_{glass} t}{h_1}} + w_1 + w_2 + \sqrt{\frac{k_{glass} t}{h_2}} \right). \tag{16}$$

The total air-to-air thermal conductance of the glazing is obtained by adding the contributions from the centre-of-glazing (eqn (15)) and the edges (eqn (16)):

$$C_{a-a,total} = C_{a-a,centre-of-glazing} + C_{a-a,edge}. \tag{17}$$

Figure 18 shows typical calculated values for thermal conductances for vacuum glazing for different glass thicknesses, and combinations of uncoated and low emittance glass, as a function of pillar radius. These data show that vacuum glazing can be produced having an overall air-to-air thermal conductance with a wide range of values. At one level, vacuum glazing with a thermal conductance of ~ 3 W m^{-2} K^{-1} offers a practical alternative to simple double glazing without low emittance coatings and low thermal conductivity gas. Such glazing can achieve a very useful reduction in the heat transport through a window compared with single glazing. It may find widespread use in retrofit applications. At the other extreme, vacuum glazing can achieve overall air-to-air thermal conductance values below 1 W m^{-2} K^{-1}. Such values can only be produced conventionally using quite sophisticated and expensive approaches. Although the

market for such “superwindows” is at present quite small, it has significant growth prospects. It is likely that vacuum glazing will be very competitive in this performance range.

The validity of the approach outlined here for determining overall heat transport rates through vacuum glazing has been confirmed by comparison with experimental heat transport data on actual samples. This comparison has been described in detail in [65]. Several large (1m square) samples of vacuum glazing were produced at the University of Sydney, and measured at overseas laboratories. Heat transport data were obtained at NRC (Canada), TNO (the Netherlands), and SINTEF (Norway). Each measurement was made in a well calibrated guarded hot box instrument. The vacuum glazing samples were mounted in the hot boxes in a way that permitted precise control of the heat transfer coefficients on both sides, and accurate knowledge of the parasitic heat flows through the mounting structure. Measurements were made over a range of temperature differences, with warm side temperatures around 23°C, and cold side temperatures from +9 °C to -21°C. In all cases, to within experimental error (typically about 6%), excellent agreement was obtained between the overseas results and estimates heat transport rates based on measurements of local heat flow and modelling calculations made at the University of Sydney.

Figure 18 Calculated values of overall air-to-air thermal conductance of vacuum glazing. Results are shown for 3 mm glass, with a pillar separation of 20 mm, and 4 mm glass with a separation of 25 mm. Data are presented for various combinations of uncoated glass and low emittance glass. The edge conductance has been calculated for a 1 m x 1 m glazing, with the edge recessed 5 mm into an insulating frame. This corresponds to contributions to the overall conductance of 0.31 W m^{-2} K^{-1} and 0.39 W m^{-2} K^{-1} for 3 mm, and 4 mm glass, respectively.

4. Mechanical stresses in vacuum glazing

The complexity of the mechanical stresses in vacuum glazing mirrors that of the heat flow through the devices. Each pillar acts to concentrate the mechanical stresses due to atmospheric pressure. In addition, there are stresses at all points in the glazing due to temperature differentials. Such temperature-induced stresses can be particularly severe near the edges of the glazing. Further edge stresses can occur in improperly designed glazing if the support pillars deform inelastically.

As for heat transport, it is impractical to attempt to develop a single model to analyse these stresses. Rather, it is necessary to seek approximate methods to analyse these several effects, and to combine the stresses in order to understand their influence on the glazing as a whole. In this section, such an analysis is presented. We first discuss the significance of stresses to the mechanical failure (fracture) of glass.

4.1 Strength of glass

Glass is an amorphous substance which can fail catastrophically under tensile stress. The fracture of glass almost always originates at a small imperfection, or flaw, on the surface. If the flaw enlarges upon application of the stress, it will usually grow progressively faster, ultimately leading to catastrophic failure. The condition for flaw growth, first proposed by Griffith [79] in 1920 is that, for an infinitesimal crack extension, the reduction in strain energy in the glass exceeds the energy required to produce new surfaces. The application of this Griffith criterion to determine whether a structure will fail strictly speaking requires a complete knowledge of the stresses within the glass, and the number and size distribution of the flaws on the surface of the glass.

In practice, conventional glass structures are designed according to standards which have been found in practice to give adequately low probabilities of failure [44, 53]. Whilst some standards claim a basis of fracture theory, the values given usually seem to be more related to practical experience. Often, the standards simply quote a maximum allowable tensile stress for glass of a specific thickness. For example, the Australian design standard [80] for installation of glass in buildings quotes allowable levels of long-term, and intermittent tensile stress of approximately 8 MPa, and 16 MPa, respectively.

The allowable values of stress in the standards serve as a rough guide for what might be acceptable in vacuum glazing. They are of limited use, however, in the detailed design of such a structure. For example, these values make no allowance for the effect of area; a large area of glass is more likely to fail than a small area when stressed to the same level simply because there is more chance of finding a flaw above the critical size. In vacuum glazing, the stresses are highly non-uniform. It seems likely that levels of stress significantly greater than those given in the standards might be tolerable provided that they occur over only a small fraction of the area of the glazing.

Several models exist for the prediction of probability of failure of glass which can, in principle, account for such variability in the stresses [81]. These models use an empirically determined relationship for the probability of failure of an area of glass which is stressed to a particular level. For a non-uniformly stressed piece of glass, the probability of failure can be determined by dividing the glass into several smaller areas of reasonably uniform stress, and combining the elemental probabilities of survival of all such elements.

Most glass failure models have been specifically developed for situations where the stress levels are quite high, and failure is likely to occur within a very short time following application of the

stress. For lower levels of stress, it is necessary to consider the slow, "sub-critical" crack growth that is known to occur under such situations. In such cases, the models can be used to estimate whether a flaw will grow to a value where catastrophic failure occurs within the time of interest.

It has been shown [53, 81] that conventional approaches to the calculation of probability of delayed failure lead to unreasonably high values of this probability over long periods of time. The probability of long-term failure of, say, 1 m^2 of glass sheet stressed to 8 MPa, is estimated to be near to unity by the models, in contradiction to the (presumably experientially based) values allowed in the standards. This discrepancy arises because of the assumption in the models that the relatively high growth rate data for cracks in glass as a function of stress may be extrapolated indefinitely to unmeasurably small rates. This is equivalent to assuming that all flaws in glass, no matter how small, will grow under tensile stress. Although the experimental data for very slow growth of cracks is limited [82, 83], it tends to contradict this assumption. Indeed, it appears that, for a flaw of a given size, there is a value of tensile stress below which crack growth does not occur at all.

Fischer-Cripps and Collins [81] have developed a Modified Crack Growth Model which incorporates such a lower limit to crack growth. The probability of long-term failure of glass at a particular stress has been shown to be equal to the probability of instantaneous failure at a higher stress, calculated by multiplying the applied stress by the ratio of the stresses at instantaneous failure, and at the sub-critical growth limit. When applied to glass containing stresses permitted by the standards, this modified model gives reasonable results.

The Modified Crack Growth Model has been applied to vacuum glazing in order to provide estimates of the probability of failure of this non-uniformly stressed structure [54, 81]. Although the model gives quantitative numbers, it is important not to place too much credence on the actual values, depending as they do on assumptions about the flaw distributions in the glass and the nature of crack growth very close to the sub-critical limit. This method is useful, however, in that it can also be applied to conventional glass structures, for which a great deal of field experience and reliability data exist. The modified model thus provides valuable comparative estimates of the probabilities of failure of vacuum glazing and conventional glazings. In Section 5, a method is described for applying the Modified Crack Growth Model to calculate the probability of failure of vacuum glazing.

4.2 Stresses due to atmospheric pressure

Figure 19 shows schematically the regions of significant stress in vacuum glazing due to atmospheric pressure. Stresses occur on the external surface of the glass sheets above each support pillar, on the inside of the glass sheets around the area of contact of each pillar, in the pillars themselves, and on the outside surfaces of the glass sheets just inside the edge seal. Each of these stresses is now discussed in turn.

4.2.1 Stresses above each support pillar

The region of tensile stress on the external surface of the glass sheets, above each support pillar, occurs because atmospheric pressure causes the glass to bend inwards over the pillar. This stress is isotropic close to the pillars. Its magnitude is determined by the separation of the pillars and the thickness of the glass sheets. This stress is essentially independent of the diameter of the pillars, for the small pillars used in practical vacuum glazing. Figure 20 shows the magnitude of the radial component of this tensile stress in the region over the pillars for 4 mm glass sheets, as obtained by finite element modelling [44, 53, 61].

The data of Fig. 20 can be used to estimate values for the separation of the pillars which lead to acceptable levels of tensile stress. As a rule of thumb, the experimental samples of vacuum glazing made at the University of Sydney have been designed to give a maximum tensile stress due to atmospheric pressure above each support pillar of 4 MPa, which is about one half of that stated in the glass standards as acceptable for continuous stresses [33, 44, 50, 80]. This corresponds to a pillar separation of about 25 mm for 4 mm thick glass. It is possible that slightly larger separations may prove to be acceptable in practice, but it would require extensive field trial experience to demonstrate this. At this time, glazings designed according to this stress criterion have never been observed to fail as a result of these tensile stresses above the support pillars. By way of comparison, glazings made with 4 mm thick glass and 50 mm pillar separation almost all fail within a few months of manufacture. The failure is due to a crack which initiates in the region of large tensile stress on the external surface of the glass, above a support pillar.

Figure 19 Schematic diagram showing regions of stress in vacuum glazing due to atmospheric pressure.

Figure 20 Radial tensile stress due to atmospheric pressure on the external surface of the glass sheet above each support pillar.

The values of pillar separation which lead to tolerable levels of tensile stress are used as one of the criteria for designing the pillar array in vacuum glazing, as discussed in detail in Section 5 below.

4.2.2 Indentation stresses at each support pillar

Each pillar supports the force due to atmospheric pressure over a unit cell of the pillar array. The pillars are therefore pressed into the glass sheets with substantial force, resulting in stresses in the glass, and a small elastic indentation of the pillar into the glass. (The implications of this indentation are discussed shortly.) This situation is not unlike that in a classical Hertzian indentation experiment [84], where a hard sphere is forced into the surface of a brittle solid, such as glass. Such indentation can lead to the formation of a conical indentation crack around the area of contact of the sphere.

The conditions for formation of a conical indentation crack, in terms of the force and the radius of the spherical indenter (or equivalently the radius of the contact area between the indenter and the glass) are well understood [85-88]. It turns out that no crack is formed, for a particular contact radius, below a well-defined value of indentation force. This result can be used to define values of pillar separation and pillar radius for which conical indentation cracks do not occur. This is the second of the design criteria for the pillar array, discussed in Section 5.

4.2.3 Stresses in the support pillars, and near the edges of the glazing

The large indentation stresses in the glass sheets are accompanied by correspondingly large compressive stresses in the pillars themselves. If the compressive strength of the pillar is not sufficiently great, plastic flow of the pillar material can occur, with a significant increase in radius and, more importantly, a large axial deformation. The deformation of the pillars can lead to unacceptably large tensile stresses around the edges of the glazing. These stresses are directed normal to the edge seal and arise because the glass sheets bend inwards from the relatively non-compliant edge seal as a result of the pillar deformation. Such effects have been observed to lead to failure of experimental samples of vacuum glazing constructed with pillars having inadequate strength.

In order to avoid this effect, it is necessary to restrict the levels of compressive stress in the support pillars to values below the compressive strength of the pillar material. This provides a third stress-related design criterion for the pillar array, as discussed in Section 5.

It is emphasised that the inelastic pillar deformations considered here are additional to the unavoidable elastic deformations associated with the indentation of the support pillars into the glass. Such elastic indentations also lead to edge bending; however, the magnitude of this indentation/bending is relatively small, and the associated stresses are also tolerably small, for most designs of pillar array [44, 53].

The magnitude of the edge stresses due to plastic deformation and indentation have been estimated by finite element modelling [54]. In this calculation, the glass sheet was simulated using a plate model, and the pillars approximated as beam supports having appropriate compliance. Figure 21 shows typical results for deflections and stresses obtained from such an approach.

Stresses near the edge seal due to pillar deformation can be combined with those due to temperature differentials, and used in the failure model to obtain an estimate of the probability of

failure of the entire glazing under specific external environmental conditions and mechanical constraints. This is discussed in Section 5.

Figure 21 An example of the stresses on the external surface of the glass sheets due to atmospheric pressure, as obtained by finite element modelling. This figure shows the stresses normal to, and perpendicular to the edge of the glazing along a line over a row of support pillars. The glass sheets are 3 mm thick.

4.3 Stresses due to a temperature differential

The existence of a temperature difference across a vacuum glazing causes the hot glass sheet to expand relative to the cold one. This results in shear forces at the bonded edge seal which have two effects [38, 44, 50, 53, 54]. Firstly, stresses occur which are compressive in the hot sheet, and tensile in the cold sheet. Secondly, moments occur around the edge of the glazing which cause the structure to bend, with the hot side becoming convex outwards. The stresses due to bending are tensile on the external surface of the hot sheet and, for an unconstrained sample and a uniform temperature difference between the sheets, exceed those due to compression by a factor of approximately 3. The nett effect is that the external surface of the hot sheet is in tension, and the internal surface of this sheet has compressive stresses of about double the magnitude of the tensile stress. Stresses in the opposite sense exist in the cold glass sheet.

In this case, the largest tensile stresses occur on the internal surface of the cold glass sheet. It might be thought that it is these stresses that are of greatest importance for fracture of the glass. This is not the case, however, because this surface is evacuated and in an essentially water-free environment. Glass is known to be very strong under such conditions. In fact, the stresses of most concern are those on the external surface of the hot side of the glazing.

The presence of the bonded edge seal significantly complicates the stress distribution within the glazing. There are four separate effects which influence the stresses. Firstly, the edge region is twice as thick as either glass sheet, and therefore significantly stiffens the structure, reducing the

bending. Secondly the curvature of the glazing at the edge in the direction along the edge is the same as that remote from the edge. Therefore, because the edge is twice as thick as either glass sheet, the stresses *due to bending* parallel to the edge are twice as large as those *due to bending* remote from the edges, or about three times the value of the centre-of-glazing stresses on the hot side. Thirdly, as discussed in Section 3.6 above, the edge seal short circuits the temperature difference between the glass sheets, and can thus significantly reduce the average temperature difference over the whole of the glazing; this decreases the bending and the associated stresses. Finally, additional stresses exist in the edge region, parallel to the edges, if the temperature of the edge is different from the average temperature of the glazing as a whole. This stress is very similar to the tensile "hoop" stress known to occur in the edge region of the hot glass sheet of conventional double glazing. In vacuum glazing, this additional stress is tensile if the temperature of the edge seal is less than the average temperature of the glazing. The analysis presented in Section 3.6 shows that the temperature of the glazing near the edges can be significantly influenced by the design of the external frame; in particular edge insulation on both sides increases the edge temperature, thus reducing these additional tensile stresses. Insulation on the cold side around the edge is even more effective at raising the edge temperature and reducing these stresses, although this may not be a realistic option for practical frame designs.

A finite element model has been developed to determine the magnitude of the stresses due to a temperature differential in vacuum glazing [38, 44, 50, 53, 54]. Typical results for an unconstrained sample with, and without edge insulation, are shown in Fig. 22. As can be seen, the largest tensile stresses are parallel to the edge on the hot side of the glazing, in the region of the edge seal. Results such as these have been used, in combination with other stresses, to provide a quantitative estimate of the probability of failure of vacuum glazing as discussed in Section 5 below.

Figure 22 Finite element modelling results for the stresses parallel to the x-axis due to a temperature difference on the external surfaces of an unconstrained sample of vacuum glazing. These data are for the ASTM conditions. The vacuum glazing is 1 m x 1 m in size, and is made from two sheets of 4 mm thick K glass, and has a glass-to-glass conduction due to radiation and pillars of 1.10 W m^{-2} K^{-1}. Stresses are shown on (a) the warm side, and (b) the cold side.

In the finite element model used to calculate stresses due to a temperature differential, the pillar array is replaced with a uniform slab of material of appropriate thermal conductivity. The justification for this approach is that, as discussed in Section 3.5, most of the heat flow through the pillars is spread fairly uniformly through the glass sheets. It is important to verify experimentally whether the finite element model does indeed accurately represent the behavior of a sample of vacuum glazing. In order to provide such justification, a sample of vacuum glazing was mounted between surfaces of well-defined temperature, in a way that established accurately known, and practically realistic heat transfer coefficients at the glass surfaces on either side [54].

Figure 23 shows a schematic diagram of the apparatus used for these measurements. The sample was 500 mm x 500 mm in size, and was supported in an essentially unconstrained configuration. The surface of the hot side of the sample was instrumented with thermocouples and strain gauges. In addition, accurate measurements were made of the bending of the sample using a dial micrometer that was moved sequentially along a bar which was located in a precisely reproducible way relative to points near the edge of the glazing.

Figure 23 Schematic diagram of the apparatus for measuring the strains and bending of an unconstrained sample of vacuum glazing under a well defined temperature difference with closely controlled heat transfer coefficients at the external surfaces.

The heat transfer at either side of the glazing was by gas conduction, and radiation across the gap between the glass surfaces and the hot and cold plates. Convection was negligible because the hot plate was on top of the cold plate. Estimates made of the heat transfer coefficients on each side were in very good agreement with values determined from accurate measurements of the surface temperature of the glass on either side, remote from the edge seal, and a knowledge of the thermal conductance of the glazing itself. The latter quantity was accurately known from measurements on the guarded hot plate, discussed in Section 3.2.1.

Figure 24 compares experimental and modelling results for the deflection across the glazing, and Fig. 25 provides a similar comparison of the maximum deflection, for various values of temperature difference. Figure 26 compares measured and calculated *stress-related* strains parallel, and perpendicular to a side of the glazing along a line perpendicular to that side and passing close to the centre of the glazing. In all cases, excellent agreement was obtained between the experimental data and the modelling results. This provides strong justification for the validity of the finite element analysis, and the approximation of uniform heat flow through the pillar array.

Figure 24 Experimental (points) and modelling (lines) values of the deflection of a sample of vacuum glazing for several temperature differences, with closely controlled external heat transfer coefficients.

Figure 25 Experimental (points) and modelling (line) values of the maximum deflection of a 500 mm x 500 mm sample of vacuum glazing as a function of temperature difference with closely controlled external heat transfer coefficients.

Figure 26 Measured (points) and modelling (lines) values of strain on the external surface of the hot side of a sample of vacuum glazing for various values of temperature difference with closely controlled external heat transfer coefficients.

4.4 Stresses due to wind

At this time, no work has been undertaken of the effect on vacuum glazing of external sources, such as wind. It is expected that the bending of vacuum glazing under a uniform external pressure differential (which is how the effects of wind are usually assessed) will be the same as that of a solid sheet of glass of thickness equal to the total of the two glass sheets in the vacuum glazing. This topic remains for future study.

4.5 Combined stress in vacuum glazing

The previous sections have described the mechanical tensile stresses in vacuum glazing due to atmospheric pressure and temperature differentials. Finite element models have been developed for determining these stresses and, in all cases, the results of the models have been validated by comparison with experimental data. It is, in principle, a straightforward matter to determine the resultant stresses in vacuum glazing by combining the stresses due to the separate effects. This can be done by determining the various stresses in two orthogonal directions - say parallel to the two axes of the glazing σ_{xx} and σ_{yy} - and the shear stress for these two axes τ_{xy}. The maximum and minimum combined principal stresses σ_{11} and σ_{22} are then obtained from:

$$s_{11}, s_{22} = \left(\frac{s_{xx} + s_{yy}}{2}\right) \pm \sqrt{\left(\frac{s_{xx} - s_{yy}}{2}\right)^2 + t_{xy}^{\,2}}. \qquad (18)$$

In practice, this procedure is far from simple [54] because of the very complex structure of vacuum glazing; a 1 m x 1 m sample typically contains in excess of 1,500 pillars. Moreover, as has been noted, the actual values of the stress due to the various influences depend on the specific design features of the glazing, and on the externally imposed conditions - temperatures, heat transfer coefficients, edge insulation, and mechanical constraints. A comprehensive design study of vacuum glazing has therefore not yet been performed. It would seem appropriate to defer such a study until a more mature design of vacuum glazing has been developed, and some progress has been made on suitable frames for this type of glazing.

Despite these reservations, some work has been done on estimating combined stresses and, from these stresses, calculating the probability of failure [53, 54]. The results of this study have provided some conclusions which probably have general significance to the as-yet-incomplete comprehensive design analysis.

Firstly, and perhaps most obviously, there is a great deal of repetition in the mechanical stresses in vacuum glazing across the unit cells of much of the pillar array. Over this part of the glazing, the important stresses are on the hot side, above each support pillar. In this region, the stresses due to a temperature difference, and those due to bending of the glass sheet over the support pillar, are both tensile.

The second general conclusion, again reasonably obvious, is that the stresses near the edge of the glazing on the hot side can contribute significantly to the probability of failure. These stresses are much more complex and depend on all of the factors mentioned above. In a poorly designed glazing where the mechanical strength of the support pillars is inadequate, the stresses normal to the edge can be very large, and failure is likely to occur due to a crack, just inside and parallel to the edge seal. The second important stress in this region - that parallel to the edge due to a temperature difference may, or may not be significant depending on the design of the glazing surrounds, including edge insulation.

A third general result is worth reporting here, although so far only very preliminary data have been obtained. The presence of mechanical constraints significantly influences the magnitude and distribution of the stresses due to a temperature difference across the glazing [53]. It appears that restricting the bending of the edges in an elastic frame reduces the stresses parallel to the edges over most of the edge, but increases these stresses in the corner region. It also seems that, with an appropriate design of edge constraint, significantly lower *overall* stresses due to a temperature difference can be obtained in vacuum glazing, relative to the unconstrained case. This is clearly an area requiring further work.

5. Design of vacuum glazing

The design process for vacuum glazing aims to determine a reasonable compromise between the thermal conductance of the glazing (mainly that associated with the pillar array), and the mechanical tensile stresses in regions which can lead to fracture. As noted in Section 3 above, the thermal conductance is influenced by heat transport due to radiation and pillar conduction within the glazing (heat transport due to gaseous conduction is negligible in well-designed glazing), and heat transfer between the environment and the external surfaces of the glass sheets. The overall, air-to-air thermal conductance is obtained by adding an additional contribution due to heat flow through the edge seal. The mechanical stresses of principal concern are on the external surfaces of the hot glass sheet, in regions above each support pillar, and near the edges.

The design process involves choosing specific criteria relating to the thermal conductance and stresses, and then determining dimensions of the pillar array for which these criteria are satisfied. Two approaches which achieve this have been developed [53, 54], and these are now discussed in turn.

5.1 Pillar array design method

The first approach to the design of vacuum glazing concentrates on the dimensions of the pillar array [33, 53]. This approach leads to a specification of device parameters which result in the lowest possible thermal conductance of the pillar array, and for which three specific stress-related criteria, as discussed in Section 4.2, are satisfied. These criteria are now re-stated.

(a) The support pillars must be made from a material having a compressive strength which is greater than the compressive stress in the pillars. Figure 27a shows values of pillar separation λ and pillar radius a which lead to specific values of compressive stress in the pillars. In order that this criterion is satisfied, the values of pillar separation and pillar radius must be chosen to be below the line corresponding to the mechanical strength of the pillar material.

(b) Conical indentation fracture in the glass sheets adjacent to the support pillars must not occur. Figure 27b shows allowable values of pillar separation and pillar radius which satisfy this criterion.

(c) The maximum tensile stress due to atmospheric pressure above the support pillars must be less than some specified value. There is considerable arbitrariness in the actual value of stress selected in this process. Figure 27c shows values of pillar separation which lead to different values of maximum tensile stress. Collins and Fischer-Cripps [33] selected 4 MPa as an appropriate design value, being about one half of the maximum allowable continuous stress in glass as specified by the Australian design standard [80]. This choice therefore leaves additional allowance for further steady state stress due to

temperature differentials. There is scope for refining this criterion in the second design approach discussed below.

Values of pillar separation and pillar radius which correspond to specific values of glass-to-glass pillar conductance are shown in Fig. 27d.

The three stress-related design criteria are combined in Fig. 27e with the appropriate relationship for pillar conductance. This leads to specific values for pillar separation and pillar radius for which all design criteria are met.

Figure 27 The pillar array design method for vacuum glazing. Values of pillar separation and pillar radius are shown which: (a) give specific values of compressive stress in the support pillars; (b) do not lead to conical indentation cracks near the support pillars; (c) lead to specific values of maximum tensile stress above the support pillars; and (d) result in specific values of the glass-to-glass pillar array conductance. In Figure 27e, these four criteria are combined to yield design values for pillar separation and pillar radius for a glazing satisfying the constraints noted.

It is not meant to imply that this method represents a rigorous approach to the design of vacuum glazing. The design values obtained are influenced by the judgment made for the appropriate value of maximum tensile stress above the pillars, and on the availability of a material for the pillars with adequate compressive strength. Nevertheless, the relationships illustrated in Fig. 27e are close to those which apply in the experimental samples of vacuum glazing made at the University of Sydney for testing in the Task 18 program.

This design approach has been very useful in developing the technology of vacuum glazings since it has led to the selection of values for pillar separation and pillar radius which give glazings of reasonable performance. In concentrating on the pillar array, however, this method does not lend itself to an optimisation of the overall glazing design. Moreover, the selection of a maximum tensile stress in the glass is inconsistent with the statistical nature of the glass fracture. In a large sample of glass, for example, the probability of finding a flaw above a critical size is greater than in a small sample, simply because of the differences in area. In vacuum glazing, the high stresses above each support pillar occupy only a small fraction of the total area of the glass. It therefore seems likely that a statistical analysis which includes the area of the sample, the spatial variations in stresses, and the overall stress levels in the glazing (due to atmospheric pressure and temperature differentials) would provide a better basis for the design of vacuum glazing. Such an approach is now described.

5.2 Probabilistic design approach

The starting point of this second design approach is the specification of certain parameters, including the area of the glazing, the maximum air-to-air temperature difference that it must withstand, the internal radiative conductance, $C_{radiation}$, and the conductances associated with heat transfer to and from the external surfaces, $C_{a\text{-}g}$ and $C_{g\text{-}a}$ respectively. The conditions derived in the first design process relating to conical indentation fracture (Fig. 27b) and maximum compressive stress in the support pillar (Fig. 27a) also apply in this case. The condition for maximum tensile stress above the pillars due to atmospheric pressure is, however, replaced by an estimate of probability of failure for the glazing. The design process proceeds as follows:

1. For each value of pillar separation, l, and pillar radius, a, the thermal conductance of the pillar array, $C_{pillar\ array}$, is determined from eqn (11). The overall glass-to-glass thermal conductance of the glazing is then calculated from eqn (14).

2. The temperature distribution within the glazing is calculated using assumed values of external air-to-glass conductances. This may be done using an analytic model involving the relevant conductances, or by numerical analysis. Both approaches give estimates of the temperatures near the middle of the glazing, and in the vicinity of the edge seal. The temperatures, particularly near the edge seal, are strongly influenced by the presence of insulation around the seal.

3. The mechanical stresses in the glazing due to temperature non-uniformities are now determined. Once again, both analytic and numerical approaches are applicable. Finite element methods are particularly useful, however, since they permit inclusion of effects due to mechanical edge constraints.

4. The temperature induced stresses are combined with stresses above the support pillars due to atmospheric pressure to give the overall mechanical stresses in the glazing. Above each support pillar, the tensile stresses on the external surfaces due to both influences are isotropic and the total stress is obtained by simple addition. Near the edge

seal in a well-designed glazing, the dominant stress is temperature-induced and is unidirectional, parallel to the edge of the glazing. For glazings in which the pillars provide inadequate support, there are additional tensile stresses due to atmospheric pressure on both sides near the edge, perpendicular to the edges.

5. The Modified Crack Growth Model [81] is then used to determine the probability of failure of vacuum glazing containing these stresses. In order to do this, the glazing is divided into small regions. The size of these regions is chosen so that the stress in each one is nearly uniform. The probability of delayed failure for region *i* of area A_i containing stress σ_i is equal to the probability of instantaneous failure at a higher, effective stress $\sigma_{e,i}$ and is given by a Weibull distribution [89]:

$$P_{f,i} = 1 - \exp(-kA_i\,\sigma_{e,i}^{m}). \tag{19}$$

According to the Modified Crack Growth Model, the effective stress for failure over long periods of time (defined as times greater than that necessary for a flaw to grow from the sub-critical crack growth limit, to catastrophic failure), is given by:

$$\sigma_{e,i} = \sigma_i\,(K_{1C}/K_{1SCC}). \tag{20}$$

In this expression, K_{1C} and K_{1SCC} are the stress intensity factors for instantaneous fracture, and at the sub-critical growth limit, respectively. The constants m and k are empirically determined from fracture experiments on glass [90, 91].

The calculation of probability of failure using eqns (19) and (20) assumes an isotropic (biaxial) stress in the glass. When the stress is not isotropic, the effective stress is reduced by a factor which can be calculated from the nature of the stress concentration around a flaw. For a uniaxial stress, the effective stress is reduced from the value given in eqn (20) by a factor of 0.8 [53].

6. The probability of failure of the entire glazing is calculated by combining the probabilities of failure, $P_{f,i}$, for each element. For the element i, the probability of survival is:

$$P_{s,i} = 1 - P_{f,i}\ . \tag{21}$$

The probability of survival of the entire glazing is given by:

$$P_s = P_{s,1} \cdot P_{s,2} \cdot P_{s,3} \ldots P_{s,i} \cdot \ldots\ . \tag{22}$$

The probability of failure of the whole glazing is then:

$$P_f = 1 - P_s\ . \tag{23}$$

This process leads to the determination of values of failure probability as a function of pillar separation *l* and pillar radius *a*. The actual values of these probabilities depend on the specific conditions chosen for the calculation: for example, the area of glazing and the maximum temperature differential. The dependence of probability of failure on pillar separation is obvious - more closely spaced pillars lead to lower values of external tensile stress. There is also a slight dependence of probability of failure on pillar radius, *a*, through the effect of this parameter on the glass-to-glass thermal conductance of the glazing. This leads to differences in the temperatures of the glass sheets (since these temperatures are affected by this conductance and the external air-to-glass conductances) and thus to variations in the stresses in the glazing.

The values of probability of failure which emerge from this process are also dependent on the specific parameters chosen which characterise the strength of glass: m and k. Values of m and k have been measured for as-received glass, and weathered glass [90, 91].

It should be emphasised that the validity of this process depends on whether the Modified Crack Growth Model is, indeed, a realistic method of estimating the probability of delayed fracture in glass. It is certainly not claimed at this stage that this model is well validated. However, the model is the only one which, for glass samples containing allowable stresses (as specified by the glass standards over long periods of time), gives values of probability of failure that are reasonably consistent with accepted industry practice.

The usefulness of this process is not so much in the absolute values of failure probability that emerge from the calculation. Ultimately, any practical design of vacuum glazing will need to be validated through an extensive, well controlled field trial. Rather, the design approach provides insights into critical features of vacuum glazing, since it indicates regions where failure is most likely to occur.

The above method has so far been used to produce preliminary results only [54]. This work however, serves to illustrate the usefulness of this design approach. For example, for an unconstrained sample of vacuum glazing with uninsulated edges, the dominance of the contribution to failure probability from the edges emphasises the importance of reducing stresses in this region by thermal insulation, or mechanical constraint of the edges. This result also highlights a shortcoming of the method in its present form: no allowance is made for the effect of the cut edges of the glass sheets. It is known in the glass industry that the edges of glass sheets are weaker than the regions away from the edges. This is presumably due to flaws at the edge associated with the cutting process. The effect of such flaws can be reduced by grinding and polishing the edges, leading to lower failure rates in applications. It is not known whether quantitative data exist on edge flaw statistics. Such data would be particularly useful since, in conjunction with the Modified Crack Growth Model, they would enable quantitative predictions of edge-initiated failure rates. This would extend the validity of the design approach discussed here. The method should also be applicable to the estimation of failure probability of other glass structures.

6. Cost of vacuum glazing

In order for the full potential of vacuum glazing to be realised, it must be manufactured at a price which enables it to be sold competitively against other forms of insulating glazing. At the present stage of development, it is not possible to make precise estimates of the manufacturing cost of vacuum glazing. Based on experience with laboratory fabrication, however, the broad features of a volume production process, including the time necessary to complete critical steps such as formation of the edge seal and evacuation, can be determined with a reasonable degree of confidence. Since most of the steps in the manufacturing process, and the equipment and material used, are conventional, it is possible to set some bounds on the likely manufacturing cost. In this paper, we present a brief summary of the results of a study of manufacturing cost, the full details of which are to be published elsewhere [47].

Figure 28 is a possible floor plan of a proposed manufacturing facility for vacuum glazing. This facility is designed to produce approximately 100,000 m^2 of completed glazing per year, with due allowance of incomplete utilisation and losses by breakage. The production process has a moderate degree of automation, sufficient to achieve a volume of manufactured product that

permits the purchase of materials at large quantity prices. The production process is relatively labour intensive, however, since at the current stage of development of this technology, it is not possible to visualise the nature of highly automated manufacture.

The assumptions made on costs of equipment and materials are based on quotations obtained from companies who supply to the glass and glazing industries. Where equipment does not exist (for example, an automated pillar setting machine) estimates have been made based on the cost of other equipment of appropriate size or complexity. Labour costs are assumed to be those in the process industries in the United States. Appropriate costs have been included for repayment of loans on the building, equipment and materials in stock. The figures given here are therefore for completed, tested glazing at the end of the manufacturing process, and do not include any marketing costs, or profits. As a rough guide, the selling price of vacuum glazing is estimated to be about 3 times the direct manufacturing costs calculated here.

Despite the large uncertainties in our current understanding of the nature of the volume production process for vacuum glazing, the cost of manufacture can be determined reasonably accurately. This is because the dominant contribution to cost is the coated low emittance glass, the cost of which in large quantities is well known at this time. In fact, doubling the equipment cost from that assumed in the analysis only changes the estimate of manufactured cost by about 5%.

In order to compare vacuum glazing with other designs of insulating glazing, the same costing approach was applied to the manufacture of high performance double glazing containing low emittance coatings and argon gas fill. Table 3 compares the manufacturing cost of vacuum glazing and double glazing, with two and one sheets of low emittance coated glass, as obtained by this procedure.

Figure 28 Floor plan of factory for the manufacture of vacuum glazing.

Table 3 Comparison of costs of insulating glazing.

It can be seen that the costs of materials in vacuum glazing, and in double glazing, containing the same number of low emittance coatings, are very similar. This is not surprising. Both types of glazing have similar glass costs, and the costs of the edge spacer and sealants in double glazing are similar to those of the pillars and solder glass in vacuum glazing, and in each case, are small. There are additional costs of manufacture of vacuum glazing due to labour (US$3 m^{-2}), equipment (US$2 m^{-2}), and utilities (US$1 m^{-2}). Again, it is not surprising that the manufacture of vacuum glazing will incur larger equipment and utility costs due to the oven and vacuum systems required, and the power requirements of these facilities. The additional cost of labour arises because of assumptions made about the number of people required to run the production process. This has been estimated on the basis of the type of manufacturing process chosen, and industry practices for operating such processes. It is possible that the labour costs could be reduced somewhat, although this would probably necessitate slightly larger costs associated with increased automation in the manufacturing equipment.

On the basis of this analysis, it is concluded that the manufacturing costs of vacuum glazing will be about US$40 m^{-2} and US$34 m^{-2} for glazings with 2, and 1 low emittance coatings respectively. This compares with figures of US$34 m^{-2} and US$26 m^{-2} for double glazing. This higher cost of vacuum glazing must be justified by other advantages such as improved thermal insulation (which would be even better if a lower emittance sputtered coating that survives the edge sealing process could be used), smaller thickness, and (perhaps) increased reliability. It is also possible that the use of vacuum glazing may lead to reduced costs elsewhere in the overall window, for example, through the use of a less complex frame. It is clear, however, that vacuum glazing is unlikely to be a very low cost option for use in insulating windows. If vacuum glazing is to establish a competitive edge over other forms of glazing, it must be through its improved performance and other associated benefits.

7. Commercialisation Prospects for Vacuum Glazing

This Report has primarily discussed matters relating to the science and technology of vacuum glazing. Of course, if this technology is to have any practical value, it must be commercialised in a way that allows vacuum glazing to be sold competitively in the insulating glazing market. Since the commencement of the vacuum glazing project at the University of Sydney in 1988, every effort has been made to ensure that the commercialization of the technology, if it is at all possible, will occur. For example, an extensive patenting program has been undertaken by the University, at considerable cost. In addition, over the entire life of the project, every opportunity has been taken to discuss commercialization prospects with companies, in order to try to generate interest in the work, and to seek advice on how best to structure the research and development program to maximise the prospects for successful commercialisation.

In 1993, the Executive Committee of the International Energy Agency Solar Heating and Cooling Program met in Sydney, and saw the vacuum glazing work in the Department of Applied Physics at The University of Sydney. As a result of that visit, Dr. S Tanemura of Japan introduced the work to Mr. H. Kawahara of Nippon Sheet Glass Company Limited of Japan. As a result, NSG entered into a licensing agreement with the University, with the object of commercializing the technology. NSG and the University are also undertaking collaborative research on vacuum glazing to aid the commercialization process.

In January, 1997, Nippon Sheet Glass announced the commercial launch of its vacuum glazing product under the trade name SPACIA. NSG have built a pilot production facility to manufacture SPACIA, and have installed numerous glazings in the field. NSG and the University are continuing to collaborate in this area,

8. Conclusions

A technology for producing vacuum glazing in useful practical sizes has been developed. The production technology is conventional, in the sense that all materials and processes are relatively minor variants of those routinely used commercially.

Experimental and theoretical work has resulted in an excellent understanding of the heat transport processes in vacuum glazing. Four separate internal processes can contribute to such heat flow: gaseous conduction, radiation, thermal conduction through the support pillars, and lateral heat flow in the vicinity of the edge seal. Models have been developed for each of these heat transport processes, and these models have been validated by experimental measurements.

The internal vacuum has been demonstrated to be high, and stable over many years. The overall heat transport through samples of vacuum glazing can be estimated by combining the heat flow due to the separate heat transport processes. Independent measurements of overall heat transport through vacuum glazing in accurately calibrated guarded hot box instruments are in good agreement with estimates made on the basis of combining the effects of the separate internal heat transport processes.

A comprehensive understanding has been obtained of the nature and magnitude of stresses in vacuum glazing which could lead to mechanical failure. Significant stresses can occur due to atmospheric pressure, and temperature differentials. The design tradeoffs between stresses and heat transfer are well understood. It has been shown that highly insulating samples of vacuum glazing can be built, and operated under quite severe environmental conditions, with probabilities of failure due to stresses which are comparable to those in conventional insulating glazings.

The techniques used to manufacture vacuum glazing are similar to those routinely employed in the mass production of evacuated structures such as television picture tubes. The cost of manufacture of vacuum glazing is likely to be only slightly greater than for conventional, high performance double glazing. For manufacture in reasonable volume, the overall cost of vacuum glazing will be dominated by the cost of materials.

Commercial manufacture of vacuum glazings has commenced.

References

1 F. Zoller, Hollow pane of glass, *German Patent No. 387 655* (1924).

2 I.M. Kirlin, Insulating pane, *US Patent No. 1370974* (1921).

3 L.W. Whattan and L.M. Meyers, Manufacturing process for insulating panels or tiles, *French Patent No. 921946* (1947).

4 A.W. Rowe, Insulating materials, *US Patent No. 3936553* (1976).

5 F.R. Chalons, Transparent insulating panel, *French Patent No. 2294315* (1976).

6 G. Falbel, Evacuated dual glazing system, *US Patent No. 3990201* (1976).

7 F. Kesselring, Thermally insulating window, *Swiss Patent No. 588008* (1977).

8 W. Hermann and H. Horster, Evacuated thermal insulating glazing unit with an infrared reflecting coating, *US Patent No. 4038797* (1977).

9 C.J. Bennett, Flat vacuum container, *German Patent No. 2755013* (1978).

10 F.T. Coppola, W.P. Lentz and R.V. Van Dewoestine, Contoured insulation window for evacuated solar collector, *US Patent No. 4186723* (1980).

11 N.S. Kenny, Vacuum window for solar transmission, *US Patent No. 4186723* (1980).

12 J. Bourdel, Transparent insulating panes, *French Patent No. 2483564* (1981).

13 Y.C. Belentepe, L-C. Lee and A.H. Wilder, Contoured insulation window for evacuated solar collector, *US Patent No. 4359042* (1982).

14 T. Assarsson, Heat-insulating glass, *European Patent No. 0047725* (1985).

15 W.S. Kreisman, Method of fabricating a thermal pane window and product, *US Patent No. 4393105* (1983).

16 C. Ivorra, Method of joining two transparent panes, *French Patent No. 2526066* (1983).

17 D.K. Benson and C.E. Tracy, Laser sealed vacuum insulation window, *US Patent No. 4683154* (1987).

18 P.D. Beuther, Evacuated dual pane window structure, *US Patent No. 4786344* (1988).

19 K-P. Viert, Multiple insulating glazing with a high vacuum and supports between the sheets, *German Patent No. 3615179A1* (1987).

20 T.P. Kerr, S.B. Liu, P.P. Harmon, W.R. Siskos, J.L. Oravitz Jr and P.E. Shaffer, Vacuum insulating unit, *US Patent No. 5124 185* (1992).

21 D.K. Benson and T.F. Potter, Compact vacuum insulation embodiments, *US Patent No. 510 7649* (1992).

22 E. Bachli, Thermally insulating construction and/or lighting element, *European Patent No. 0247098B1* (1992), *US patent No. 5009218* (1991).

23 E. Bachli, Gastight edge seal, *European Patent No. 0434802B1* (1992).

24 Y. Demars and R. Muller, Method to produce a vacuum in an insulating glazing and an insulating glazing, *European Patent No. 0645516AZ* (1995).

25 K.R. Parker, Insulating glazing unit, Canadian Patent No. 1290624 (1991).

26 D.K. Benson, C.E. Tracy, G.J. Jorgensen, Laser sealed evacuated window glazings, *28th SPIE International Technical Symposium on Optics and Electro-Optics*, San Diego, CA (1984).

27 D.K. Benson and C.E. Tracy, Evacuated window glazings for energy efficient buildings, *29th SPIE International Symposium on Optics and Electro-Optics*, San Diego, CA (1985).

28 D.K. Benson and C.E. Tracy, Vacuum window glazings for energy-efficient buildings, *Internal Report PR-2901, Solar Energy Research Institute*, Golden CO (1986).

29 D.K. Benson, C.E. Tracy, J. Susemihl, T. Potter, D.E. Soule, Vacuum window glazings for energy-efficient buildings, *Internal Report PR-3159, Solar Energy Research Institute*, Golden, CO (1987).

30 D.K. Benson, L.K. Smith, C.E. Tracy, T. Potter, C. Christiansen, D.E. Soule, Vacuum window glazings for energy-efficient buildings: *Summary report, Internal Report SERI/TP-212-3684, Solar Energy Research Institute*, Golden, CO (1990).

31 S.J. Robinson and R.E. Collins, Evacuated windows - theory and practice, *ISES Solar World Congress* , International Solar Energy Society, Kobe, Japan (1989).

32 R.E. Collins, L. Poladian, B.A. Pailthorpe and R.C. McPhedran, Heat conduction through support pillars in evacuated windows, *Aust. J. Phys.*, **44**, 73-86 (1991).

33 R.E. Collins and A.C. Fischer-Cripps, Design of support pillar arrays in flat evacuated windows, *Aust. J. Phys.*, **44**, 545-563 (1991).

34 R.E. Collins and S.J. Robinson, Evacuated glazing, *Solar Energy*, **47**, 27-38 (1991).

35 R.E. Collins, A.C. Fischer-Cripps and J-Z. Tang, Recent developments in the science and technology of evacuated windows, *ISES Solar World Congress* , International Solar Energy Society, Denver, USA, 4025-4030 (1991).

36 R.E. Collins, Evacuated glazing, *SPIE International Symposium on Optical Materials Technology for Energy Efficiency and Solar Energy Conversion XI*, Toulouse, France, 18-22 (1992).

37 R.E. Collins, C.A. Davis, C.J. Dey, A.C. Fischer-Cripps, S.J. Robinson, T. Simko, J-Z. Tang, G.M Turner and X. Zhou, Measurement of thermal conductance of evacuated glazing, *TI5 Proceedings of the Fifth International Meeting on Transparent Insulation Technology*, edited by L.F. Jesch, 121-124 (1992).

38 R.E. Collins, A.C. Fischer-Cripps and J-Z. Tang, Transparent evacuated insulation, *Solar Energy*, **49**, 333-350 (1992).

39 A.C. Fischer-Cripps and R.E. Collins, Stresses and fracture probability in evacuated glazing, *TI5 Proceedings of the Fifth International Meeting on Transparent Insulation Technology*, edited by L.F. Jesch, 53-56 (1992).

40 R.E. Collins, C.A. Davis, C.J. Dey, S.J. Robinson, J-Z. Tang and G.M. Turner, Measurement of local heat flow in flat evacuated glazing, *Int. J. Heat Mass Transfer*, **36**, 2553-2563 (1993).

41 R.E. Collins, C.J. Dey, A.C. Fischer-Cripps, G.M. Turner, J-Z. Tang, T. Simko and D.A. Clugston, Production and properties of evacuated glazing, *ISES Solar World Congress* , International Solar Energy Society, Budapest, Hungary (1993).

42 D.A. Clugston and R.E. Collins, Pump down of evacuated glazing, *J. Vac. Sci. Technol. A* **12**, 241-247 (1994).

43 A.C. Fischer-Cripps and R.E. Collins, The probability of Hertzian fracture, *J. Mat. Sci*, **29**, 2216-2230 (1994).

44 A.C. Fischer-Cripps and R.E. Collins, G.M. Turner and E. Bezzel, Stresses and fracture probability in evacuated glazing, *Build. Environ.* **30**, 41-59 (1995).

45 G.M. Turner, R.E. Collins, G.R. Facer and M.A. Scheumack, Outgassing effects in evacuated glazing, *SPIE International Symposium on Optical Materials Technology for Energy Efficiency and Solar Energy Conversion XIII*, Freiburg, Germany (1994).

46 G.M. Turner, R.E. Collins, A.C. Fischer-Cripps and J-Z. Tang, Limits to performance of evacuated glazing, *SPIE International Symposium on Optical Materials Technology for Energy Efficiency and Solar Energy Conversion XIII*, Freiburg, Germany (1994).

47 J.D. Garrison and R.E. Collins, The manufacture and cost of vacuum glazing, *Solar Energy* **55**, 51-61 (1995).

48 Q.-C. Zhang, T.M. Simko, C.J. Dey, R.E. Collins, G.M. Turner, M. Brunotte and A. Gombert, The measurement and calculation of radiative heat transfer between uncoated and doped tin oxide coated glass surfaces, *Int. J. Heat Mass Transfer*, **40**, 61-71 (1996).

49 T.M. Simko, R.E. Collins, F.A. Beck and D. Arasteh, Edge conduction in vacuum glazing, *Proceedings of Thermal Performance of the Exterior Envelope of Buildings IV, Heat Transfer in Fenestration II*, 601-611, Florida (1995).

50 R.E. Collins, G.M. Turner, A.C. Fischer-Cripps, J.-Z. Tang, T.M. Simko, C.J. Dey, D.A. Clugston, Q.-C. Zhang and J.D. Garrison, Vacuum glazing - a new component for insulating windows, *Build. Environ.* **30,** 459-492 (1995).

51 R,E. Collins, T.M. Simko, G.M. Turner. J.-Z. Tang, J.D. Garrison and A.C. Fischer-Cripps, An overview of the technology of vacuum glazing, *Window Innovations '95*, 578-587, Toronto (1995).

52 G.M. Turner and R.E. Collins, Measurement of heat flow through vacuum glazing at elevated temperatures, *Int. J. Heat Mass Transf.* in press.

53 A.C. Fischer-Cripps, Stresses and fracture probability in evacuated glazing, Ph.D Thesis, The University of Sydney (1993).

54 T.M. Simko, Heat transfer processes and stresses in vacuum glazing, Ph.D. Thesis, The University of Sydney (1996).

55 C.J. Dey, Measurement and characterisation of novel insulating materials, Ph.D. Thesis, The University of Sydney (1997).

56 R.E. Collins and S.J. Robinson, A thermally insulating glass panel and method of construction, *Patent Application No. PCT/AU90/00364* (1990).

57 R.E. Collins and J.-Z. Tang, Improvements to thermally insulating glass panels, *Patent Application No. PCT/AU93/00040* (1993).

58 R.E. Collins, J.-Z. Tang and D.A. Clugston, Methods of construction of evacuated glazing, *Patent Application No. PCT/AU94/00305* (1994).

59 R.E. Collins and J.-Z. Tang, Design improvements to vacuum glazing, *Patent Application No. PCT/AU95/00640* (1995).

60 R.E Collins, A.C. Fischer-Cripps, J-Z. Tang, G.M. Turner, C.J. Dey, D.A. Clugston, E.B. Hansen, T. Simko, B. Norton, P.C. Eames, S.N.G. Lo, V. Wittwer and M. Brunotte, State-of-the-art of evacuated glazing, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD1/93 (1993).

61 A.C. Fischer-Cripps, R.E. Collins, G.M. Turner and E. Bezzel, Stresses and fracture probability in evacuated glazing, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD5/94 (1994).

62 Q.-C. Zhang, T.M. Simko, C.J. Dey, R.E. Collins and G.M. Turner, The measurement and calculation of radiative heat transfer between uncoated and doped tin oxide coated glass surfaces, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD6/95 (1995).

63 T.M. Simko and R.E. Collins, Stresses and deflections due to temperatures across vacuum glazings, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD7/95 (1995).

64 P.W. Griffiths, P.C. Eames and B. Norton, Thermal properties of evacuated glazings based on experimental solar simulation and computer based simulation modelling, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD3/96 (1996).

65 T.M. Simko, Results of Task 18 tests on large area vacuum glazing samples, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B5/WD8/97 (1997).

66 H. Arnesen, Ø. Aschehoug, S. Uvsløkk and I. Aske, Thermal transmittance, U-value, of wood window with vacuum glazing, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B9/NOR2/96 (1996).

67 S. Uvsløkk and I. Aske, Measurement of an outward opening window of wood with vacuum glazing, *Report to SINTEF* (1996).

68 H.A.L. van Dijk, G.P. van der Linden and H. Oversloot, Test report on measurement of U-value of evacuated glazing sample for IEA 18 at TNO, *TNO projectnr: 6.26.3688* (1996).

69 A.H. Elmahdy, Window performance and research support of the Canadian window industry, *International Energy Agency Solar Heating and Cooling Program, Working paper* T18/B9/CAN1/96 (1996).

70 B. Norton, P.W. Griffiths, P.C. Eames and S.N.G. Lo, Detailed simulation of heat transfer across planar evacuated glazing, *Window Innovations '95*, Toronto (1995).

71 Pilkington K Glass information sheet, Pilkington Glass Limited, England (1989).

72 Samples of Energy Advantage were supplied by C. Barry of LOF.

73 Samples of Sungate 500were supplied by M. Peterson of PPG.

74 Samples of Comfort EM 5000 a were siupplied by K. Vandiest of Glaverbel.

75 Samples of Comfort E^2 were supplied by G. Breitner of AFG.

76 Determining the steady state thermal transmittance of fenestration systems, *ASTM Standard E 1423* (1991).

77 R.J. Corrucini, Gaseous heat conduction at low pressures and temperatures, *Vacuum*, **7-8**, 19-29 (1957).

78 R. Holm, *Electrical contacts, theory and application, 4th edn*, Springer-Verlag, Berlin (1979).

79 A.A. Griffith, Phenomena of rupture and flow in solids, *Phil. Trans. Roy. Soc.*, A**221**, 163-198 (1920).

80 Standards Association of Australia, Glass in buildings - Selection and Installation, *Australian Standard AS 1288-1989* (1989).

81 A.C. Fischer-Cripps and R.E. Collins, Architectural glazings - design standards and failure models, *Build. Environ.* **30**,29-40 (1995).

82 S.M. Wiederhorn, S.W. Frieman, E.R. Fuller and C.J. Simmons, Effects of water and other dielectrics on crack growth, *J. Mat. Sci.*, **17**, 3460-3478 (1982).

83 K.T. Wan, S. Lathabai and B.R. Lawn, Crack velocity functions and thresholds in brittle solids. *J. Amer. Ceram. Soc.*, **44**, 21-26 (1991).

84 H. Hertz, On hardness, Verhandlungen des Vereins zur Beforderung des Gewerbe Fleisses, Translated and reprinted in *Hertz's Miscellaneous Papers*, Ch. 6, Macmillan & Co., New York (1896).

85 F. Auerbach, Measurement of hardness, *Ann. Phys. Chem.*, **43**, 61-100 (1891).

86 F.B. Langitan and B.R. Lawn, Hertzian fracture on abraded glass surfaces as definitive evidence for an energy balance explanation of Auerbach's law, *J. Appl. Phys.*, **40**, 4009-4017 (1969).

87 R. Mouginot and D. Maugis, Fracture indentation beneath flat and spherical punches, *J. Mat. Sci.*, **20**, 4354-4376 (1985).

88 F.C. Frank and B.R. Lawn, On the theory of Hertzian fracture, *Proc. Roy. Soc.*, A**229**, 291-306 (1967).

89 W. Weibull, A statistical theory of the strength of materials, *Ingenors Vetenskaps Akademins, Handlingar (Royal Swedish Institute for Engineering Research)*, 151 Stockholm (1939).

90 W.G. Brown, A practicable formulation for the strength of glass and its special applications to large plates, *Publication No. NRC 14372, National Research Council of Canada*, Ottawa (1974).

91 W.L. Beason, A failure prediction model for window glass, *NTIS Accession No. PB81-148421, Institute for Disaster Research*, Texas Tech University, Lubbock, Texas (1980).