THE JOURNAL OF

THE BRITISH NUCLEAR ENERGY SOCIETY

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JULY 1964
VOLUME 3, NUMBER 3

PRINCIPAL CONTENTS

Correspondence is welcomed from members on any of these subjects

DISCUSSIONS

REGULAR FEATURES

Hon. Editor: J. M. Kay, MA, PhD, MIMechE, MIChemE. The Journal is published quarterly by the British Nuclear Energy Society and is sent free to members. Membership, which is not confined to those of British nationality, costs £2.10.0 ($9) per year below the age of 27 and £4.4.0 ($13) above 27. The annual subscription to the Journal for non-members is £6.6.0 ($20); extra single copies, 15/9 ($2.5) to members; 31/6 ($5) to non-members; April 1963 special issue £4 ($13) to non-members. *The Society is not responsible for statements or opinions in the Journal. Telephone: WHI 4577; Telegrams: Institution, London, SW1*

MEETINGS

Unless otherwise stated, meetings are at the Institution of Civil Engineers, Gt George St, SW1; 5 p.m. (tea) for 5.30 p.m.

LONDON MEETINGS

11 Nov. *HALF-DAY SYMPOSIUM ON SAFETY OF MAGNOX REACTORS*

2.30 p.m. Introduction by F. R. Farmer;
1 Basic transient flow studies in Stage 1 reactor models by J. G. Moore and F. J. Walford;
2 Core flow transients following circuit leakage: estimation of longest core stagnation time and study of the effects of duct friction and position and size of fracture by W. A. J. Wall;
3 Application of the safety limitation against depressurization to the Calder and Chapelcross reactors by H. A. Hughes and R. M. Horsley;
4 Studies of temperature transients in Calder and Chapelcross reactors following assumed depressurization by B. Wilson and R. Dodds;
5 Chance of a channel fire immediately following the depressurization of a Stage 1 reactor by F. M. Leslie and F. B. Boardman.
Published in this issue, pp. 173–213.

9 Dec. 5.30 p.m. **Operating experience with the Kahl nuclear power station** by Dr A. Weckesser (Joint meeting with British Nuclear Forum). To be published in the January 1965 issue: **preprints will be available.**

EAST MIDLAND BRANCH

10 Nov. Tuesday Geneva Discussion. Details on request.

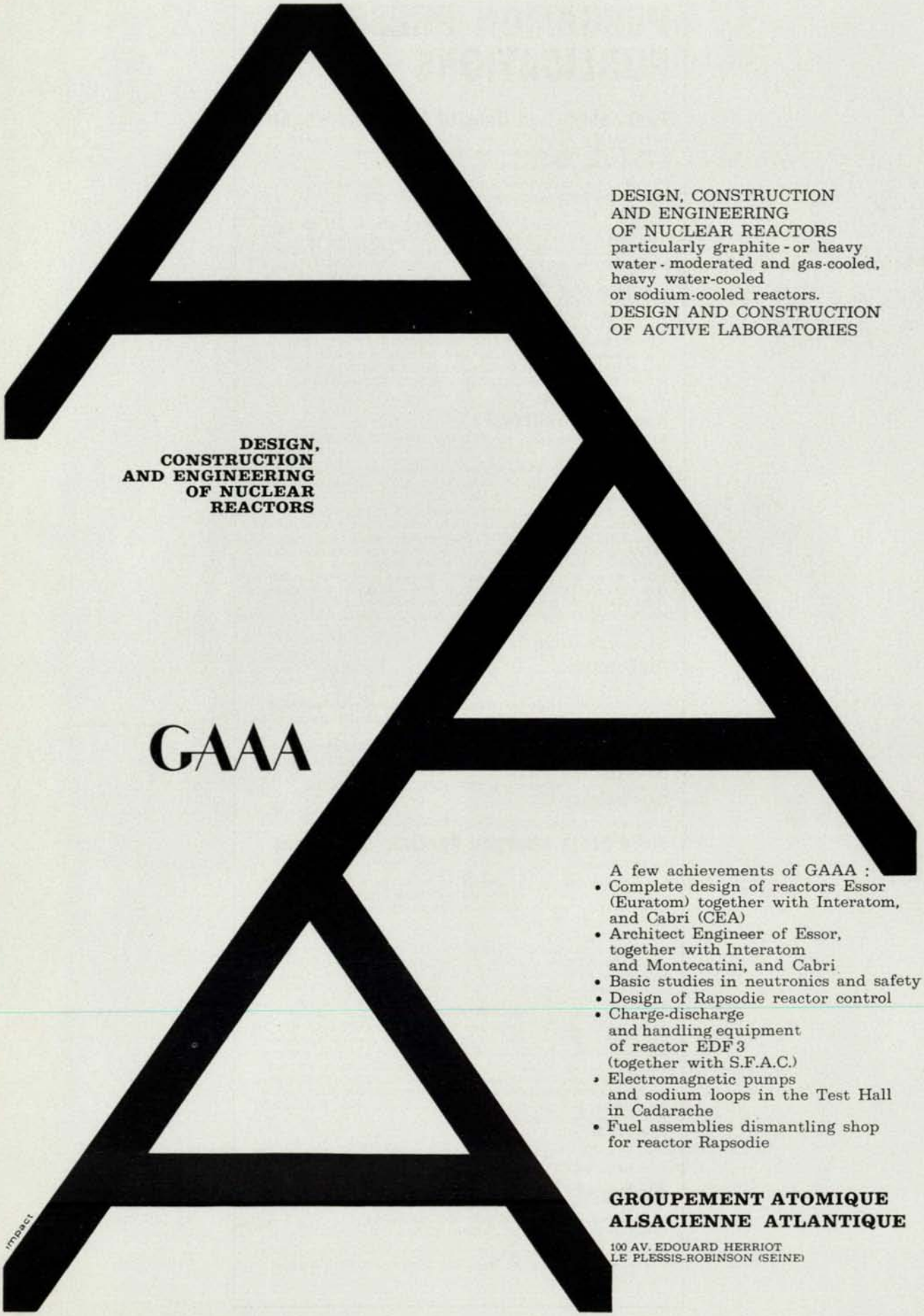
9 Dec. Wednesday Paper and 40-min film on **Latina Power Station** presented by W. Macrae at Loughborough College of Technology, Lecture Theatre 1, Edward Herbert Building.

Honorary Secretary: Mr A. J. Hawkins, c/o RED/FPDO, The English Electric Co. Ltd, Whetstone, Leicester.

OTHER MEETINGS OF INTEREST TO MEMBERS

26 Feb. 1965 THE INSTITUTE OF PHYSICS AND THE PHYSICAL SOCIETY is organizing in collaboration with the Joint British Committee for Vacuum Science and Technology a one day conference on **Cyrogenics in relation to vacuum** to be held at the Institution of Electrical Engineers, London. Any correspondence regarding the programme should be addressed to the Conference Technical Secretary, Mr F. A. Inkley, Basic Research Projects, BP Research Centre, Chertsey Road, Sunbury-on-Thames, Middlesex. Advance registration is necessary and further details and application forms are available from the Administration Assistant, The Institute of Physics and the Physical Society, 47 Belgrave Square, London, S.W.1.

15-21 July 1965 THE INSTITUTE OF PHYSICS AND THE PHYSICAL SOCIETY, under the auspices of the Education Commission of the International Union of Pure and Applied Physics, is arranging an international conference on **The education of professional physicists** to be held in the Physics Department of the Imperial College of Science and Technology, South Kensington, London. Application forms from Miss Patricia N. Boston, The Institute of Physics and the Physical Society, 47 Belgrave Square, London S.W.1.



DESIGN, CONSTRUCTION
AND ENGINEERING
OF NUCLEAR REACTORS
particularly graphite - or heavy
water - moderated and gas-cooled,
heavy water-cooled
or sodium-cooled reactors.
DESIGN AND CONSTRUCTION
OF ACTIVE LABORATORIES

**DESIGN,
CONSTRUCTION
AND ENGINEERING
OF NUCLEAR
REACTORS**

GAAA

A few achievements of GAAA :

- Complete design of reactors Essor (Euratom) together with Interatom, and Cabri (CEA)
- Architect Engineer of Essor, together with Interatom and Montecatini, and Cabri
- Basic studies in neutronics and safety
- Design of Rapsodie reactor control
- Charge-discharge and handling equipment of reactor EDF 3 (together with S.F.A.C.)
- Electromagnetic pumps and sodium loops in the Test Hall in Cadarache
- Fuel assemblies dismantling shop for reactor Rapsodie

**GROUPEMENT ATOMIQUE
ALSACIENNE ATLANTIQUE**

100 AV. EDOUARD HERRIOT
LE PLESSIS-ROBINSON (SEINE)

impact

EDITORIAL

L. Grainger, *BSc,*
Richard Thomas & Baldwins Ltd

To those who have not seen the Powell Report, reading the recent White Paper is like entering a room just as the punch-line of a joke is delivered: without knowing how the Englishman, Irishman, and Scotsman arrived in their situation, the outcome can hardly be appreciated. The White Paper propounds some basic economic principles and suggests the size of the Second Programme (for 'planning purposes'), without revealing any of the specific logic on which these conclusions are based.

Press comments on the White Paper seemed somewhat apathetic and, as usual, greatly over-simplified the problems; an undue personalization of the issues was also unfortunate. What seems to be clear, however, is that many questions concerning the future reactor policy, far from being determined, are to be debated more strongly than ever in the next year or so, with tremendous potential effect on the national economy.

It would not be appropriate for the Society to take part directly in the public reflexions of these policy debates. However, as is well known, the comparison of power schemes depends crucially both on the method of economic comparison and on the detailed technical assumptions about materials and components. A crude economic model on the one hand, together with ultra-conservative assumptions about the limitations of our materials and designs, could be the elements leading to a typically British understatement and the consequent loss of an opportunity of unique promise.

In this edition of the *Journal* is reported a discussion of a Paper by Iliffe and Wright on economic assessment. This Paper and the discussion, which was unfortunately not so prominently attended as it deserved, show how important it is to develop more sophisticated economic models, which can take account of all the circumstances in a more comprehensive and scientific fashion.

The Society has already provided a regular outlet for demonstrating the technical achievements of the British nuclear effort and for indicating how vital continuing progress in these directions could be; the impressive behaviour of the magnox fuel element is a particular example, demonstrating that developments based on sound scientific principles can generally be pressed substantially beyond the first cautious estimates. In this connexion, it was heartening to note, in the last editorial, that Bradwell had already exceeded its design output. The effect of a modest increase in output, together with a longer station life, on the economic status of the magnox reactors needs no emphasis. Experience with the Calder and Chapelcross reactors suggests that such extra potential may well exist in the CEGB reactors and it would be interesting to discuss a programme for examining and exploiting this probability in time to have an effect on current appraisals.

The Society has already discussed plutonium utilization and breeding, subjects which were surprisingly omitted from any mention in the White Paper. There is reason to suppose that, in this vital area also, British achievements are unexcelled and it would be helpful if these subjects were discussed regularly.

The Society can therefore continue to serve a most important role by presenting and discussing in an objective atmosphere, and in appropriate context, the facts which could help to determine these great national issues.

Sir Leonard Owen Prize

The Sir Leonard Owen Prize for the best short paper of the year has been awarded for 1964 to Mr K. E. G. Meredith (of the Central Electricity Generating Board's Berkeley Nuclear Laboratories) for his paper on 'An integral concept for magnox fuel'.

A formal presentation will be made to Mr Meredith at the opening meeting of Session 1964/65 at 5.30 p.m. on Wednesday, 21 October.

The prize takes the form of books and/or instruments of the winner's choice to the value of £10.

EAST MIDLAND BRANCH

Chairman's report for Session 1963–64

The inaugural meeting of the Branch was held at the Leicester College of Technology on 7 November, 1963 beginning with the first General Meeting presided over by Dr J. V. Dunworth, *CBE*, MA, PhD, MIEE, FInstP, representing the Board of the BNES.

The Branch Chairman, Honorary Treasurer, Honorary Secretary, and Committee were elected for the year 1963–64 (see January 1964 *Journal*). The first Ordinary Meeting then followed, when a Paper on 'Prestressed concrete pressure vessels for nuclear reactors' by T. C. Waters and N. T. Barrett, was presented, followed by a discussion. Twenty-two members and five visitors were present.

The second meeting, also held at the Leicester College of Technology on 9 January, began with the election of auditors, followed by a Paper on the 'Economic assessment of nuclear power reactors' by C. E. Iliffe and J. K. Wright. Fourteen members were present together with eight visitors.

The third meeting of the Branch was held at the Whetstone Works of the English Electric Co. Ltd on 27 February. This meeting started with a tour around the Whetstone works for members outside the English Electric Co. Ltd. This was followed by tea and a Paper 'Spectral shift control reactors' by Dr M. C. Edlund, which was presented by P. D. Potter. Twenty-one members and ten visitors were present.

On 10 March at the fourth Ordinary Meeting, again held at the Leicester College of Technology, F. R. Farmer gave his Paper 'Towards the simplification of reactor safety'. Unfortunately only ten members and one visitor were present to hear this interesting Paper.

On 18 March, thirty-nine members travelled to Harwell to join the small party from London for the visit arranged to AERE. Following the success of this visit, the Branch is planning further visits in the coming session.

The Annual General Meeting of the Branch was held at the Leicester College of Technology on 8 April. The Chairman's report and the accounts of the Branch were presented and adopted. The following elections to the Committee for the Session 1964–65 were made:

Chairman: J. B. L. Faulkner, BSc, MICE
Vice-Chairmen: E. Long, MIMechE and P. H. W. Wolff, MA, MIMechE
Members ex-officio: S. S. Smith, FIM, Birmingham, and J. G. Keith-Hitchens, MIMechE, MILocoE, Leicester (Members, BNES Board)
Honorary Treasurer: J. B. Stuart, BSc*

G. Dakin*	D. W. Lyne
L. Gregson	P. McLoughlin
J. Higginbottom†	W. E. Phillips
J. S. Ilsley	N. S. Sehgal

V. C. White, AIMechE*

Honorary Secretary: Mr A. J. Hawkins, c/o RED/FPDO, The English Electric Co. Ltd, Whetstone, Leicester.

* It was resolved that the three retiring members continue in office, in accordance with By-law 4(c).
† Auditor.

Twenty-nine members and fourteen guests were present at the meeting.

Following the formal business meeting three films were presented:
'Operating a Calder Hall reactor full power'
'Dounreay Symposium'
'AGR fuel handling'

In general the first session of the Branch has been as successful as expected when the difficulties of starting up an organization of this nature are considered. From the experience gained in the past few months the committee have been able to judge the demand for, and likely response to various proposed activities and the programme for the coming session will take note of these.

It is interesting to note that as a direct result of this Branch being formed there has been an increase of at least 27 in the membership of the Society.

The committee has begun to prepare the programme for the coming session when it is hoped to have several papers from members of the Branch or written at the invitation of the Branch.

It is also intended as an experiment to hold one meeting of the Branch at Loughborough in December 1964.

Finally I would wish to acknowledge the great help and encouragement I have received from the Branch committee, from the Board of the BNES and their secretariat and to thank them for all their hard work in the establishing of this Branch of the Society

BNES NEWSLETTER

Progress on MHD

Over the past few years there has been an increasing interest in magnetohydrodynamic (MHD) or, as it is occasionally called, magnetoplasmadynamic (MPD) generation. The incentives behind this are the need to increase the overall efficiency of electrical power stations, as well as the need to generate electricity in new environments, such as outer space.

The basic principles of MHD are well known. In a normal dynamo an electrical conductor (often copper) is passed through a magnetic field and the induced current is collected and fed into a load. In an MHD generator a hot electrical gas replaces the copper. The gas flows along a duct in a direction normal to a strong magnetic field and the induced currents are collected on electrodes and fed into the load. The currents that are flowing in the gas react on the magnetic field in such a way as to tend to slow down the gas and in this way the kinetic energy of the gas is converted into electrical energy. The main lines of research are at the moment concentrated on two main classes of generator, namely, the open cycle and closed cycle systems, and it is convenient to treat each of these separately.

In the open cycle system the working fluid is simply the combustion products of normal fossil fuel, and in order to increase the electrical conductivity a trace of some low ionization potential material, normally potassium, is added. The electrical conductivity of the combustion products increases rapidly with temperature and therefore it is normal to preheat the combustion air in order to achieve high flame temperature. In small experimental generators it is customary to obtain high temperatures by burning in oxygen, rather than in air, but enrichment of the air by oxygen would be uneconomic on a large scale power station. After passing through the MHD generator the gases are still hot enough to be used to preheat the combustion air and also to power an ordinary conventional boiler. With such a combined system, approximately half the electrical output would come as d.c. at several thousand volts from the MHD generator, and the other half would be obtained from the conventional steam cycle. Power must be used to compress the combustion air since there is a pressure drop of several atmospheres across the MHD duct. This system has the potentiality of raising the efficiency of conventional power stations from 40% to perhaps 50%.

The basic physical processes occurring in an open cycle MHD generator are now reasonably well understood. There has been a fair amount of success in the development of long-lived ducts and electrodes using water-cooled metal systems, insulation being provided by thin insulating strips between cooled metal sections. Powers of the order of $1\frac{1}{2}$ MW have been generated in an experimental apparatus at the Avco-Everett Research Laboratories in the USA and a much larger apparatus should soon be operational. In Great Britain research has been on a much smaller physical scale, but at the Central Electricity Research Laboratories ducts have run for several hours without appreciable sign of wear. The most severe problems on the open cycle generator are related to the design of high temperature air heaters and the recovery of a significant fraction of the potassium seed.

The other main type of MHD generator is the closed cycle generator in which gas is compressed, heated in some form of heat exchanger (possibly a nuclear reactor), passed through the MHD duct and then recycled through the compressor. The principal point about this type of generator is that although it has the disadvantage of requiring high temperature heat exchangers, it has the advantage of giving a wider choice of working fluid and seed material. Thus it is possible to use caesium, which has a lower ionization potential than potassium, and thereby to obtain a greater degree of ionization at any given temperature. Moreover, the use of a gas such as argon with a low cross-section for elastic collisions with electrons, enables a higher electrical conductivity of the working fluid for a given degree of ionization. An additional advantage is that the recombination time of electrons and ions is much lower in a monatomic gas and the relaxation time for energy exchange between atoms and electrons is much longer. It is therefore possible to consider various methods of producing ionization other than by heating the gas to a high temperature. One such method that has considerable potential uses the electric fields generated by the MHD processes to partially break down the gas. Although this effect has been observed in static experiments where an electric field is applied to the gas, only one experiment, by Talaat in the USA, has been reported, in which increased conductivity has been obtained by passing the gas through a magnetic field.

Recently, the International Research and Development Company (IRDC) in Newcastle have reported that a small closed cycle loop which they have built is now operational. Powers of up to $\frac{1}{2}$ W have been generated and, although to date the system has operated probably with thermal ionization, the system was designed to be suitable for studies of non-equilibrium processes.

Whereas the basic physical processes in open cycle generator systems are now well understood and there is a clear economic case for the development of fossil fuel MHD power stations, with the closed cycle nuclear system, not only have the basic

MHD processes to be firmly established but there appears to be some controversy whether a nuclear MHD power station would be economically attractive. Reactor exit gas temperatures of the order of 1500°C seem to be required and the development of a suitable, sufficiently cheap reactor is no easy task. Moreover, a gas turbine running with a rather lower reactor outlet temperature would seem to give comparable efficiencies. The IRDC press release has highlighted this problem. During the past 3 years they have spent £300 000 constructing their apparatus and now need financial support to carry the project further.

NIRNS

The Sixth Annual Report for 1962/63 of the National Institute for Research in Nuclear Science (NIRNS) was recently issued. The term 'nuclear science' in the Institute's title covers several fields of research of great importance to universities, which require facilities beyond the scope of a single university. The purpose of the Institute is to provide and operate such facilities for use by all universities. The main fields of research are high energy physics and nuclear physics, which require particle accelerators, and experiments covering a wide range using nuclear reactors as radiation sources.

The Institute's biggest task so far has been to build NIMROD, the high energy proton synchrotron, at its Rutherford Laboratory. The start-up of this 7 GeV, £11 million machine in August 1963, was reported in an earlier Newsletter.

The Institute's interests are varied and appear to become more so with the progress of time. The Rutherford Laboratory, which has really been built around NIMROD and its small 50 Mev proton linear accelerator, now has a staff of 900 (300 of whom were transferred to NIRNS from AERE). Over 100 graduate university physicists are basing a large part of their research programmes on the Laboratory's accelerators. The British national 1·5m hydrogen bubble chamber, which is a joint project of several universities, financed by the DSIR, was completed at the Rutherford Laboratory early in 1963 and is now operating successfully on the 25 GeV proton synchrotron at CERN. Two other bubble chambers are under construction. One is a heavy liquid chamber, in which University College is particularly interested, and the other a helium chamber, designed principally by the team at Oxford University. To support the bubble chambers four manual track analysis machines are in use, and a more advanced machine, which can deliver its measurements directly to a computer, is being built in collaboration with CERN, Brookhaven, and Berkeley. A small computer ORION has been installed at the Laboratory and this has the facility for direct connexion to the planned track analysis machines and spark counters.

Located on the site of the Rutherford Laboratory is the Atlas Computer Laboratory. Construction is under way and the complete installation should be handed over on 1 April, 1965.

The second laboratory of the NIRNS is at Daresbury (birthplace of Lewis Carroll), where NINA, a 4 GeV electron synchrotron, is being constructed. The site was chosen only last year and one of the major problems facing the new laboratory was to get the synchrotron and its new buildings designed as quickly as possible. Help from the Universities of Glasgow and Liverpool and assistance from the UKAEA and Rutherford Laboratory has enabled the project to proceed at a fast rate. Very stable rock base foundations were needed for the site, for the synchrotron magnets must be positioned with great accuracy, to about 2 parts in one million on the diameter of 200 ft.

One aspect of the NIRNS work which is not too prominently known is its support of university work on research reactors. This support is mainly channelled towards the use of the AWRE 5 MW research reactor HERALD, although the Institute has supported and continued to help universities to use the other research reactors of the UKAEA. HERALD at Aldermaston is now completely accessible to university workers without security restrictions and the main interest at present stems from the Universities of Reading, Birmingham, and Cambridge. The Institute has provided a liquid-nitrogen-cooled irradiation facility in the maximum neutron flux ($\sim 3 \times 10^{13}$ n cm^{-2}sec^{-1}) for radiation damage experiments and is having constructed a liquid hydrogen moderator chamber for producing beams of 'cold' ($\sim$20°K) neutrons. This latter facility should prove an extremely useful asset and with its high beam intensity should enable high class research work to be performed. The design and operation of this moderator chamber will be watched with interest, for several hundred watts must be extracted at liquid hydrogen temperature. The cold neutrons with their long wavelengths are particularly suitable for the study of lattice defects in crystals and for inelastic scattering measurements. These two pieces of equipment will provide facilities for universities equal to those which can be obtained anywhere else in the United Kingdom and their use should enable the true interest of the universities in this form of work to be assessed.

USAEC Annual Report 1963

A recent United States Atomic Energy Commission publication defines what is meant by basic and applied research. It considers basic research as 'effort directed to acquiring or confirming scientific information, the specific use for which is not foreseen at the beginning of the effort'. Applied research is defined as being 'the effort directed to acquiring or confirming scientific information the use for which is foreseen at the beginning of the effort'. The results of applied research sponsored by the USAEC and which are actually directed toward scientific projects, are summarized in the recently published 'Annual Report to Congress of the Atomic Energy Commission for 1963', January 1964, US Government Printing Office, Washington, DC. Some of the highlights of this

report in the nuclear power reactor field are given below.

During 1963, the overall atomic energy effort in the USA was considered to have been marked by a continued steady growth toward a healthy and well balanced nuclear economy of the future. Five nuclear power plants attained criticality, five plants were under construction, applications for construction of five plants were under AEC consideration, and firm plans for two other nuclear power units were announced by public utilities. The nation's net installed electrical generating capacity from nuclear plants exceeded 1 000 000 kW at the end of the year despite delays in several projects.

The Humboldt Bay Power Plant reactor of 45·5 MW(e), situated near Eureka, California, attained criticality in February and reached its licensed 165 MW(t) power level in May. The reactor performed well during a series of experiments performed at this power level and early in December the owners, the Pacific Gas and Electric Co., were authorized to conduct a special 15 day test programme to demonstrate operation of the reactor at power levels up to 230 MW(t), 68·5 MW(e). Based on the results of these tests, the company hopes to obtain authorization for increased power operation in 1964. The only heavy-water-moderated power reactor in the USA, the 17 MW(e) Carolinas–Virginia tube reactor at Parr, South Carolina, went critical in March. Since achieving criticality the reactor has been operating at low power for physics testing and operator training. Repair of leakages in the reactor header cavity and modifications to eliminate vibrations which were causing extensive heat exchanger tube wear contributed to the delay from July to December of the reactor's attainment of its presently authorized power level of 44 MW(t), about two-thirds of its ultimate design power level. The 11·4 MW(e) Piqua Nuclear Power Facility became critical in June. This is the first nuclear power reactor built for a municipal power utility, and the first commercial plant to be powered by an organic reactor. Completion of the plant was originally scheduled for late 1961 but operation was delayed by a combination of factors, ranging from general construction difficulties to a fire. Full-power testing was expected to be completed in early 1964, when it was to be turned over to the City of Piqua to operate for the AEC. The 60·9 MW(e) sodium-cooled fast reactor of the Enrico Fermi Atomic Power Plant, built by the Power Reactor Development Co. at Lagoona Beach, Michigan, became critical in August. During the remainder of the year and extending into early 1964, the reactor was being subjected to a physics testing programme at the presently authorized 1 MW(t) power level. Completion of the plant was originally scheduled for late 1960 but was delayed for a number of factors, mainly because it is the first facility of its kind. The 16·5 MW(e) Experimental Breeder Reactor No. 2 (EBR-2) located at the National Reactor Testing Station, Idaho, achieved criticality in November. This liquid-sodium cooled fast reactor was scheduled for mid-1962 operation but was delayed by the reaction of the liquid-sodium coolant on various components which developed during the pre-criticality testing phase. Current plans call for experimental operation of the plant to determine its operational behaviour, and for using the plant as an experimental facility to derive information for the fast reactor programme. The plant is the first fully integrated facility, since it includes a fuel re-processing system. The reactors in the nuclear power plants located at Big Rock Point, Michigan (boiling-water reactor of 47·8 MW(e) extended eventually to 75 MW(e)); Indian Point, New York (pressurized water reactor of 255 MW(e)); Hallam, Nebraska (sodium-cooled, graphite-moderated reactor of 75 MW(e)); Humboldt Bay, California (boiling-water reactor of 48·5 MW(e)); and Saxton, Pennsylvania (boiling-water reactor of 3·25 MW(e)), reached their design power levels for the first time during the year.

At the end of the year five plants were under construction. The Boiling Nuclear Superheat Reactor (BONUS), an integral nuclear superheat reactor system designed to produce 16·3 MW(e), situated at Punta Higuero, near Rincon, Puerto Rico, was essentially completed in 1963 and is scheduled to be turned over to the Puerto Rican Water Resources Authority during 1964. The Pathfinder Atomic Power Plant, of 58·5 MW(e) output, employing a boiling-water nuclear superheat reactor was essentially complete at the end of 1963. The Peach Bottom Atomic Power Station at Peach Bottom, Pennsylvania, employing a 40 MW(e) high temperature helium cooled reactor is scheduled for completion in 1964. This reactor design employs pyrolitic graphite-coated fuel particles of thorium carbide–uranium carbide contained in graphite fuel elements. A full diameter fuel element was tested throughout 1963 in an irradiation loop in the General Electric Test Reactor in California and had accumulated 35 000 MWd/t burn-up by the end of December, at a 1400°C outlet gas temperature. An ultimate burn-up of 50 000 MWd/t was planned by mid-1964. Construction of the 50 MW(e) LaCrosse boiling-water reactor plant at Genoa, Wisconsin, which was begun early in 1963, is expected to be completed and to attain criticality late in 1965, and to be ready for full power operation in 1966. The 21·9 MW(e) experimental gas-cooled reactor under construction at Oak Ridge, Tennessee, is expected to be completed in 1965. This stainless steel clad uranium oxide-fuelled reactor is not expected to compete economically with other systems but its operation should give valuable information on the large-scale handling of hot gas and performance of reactor system components. The reactor will probably be used as a fuel test facility, at a later date, in support of the gas-cooled reactor programme.

During 1963, the Shippingport Atomic Power Station continued to operate with the fourth seed of its first core; the Yankee Nuclear Power Station passed the $2{\cdot}5 \times 10^9$ kWh mark after two years operation; the Dresden Nuclear Power Station resumed operation following its first complete inspection (for maintenance overhaul) since 1959, having produced over 2×10^9 kWh of electricity; and the Experimental Boiling Water Reactor was shut down for modifications to permit its use in other programmes. The organic moderated reactor experiment terminated operation in April, and at the end of the year, plans were being made to shut down the Sodium Reactor Experiment for modifications to permit operation at higher power. Superheated steam using the heat from nuclear fission was produced for the first time in BORAX-5. In the gas-cooled reactor field a design and development study has been initiated by General Atomics on the 1000 MW(e) TARGET (Thermal Advanced Reactor, Gas Cooled, Employing Thorium System). The purpose of this project is to evaluate various advances in gas-cooled technology to achieve low-power cost and high-fuel utilization. During the year Congress authorized the construction of FARET, a Fast Reactor Test Facility. This 50 MW(t) sodium-cooled, fast breeder will be used to investigate the characteristics of many different fast-breeder reactor designs. In May the AEC received a proposal

from the Southwest Atomic Energy Associates, a group of 17 investor-owned southern and southwestern utility companies in association with the US General Electric Co. and Gesellschaft für Kernforschung of West Germany, for research and development support of an experimental breeder reactor fuelled with a mixture of plutonium and uranium oxides. The proposed facility, designated SEFOR, would be located near Fayetteville, Arkansas, and would serve primarily as a physics tool to furnish data needed to design large fast-breeder reactors fuelled with mixed oxides. During the year, studies of both large size (1·5–25 MW(t)) and intermediate size (200–1500 kW(t)) reactor plants for sea-water distillation and electricity production were started.

Marine Reactors

The world's first nuclear-powered cargo–passenger ship, the *NS Savannah*, was built under the AEC's maritime reactors programme.

To date, two years of plant operating experience and 30 000 miles of travel have established the acceptable design characteristics for a commercial nuclear-powered plant and ship. On 7 May, after undergoing her annual inspection and overhaul in Galveston, Texas, the *NS Savannah* was ready to continue her domestic demonstration voyage and make her first trip overseas. However, because of a labour dispute, the voyage was postponed and the operating agreement was terminated. On 15 July, operation of the *NS Savannah* was assigned to American Export and Isbrandtsen Lines, the ship's new general agent, and on 28 November, the reactor was brought to criticality in Galveston by members of the ship's technical staff. New seagoing crews began training in December and it is expected that tours to domestic and foreign ports will be resumed in 1964. Experience with the *Savannah*'s nuclear plant has indicated that by 'integrating' certain of the propulsion system components, there might result more efficient, lighter weight, more easily automated and economically attractive reactor propulsion systems for maritime use.

Two pressurized-water steam generators in which certain components have been integrated are being investigated. These are the Consolidated Nuclear Steam Generator (CNSG) concept developed by the Babcock and Wilcox Co., and the Unified Modular Plant (UNIMOD) developed by Combustion Engineering, Inc. CNSG is basically a compact version of the *Savannah*'s present plant in which the entire steam generating system would be included within the reactor pressure vessel. The UNIMOD would be a pressurized-water reactor designed to permit factory pre-assembly into modules for easy installation in a ship. The feasibility of using a gas-cooled reactor for marine propulsion is also being studied. This is the 630 A air-cooled reactor being developed by the General Electric Co. and it has been indicated that when the reactor is combined with the boiler, a compact nuclear steam generator is formed. The economics of this concept are being evaluated by fuel evaluation studies and reactor design programmes.

Naval Reactors

At the end of 1963 the US Navy had in operation a nuclear-powered fleet of three surface ships (the aircraft carrier *Enterprise*, the guided missile cruiser *Long Beach*, the guided missile destroyer *Bainbridge*) and thirty-four submarines, of which sixteen are Polaris-missile-launching. An additional fifty-two submarines and one surface ship were in various stages of planning or construction.

Missile Reactors

The objective of the AEC's PLUTO project is the development of reactors which can be used as a heat source in ramjet propulsion systems to propel extended-range, high-speed, low-altitude missiles. Since 1957, the Lawrence Radiation Laboratory, Livermore, California, has been developing and testing the initial reactors—designated the TORY series—for this programme. The TORY IIC reactor is a step between an engineering test reactor and a flyable prototype. Its design power, temperature, and size are those necessary for a propulsion system to permit low-altitude supersonic flight. During the first half of 1963, the TORY IIC reactor was assembled and criticality tests were initiated in July. Operational check-out tests were initiated in June but following several pre-nuclear operational runs at design conditions, a detailed inspection of the facility late in October revealed several deficiencies in the expansion joints. Repairs and modifications have been made to the system, but start of the tests of the TORY IIC will be delayed to early 1964.

Army Reactors

Of the US Army's reactors, four stationary or portable nuclear power plants are in operation, and a fifth is in the process of being relocated. These systems are demonstrating their reliability and providing information to improve their performance.

The development of mobile plants for tactical applications is in the early research and development–engineering test evaluation stage. The Mobile Low Power Plant No. 1, ML–1, located at the National Reactor Testing Station, Idaho, is a prototype nitrogen-cooled plant designed to provide 300 kW(e) net within 12 hours after its arrival at an operating site and operate for 10 000 hours on a single fuel loading.

The entire plant weighs less than 40 tons. ML–1 was brought to criticality on 28 February, achieved full design power of 3·3 MW(t) and ran for more than 100 hours at 2 MW(t), successfully achieving all experimental objectives. It was later found that the pressure vessel had developed a small leak and that the gas turbine-compressor set required repair

modification before it could attain its design electrical output. The repair programme is in progress and test operations are scheduled for early 1964. The preliminary design of the Military Compact Reactor (MCR) is expected to be completed by mid-1964. This plant will combine a liquid-metal-cooled reactor with open-cycle gas-turbine power-conversion equipment to produce from 2 to 3 kW(e).

Space Reactors

The AEC's activities in developing nuclear power for space and specialized uses fall into two broad categories: the Rover programme for developing a nuclear-rocket engine capable of propelling vehicles in space, and the SNAP (Systems for Nuclear Auxiliary Power) programme to develop a series of compact nuclear reactors and nuclear devices for supplying power for a variety of space, terrestrial and marine uses. During the year the RIFT (Reactor in Flight Test) project of the Rover programme was terminated, and the programme revised to place emphasis on ground based research and engineering. Certain revisions of the SNAP programme also were made and for the first time a SNAP prototype unit was mounted on a rocket and sent through the atmosphere at several thousand miles an hour to determine the effects of re-entry heating.

The magnitude of the US effort in the nuclear power reactor field is shown by the fact that the cost of research and development in this field for the fiscal year 1963 was $507·3 million. This was an increase of $74 million on the previous year's costs, due principally to increased effort in the space, nuclear safety and naval reactors programme.

Experience with Canada's first nuclear power station (NPD) and the prospects for heavy water power reactors*

Dr W. B. Lewis, *CBE*, FRS

Senior Vice-President, Research and Development, Atomic Energy of Canada Ltd

Dr Lewis, who was introduced by Sir Leonard Owen, said that the American Nuclear Society (ANS), of which he was a Past President, had asked him to convey greetings to the BNES.

The lecture was divided into two parts—experience with NPD and the indications that it gave for the future. The experience was real, the future speculative. Dr Lewis pointed out, however, that a great deal of what used to be technical speculation had long since passed that stage and the fact that a reactor had not reached completion did not mean that its design was unsound.

On the question of the future of uranium, Dr Lewis felt a need for economy in its use because an increased demand would arise from expansion in the industry whatever road it took.

THE Nuclear Power Demonstration (NPD) plant at Rolphton, Ontario, is an important step in the development of an advanced nuclear power system that stresses economy in the use of fissile material or, in other words, economy of neutrons, and as a result achieves a very low fuelling cost.

2. The key position occupied by NPD is apparent when we review the overall setting for nuclear power. In the early pioneer years of nuclear reactors, the mid-1940s, there was a general realization, in which Canada shared, that the available energy resources of the world had received a great increase. How great was the increase remained a subject of scientific–economic debate for many years. The only natural fissile material was the isotope U^{235}, but even then it was known that from the fission of U^{235} spare neutrons could be used to produce other fissile nuclides, plutonium-239 and uranium-233, that had special properties. It was then believed and has since been established that plutonium could multiply itself or breed in a fast neutron reactor and uranium-233 could breed in a thermal neutron reactor. Next, however, the cold grip of competitive economics set in. It was particularly severe for nuclear engineers in Canada because of our abundant resources as well as new discoveries and developments of coal, oil, and natural gas. There was, however, a very special set of circumstances in Ontario, an industrial province with a large and expanding demand for power; its large hydroelectric resources were approaching saturation, it was a long way from Canadian sources of fossil fuel, but close to Pennsylvania coal. All along, those developing nuclear power have faced a moving target—a rapidly descending cost of electric power generation from fossil fuel. In 1951 we thought uranium might be available at \$3.50/lb U_3O_8 although probably in such short supply that breeding might be necessary. Coal for Ontario was \$8/short ton and expected to rise. In 1952 a new coal-fired generating station, the R. L. Hean plant in Toronto, was planned and estimated to yield power at 7·5 mill†/kWh. This target seemed easy to meet. By 1953, however, the prospective price of uranium was \$12/lb U_3O_8. By 1954 there had been major new discoveries of uranium, and importantly in Ontario. We committed ourselves to build NPD, a nuclear power demonstration reactor too small to be economic, but which would demonstrate the techniques we developed in NRX at Chalk River of high pressure water cooling and zirconium clad fuel. The fuel would be natural uranium metal or oxide and the moderator heavy water. The reactor was to have a steel pressure vessel. The world's 1955 excess of enthusiasm probably helped us, but the real transformation came in 1956 when the engineering design group moved on to consider a full-scale economic natural uranium heavy water design, and three major advances came and fitted together. The first was to do away with the pressure vessel and use zircaloy pressure tubes. The second was to make the channels horizontal in order to avoid the congestion at the top of a vertical reactor arising from the provision there of (1) mechanical support; (2) coolant flow connexions, and (3) on-power refuelling. The third was to revert to an old characteristic of early reactors with horizontal channels, namely, the use of short fuel slugs instead of long rods or bundles, but with one very important difference, namely, bi-directional on-power fuelling. In alternate channels new fuel is supplied from opposite ends so that reactivity is balanced along the axis, and each fuel slug can be taken to the same maximum burn-up.

3. The low fuel cost resulting from the prospective high burn-up of 10 000 MWd/tonne natural uranium was recognized as a major and even essential feature to hold our position against the moving target of economic competition. In our large reactors we now look forward to fuel costs of less than 0·7 mill/kWh (0·06 *d.*/unit).

4. These three advances introduced three new techniques requiring demonstration but were so promising that in 1957 we put NPD-1 (as it then had to be called) into suspense, leaving only a caretaker at the site, while the Canadian General Electric, the engineers for the project, considered whether NPD could be converted to horizontal pressure tubes on the same site without too much expense. They reported

* Third Annual Lecture of the BNES, 17 April, 1964.

† 1 mill = $\frac{1}{1000}$th of a dollar.

favourably so we met the cancellation charges on the pressure vessel with inward joy and switched to NPD-2.

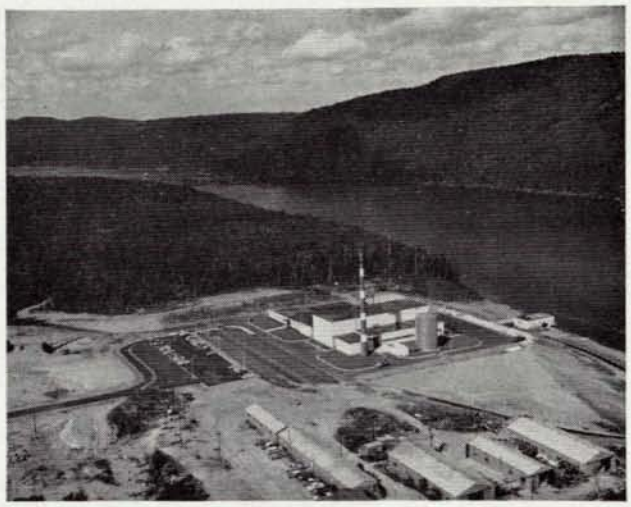
5. We still have much to advance in speed of design, manufacture, and construction, for the reactor was not critical until April 1962, a year later than we and the CGE had hoped. Full power was reached on 28 June, 1962. In the meantime, something very important to NPD-2 occurred, namely, the decision to build the full scale 200 MW(e) plant CANDU at Douglas Point, retaining horizontal pressure tubes and fuel bundles of the same dimensions. This meant, among other things, that the NPD-2 fuelling machine was a full-size machine, although not applicable at Douglas Point for another reason. It followed that more could be spent on the development, and in fact, five machines have been built and each in turn receives the latest modifications.

OPERATING EXPERIENCE WITH NPD

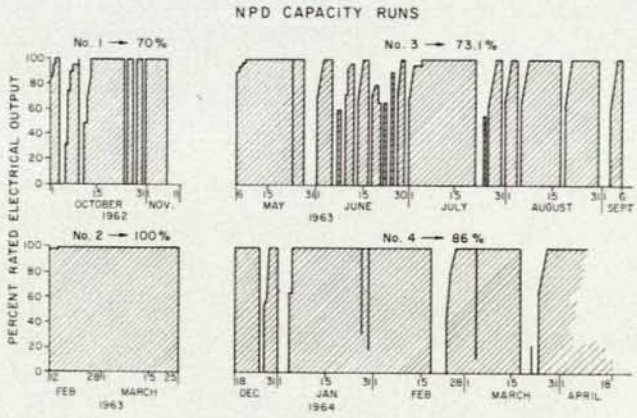
6. Figs 1 and 2 show views of the completed station. Since NPD is both a trial ground for continuing development as well as a 'demonstration' generating station, its operation is conducted in two phases called (1) demonstration and (2) improvement periods. The results from the demonstration periods are shown in Fig. 3 and are very satisfactory. When we examine in detail the reasons for outages, we find that the majority are attributable to failures that should not recur. The original diesel generator maintaining essential emergency power supplies was not suitable for continuous operation and has been replaced. Difficulties with the stop valves and governor valves of the turbine can be met by improvements and preventive maintenance. The outages that have called for improvements, such as failures of shaft seals on the primary pumps, numerous leaks from mechanical couplings on instrument piping, should all become less as the improvements are made. I will discuss some of these interesting improvements later.

7. Reactivity has been as expected. On-power fuel shifting has been routine since November 1963 and has caused no shut-down. No fuel has failed. The highest burn-up reached so far is about 4600 MWd/t(U). Twelve fuel bundles have been removed for examination. Some of the depleted uranium bundles required to hold down initial reactivity have been discharged.

1—*NPD generating station on Ottawa River at Rolphton*



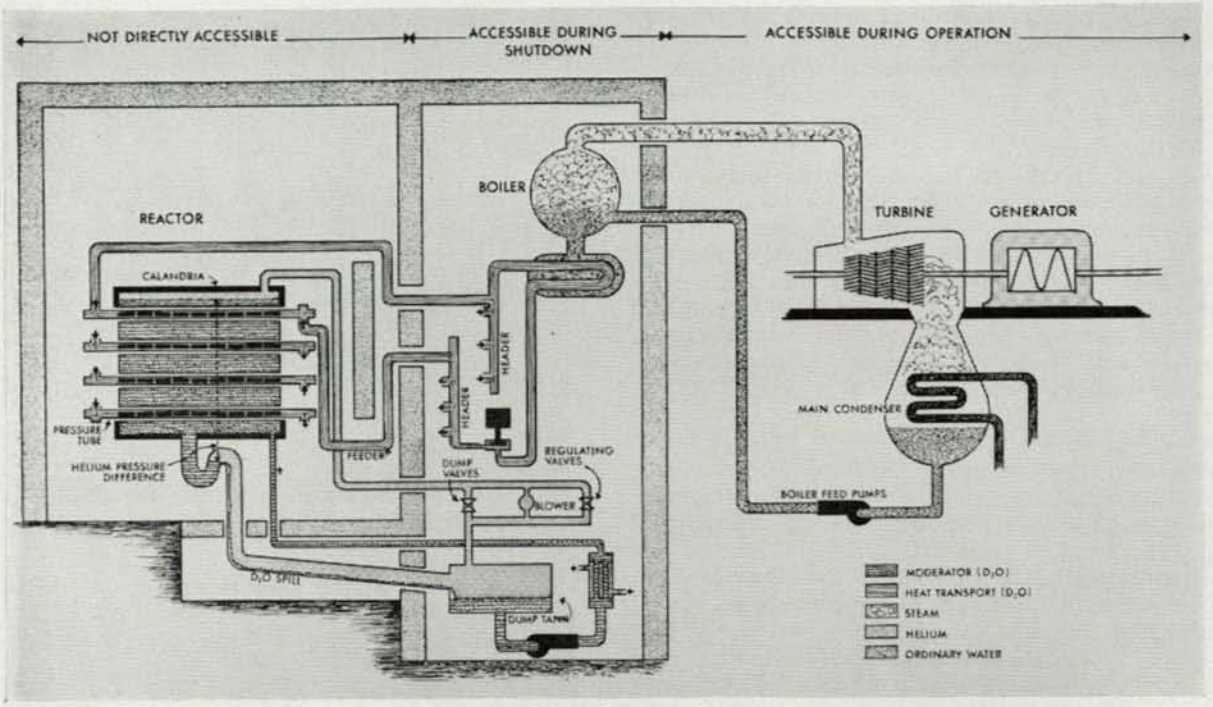
2—*NPD generating station on Ottawa River at Rolphton*



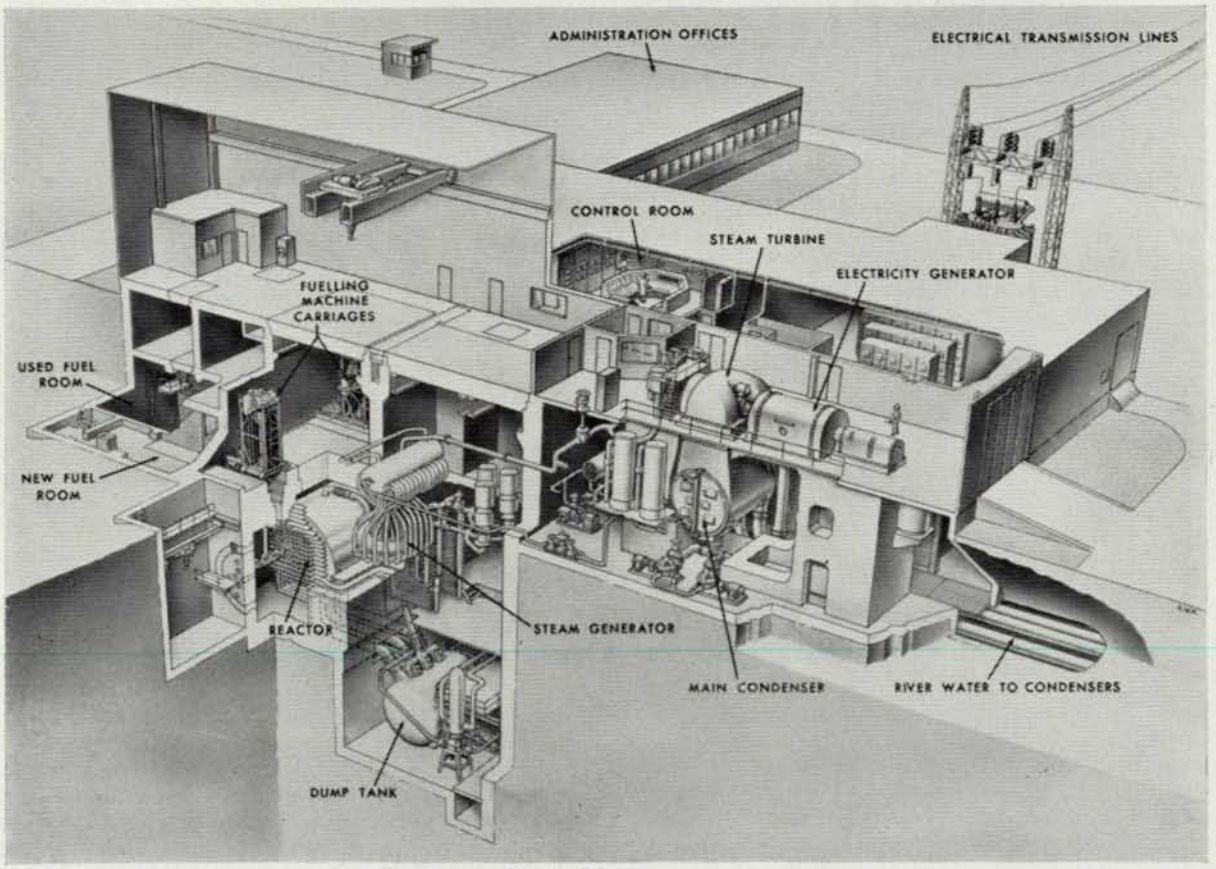
3—*Results from demonstration periods of NPD*

IMPROVEMENTS

8. To understand some of the improvements, we may review briefly the layout and nature of some of the components. Let us approach from a simple schematic, Figs 4 and 5. There are two heavy water systems; first, the moderator which is cold and has helium as a cover gas. Balancing the helium pressure over the two surfaces brings about a rapid dump—with timing in seconds only. The other heavy water system is the primary coolant circuit, which is hot and at high pressure. For any fuel shifting the two fuelling machines have to couple to opposite ends of a channel and their auxiliary heavy water systems are pressurized to match the primary coolant. Fig. 5 shows a general cutaway sketch of the whole station. Note the enclosed reactor vault, the boiler room with the primary coolant circulating pumps and the moderator circulating pump and heat exchanger. Most of the improvements on the nuclear side are in the boiler room or connected with the fuelling machines. These are operated from a panel in the control room governing the motion of the carriages shown in Fig. 6 as well as the heads. The heads are withdrawn into the fuelling machine room for maintenance. They are quite complex, as shown in Fig. 7.



4—*Simplified schematic of NPD*



5—*Cutaway sketch of NPD generating station*

Fuelling machines

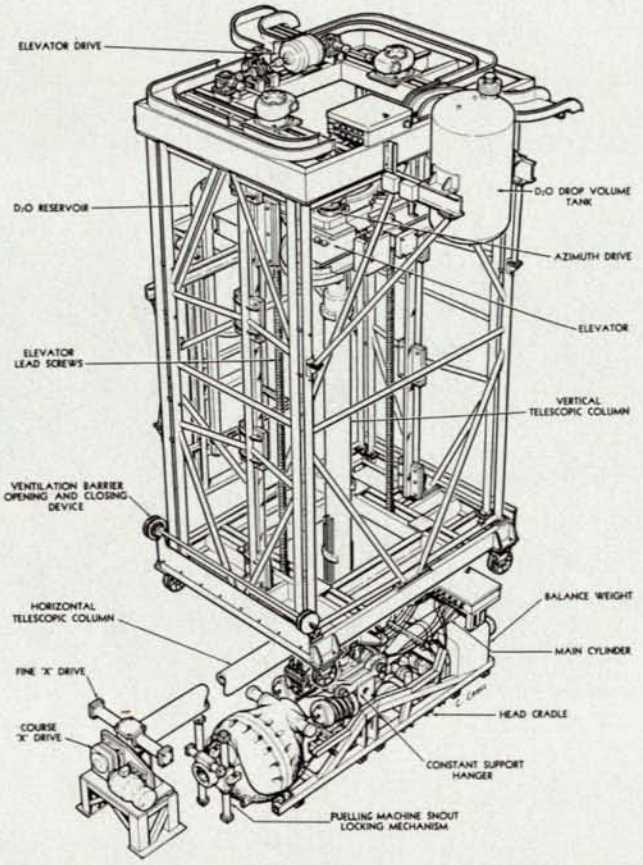
9. Although the fuelling machines were used for loading the 1188 fuel bundles into the reactor early in 1962, development had not been completed. The first attempt to use the machines at power was in December 1962 and met with an accident due to a double break-down. After coupling to the reactor and withdrawing the plug, a major leak developed at the seal between the snout and the reactor channel. Almost simultaneously a small pipe coupling on the machine broke, releasing the hydraulic pressure. Without this pressure the plug could not be replaced in the reactor channel so that the leak continued for 52 hours. Initially this was hot heavy water flashing to steam, that raised the pressure in the reactor vault and brought into action the emergency dousing by moderator heavy water from the dump tank. This water returned to the sump and was recirculated but unfortunately also flooded an oil catch tray and picked up chemical grout from the sump. So the still warm fuel in the reactor was smeared with oil and particles of grout, for which it was not designed, but fortunately it came to no harm. Moisture in the air leaking into the reactor vault had in the previous weeks been allowed to accumulate in the vault atmosphere scrubbing system, and led to downgrading of all the heavy water from 99·7 to 99·1% D_2O. Within 12 days the reactor was in operation again, but due to the downgrading, some reactivity had been lost and it had been necessary to shift the depleted fuel bundles from central to end positions in some channels. The water was first cleaned up by a progressive bleed and feed system and over a longer period has been upgraded. This experience gave the operators some confidence but set back the fuelling machines for further development. The snout sealing gasket operates at too high a temperature for an elastomer and has a very short life. Leaks observed in development had been blamed on the gasket, but it was found afterwards that the gap closing mechanism was unreliable and had to be redesigned. The machines were modified and approved for on-power operation in November 1963 and have operated with only minor difficulties routinely with the reactor at power for about 60 fuel shifts so far.

Primary pump shaft seals

10. During the first year of operation there was considerable trouble from shaft seals failing after only a few tens or hundreds of hours operation. Since the theory underlying the operation of these seals was not established, an intensive development was undertaken. We now have sufficient understanding to meet any future failures and have a variety of designs which can be satisfactory. It was necessary to select erosion resistant materials, provide against flexing of the graphite face, ensure a water film to cool the rubbing faces and strip the gas from the high pressure water. Seals of two modified designs now in operation are still satisfactory after more than 5000 hours.

Moderator cover gas

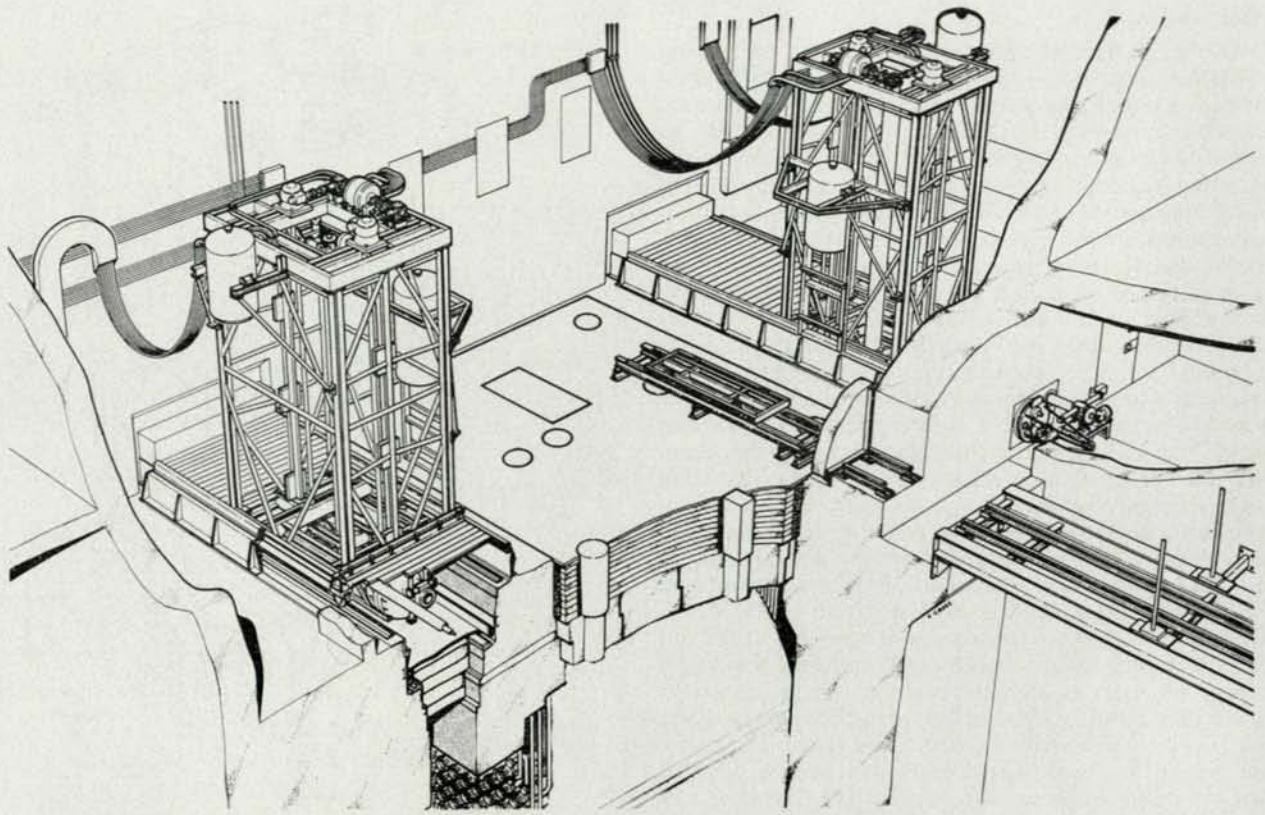
11. The helium above the moderator is below atmospheric pressure, and miscellaneous joints and other fittings have allowed air to leak in. The mixed gas is at higher pressure in the dump tank and becomes dissolved and entrained in the circulating water. At the lower pressure in the reactor vessel it bubbles out and entrains the radiolytic D_2 and O_2. Nitrate ions are formed but removed by the ion exchange purifier. D_2 and O_2 are recombined, leaving an excess of nitrogen. The equilibrium that is reached appears acceptable. It is clearly possible to consider eliminating the helium in future designs with a possible saving in cost.



6a—*Fuelling machine head and carriage assembly*

Water line couplings and freeze plugs

12. A feature of the NPD installation that should not recur in future plants is the too liberal use of mechanical couplings in the small hot water lines. During commissioning it was found that some 25 000 of these couplings had been incorporated mostly in the lines to the differential pressure sensors and the activity monitors. There is no evidence yet that if very carefully assembled and operated with limits on temperature, temperature changes, vibration, etc., the couplings might not be satisfactory. About 5000 were condemned and remade on initial inspection. These operate relatively cool and have given no trouble. Four others failed initially in pressure and temperature tests. Forty or more have subsequently developed leaks. Most of these are at junctions between heavy section $\frac{3}{4}$ in. dia. fittings and $\frac{3}{8}$ in. lines operating at high temperature (250°C) and pressure (1100 psi). Some refitted couplings have failed, but these had been remade under very awkward working conditions and may not have been satisfactory. An intensive study of the problem is under way. In future installations welding will be used where



6b—*Cutaway sketch of fuelling machine carriages and reactor vault*

possible, and mechanical couplings will be restricted to positions that are properly accessible for refitting using proper tools even if wearing complete protective clothing.

13. Other leaks have been encountered at valve stems and other points, and for their repair very rapid procedures have been developed employing liquid nitrogen and plastic wraps to freeze the D_2O in the lines (Fig. 8). In awkward locations this technique has introduced its problems. It is necessary to guard against ice crystals interfering with mechanical joints and packings. In one instance liquid nitrogen spilling on the floor made an unintended freeze plug in a line that was sealed off elsewhere leading to the classical burst water-pipe.

Control of tritium

14. These difficulties with leaks have led to a radically different approach to the tritium problem that promises other important advantages. Tritium builds up in heavy water by neutron irradiation, and when heavy water escapes, TDO is present in the airborne moisture. It is then liable to be absorbed through the skin as well as inhaled so that it is necessary to work with air masks and protective clothing. Even so, it is necessary to restrict the concentration in the air. Hitherto it has been the practice to purge the air from a room where maintenance work is necessary; now it appears preferable to dry the air. For many years industrial adsorber dryers (Lectrodryers) have been available, but only in recent years have we used them for heavy water recovery. NPD was originally only equipped with a freezer dryer in the reactor vault. This proved insufficient when it was not found practicable to achieve the target of 1 ft^3/min for air inleakage and 10 ft^3/min was allowed. To meet this a Lectrodryer was installed and was so successful that one of larger capacity, 600–1000 ft^3/min, has also been installed in the boiler room. It is also used to replace the purge previously applied to the fuelling machine room that has had to be entered frequently. Another advantage of this procedure arises from the consideration that upgrading costs for recovered D_2O are much lower than the price of fresh D_2O. Moreover, by maintaining the atmosphere in the boiler room 'desert dry', losses of D_2O to the concrete walls are minimized, and at the same time by analysing the moisture condensed on a 'cold finger' monitor, any change in the relative leakage of D_2O and H_2O is more readily detectable. A further advantage, if used in other installations, is that it becomes acceptable to operate at or above atmospheric pressure because any outleakage carries little D_2O or TDO, and inleakage of moisture laden air is minimized.

Heavy water losses

15. One of the important questions concerning heavy water cooled reactors that NPD had to answer by its demonstration was the practical loss rate. The answer cannot be given and was not expected as a single figure of so many pounds a day, because it has always been realized that the loss is a balance between the *leak* rate and the fraction one can afford to *lose*. Leaks are not losses. By providing more secondary containment and recovery systems, the losses can be made negligible but the cost rises.

16. From what I have already said, it will be clear that we have installed some improvements for the recovery of heavy water, and we expect to bring the losses that have ranged between 10 and 30 lb/day down to only a few pounds per day. Detailed study of the losses from NPD gives no reason to expect that losses from a full scale plant would be significantly higher. A loss of 20 lb/day from the 200 MW(e) CANDU with an inventory of 360 000 lb would be about 2% per year, which is quite acceptable.

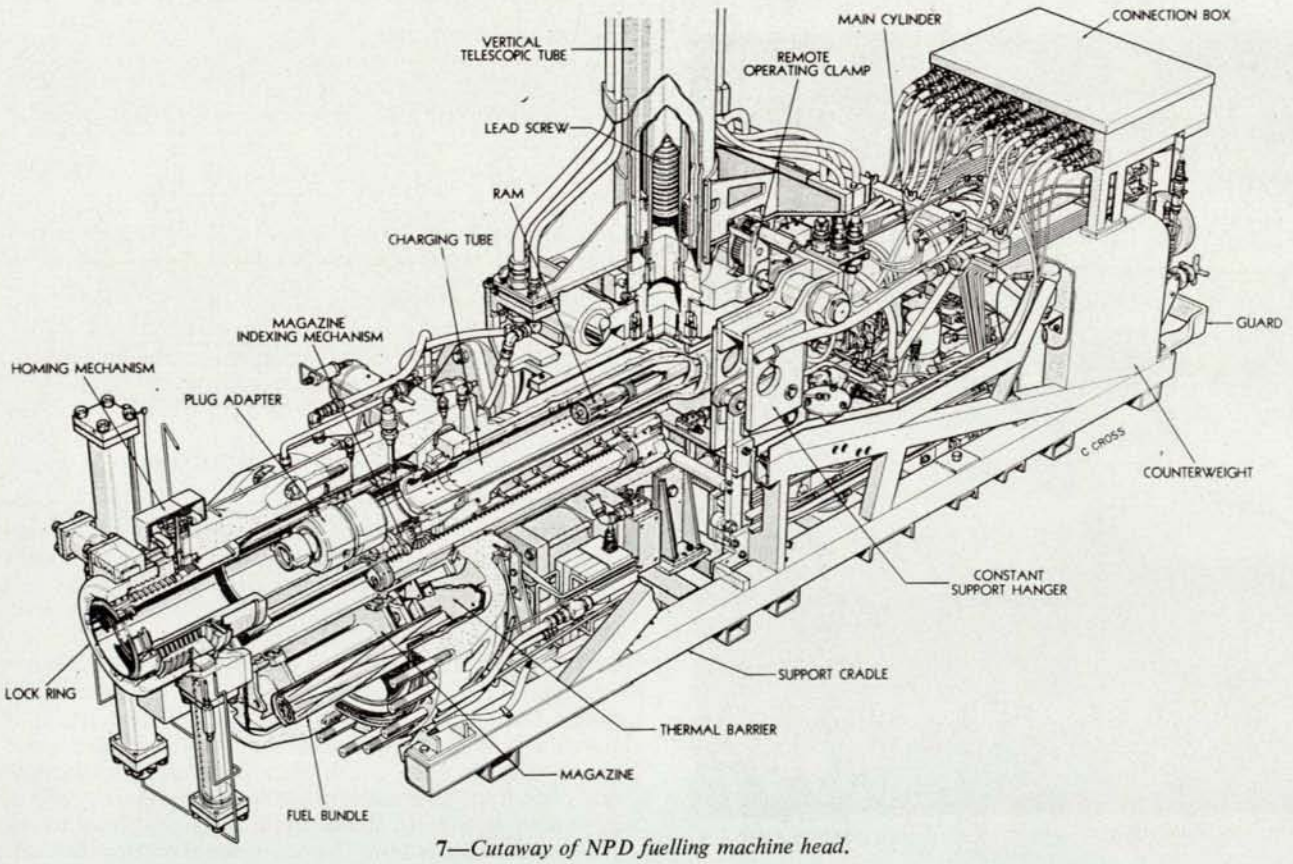
FUTURE HEAVY WATER MODERATED POWER REACTORS

17. The 200 MW(e) CANDU at Douglas Point on Lake Huron is expected to be completed this year and commissioned in 1965. Construction costs are still within the estimates, despite the devaluation of the Canadian dollar and the imposition of taxes on some construction materials. The cost of power should be below 6 mill/kWh, but the moving target is now lower. We are therefore designing a station that would initially have two 500 MW(e) reactors, and preliminary estimates made a year ago suggest 3·5 mill/kWh for the cost of the power. This figure, however, assumes some improvements such as the use of Zr–2·5% Nb alloy for the pressure tubes stressed at 26 000 psi, the development for which is not yet completed. There are, however, other potential improvements of which a shortening of the construction time would be more rewarding. The recent challenge by the GE in their Oyster Creek proposal will probably help us in that direction. At the present time some important decisions are being made on the design so that it is not possible to speak definitely, but the prospects seem good.

18. A 200 MW(e) plant similar to CANDU is to be built in Rajasthan, India; other international projects are being studied, in particular the Pakistani government has approved a projected 132 MW(e) CANDU type reactor for Karachi.

19. At the same time our development studies on other coolants for heavy water reactors are continuing. In the evaluation made a year ago (AECL-1730), it seemed that all the four coolants, heavy water, light water fog, light water boiling, and the organic liquid, showed almost equal economic promise in the 450 MW(e) unit size, with a slight margin in favour of the organic liquid. The position remains much the same except that the prospects for D_2O coolant and boiling H_2O have improved so that the prospective economic advantage of the organic coolant appears no longer significant. Somewhat higher burn-up is now predicted and, when the scale is large, lower fuel fabrication costs.

20. Our boiling H_2O prospective design lies between the SGHW proposal of the UKAEA and boiling H_2O design of the Du Pont study (DP-875). Like the Du Pont design, the reactivity controlled by the coolant is kept low by increasing the fuel in the channel and limiting the specific power rating, but we retain natural uranium fuel and a well moderated reactor. We expect to reach in technique the stage of being able to undertake the design of a prototype at the end of this year. There are experiments in the reactor loops at Chalk River and in heat transfer rigs that should yield valid design parameters this year.



7—Cutaway of NPD fuelling machine head.

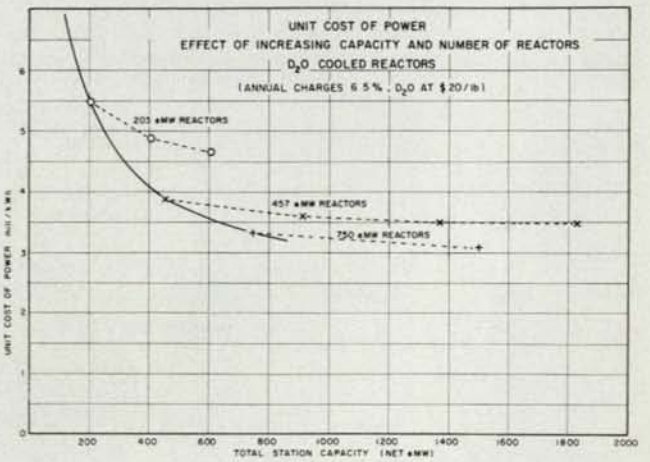
Table 1: *Estimates for 450 MW (e) (nominal) power plants (single-reactor stations)*

Coolant		D_2O	H_2O Fog	Boiling H_2O	Organic
Net electrical output	MW(e)	457	454	457	457
Plant direct costs		$ 59 020 000	$ 60 921 000	$ 62 080 000	$ 54 953 000
D_2O inventory cost (at $20/lb D_2O)		14 300 000	10 790 000	12 408 000	10 034 000
½ fuel charge		2 320 000	3 230 000	3 320 000	3 400 000
Indirect costs (inspection, design, purchasing, admin., commissioning)		16 100 000	16 100 000	16 100 000	16 100 000
Contingency		12 000 000	12 000 000	12 000 000	12 000 000
Interest during construction		11 400 000	11 300 000	11 600 000	10 600 000
Total plant cost		115 140 000	114 341 000	117 508 000	107 087 000
Specific capital cost	$/kW(e)	252	251·9	257	234·3
Unit power cost*	mill/kWh	3·86	3·89†	3·77	3·45
Fuelling cost	mill/kWh	0·80	0·86	0·69	0·42
Burn-up	MWd/tonne U	8680	7600	8985	11 300
Unit fuel cost	$/kg U	52·00	52·37	49·64	38·50
Steam cycle efficiency	%	34·6	36·7	36·7	38·0
Net station efficiency	%	31·3	33·5	33·5	34·2

* Note there is a minor difference from Fig. 9 in that the annual charge rate on D_2O is 5·4%. Other capital charges are taken at 6·5% per year. Charges are spread over a 7000-hour year (i.e. 80% utilization).

† G. A. Pon points out that redesign of the fuel for the fog-cooled reactor on 'assumptions that are more consistent with the level of optimism indicated in the boiling H_2O reactor report' would decrease the unit energy cost by 0·14 mill/kWh. The result, 3·75 mill/kWh, is effectively identical with that for the boiling H_2O cooled reactor.

8—*Pouring liquid nitrogen for partial freeze plug wearing protective clothing*



9—*Cost of power versus station capacity*

THE LONGER TERM FUTURE

21. When, in any area, the scale of nuclear power generation reaches ten or more million kilowatts, the cost of reprocessing fuel is expected to become low enough to support the recovery of fissile material, and spent fuel from the heavy water reactors would acquire a credit value. The recovered plutonium could be recycled as proposed in my paper to the 1955 Geneva Conference, but as foreshadowed from the beginning, it appears more profitable to irradiate thorium rather than U^{238} in thermal reactors. For initiating the thorium–U^{233} cycle, plutonium may have one advantage: if mixed with thorium it can after irradiation be chemically separated from both thorium and uranium, whereas U^{235} and U^{233} both grow U^{236}. Plutonium is quite effective if the neutron temperature is low. The heavy water reactors can adapt

readily to such changes in fuel and because of their good neutron economy seem likely to hold their own indefinitely. When the price of fissile material rises, neutron economy and reactors requiring only a low fuel inventory acquire even greater importance.

ACKNOWLEDGEMENT

22. Making this review brings to mind the many years of work and growing experience of so many. I take this opportunity to express appreciation of the many indispensable contributions made by the staffs of the three organizations responsible, namely, the Canadian General Electric, the Hydro-Electric Commission of Ontario and Atomic Energy of Canada Ltd and by all the manufacturers and constructors. I would like in particular to mention L. G. MacConnell and the operating staff he brought together and inspired, so that NPD is proving a potent source of improved operating technique and design improvements.

DISCUSSION

Mr P. H. W. Wolff (*English Electric Co. Ltd*) said that the line that Dr Lewis had been able to follow was truly admirable. One saw one continuous thread of policy. It happened to be the policy of neutron economy, and Dr Lewis had followed it unswervingly, and for reasons which one could appreciate were good and sufficient reasons for Canada in particular, where there was no investment for military purposes in a plant to concentrate on fissile material; and where, because of the smallness of the population, the scale of operation—the potential scale of the electrical system—was smaller than in the USA, Russia or the UK. Linked to the question of the scale of operation therefore, was the question of plutonium recovery. Both things had driven Canada in the direction of searching for neutron economy. Dr Lewis had been the apostle of that, and he had followed the line unswervingly.

24. Atomic Energy of Canada Ltd were to be congratulated on a good engineering job. He did not see that Dr Lewis had to be apologetic about the fact that the reactor took him from 1956 to 1962 to get on power. Similar things had happened to other people. He mentioned his own organization's Canadian company. They knew that the Douglas Point plant showed excellence of detail and considerable ingenuity. He would mention particularly the heat exchanger designs, which were particularly economical in the heavy water investment.

25. He had been proposing to congratulate Dr Lewis on having an annual charge rate of 6·5%. Had Dr Lewis meant by that the interest rate or the total annual amortisation rate?

Dr Lewis said in reply that he meant the total amortisation rate. The long-term interest rate which was being used at Ontario hydro was close to 4·5% at the time that figure was given. It had now given them the figure of 5·5%. But it seemed odd that they still kept a little below 7% on final charges. That was in part due to a change of timing. They were told previously to amortise the reactor in 15 years. Now they had been allowed 30 years.

Mr Wolff said that if, as Dr Lewis said, it was the amortisation rate, he truly congratulated him. It was the biggest contribution to nuclear power that Dr Lewis's Government had made for him.

Mr D. R. Berridge (*CEGB*) said that Dr Lewis had a reputation for giving a good lecture, and the lecture that evening had been no exception.

29. It seemed that accounts of operations always had a fascination for people, whether applied to engineering or to human machinery. Their curiosity and fascination that evening had been far from morbid. They should congratulate Dr Lewis on being so forthcoming and thank him for exposing not only his successes but his difficulties.

30. Those who had to follow closely the development of reactors in various parts of the world as part of their business had their particular phobias about each type, and that

included the CANDU. The principal question marks in his own mind had always been heavy water losses and pressure tube life.

31. With regard to heavy water, he had mixed feelings about the use of it in reactors. It was a very attractive moderator if one could use it at quite low pressures, but it seemed to have no merit at all as a high pressure coolant.

32. The sort of accident that Dr Lewis had described in respect of his charging machine was the kind of thing that happened during commissioning and early operation, and had a wonderful ring of truth about it; but it did seem to be a potential source of heavy loss. That sort of difficulty might cost money not only in view of the possibility of heavy water losses, no matter how carefully the reactor system was designed, but also in plant outage. With an expensive coolant and outage, the recovery time might well be much longer than with a less precious coolant.

33. He was relieved to see that the organic liquid proposition no longer looked as attractive as it had done. If such a proposition looked attractive on paper, one had to chase after it, but one could not help feeling that there would be operational snags with the use of organic liquids which might offset any marginal paper savings.

34. He wondered whether the fuel cost for the boiling light water reactor could be reduced in the same way as that for the CANDU had been reduced. Did it mean that the light water boiling, D_2O moderating system still looked less attractive? If so, the lines of development in that case in Canada and the UK seemed to come together.

35. On the subject of pressure tubes, he understood that all was going well so far. He had been impressed by Dr Lewis's free and easy attitude towards the changing of pressure tubes. Nevertheless, he did not really like the idea of having to plan to do it. Was there any more that Dr Lewis could say about his proven work on the tubes, especially about the new work on $2\frac{1}{2}\%$ niobium alloy?

36. One heard of difficulties over achieving adequate gland life on the Douglas Point circulating pumps. The meeting had provided an opportunity for Dr Lewis to debunk that.

37. The division of reactor operation into demonstration and improvement runs was very ingenious and, indeed, logical, but he was sure that operators in the audience must appreciate that the forced outage could often be deferred for an appreciable time until one had a planned outage. There must be a question mark in the minds of people as to how meaningful the availability figures over relatively short periods of operation were.

38. He thought that they should congratulate Dr Lewis or his salesmen upon selling one and perhaps two CANDUS overseas on the basis of operating experience and other information which was rather less than some of their friends in the UK would lead them to believe was necessary to effect such a thing.

Dr Lewis said in reply that the contribution had opened up a discussion which could well go on for the rest of the evening, but he would deal with just a few of the points raised.

40. With regard to the subject of pressure tubes and their experience, perhaps he should have mentioned it. One of the things that they did during the improvement period of NPD was to take out all the fuel from one channel and go in and take plastic mould casts to find out what the condition of the surface of the tube was in regions where they could hardly see it. They also gauged it very exactly to see if it had become elliptical or deformed. That was done just before the last run started in December. The pressure tubes would have been in since August 1961. They had had the full load in in February 1962, and had been operating as described ever since. The condition appeared in all ways to be satisfactory. Small scratches and other little marks could be found, but there was nothing that caused anybody any concern. The roundness and the bowing were all perfectly satisfactory. They would have been a little worried if it had not been, because in NPD the tubes were designed for 10 years' life.

41. He agreed that they could not take the operating experience on NPD as really indicative of the operating experience that should be expected from CANDU. In the design and construction of NPD they were often told that they were designing a reactor that must not last for more than 10 years. In other words, they were never able to plead for any little luxury about the reactor. It had just to go on for 10 years. Therefore, a great many things were left a little unprovided for, and the matter of the conditioning atmosphere in the boiler-room was a case in point.

42. They had written a specification for dealing with the atmosphere in the boiler-room and the atmosphere in the reactor vault, but during the construction and commissioning period those concerned failed to meet the condition that there should be only 1 ft^3/min of air getting into the reactor vault, and they found that they were getting ten times that amount, which upset the calculations. They were not pretending that it was more than a demonstration reactor. They appreciated the advantages that when one was operating one could carry out tests and other things, but they were really using the improvement periods for such things as checking up on the pressure tubes and so on, which was something which should not have to be done in the full-scale reactor.

43. Mr Berridge had suggested that heavy water had nothing to commend it for use as a coolant. Mr Berridge was, he was sure, speaking almost as a scientist there. They had wanted to use the light water boiling coolant, something with a higher temperature, and so on, but the engineers were in favour of heavy water as a coolant as they knew how to design with it. Other experiments were proceeding with boiling water, but they did not feel sufficiently confident about all the necessary features of that design to commit themselves yet. They had experiments going on in loops, and he was happy that from the technical point of view by the end of the year he would have enough information for a prototype boiling water cooled reactor.

44. He did not want to give the wrong impression. They had been running on a light water boiling loop in CANDU during last year up to November, when they had shut down for conversion. They also had a steam loop and a fog cooled loop in the NRX reactor. They had a lot of experience, but not sufficient yet to enable them to commit themselves to light water coolants as they had committed themselves to heavy water.

45. By working with heavy water one learnt not to worry about it quite as much as one was apt to do when thinking of all the horrors of tritium and the expense of losing it. It went into one's routine.

Capt. H. F. Atkins* said that he worked for the FBI but was not speaking for the FBI.

47. The great work which Dr Lewis had described showed that even in reactor development God was not on the side of the big battalions; that clear thought, good judgement, and steady purpose could do more than big budgets, vast establishments, and hordes of men busy taking in each other's washing.

48. Dr Lewis had led the world in ridding them of reactor pressure vessels, but too few had followed his example. The first reactor pressure vessel failure above 200 fathoms deep would cause a mad rush to pressure tubes.

49. Dr Lewis had led the world, too, in heavy water moderation. He distinguished clearly between leaks and losses of heavy water. Any steam generator tube leak must, he presumed, be a loss, since the heavy water was too diluted for economic recovery. In integral reactors, detection, location, and isolation of a leaky tube was far harder, if indeed possible.

50. In one way, and one only, Dr Lewis had disappointed him greatly. For the future he had havered between boiling water, fog cooling, and the steam generating heavy water reactor, although he finally mentioned the steam loop. Why not avoid two-phase flow with all its troubles of heat transfer and of flux giving thermal stresses to cause crazy cracking; its chugging, and the deposition of solids to help over-heating, pitting, and stress corrosion cracking? Besides, with saturated steam one was tied down to very poor conditions. The AEA steam generating heavy water reactor was a very poor imitation of the Rolls-Royce steam cooled heavy water marine reactor. Flow through the tubes of the steam generating heavy water reactor tended to be unstable, and as dry steam left the reactor, all solids in the feed were left in the core. As he knew, Dr Lewis could not find a zircaloy to stand the temperature. Rolls-Royce had a nice solution of a thin barrier tube of an austenitic steel of very high nickel chrome content or of nimonic and a thick cold pressure tube which could be of aluminium.

Dr Lewis said in reply that with regard to the point about the pressure vessel and the pressure tubes, he had forgotten what the cancellation charges were, but they certainly amounted to hundreds of thousands of dollars. They were certainly large. Their relief about getting away from the pressure vessel arose from the fact that it was exerting its own will on the design of the station. In view of the rates at which they were allowed to heat it up and cool it down and the amount of subsidiary and accelerated cooling that they had to put inside, they were going the wrong way with the pressure vessel. In view of the fact that they had to move the fuel moderator up and down and put a hot coolant through, it was a bad design. Pressure vessels, and particularly prestressed concrete pressure vessels, had a great deal to commend them, but he was still extremely glad that they had turned to the pressure tube reactor.

52. It had been suggested that they were not getting what they might by keeping to the low temperature saturated steam. One should look very carefully at the detailed specification for CANDU, where the steam temperature was 482°F and the pressure only 560 lb/sq. in., and the steam cycle efficiency was 33·3% with an overall net station efficiency of 29·1%. They did not regard themselves as an inferior league in respect of reactor and power station efficiency. Moreover, when they introduced boiling, they saw that with, for steel, 1000–1200 lb/sq. in. pressure in the tubes in the reactor they could increase their efficiency so that the net station efficiency was in the region of 32%.

53. With the organic type, they were even more efficient. With that they were of course able to go to a certain amount of super-heat, although perhaps he would argue that they ought to have gone to higher pressure design. All the problems about two-phase flow, put so admirably in Capt. Atkins' concise statement, were there, they knew, but they had nevertheless to find a way of working.

Miss Mary Goldring (*The Economist*) said that she had always been a tremendous admirer of CANDU, but Dr Lewis's figure of $252/kW seemed high for a reactor. With a pressure vessel, had the cost gone on the heavy water inventory, on the high engineering charges in CANDU, or on Dr Lewis's devilish charge machine?

55. Her next question might be unfair, but she would be grateful for an answer from Dr Lewis. She had heard it said several times that, although there was a tremendous interest in the UK in CANDU, the royalty policy of Atomic Energy of Canada Ltd had tended to price it out of the British market. What were Dr Lewis's views on that?

Dr Lewis said in reply that AECL 1730 gave a breakdown of the estimates. The figure for the 457 MW case was $252/kW(e) for the first reactor. He was sure that was high, and that in the future they would do better. Heavy water had contributed $14.30 million to the cost, so that it represented 12–13%.

57. The fuelling machine might be devilish, but it did not add a great deal to the cost. One had to find some means of handling the fuel. Once one was over the development, the actual cost of the machine was not so very high.

Mr Haslam asked whether Dr Lewis had studied the possible use of enriched fuel in heavy water reactors—enrichment either by uranium-235 or by plutonium? It seemed to him that a great limitation of Dr Lewis's system was its high capital cost. He wondered whether Dr Lewis had permitted himself the use of enriched fuel. He could use uranium oxide to better advantage, taking it to the sort of irradiation levels that the Americans went for—20 000 MWd compared with his 10 000. By that means he might be able to use H_2O instead of the expensive heavy water.

Dr Lewis said that the question was not quite as simple as it seemed. He and his colleagues had looked at the cost of enrichment, and although their official policy—and he was responsible for it—was not to process the fuel from CANDU but to put it into storage, he was still secretly counting that as an asset, believing that some day when the price of fissile material began to rise it would be worth while extracting the plutonium and possibly the residual uranium-235 that was in the fuel—certainly the plutonium. He believed that the day

* Capt. Atkins died on 3 August, 1964.

would come when processing plants were on such a scale that one would not be charged $20/kg, which was the sort of minimum round figure which was in all the USA forecasts. If someone would want in the future, as he suspected, to take that plutonium out, it was rather better as it was now than it would be if they used enriched uranium, taking the uranium on further and putting more neutrons in the fission products. They were getting better neutrons economically from their rather limited irradiation, from the 10 000 MWd, than they would with the 20 000 MWd with enriched uranium.

60. Therefore, it was clear that they had looked at the possibility of enrichment and had put it into their cost optimizing formula. In the document to which he had referred it was stated that for none of the reactors was there a significant advantage to be gained from using enrichment, and certainly none at all in the case of the organic reactor, with its high burn-up. In the year that had passed since then, they had reinforced that conclusion by further work on plutonium and lattices. They were now putting up the expected burn-up, and it tended to make it still more advantageous to use natural uranium with no reprocessing.

61. He felt that they were right to be striving for the high neutron economy in their reactors, and were certainly not running away from using enrichment.

Mr Powell said that when Dr Lewis quoted the overall station efficiency, he had probably referred to the efficiency of the heat carried away from the fuel by the coolant, but Dr Lewis replied that that was not so. He asked whether the figure included the 6–7% which went to the moderator, and Dr Lewis said that it did. Mr Powell said that the point about pressure tube reactors was that it was difficult to recover any significant amount of heat going into the moderator because the heat had such low values that it was hardly worth recovering. Had Dr Lewis any plans to recover it? It would be worth as much as super-heating in the reactor.

Dr Lewis replied that the question raised two very interesting design points. They had not closed their eyes against using the moderator hotter. The advantage of using the moderator cool was that the neutron economy was better. The values in respect of plutonium and uranium were higher.

64. It would be seen that the heavy water moderator reactor in question used a 4 in. dia. pressure tube instead of a 3½ in. tube. That had an influence. It meant that the neutrons would be hotter anyway. If they were, letting the moderator rise a few degrees in temperature, say to 80°C, would put it in the region where it could be useful for feed heating, and that had been put into the design instructions as they had come along.

65. With regard to the other point the heat which went into the moderator, although very useful, was also very cheap. In breaking down the capital cost of the reactor, it was found that a very large amount of it was proportional to the high temperature heat being handled. The fuel cost was extremely low. The reason why they still had to chase high efficiency—high thermal to electrical conversion efficiency—was that a large component of the cost of the plant went up with the total heat being taken out at high temperature. It did not arise from the heat given out by the moderator. Therefore, the gain was not as great as might be expected.

Studies to determine the safety margins available on magnox-type gas-cooled reactors in a depressurization accident

F. R. Farmer, BA

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An introduction to five Papers for presentation and discussion at a half-day symposium at 2.30 p.m. on 11 November, 1964 at the Institution of Civil Engineers, London, SW1*

MAGNOX has proved itself a reliable canning material for use in gas-cooled reactors up to temperatures around 450°C. At higher temperatures no sudden deterioration occurs until the region of the melting point (645°C), when ignition in carbon dioxide is possible. If the reactor lost gas pressure through a circuit failure and as a consequence some can temperatures reached 645°C, fission products would be released from the fuel and might escape through the circuit breach to atmosphere. The Papers in this series report studies aimed at evaluating this risk, taking as their starting-point the assumption that a circuit failure has occurred. Some discussion of this question has been given in the Paper by Irvine *et al.* in the January 1964 *Journal*, which dealt with possibilities of intrinsic material failures for steel pressure circuits. The following aspects of the problem are dealt with in the present series:

(*a*) the abnormal flow of coolant over the fuel elements which might follow a broken circuit (Papers 1 and 2); it is shown in Paper 2 that the worst heat removal from fuel occurs if a coolant duct is severed somewhere near the inlet to the reactor pressure vessel;

(*b*) the temperature rise at the fuel cans during this interval of flow disturbance (Paper 3); and

(*c*) the starting temperatures on which this transient rise would be superimposed; out of some 10 000 fuel elements in a typical magnox reactor, a point temperature on a few tens of elements may be measured.

The assessment of the real temperature distribution from this sample required a good general appreciation of the physics of the reactor, and some shrewd operational research on the part of those running the plant. The approach to this problem in the case of the Calder and Chapelcross reactors is described in Paper 4. Inevitably the final statement of the temperature distribution must be statistical—that there is a given chance of a number of elements exceeding a given temperature. Thus for a given starting temperature, there is a calculable chance that the event will cause one or more elements to exceed a limiting temperature. Paper 4 includes a description of a method by which the information in the previous Papers may be combined to calculate this chance of ignition. In fact, from the practical standpoint, it deals with the reverse problem—how, starting with a desired chance of ignition (the Authors have chosen 1 in 10), one may determine permissible starting, i.e. normal operating temperatures. This criterion is accepted by the UKAEA as a limit on the operating conditions of the Calder and Chapelcross reactors.

The final Paper, 5, points out that other data used in the previous calculations have some scatter of uncertainty, and argues that it would be logical to allow for this in setting the operating limits. The present tendency is to allow for worst values where data are in doubt, so that this approach would relax operating limits. This idea does not yet have formal acceptance within the UKAEA.

Many aspects of the studies are continuing. Although the concept of 'designing and operating to be safe against rapid loss of pressure' has a somewhat arbitrary flavour, it has the advantage of bringing together a number of diverse aspects of reactor safety—normal running temperatures and ratings, response of protection, failure characteristics of the cans, and transient flow characteristics of the given gas circuit—and combining these into a figure of merit which may be used by the designer and operator. It is this underlying usefulness which prompts the offering of these Papers, rather than the questions of whether a fire would result if the limit were transgressed.

* The Papers are printed on pp. 174–213.

1 Basic transient flow studies in Stage 1 reactor models

J. G. Moore, BSc, AInstP, and **F. J. Walford***

This Paper is the first of five to be presented at a half-day symposium of the Society on 11 November

A theory has been developed which will predict satisfactorily pressure transients in a typical gas-cooled reactor system which is depressurizing rapidly (within seconds). The theory has been tested against experimental results and the agreement is satisfactory.

NOTATION

A = area
C = discharge coefficient at duct entry
c = velocity of sound
D = diameter
f = friction factor
g = acceleration of gravity
k = pressure drop coefficient $\Delta P/\frac{1}{2}\rho u^2$
K = discharge coefficient for orifice
L = length of duct
m = mass flow
M = Mach number
n = number of heat exchangers
P = pressure
r = pressure ratio across orifice
R = gas constant
Re = Reynolds number
T = absolute temperature
u = fluid velocity
V = volume
W = mass
Y = expansion factor for orifice
β = ratio of orifice to pipe diameter
ϵ = parameter, equal to unity or γ
ρ = density
γ = ratio of specific heats

Subscripts

c = core
e = heat exchanger
i = initial value
L = lower duct
o = orifice, stagnation conditions
u = upper duct

Other subscripts refer to positions in Fig. 2.

INTRODUCTION

THE system under consideration is a gas-cooled reactor and the fault postulated is the loss of coolant due to a complete duct failure. The problem is to determine the pressure difference across the core and also the flow of gas through the core. Both are very important from a safety point of view. The pressure difference across the core will determine the force on it since in a hot duct failure the possibility exists of core movement. In the case of a cold duct failure, the possibility exists of stagnant or very small flows of gas in the coolant channels which would lead to unsatisfactory cooling of the fuel elements.

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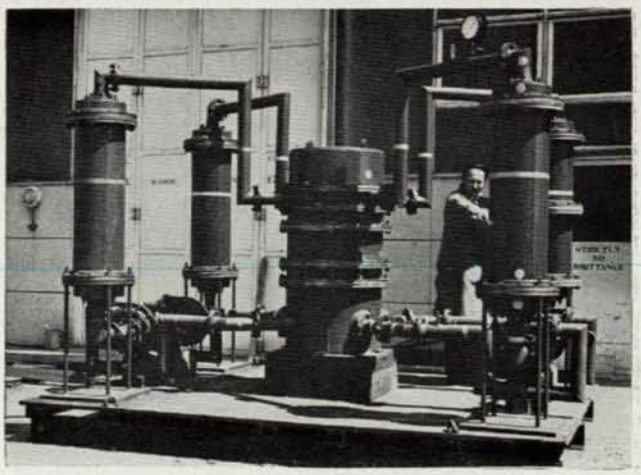
EXPERIMENTAL INVESTIGATION

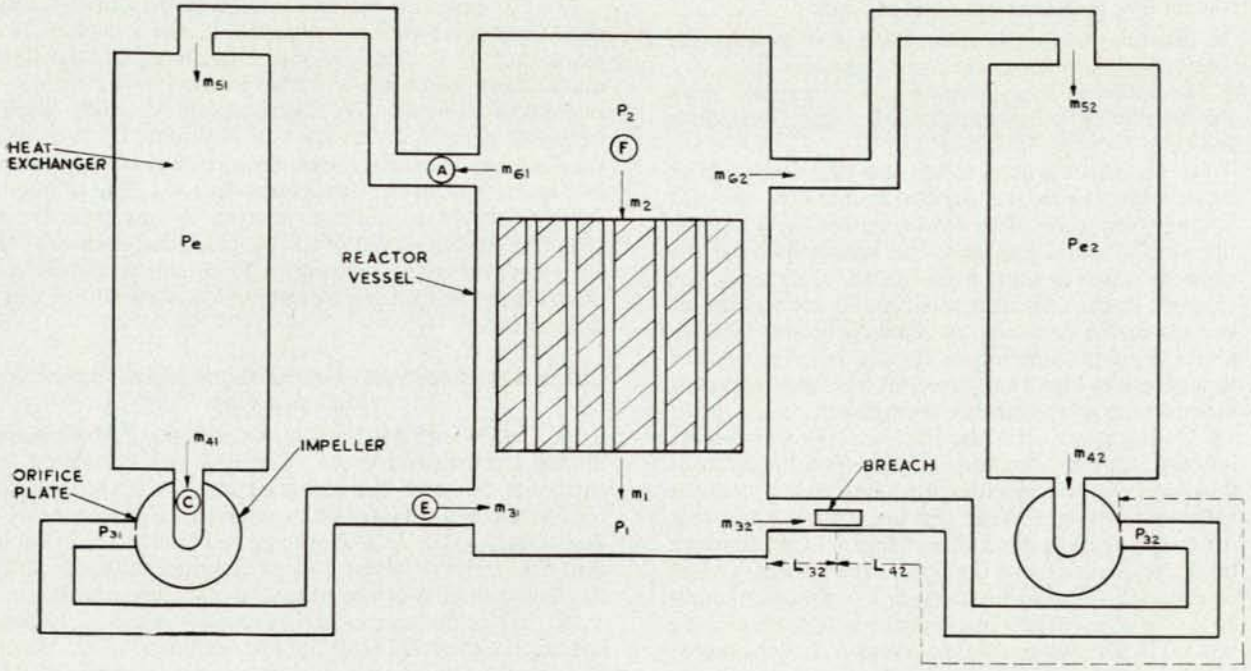
2. A photograph of the assembled model is shown in Fig. 1. It consisted of a pressure vessel connected to four circuits each of which contained a chamber to represent the heat exchanger units. A flow of air was used to calibrate the equipment and to adjust the flow resistances of the core and heat exchangers to the required values. In order to make the theoretical analysis simpler, the rotors were removed from all four impellers and orifice plates placed in the ducts on the output side of the impeller casings.

Methods of measurement and simulation of fracture

3. Rectangular bursting ports were introduced into the walls of both hot and cold ducts as near to the reactor vessel as practicable. The area of each port was 16·5 sq. in., approximately twice the area of the duct. To simulate a sudden fracture, the diaphragm used to seal the port was ruptured by means of a spring-loaded pricker, operated remotely. The decay of pressure in the model was measured using type MQ 18 piezoelectric pressure gauges with associated amplifiers and oscillographs. Gauges were placed in pairs diametrically opposite each other in positions A, C, and E as shown in Fig. 2. A gauge was mounted in position F in the wall of the



1—*Reactor model*



2—Diagram showing experimental gauge positions (A, C, E, and F) and positions of calculated pressures and mass flows

upper chamber of the reactor vessel and a further gauge was placed in the ducting near the bursting port.

4. For convenience most of the tests were performed with air in the model, but carbon dioxide was used in some tests since this is used as coolant in the Stage 1 reactors. These tests were done with the gas initially at room temperature and a pressure of 100 psig. Additional tests were performed with air at initial pressures of 150 psig and 60 psig. When the pressure decay curves of these tests were normalized, however, they were found to be almost identical and are therefore not considered further.

Variation of circuit parameters

5. Some tests were repeated in which various circuit parameters were altered in order to provide more information to test validity of the theoretical model. The orifice plates used to simulate the impellers had a nominal orifice diameter of 2 in. The tests were repeated with orifice plates having a nominal diameter of 1 in. In addition, the tests were repeated with two circuits blanked off. All these tests were done for hot and cold duct bursts. The experimental results quoted in this Paper represent the mean of eight measurements and the standard errors of the pressure decay curves are typically $\pm 0{\cdot}10$ psi at 15 msec rising to $\pm 0{\cdot}50$ psi at 100 msec.

THEORETICAL INVESTIGATION

6. The theoretical treatment of the gas flow in the unfractured ducts, pressure vessel and heat exchangers assumed that quasi-steady state conditions, i.e. steady at each instant of time, existed throughout the depressurization. In the fractured ducts, because of the severity of the pressure changes the conditions were assumed unsteady. The theory for these ducts is given in Appendix 2.

7. The first set of equations which were produced (Appendix 3, equations 1–23) are completely general, but are later modified to illustrate various assumptions which can be made to facilitate computation of the pressure transients. The validity of these various theoretical treatments and the importance of the various parameters was tested by comparing the results of the theory with experimentally determined pressure transient results.

Calculation of conditions in gas spaces in pressure vessel and heat exchangers

8. To calculate the rate of depressurization in the heat exchangers it was assumed that the velocity of the gas in them was low because of the relatively large volume. These heat exchangers were then treated as simple volumes whose pressure varied according to the net rate of inflow of gas. This approach takes no account of heat exchanger resistance. It was found that good agreement could be obtained between the experimental and theoretical results without including it, but in general the effect would have to be included.

9. The variation in pressure in the heat exchangers and pressure vessel is given by equations 1–4. These equations have been deduced from the conservation of energy applied to each vessel which is discharging and receiving gas simultaneously (Appendix 1) and under adiabatic conditions. Equations 1–4 are completely general in that they involve temperatures only. In the computation of the pressure transient, assumptions were made about the nature of the gas expansion and two sets of equations were used; simulating an adiabatic expansion, equations 1a–26a and an isothermal expansion, equations 1b–23b. The relative merits of the two treatments are discussed later.

Calculation of flow in unfractured ducts and core

10. In general, three types of equation were used for the flow in the unfractured ducts and core (Appendix 3).

(*i*) The variation in gas density was determined from the net rate of flow of gas into the volume (equations 5–8).

(*ii*) (*a*) The mean density of the gas was derived from Boyle's law, using the arithmetic mean of the two boundary pressures. The temperature of the gas in the unfractured ducts and core was assumed to be the same as the upstream reservoir for each case, for example in the cold duct fracture the temperature of the gas in the core was assumed to be the same as above the core (equations 9–12). (*b*) The directions of flow shown in Fig. 2 are, however, hypothetical and in order to assign the correct temperature to the gas it was necessary to solve the final equations assuming, initially, arbitrary directions of flow. The directions of flow were then examined and the correct temperatures assigned to the gas. When the new temperatures were inserted in the equations the difference in the pressure transient obtained was negligible. This is what would be expected since the pressures have only fallen about 20% from the initial value and the temperatures even less. (*c*) In some cases the flow reversed. The equations, however, were programmed for the computer in such a way that the temperature of the gas could not be changed if the flow reversed. As indicated above the error would be small anyway. However, even when the flow in a duct reversed, the flow tended to be in one direction for most of the transient, so the temperature of the gas was given the corresponding upstream temperature for the whole transient.

(*iii*) The acceleration of the gas was determined using an approximate form of the momentum equations (equations 20–23).

These equations are an approximate form of the type used for the fractured ducts where compressibility effects are most important and cannot be treated approximately.

Calculation of flow of gas in fractured ducts

11. In the fractured ducts it was necessary to consider the flow as unsteady initially, because of the severity of the pressure changes which occur. (The details are given in Appendix 2 where the variables are given in dimensionless form enabling the fracture to be considered occurring at any position in the duct.) However, because of the relatively large volumes of gas upstream of the fractured ducts it was assumed that the effects of pressure waves, originating at the fracture, would be negligible in these. Hence, the rest of the flow processes may be treated as quasi-steady.

12. This treatment introduces delays in the fall of pressure at the two upstream reservoirs of the broken ducts. This is in accordance with the experimentally observed rates of depressurization. In addition, time lags between the pressure varying at a point in the circuit and the mass flow which this induces will occur because of the inertia of the gas. The effects of inertia are represented in the flow equations by the terms involving acceleration, i.e. those terms involving time derivatives in equations 20a–23a. The effect of removing these terms and also the time lags associated with the unsteady flow in the fractured ducts is discussed later.

13. To calculate the flow following the initial transient conditions (discussed in Appendix 2) the assumptions are made that (*i*) the flow is one-dimensional, (*ii*) the flow is quasi-steady; (*iii*) there is neither internal heat exchange nor internal shaft-work; (*iv*) differences in elevation produce negligible changes compared with frictional effects, and (*v*) the ducts are of constant area. Shapiro[4] has solved the problem assuming that the fluid is a perfect gas. The solution, in terms of the Mach number, is given by equations 27a and 28a. The mass flows out of the two fractured ducts are calculated in terms of their respective Mach numbers, from equations 14a in one case and a combination of 16a and 13a in the other case.

Calculation of the maximum values of Mach number in the fractured duct

14. In the initial investigation the limiting Mach number for the fractured ducts M_{32} and M_{42} was calculated from equations 27a and 28a assuming straight ducts and using friction factors obtained from reference 5. The approximation of treating the ducts as straight was found to be too simplified. The fractured ducts contain right-angled bends so that the flow cannot really be treated as one-dimensional.

15. Higginbotham *et al.*[6] have studied the performance characteristics of 90° bends in circular ducts up to Mach 1. The results showed that choking occurs downstream of the elbow. Higginbotham suggested that choking is obtained because of the loss in total pressure and consequent change in density in a manner similar to that for a long straight pipe.

16. Thus the possibility exists of estimating the choking Mach number for a particular elbow design from a value of the loss coefficient. It is recommended, therefore, to follow Higginbotham and treat the loss due to the bend as a loss in total pressure and find the inlet Mach number from the total pressure lost due to the bends and friction.

Calculation of flow through orifice plates

17. The flow through the orifices was calculated using the recommended expressions given by ASME[1] for low pressure ratios (equations 15a–17d). However, at high pressure ratios $\leqslant 0{\cdot}63$, experimenters[2, 3] have shown that contrary to the behaviour of a convergent nozzle, the square edge orifice does not choke, viz. reach a maximum flow rate which is independent of higher pressure ratios. Experiments have shown that the flow rates (for constant upstream conditions) continue to increase at all pressure ratios between the critical and zero. For high pressure ratios i.e. $\leqslant 0{\cdot}63$, equation 17d is used instead of 17c. Equation 17d has been deduced from Cunningham's experimental results for the geometry applicable to each test in the model. This procedure avoids using arbitrary discharge coefficients and gives the data used some experimental verification.

COMPARISON OF THEORETICAL AND EXPERIMENTAL RESULTS

18. In this Paper only the cold duct burst experiments are considered and only four experimental conditions of the model are included. The validity of each particular model was assessed by solving each set of equations simultaneously using a Mercury digital computer and comparing the results with the experimentally determined pressure transients as shown in Figs. 3a–18f. The basic data used in the solution of the

Table 1. *Summary of variable case data used in the calculation of the theoretical pressure transients Figs 3a–18f*

Theoretical model	Case	k_c	f_{32}	C_{32}	K	ϵ	D_o(in.)	n	Gas	Fig. No.
Adiabatic	1	1·35	0·04855	0·75	0·72		2	4	Carbon dioxide	3a–3f
	2	1·35	0·0971	0·75	0·72		2	4		4a–4f
	3	0·295	0·04855	0·75	0·72		2	4		5a–5f
	4	0·295	0·0971	0·75	0·72		2	4		6a–6f
	5	1·35	0·04855	0·90	0·72		2	4		7a–7f
	No acceleration	1·35	0·04855	0·75	0·72		2	4		8a–8f
Isothermal		1·35	0·04855	0·75	0·72	γ	2	4		9a–9f
		1·35	0·04855	0·75	0·72	1	2	4		10a–10f
	No time lags	1·35	0·04855	0·75	0·72	γ	2	4		11a–11f
		1·35	0·04855	0·75	0·72	1	2	4		12a–12f
Adiabatic		1·35	0·04855	0·75	0·72		2	4	Air	13a–13f
		1·35	0·04855	0·90	0·72		2	4		14a–14f
		1·35	0·04855	0·75	1·00		1	4		15a–15f
		1·35	0·04855	0·75	0·62		1	4		16a–16f
		1·35	0·04855	0·90	1·00		1	4		17a–17f
		1·35	0·04855	0·75	0·72		2	2		18a–18f

equations are given at the end of the Appendixes and Table 1 shows the parameters which were varied to produce the comparisons.

Adiabatic and isothermal treatments

19. The adiabatic treatment (Fig. 3a–f) can be seen to give the best agreement and should, therefore, be used when the maximum accuracy is required, unless it can be shown that adiabatic expansion is not taking place. An isothermal treatment (equations 1b–23b) has also been developed. If such a treatment is used, the theory tends to predict slower rates of depressurization. The pressure differences across various parts of the circuit give about the same values as the adiabatic treatment, presumably as systematic differences in each pressure may have been subtracted out completely. This would not necessarily occur in general.

20. A comparison of the adiabatic and isothermal equations 1–4 shows that when the terms involving the ratio pressures are nearly unity the rate of depressurization will be a factor of γ different. Thus while $(P/P_i)^{\frac{\gamma-1}{\gamma}} \sim 1$ the isothermal treatment will give about the same results as an adiabatic treatment simply by multiplying the rate of depressurization by putting $\epsilon = \gamma$ in the equations. This is illustrated in Figs. 9a–10f. The modified isothermal treatment will, however, overestimate the rate of depressurization when $(P/P_i)^{\frac{\gamma-1}{\gamma}}$ is no longer nearly unity.

Initial transient

21. The effect of assuming that the flow rates take on a steady value immediately the duct bursts, instead of a finite time as explained in Appendix 2, is shown in Figs 11a–12f. In the cases where the build up in flow is included (Figs 9a–10f), the agreement with the experimental results is seen to be good; but when this is omitted the pressures are seen to fall immediately at time zero. The effect on the rest of the transient is small in this case. In the experimental results the time lags are accurate to within $\pm 0{\cdot}25$ msec.

22. The correct time lags should be included when short term phenomena are of interest, such as the calculation of the initial impulse to the core following a duct burst. When the time lags are omitted, the impulse is overestimated. An impulse to the core could be important in the case of a hot duct failure when it is necessary to be sure that large pressure differences cannot be produced, since this would produce disarrangement of the core and possibly prevent the insertion of the control rods.

Effect of neglecting the acceleration in the equations of motion 20a–23a

23. A simplified treatment without acceleration effects—i.e. those terms involving time derivatives in equations 20a–23a—could be used for pressure transients which are not primarily intended to show the initial effects (for example in following cooling effects on the core). There are no peaks in the pressure differences (Figs 8e and 8f) but the agreement is reasonably good with the adiabatic treatment for the actual pressures.

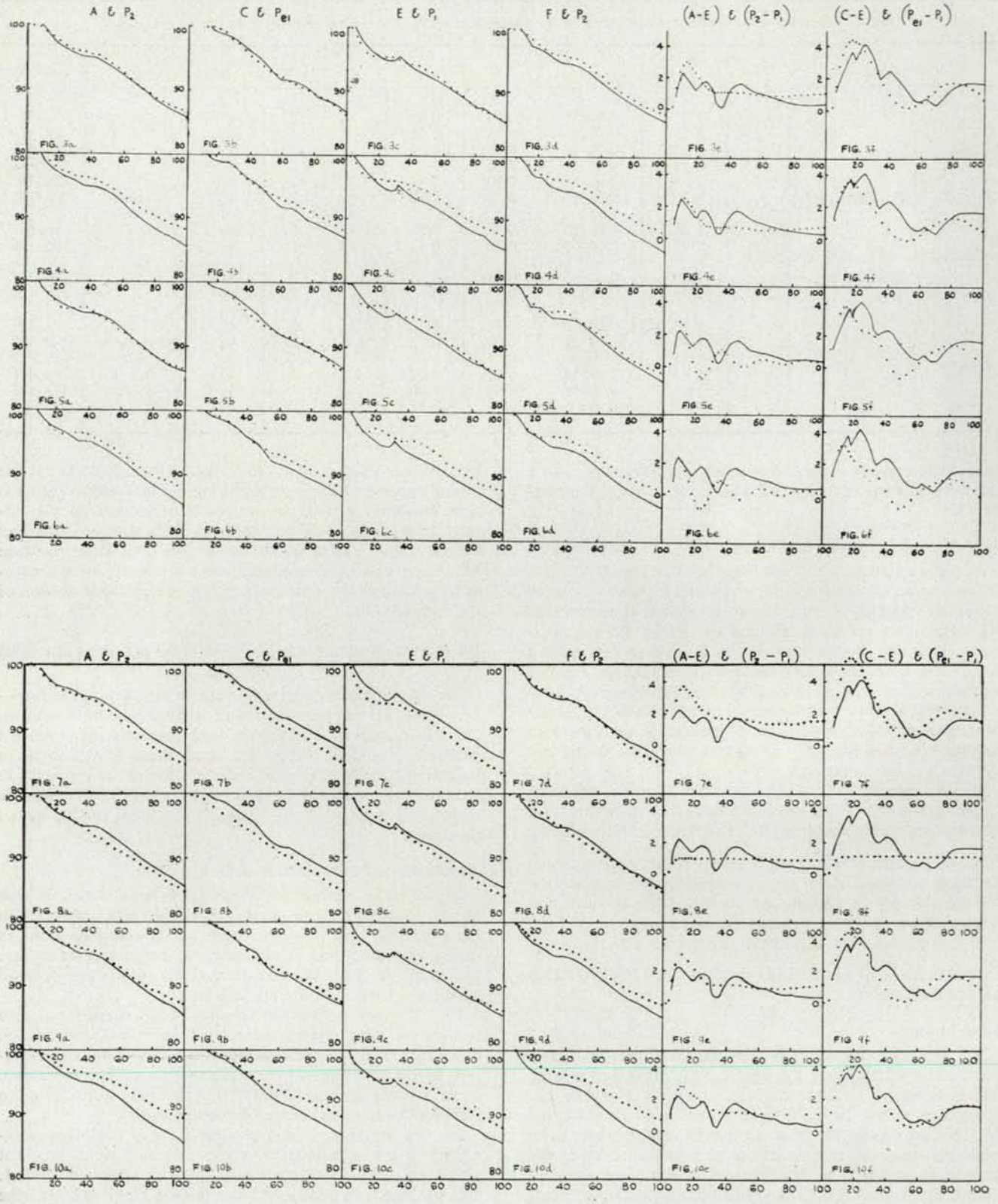
Justification of theoretical model

24. Various parameters affecting the results such as type of gas, orifice size, and number of circuits were changed in the experiment to provide results which could be used as further confirmation of the theory. All these parameters were varied in the theoretical model and the results are in good agreement with the experimental results.

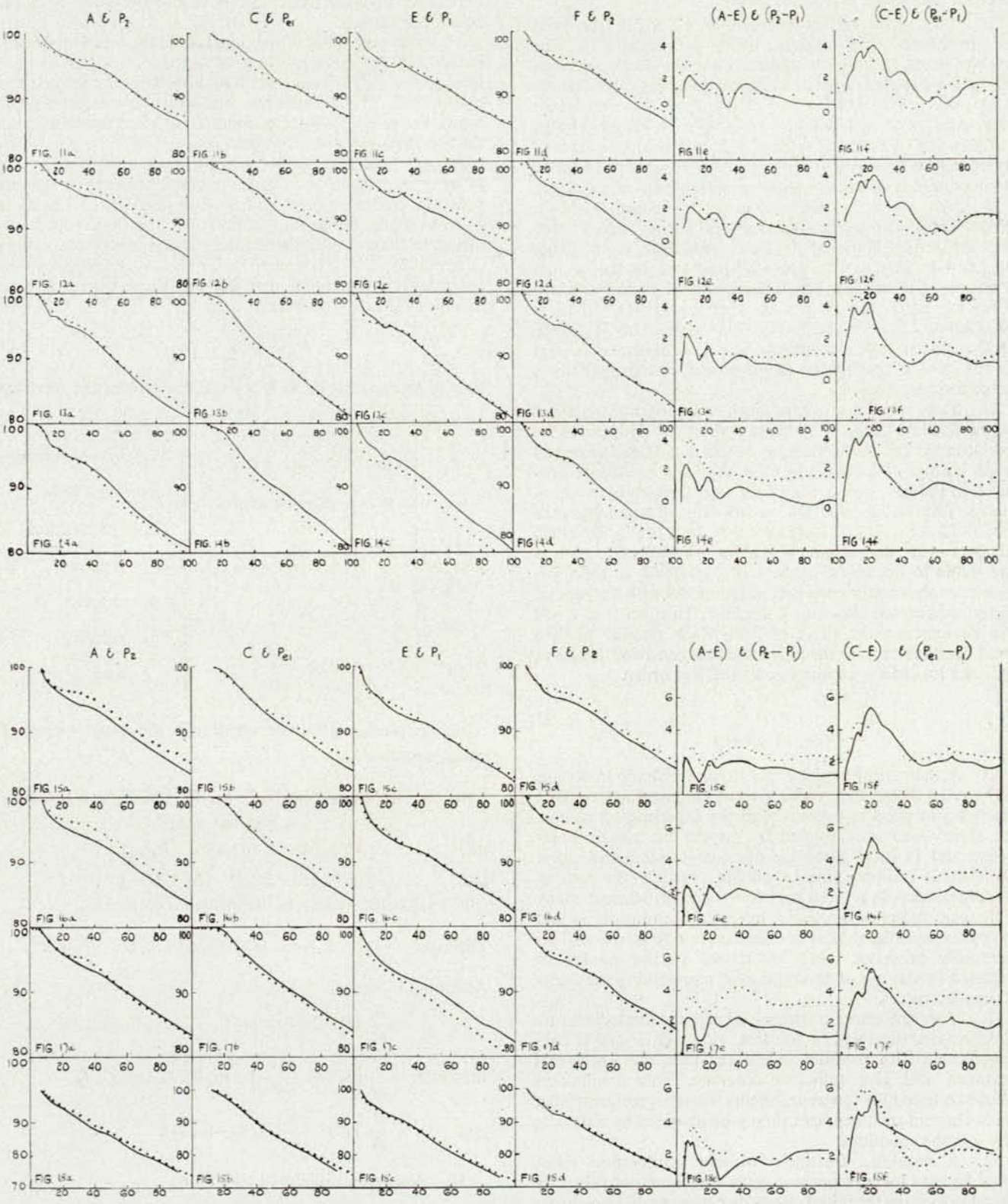
25. The comparisons which have been carried out also include variation of parameters in the theoretical model which had not been measured experimentally and which had therefore to be determined from existing data or by estimation. In such a case extreme values of the parameter were chosen to indicate the complete range of possible values.

26. In addition, it was possible to vary the other parameters in the equations to determine which were the most significant. The most important of these are shown in Table 1. One of the effects producing uncertainties in the values of the parameters used is described in the next section.

Text continued on p. 180



3–10—*Comparison of experimental (A, C, E, and F) and theoretical (P_1, P_2, and P_{e1}) pressure transients. (Pressure ordinates are in psig and time abscissae in msec)* —— *experimental* – – – – *theoretical*



11–18—*Comparison of experimental (A, C, E, and F) and theoretical (P_1, P_2, and P_{e1}) pressure transients. (Pressure ordinates are in psig and time abscissae in msec)* —— *experimental* – – – – *theoretical*

Effect of duct length on friction factor

27. The friction factors from reference 5 which have been used in the derivation of some of the data assume that the velocity profile is fully developed. Near the duct's inlet one dimensional analysis is misleading, on account of the changes in velocity profile which occur. These have associated with them changes of momentum flux of appreciable magnitude. In the region of changing velocity profile, the friction factor f represents an apparent friction coefficient, since it included momentum flux effects as well as the effects of shearing stress.

28. Measurements have been made by Shapiro *et al.*[7] on friction coefficients in the inlet length of smooth round tubes. The integrated apparent friction coefficient varies from 0·9 ($Re = 4 \times 10^4$) to 1·25 ($Re = 2{\cdot}5 \times 10^5$) times the normal von Kármán value for tubes 20 diameters in length. The range of friction factors for tubes of about 10 diameters in length is 0·8 ($Re = 4 \times 10^4$) to 1·35 ($Re = 2{\cdot}5 \times 10^5$). The Reynolds numbers in the fractured ducts based on the diameter is about 5×10^6 so that the friction factor would be increased by a factor between 1 and 2.

29. Tests were also carried out[7] to investigate the effect on the friction factor of induced turbulence. This could be significant in the model because of the mixing which occurs at the bottom of the pressure vessel and the turbulence produced in the gas flowing out of the heat exchanger to atmosphere. The tests showed that at low Reynolds numbers the friction factor could be increased eight-fold but at Reynolds numbers $\sim 10^6$ the increase was about a factor of 2. As it is impossible to define the amount of turbulence in the flow, results are shown using the theoretical model with the normal friction factor and also with it doubled. This factor of 2 will also demonstrate the effect of inlet Mach number on low speed bend losses since the difference between bend losses at high and low Mach numbers is also of this order.

CONCLUSIONS

30. A theoretical model based on an adiabatic treatment has been developed and produced results of pressure transients which are in good agreement with the experimental results. The theory was substantiated by varying the circuit parameters and in most cases the agreement was satisfactory. However, it is impossible to predict precisely the rate of depressurization in a given part of the circuit, because many of the parameters of the circuit have not been measured and an approximate theoretical estimate has had to be made. This necessarily produces some uncertainty in the results, as indicated by the use of several curves representing the limits of the parameters.

31. If an isothermal treatment is used, the immediate rate of depressurization will be too low. However, if one is concerned only with pressure differences then the isothermal treatment will give sufficient accuracy. This assumption should be tested for longer transients. Good agreement using the isothermal treatment can simply be obtained by replacing ϵ by γ in the equations.

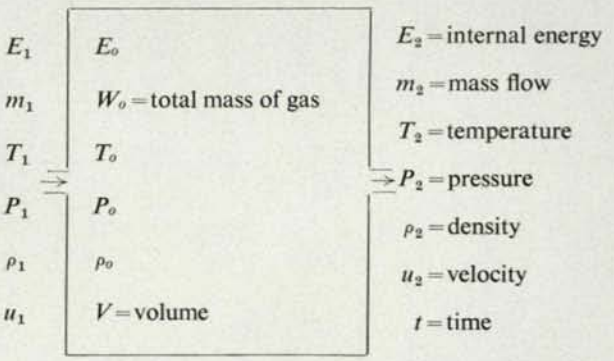
32. A simplified treatment without acceleration terms could be used for pressure transients which are not primarily intended to show the initial effects, for example if an estimate of flow is required for the purpose of calculating fuel element temperature changes. This produces no peaks in the pressure differences and the agreement is reasonably good with the actual pressures.

33. The prediction of the time lags in the build up of flow in the fractured ducts was in good agreement with the experimental results. The effect was only important for the first few milliseconds of the transient, but will become increasingly significant as the distance of point of fracture increases from the pressure vessel or heat exchanger.

34. The theoretical approach has been confirmed by the experimental results and could, therefore, be used as a starting point for predicting pressure transients in gas-cooled reactors —for example, under fault conditions. The important parameters have been indicated. If their values can be reasonably determined for a given plant, then this work indicates that the pressure transients can be predicted with sufficient accuracy for safety purposes.

APPENDIX 1

Rate of change of pressure in a vessel discharging and receiving gas



36. Assuming adiabatic conditions the conservation of energy gives:

$$m_1 E_1 + m_1 P_1/\rho_1 + \tfrac{1}{2} m_1 u_1^2 - m_2 E_2 - m_2 P_2/\rho_2 - \tfrac{1}{2} m_2 u_2^2 = (d/dt)(W_o E_o);$$

now $$\tfrac{1}{2} u_2^2 = (E_o + P_o/\rho_o) - (E_2 + P_2/\rho_2)$$

and $$\tfrac{1}{2} u_1^2 = (E_e - P_e/\rho_e) - (E_1 + P_1/\rho_1),$$

where subscript e refers to the upstream conditions.

Therefore $$\frac{d}{dt}(W_o E_o) = m_1 (E_e + P_e/\rho_e) - m_2 (E_o + P_o/\rho_o).$$

Assuming C_p and $C_v \neq f(T)$,

$$\frac{d}{dt}(W_o C_v T_o) = m_1 C_p T_e - m_2 C_p T_o;$$

therefore $$\frac{d}{dt}\left(W_o C_v \frac{P_o V}{R W_o}\right) = m_1 C_p T_e - m_2 C_p T_o$$

and $$\frac{d}{dt}(P_o) = \frac{\gamma R}{V}(m_1 T_e - m_2 T_o).$$

37. Assuming isothermal conditions

$$\dot{V\rho_o} = m_2 - m_1;$$

therefore $$\dot{VP_o} = RT_o (m_2 - m_1).$$

APPENDIX 2

Application of the 'method of characteristics' for the introduction of time lags and build-up in flow in fractured ducts

38. In order to formulate the build-up in flow it was necessary to consider the problem of unsteady flow of gas in a duct connected to a reservoir containing a high pressure gas. Formally, this requires the solution of partial differential equations of the hyperbolic type of which there is no analytical solution. However, a graphic method of solution was used based on the general properties of the differential equations involved.[8] The procedure was then to introduce these time lags and variation of mass flow with time into the equations for the fractured ducts.

39. Consider unsteady, one-dimensional motion. This latter assumption implies that all fluid properties are uniform over each cross-section of the duct and that changes, if any, in cross-section take place very slowly. The viscosity and thermal conductivity of the gas are neglected and the transient is assumed to take place adiabatically. This means that all parts of the gas are linked through the isentropic relations. We also assume that the equation of state is that of a perfect gas and that gravity effects are negligible.

40. The fundamental equations for flow are the equation of continuity, 1, and Euler's equation of motion, 2.

$$\frac{\partial \rho}{\partial t}+u\frac{\partial \rho}{\partial x}+\rho\frac{\partial u}{\partial x}=0 \quad . \quad . \quad . \quad . \quad . \quad (1)$$

$$\frac{\partial u}{\partial t}+u\frac{\partial u}{\partial x}+\frac{1}{\rho}.\frac{\partial p}{\partial x}=0 \quad . \quad . \quad . \quad . \quad . \quad (2)$$

where x is the abscissa measured along the pipe axis, p the pressure, t the time, ρ the density of the fluid, and u the fluid velocity in the direction of the x-axis.

41. Combining 1 and 2 and using the equation of state relating p and ρ we eventually get

$$\frac{\partial^2\phi}{\partial t^2}+2\frac{\partial\phi}{\partial x}.\frac{\partial^2\phi}{\partial x\,\partial t}+\left[\left(\frac{\partial\phi}{\partial x}\right)^2-c^2\right]\frac{\partial^2\phi}{\partial x^2}=0 \quad . \quad (3)$$

where $c=\sqrt{(\partial p/\partial\rho)}$ the velocity of sound, and $u=\partial\phi/\partial x$; ϕ being the velocity potential.

We now adopt the following notation:

$$\phi_x=\frac{\partial\phi}{\partial x} \text{ and } \phi_t=\frac{\partial\phi}{\partial t}.$$

42. It is known that equation 3 does not admit of a general analytical solution which is valid in the whole plane (t, x) and satisfies the boundary conditions. Instead it is necessary to introduce partial solutions which are valid for limited regions and join up to each other in a continuous fashion.

43. If ϕ' and ϕ'' are two such solutions each of which holds good in a certain region, and are represented by two surfaces situated above the plane (t, x), they will cut each other along a curve in space, the so-called 'characteristic'. Along this characteristic it is clear that $\phi'=\phi''$, and as the connexion of the solutions must be continuous $\phi_x'=\phi_x''$ and $\phi_t'=\phi_t''$. From this it can be shown with the help of equation 3 that

$$\frac{dx}{dt}=\phi_x\pm c. \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

44. Equation 4 is the equation of the projections of the characteristics on the (t, x) plane (Fig. 19). As the characteristics depend on ϕ_x and c, they are still unknown. They form two sets of curves α and β whose tangents are given by

$$\frac{dx}{dt}=\phi_x\pm c \quad . \quad . \quad . \quad (4\alpha) \qquad \frac{dx}{dt}=\phi_x\mp c \quad . \quad . \quad . \quad (4\beta)$$

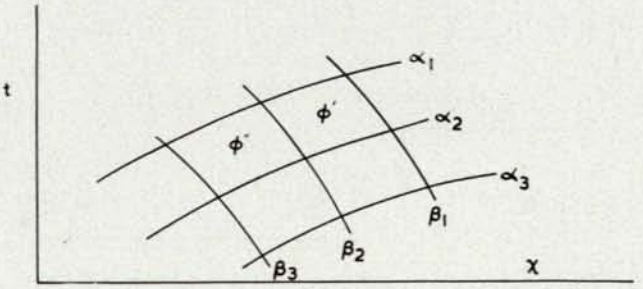
45. When passing along a characteristic the velocity of flow u is related to the velocity of sound c by

$$du=\pm\frac{2}{\gamma-1}dc \quad . \quad . \quad . \quad . \quad . \quad . \quad (5)$$

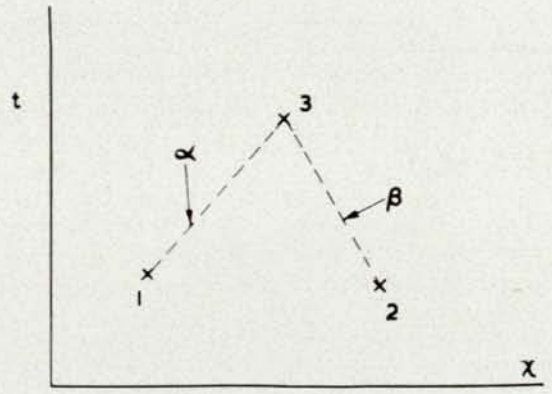
where γ is the ratio of the specific heats.

46. Using equations 4 and 5 a graphic method of solution is possible. This consists in constructing simultaneously a diagram of position (t, x) and a diagram of state (c, u) (Figs 20 and 21).

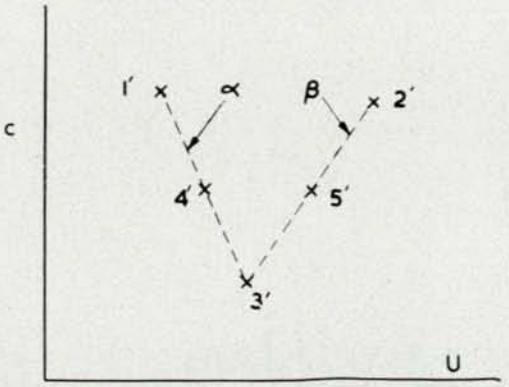
47. In general, the fundamental operation is the determination of a third point 3 (Fig. 20) from the two points 1 and 2



19—*Characteristic quadrilateral in which one solution holds good*



20—*Diagram of position*



21—*Diagram of state*

assuming the values of c and u are known for 1 and 2, viz. points 1′ and 2′ (Fig. 21). For example, since the points 1′ and 2′ are known the characteristics may be drawn through these points with slopes $-2/(\gamma-1)$ and $+2/(\gamma-1)$ respectively. The intersection gives the point 3′. Provided the mesh in Fig. 21 is sufficiently small the values of c and u may be considered constant, and their mean values, given by points 4′ and 5′, used to construct the characteristics through points 1 and 2 having slopes given by $u_4'+c_4'$ and $u_5'-c_5'$ respectively.

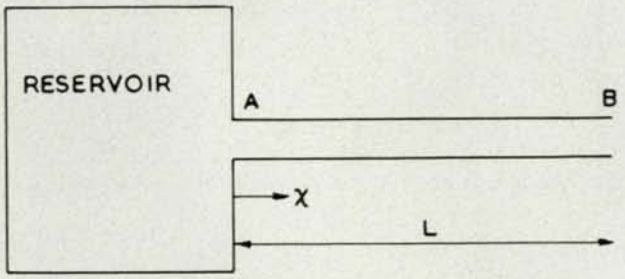
48. The equations for the above construction are obtained by integration of equations 4 and 5 and are summarized below.

$$c_3-c_1=-\left(\frac{\gamma-1}{2}\right)(u_3-u_1)$$

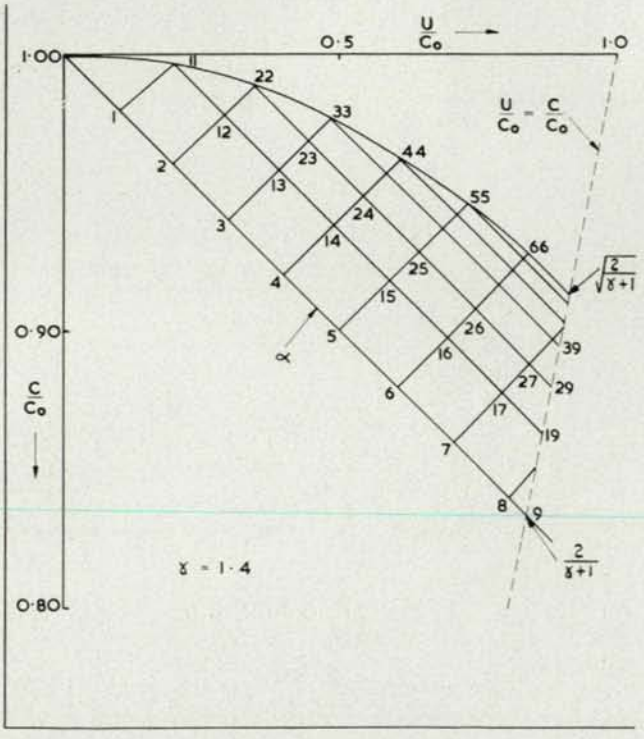
$$c_3-c_2=+\left(\frac{\gamma-1}{2}\right)(u_3-u_2)$$

$$x_3-x_1=(u_4'+c_4')(t_3-t_1)$$

$$x_2-x_2=(u_5'-c_5')(t_3-t_2)$$



22—*Flow from duct connected to a high pressure reservoir*



23—*Diagram of state*

49. The propagation speed of the disturbance dx/dt is given by $u\pm c$. This means that the velocity of propagation relative to the fluid itself is the local velocity of sound c. The plus sign indicates a wave travelling to the right, and the minus sign a wave travelling to the left, each with respect to a fluid particle (assuming that x is positive towards the right) (Fig. 22). By repeated application of the above technique the whole set of characteristics in both planes can be constructed, the limits being determined by the boundary conditions applicable to the problem.

50. As a particular example, consider the case of discharge to atmosphere from a duct of constant cross-section, connected to a reservoir of constant pressure (Fig. 22). In the case of the theoretical investigation of the model, the 'duct' corresponds to the broken duct and the 'reservoir' to the lower plenum chamber in the case of cold duct fracture, and to the volume above the core in the case of hot duct fracture.

51. Initially the end B (Fig. 22) is closed to atmosphere and the gas in the system is stagnant. At time $t=0$ the end B is opened, thus exposing the right-hand end to a region of very low pressure.

52. It will be found of practical advantage to replace the variables x, t, and u by the non-dimensional variables x/L, c_0t/L and u/c_0 where L represents a reference length and c_0 the velocity of sound of the fluid at rest in its initial state.

53. At time $t=0$ the front of the wave is at the state $u/c_0=0$ and $c/c_0=1$ (Fig. 23); the rear at the sonic condition $u/c_0=c/c_0=2/(\gamma+1)$. Thus the boundary condition at the exit is $u/c_0=c/c_0$ and is represented by the straight line dotted in Fig. 23.

54. The energy equation for flow from the reservoir to the duct inlet may be written in the form:

$$H_0=H+u^2/2 \quad . \quad . \quad . \quad . \quad (6)$$

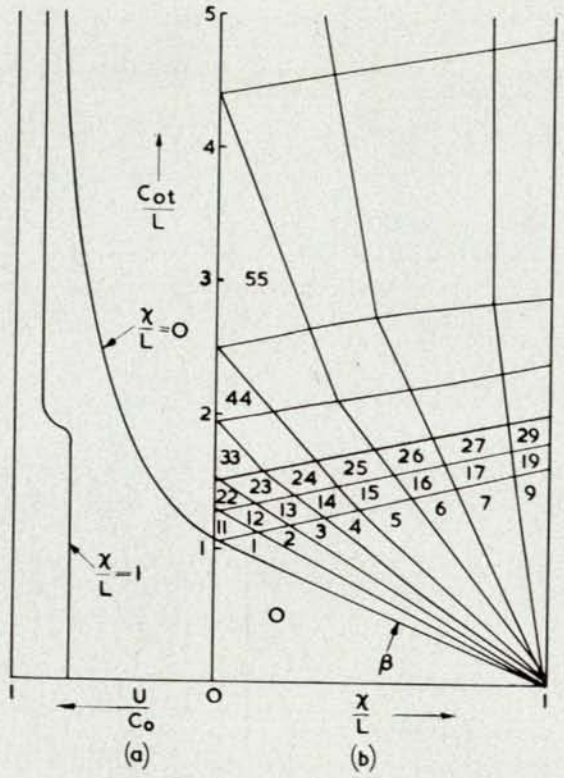
where H_0 is stagnation enthalpy in reservoir, and H the enthalpy at point A (Fig. 22), the duct entry.

Now $H=c^2/(\gamma-1)$; therefore by substitution in equation 6 and rearranging we get

$$1=\left(\frac{c}{c_0}\right)^2+\frac{\gamma-1}{2}\left(\frac{u}{c_0}\right)^2,$$

which is the equation of an ellipse—the steady state ellipse on which all inlet conditions must lie. This is the second boundary condition (Fig. 23).

55. At time $t=0$, the state of the gas at the exit end of the pipe B is given by the intersection of the characteristic passing through the initial point $c/c_0=1$ and $u/c_0=0$ and the straight line $u/c_0=c/c_0$. The expansion waves are propagated towards the reservoir where the conditions are represented by the steady state ellipse. There they produce compression waves which return to the admission end; the compression waves are again reflected, and so forth. The primary expansion waves are propagated at a constant velocity so long as they do not meet any reflected waves. The characteristics β starting from $x/L=1$, $c_0t/L=0$ are thus straight lines, and their images in the diagram of state are points on the characteristic α passing through $u/c_0=0$, $c/c_0=1$. As soon as the primary waves meet reflected waves, their velocity of propagation is no longer constant and their characteristics in the diagram of position are bent. The corresponding lines in the diagram of state are 1–11, 2–22, etc. (Fig. 23). The state at the moment of reflexion (points 11, 22, 33, etc.) must lie on the steady state



24—*Diagram of position*

ellipse. The reflected waves are propagated parallel to the characteristics α.

56. The velocity of flow in the pipe tends towards the critical value which is given by the intersection of the straight line $u/c_o = c/c_o$ and the ellipse, i.e.

$$\frac{u^*}{c_o} = \frac{c^*}{c_o} = \sqrt{\left(\frac{2}{\gamma+1}\right)} = 0{\cdot}913 \quad (\gamma = 1{\cdot}4).$$

It may be seen from Fig. 24a that this condition is approximated with a value of $c_o t/L$ of about 5. At the right-hand end of the duct B, sonic speed is attained with a value of $c_o t/L$ of only about 2. (This corresponds to the time required for a sound wave to travel the distance $2L$ at the speed c_o.)

57. In this Paper the curve of u/c_o as a function of $c_o t/L$ for $x/L = 0$ was used to predict the build-up in flow in the duct. The time lag in the build-up is seen to be $c_o t/L = 1$ corresponding to the time required for a sound wave to travel the distance L at speed c_o.

58. In order to predict the mass flow from the circuit it was decided to use Mach number as the independent variable obtained from the graph of u/c_o as a function of time.

$$\text{Mach number } M = \frac{u}{c_o} \cdot \frac{c_o}{c}.$$

c/c_o was eliminated by using the equation for the steady state ellipse, viz.

$$1 = \left(\frac{c}{c_o}\right)^2 + \frac{\gamma-1}{2}\left(\frac{u}{c_o}\right)^2.$$

Hence, once M has been calculated as a function of time all other properties such as mass flow and pressure may be found. M is allowed to vary until it reaches its maximum value determined by equations 27a and 28a.

APPENDIX 3

Equations for general treatment involving temperature—cold duct fracture

$$\dot{P}_1 = \gamma/V_1 \{(n-1)\, m_{31}\, R\, T_{e1} + m_1\, R\, T_2 - m_{32}\, R\, T_1\} \quad (1)$$

$$\dot{P}_2 = \gamma/V_2 \{-m_2\, R\, T_2 - m_{62}\, R\, T_2 - (n-1)\, m_{61}\, R\, T_{e1}\} \quad (2)$$

$$\dot{P}_{e1} = \gamma/V_{e1} \{m_{51}\, R\, T_{e1} - m_{41}\, R\, T_{e1}\} \quad (3)$$

$$\dot{P}_{e2} = \gamma/V_{e2} \{m_{52}\, R\, T_2 - m_{42}\, R\, T_{e2}\} \quad (4)$$

$$\dot{\rho}_c = (-m_1 + m_2)/V_c \quad (5)$$

$$\dot{\rho}_{u1} = (m_{61} - m_{51})/V_{u1} \quad (6)$$

$$\dot{\rho}_{u2} = (m_{62} - m_{52})/V_{u2} \quad (7)$$

$$\dot{\rho}_{L1} = (m_{41} - m_{31})/V_{L1} \quad (8)$$

$$\rho_{L1}\, R\, T_{e1} = \tfrac{1}{2}(P_1 + P_{31}) \quad (9)$$

$$\rho_{u1}\, R\, T_{e1} = \tfrac{1}{2}(P_2 + P_{e1}) \quad (10)$$

$$\rho_{u2}\, RT_2 = \tfrac{1}{2}(P_2 + P_{e2}) \quad (11)$$

$$\rho_c\, RT_2 = \tfrac{1}{2}(P_1 + P_2) \quad (12)$$

$$P_{e2} = P_{32}\left(1 + \frac{\gamma-1}{2} M_{42}^2\right)^{\frac{\gamma}{\gamma-1}} \quad (13)$$

$$m_{32}\sqrt{(RT_1)} = C_{Du}\sqrt{(g\gamma)}\, A_{L1}\, P_1 \frac{M_{32}}{\left(1 + \frac{\gamma-1}{2} M_{32}^2\right)^{\frac{\gamma+1}{2(\gamma-1)}}} \quad (14)$$

$$m_{41}\sqrt{(RT_{e1})} = K\sqrt{(2g)}\, A_o\, P_{e1}\, Y_{41}\sqrt{(1 - r_{41})} \quad (15)$$

$$m_{42}\sqrt{(RT_{e2})} = K\sqrt{(2g)}\, A_o\, P_{e2}\, Y_{42}\sqrt{(1 - r_{42})} \quad (16)$$

where $$Y_{41} = 1 - (0{\cdot}41 + 0{\cdot}35\, \beta^4)\frac{1 - r_{41}}{\gamma} \quad (17a)$$

for $1{\cdot}0 \geqslant r_{41} \geqslant 0{\cdot}63$;

and $$Y_{41} = 1 - (0{\cdot}5397 - 0{\cdot}5573\, r_{41})\frac{1}{\gamma} \quad (17b)$$

for $r_{41} < 0{\cdot}63$, similarly for r_{42}.

$$r_{41} = P_{31}/P_{e1} \quad (18)$$

$$r_{42} = P_{32}/P_{e2} \quad (19)$$

$$\dot{m}_{51} + \dot{m}_{61} - \frac{2\,\dot{\rho}_{u1}(m_{51} + m_{61})}{\rho_{u1}} = \frac{2gA_{u1}^2\,(P_2 - P_{e1})}{V_{u1}} - \frac{k_{Fu}(m_{51} + m_{61})^2}{2\,V_{u1}\,A_{u1}\,\rho_{u1}} \quad (20)$$

$$\dot{m}_{41} + \dot{m}_{31} - \frac{2\,\dot{\rho}_{L1}(m_{41} + m_{31})}{\rho_{L1}} = \frac{2g\,A_{L1}^2(P_{31} - P_1)}{V_{L1}} - \frac{k_{FL}(m_{41} + m_{31})^2}{2\,V_{L1}\,A_{L1}\,\rho_{L1}} \quad (21)$$

$$\dot{m}_{52} + \dot{m}_{62} - \frac{2\,\dot{\rho}_{u2}(m_{52} + m_{62})}{\rho_{u2}} = \frac{2gA_{u2}^2\,(P_2 - P_{e2})}{V_{u2}} - \frac{k_{Fu}(m_{52} + m_{62})^2}{2\,V_{u2}\,A_{u2}\,\rho_{u2}} \quad (22)$$

$$\dot{m}_1 + \dot{m}_2 - \frac{2\,\dot{\rho}_c(m_1 + m_2)}{\rho_c} = \frac{2gA_c^2\,(P_2 - P_1)}{V_c} - \frac{k_c(m_1 + m_2)^2}{2\,V_c\,A_c\,\rho_c} \quad (23)$$

Equations for adiabatic treatment—cold duct fracture

$$\dot{P}_1=\frac{\gamma}{V_1}\left\{(n-1)\,m_{31}\left(\frac{P_{e1}}{P_{i e1}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i e1}+m_1\left(\frac{P_2}{P_{i2}}\right)^{\frac{\gamma-1}{\gamma}}.\right.$$

$$\left.\times RT_{i2}-m_{32}\left(\frac{P_1}{P_{i1}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i1}\right\} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (1a)$$

$$\dot{P}_2=\frac{\gamma}{V_2}\left\{-m_2\left(\frac{P_2}{P_{i2}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i2}-m_{62}\left(\frac{P_2}{P_{i2}}\right)^{\frac{\gamma-1}{\gamma}}.\right.$$

$$\left.\times RT_{i2}-(n-1)\,m_{61}\left(\frac{P_{e1}}{P_{i e1}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i e2}\right\} \quad . \quad . \quad . \quad . \quad (2a)$$

$$\dot{P}_{e1}=\frac{\gamma}{V_{e1}}\left\{m_{51}\left(\frac{P_{e1}}{P_{i e1}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i e1}-m_{41}\left(\frac{P_{e1}}{P_{i e1}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i e1}\right\} \quad (3a)$$

$$\dot{P}_{e2}=\frac{\gamma}{V_{e2}}\left\{m_{52}\left(\frac{P_2}{P_{i2}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i2}-m_{42}\left(\frac{P_{e2}}{P_{i e2}}\right)^{\frac{\gamma-1}{\gamma}}.\;RT_{i e2}\right\} \quad (4a)$$

$$\dot{\rho}_c=(-m_1+m_2)/V_c \quad . \quad . \quad . \quad . \quad (5a)$$

$$\dot{\rho}_{u1}=(m_{61}-m_{51})/V_{u1} \quad . \quad . \quad . \quad . \quad (6a)$$

$$\dot{\rho}_{u2}=(m_{62}-m_{52})/V_{u2} \quad . \quad . \quad . \quad . \quad (7a)$$

$$\dot{\rho}_{L1}=(m_{41}-m_{31})/V_{L1} \quad . \quad . \quad . \quad . \quad (8a)$$

$$\rho_{L1}/\rho_{e1}=(P_1+P_{31})/2\,P_{e1} \quad . \quad . \quad . \quad (9a)$$

$$\rho_{u1}/\rho_{e1}=(P_2+P_{e1})/2\,P_{e1} \quad . \quad . \quad . \quad (10a)$$

$$\rho_{u2}/\rho_2=(P_2+P_{e2})/2\,P_2 \quad . \quad . \quad . \quad (11a)$$

$$\rho_c/\rho_2=(P_1+P_2)/2\,P_2 \quad . \quad . \quad . \quad (12a)$$

$$P_{e2}=P_{32}\left(1-\frac{\gamma-1}{2}M_{42}^2\right)^{\frac{\gamma}{\gamma-1}} \quad . \quad . \quad . \quad (13a)$$

$$m_{32}\sqrt{\left(RT_{i1}\,P_{i1}^{\frac{1-\gamma}{\gamma}}\right)}=C_{32}\,\sqrt{(g\gamma)}\,A_2\,P_1^{\frac{1-\gamma}{2\gamma}}\times\frac{M_{32}}{\left(1+\frac{\gamma-1}{2}M_{32}^2\right)^{\frac{\gamma+1}{2(\gamma-1)}}} \quad . \quad . \quad . \quad . \quad (14a)$$

$$m_{41}\sqrt{\left(RT_{i e1}\,P_{i e1}^{\frac{1-\gamma}{\gamma}}\right)}=K\,\sqrt{(2g)}\,A_o\,P_{e1}^{\frac{1+\gamma}{2\gamma}}\,Y_{41}\sqrt{(1-r_{41})} \quad (15a)$$

$$m_{42}\sqrt{\left(RT_{i e2}\,P_{i e2}^{\frac{1-\gamma}{\gamma}}\right)}=K\,\sqrt{(2g)}\,A_o\,P_{e2}^{\frac{1+\gamma}{2\gamma}}\,Y_{42}\sqrt{(1-r_{42})} \quad (16a)$$

where $$Y_{41}=1-(0{\cdot}41+0{\cdot}35\beta^4)\frac{1-r_{41}}{\gamma} \quad . \quad . \quad (17c)$$

for $1{\cdot}0\geqslant r_{41}\geqslant 0{\cdot}63$;

and $$Y_{41}=1-(0{\cdot}5397-0{\cdot}5573\,r_{41})\frac{1}{\gamma} \quad . \quad . \quad (17d)$$

for $r_{41}<0{\cdot}63$, similarly for r_{42}.

$$r_{41}=P_{31}/P_{e1} \quad . \quad . \quad . \quad . \quad . \quad (18a)$$

$$r_{42}=P_{32}/P_{e2} \quad . \quad . \quad . \quad . \quad . \quad (19a)$$

$$\dot{m}_{51}+\dot{m}_{61}-\frac{2\,\dot{\rho}_{u1}\,(m_{51}+m_{61})}{\rho_{u1}}=\frac{2g\,A_{u1}^2\,(P_2-P_{e1})}{V_{u1}}-\frac{k_{Fu}\,(m_{51}+m_{61})^2}{2\,V_{u1}\,A_{u1}\,\rho_{u1}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (20a)$$

$$\dot{m}_{41}+\dot{m}_{31}-\frac{2\,\dot{\rho}_{L1}\,(m_{41}+m_{31})}{\rho_{L1}}=\frac{2g\,A_{L1}^2\,(P_{31}-P_1)}{V_{L1}}-\frac{k_{FL}\,(m_{41}+m_{31})^2}{2\,V_{L1}\,A_{L1}\,\rho_{L1}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (21a)$$

$$\dot{m}_{52}+\dot{m}_{62}-\frac{2\,\dot{\rho}_{u2}\,(m_{52}+m_{62})}{\rho_{u2}}=\frac{2g\,A_{u2}^2\,(P_2-P_{e2})}{V_{u2}}-\frac{k_{Fu}\,(m_{52}+m_{62})^2}{2\,V_{u2}\,A_{u2}\,\rho_{u2}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (22a)$$

$$\dot{m}_1+\dot{m}_2-\frac{2\,\dot{\rho}_c\,(m_1+m_2)}{\rho_c}=\frac{2g\,A_c^2\,(P_2-P_1)}{V_c}-\frac{k_c(m_1+m_2)^2}{2\,V_c\,A_c\,\rho_c} \quad (23a)$$

$$P_2/\rho_2{}^\gamma=P_{i2}/\rho_{i2}^\gamma \quad . \quad . \quad . \quad . \quad (24a)$$

$$P_{e1}/\rho_{e1}{}^\gamma=P_{i e1}/\rho_{i e1}{}^\gamma \quad . \quad . \quad . \quad . \quad (25a)$$

$$P_{e2}/\rho_{e2}{}^\gamma=P_{i e2}/\rho_{i e2}{}^\gamma \quad . \quad . \quad . \quad . \quad (26a)$$

The maximum values which M_{32} and M_{42} can have are given by the following equations respectively.

$$\frac{4f_{L1}\,L_{L1}}{D_{L1}}=\frac{1-M_{32}^2}{\gamma M_{32}^2}+\frac{\gamma+1}{2\gamma}\ln\left[\frac{(\gamma+1)\,M_{32}^2}{2\left(1+\frac{\gamma-1}{2}M_{32}^2\right)}\right] \quad (27a)$$

$$\frac{4f_{L2}\,L_{L2}}{D_{L2}}=\frac{1-M_{42}^2}{\gamma M_{42}^2}+\frac{\gamma+1}{2\gamma}\ln\left[\frac{(\gamma+1)\,M_{42}^2}{2\left(1+\frac{\gamma-1}{2}M_{42}^2\right)}\right] \quad (28a)$$

Isothermal treatment—cold duct fracture

$$\dot{P}_1=\epsilon RT/V_1\,\{(n-1)\,m_{31}+m_1-m_{32}\} \quad . \quad (1b)$$

$$\dot{P}_2=\epsilon RT/V_2\,\{-m_2-m_{62}-(n-1)\,m_{61}\} \quad . \quad (2b)$$

$$\dot{\rho}_c=(-m_1+m_2)/V_c \quad . \quad . \quad . \quad (5b)$$

$$\dot{P}_{e1}=\epsilon RT/V_{e1}\,(m_{51}-m_{41}) \quad . \quad . \quad . \quad (3b)$$

$$\dot{P}_{e2}=\epsilon RT/V_{e2}\,(m_{52}-m_{42}) \quad . \quad . \quad . \quad (4b)$$

$$\dot{\rho}_{u1}=(m_{61}-m_{51})/V_{u1} \quad . \quad . \quad . \quad (6b)$$

$$\dot{\rho}_{u2}=(m_{62}-m_{52})/V_{u2} \quad . \quad . \quad . \quad (7b)$$

$$\dot{\rho}_{L1}=(m_{41}-m_{31})/V_{L1} \quad . \quad . \quad . \quad (8b)$$

$$P_1+P_{31}=2\,RT\,\rho_{L1} \quad . \quad . \quad . \quad (9b)$$

$$P_{e1}+P_2=2\,RT\,\rho_{u1} \quad . \quad . \quad . \quad (10b)$$

$$P_{e2}+P_2=2\,RT\,\rho_{u2} \quad . \quad . \quad . \quad (11b)$$

$$P_1+P_2=2\,RT\,\rho_c \quad . \quad . \quad . \quad (12b)$$

$$P_{e2}=P_{32}\left(1+\frac{\gamma-1}{2}M_{42}^2\right)^{\frac{\gamma}{\gamma-1}} \quad . \quad (13b)$$

$$m_{32}\,\sqrt{(RT)}=C_{32}\,\sqrt{(g\gamma)}\,A_{L2}\,P_1\frac{M_{32}}{\left(1+\frac{\gamma-1}{2}M_{32}^2\right)^{\frac{\gamma+1}{2(\gamma-1)}}} \quad (14b)$$

$$m_{41}\,\sqrt{(RT)}=K\,\sqrt{(2g)}\,A_o\,P_{e1}\,Y_{41}\,\sqrt{(1-r_{41})} \quad . \quad (15b)$$

$$m_{42}\,\sqrt{(RT)}=K\,\sqrt{(2g)}\,A_o\,P_{e2}\,Y_{42}\,\sqrt{(1-r_{42})} \quad . \quad (16b)$$

Y_{41}, Y_{42}, r_{41}, r_{42} are defined by equations 17, 18, and 19.

$$\dot{m}_{51}+\dot{m}_{61}-\frac{2\,\dot{\rho}_{u1}\,(m_{51}+m_{61})}{\rho_{u1}}=\frac{2g\,A_{u1}{}^2\,(P_2-P_{e1})}{V_{u1}}-\frac{k_{Fu}\,(m_{51}+m_{61})^2}{2\,V_{u1}\,A_{u1}\,\rho_{u1}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (20b)$$

$$\dot{m}_{41}+\dot{m}_{31}-\frac{2\ \dot{\rho}_{L1}\ (m_{41}+m_{31})}{\rho_{L1}}=\frac{2g\ A_{L1}{}^2\ (P_{31}-P_1)}{V_{L1}}$$
$$-\frac{k_{FL}\ (m_{41}+m_{31})^2}{2\ V_{L1}\ A_{L1}\ \rho_{L1}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (21b)$$

$$\dot{m}_{52}+\dot{m}_{62}-\frac{2\ \dot{\rho}_{u2}\ (m_{52}+m_{62})}{\rho_{u2}}=\frac{2g\ A_{u2}{}^2\ (P_2-P_{e2})}{V_{u2}}$$
$$-\frac{k_{Fu}\ (m_{52}+m_{62})^2}{2\ V_{u2}\ A_{u2}\ \rho_{u2}} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (22b)$$

$$\dot{m}_1+\dot{m}_2-\frac{2\ \dot{\rho}_c\ (m_1+m_2)}{\rho_c}=\frac{2g\ A_c{}^2\ (P_2-P_1)}{V_c}$$
$$-\frac{k_c\ (m_1+m_2)^2}{2\ V_c\ A_c\ \rho_c} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (23b)$$

The maximum values which M_{32} and M_{42} can have are given by equations similar to 25 and 26.

DATA (CASE 1) FOR USE IN THE SOLUTION OF THE EQUATIONS

D_o/D_{32}		0·7106
Initial pressure (lb/ft²)	P_i	16510
Ratio of specific heats	γ	1·28
Gas constant (ft/degK)	R	63·13
Initial temperature (°K)	T	288
Acc. of gravity (ft/sec²)	g	32·2
No. of heat exchangers	n	4
Discharge coefficient for orifices	K	0·72
Vol. below core (ft³)	V_1	2·068
Vol. above core (ft³)	V_2	2·986
Vol. of 'heat exchangers' (ft³)	V_{e1}	1·807
Vol. of 'heat exchangers' (ft³)	V_{e2}	1·807
Vol. of upper duct (ft³)	V_{u1}	0·3658
Vol. of upper duct (ft³)	V_{u2}	0·3658
Vol. of lower duct (ft³)	V_{L1}	0·4187
Vol. of core (ft³)	V_c	0·2459
Duct areas (ft²)	A_{L1}	0·04907
	A_{L2}	0·04907
	A_{u1}	0·04907
	A_{u2}	0·04907
Area of 'heat exchangers' (ft²)	A_{e1}	0·7855
	A_{e2}	0·7855
Area of orifice (ft²)	A_o	0·02592
Effective area of core (ft²)	A_c	0·164
Total pressure loss coefficients $P=kv^2/gA$, including 'friction' and 'bend' losses	k_{FL}	0·174
	k_{Lu}	0·135
	k_c	1·35
Diameter of ducts (ft)	D_{32}	0·2557
	D_{42}	0·2557
Effective friction factor for L_{32}	f_{32}	0·04855
Effective friction factor for L_{42}	f_{42}	0·0954
Length of broken duct from reactor vessel to fracture (ft)	L_{32}	2·906
Length of broken duct from orifice plate to fracture (ft)	L_{42}	5·25
Discharge coefficient associated with M_{32}	C_{32}	0·75
Diameter of orifice (ft)	D_o	0·1817

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2 Core flow transients following circuit leakage: Estimation of longest core stagnation time and study of the effects of duct friction and position and size of fracture

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This Paper is the second of five to be presented at a half-day symposium of the Society on 11 November

Leakage of gas from a reactor will cause redistribution of the circuit flows, and under certain conditions of discharge small core flows would result. Considering the fracture of a duct of a reactor within which a constant temperature distribution is maintained, a simple method is presented for the calculation of the longest period of time for which near zero core flow can occur, and conjointly the effective fracture area to produce this state is also found.

Zero core flow during the depressurization transient leads to peak fuel temperatures, but the improvements to cooling to be gained by small deviations from the critical discharge area are illustrated.

INTRODUCTION

In a gas-cooled reactor system, pressurized gas circulates and transfers heat between core and heat exchangers. If gas escapes because of a leak, the circuit mass flow is reduced due to reduction in gas pressure, but this is a relatively slow process depending on the size of the leak and the mass of gas stored in the circuit. Also, the flow of gas from the leak causes an immediate redistribution of flow in the circuit, and under certain conditions can significantly affect or stop the flow of coolant through the reactor core. It is desirable to have a simple and reliable method for estimating the longest time for which stagnation could persist in a gas-cooled reactor circuit, and how this maximum interruption of flow is affected by changes in position and size of the leak. An actual reactor would consist of a complex distribution of volumes and resistive flow paths as suggested by Fig. 1, but for the purpose of this study the reactor circuit is taken to comprise a set of volumes connected by ducts; in physical form these ducts may be actual pipes or merely points of junction between the volumes in question.

TREATMENT

Frictionless ducts

2. The simplified reactor system to be considered would take the form shown in Fig. 2a and consist of a core resistance, two capacities containing gas masses M_1 and M_2, and gas circulators which in reality act as metering devices. The mass flow passed by a circulator would be proportional to the rotational speed of the circulator and the pressure of the gas at its inlet. The heat exchanger and hot duct gas contents are here assumed to be lumped with that part of the pressure vessel volume above the core. In general, the fracture of a duct would result in there being a core flow, here signified by m_1, and for the direction shown $P_2 > P_1$.

3. If at some time in the transient, a state of affairs should arise to make P_1 equal to P_2, then ignoring the effects of gas inertia, core flow could cease and stagnation of this flow could occur. In order that this state of stagnation should persist, the pressure equality would have to be accompanied by the equality of the pressure time derivatives:

$$\dot{P}_1 = \dot{P}_2 \quad . \quad . \quad . \quad . \quad . \quad (1)$$

Assuming that the temperature of the gas in each volume remains constant the rate of change of pressure in each volume may be written

$$\dot{P}_1 = [(P_1)_0/(M_1)_0]\,[m_1 - (N-1)\,m_3 - m_4] \quad . \quad (2)$$

and

$$\dot{P}_2 = [(P_2)_0/(M_2)_0]\,[(N-1)\,m_3 - m_1 - m_2] \quad . \quad (3)$$

in which N is the number of heat exchangers in the reactor system, and the zero suffix indicates the initial values of pressure and gas mass content.

4. If the pressures P_1 and P_2 are changing at the same rate it will be found by manipulation of equations 2 and 3 that

$$m_2 = \left[1 + \frac{(P_1)_0\,(M_2)_0}{(P_2)_0\,(M_1)_0}\right](N-1)\,m_3 - \left[1 + \frac{(P_1)_0\,(M_2)_0}{(P_2)_0\,(M_1)_0}\right] m_1 + \frac{(P_1)_0\,(M_2)_0}{(P_2)_0\,(M_1)_0}\,m_4 \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

The above equation simply states a rule which must hold if the pressures P_1 and P_2 are to decay at the same rate, but if in addition the two pressures in question are to be equal in magnitude, making m_1 the core flow zero, then the relationship reduces to

$$m_2 = \left[1 + \frac{(P_1)_0\,(M_2)_0}{(P_2)_0\,(M_1)_0}\right](N-1)\,m_3 + \frac{(P_1)_0\,(M_2)_0}{(P_2)_0\,(M_1)_0}\,m_4 \quad . \quad (5)$$

5. *Period of core flow stagnation.* When stagnation exists the pressure transient will be given by the pressure decay in either the upper or lower plenum in the case of a Stage 1 reactor, but for convenience it is more appropriate to consider the upper volume. Here, the pressure decay is solely due to gas extraction by way of the circulators, and since the circulators simply meter the gas by volume we may write, for constant circulator rotational speed,

$$m_3 = k_1 P_1 \quad . \quad . \quad . \quad . \quad (6)$$

$$m_4 = k_2 P_1, \quad . \quad . \quad . \quad . \quad (7)$$

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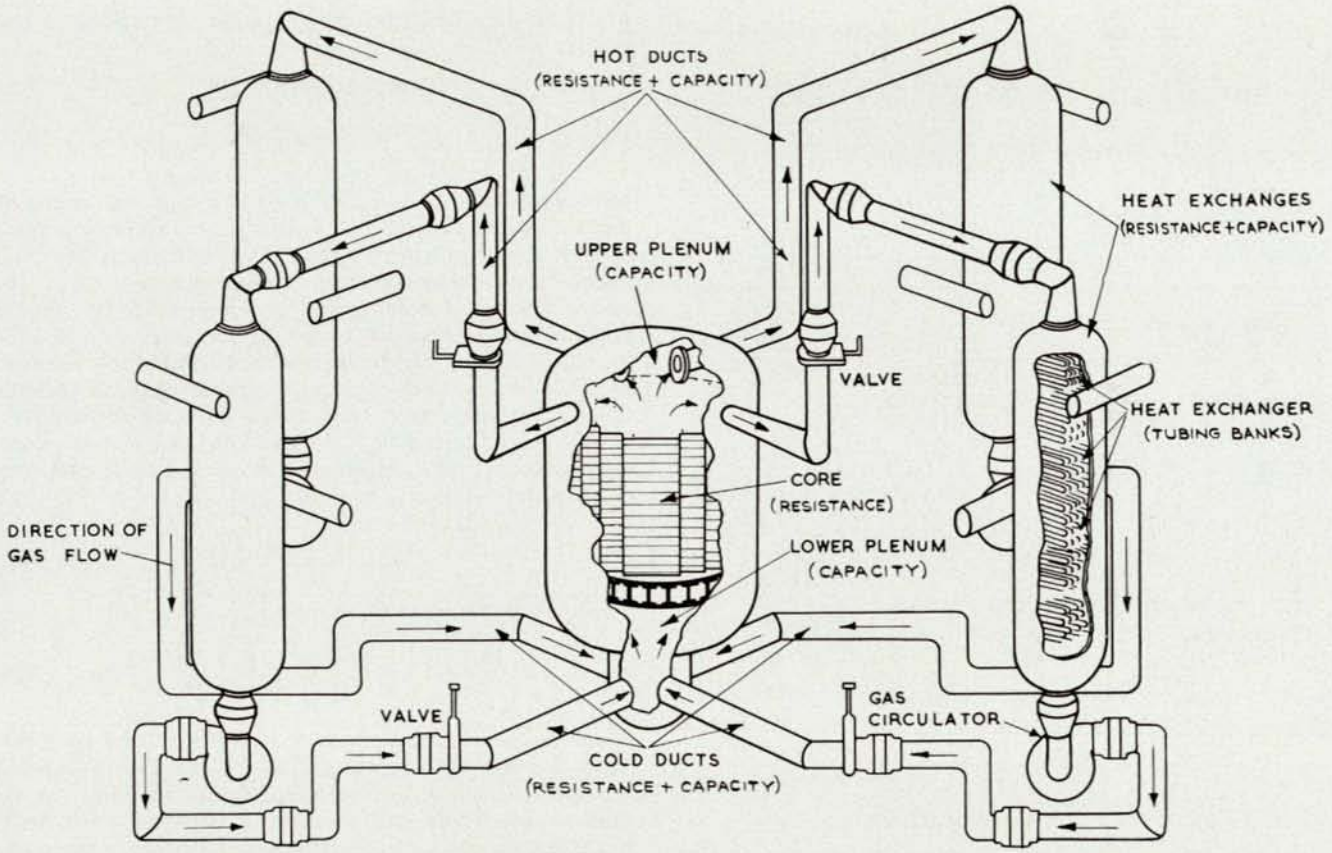


1—*Typical distributions of main resistances and capacities in a gas-cooled reactor system*

although the form of equation 7 would also result should choking of flow occur in the fractured leg as a result of the high overall pressure drop through the leg. The pressure transient exhibited by the gas in the upper plenum would now be given by

$$P_1=(P_1)_0 e^{-t/t_0} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (8)$$

where

$$\frac{1}{t_0}=\frac{[k_1(N-1)+k_2](P_1)_0}{(M_1)_0}, \quad . \quad . \quad (9)$$

as obtained by the elimination of m_3 and m_4 from equation 2, with m_1 zero by means of equations 6 and 7, followed by integration. Equations 6 and 7 taken with 5 suggest that when the postulated core flow stagnation exists, the outflow m_2 through the fracture must also be proportional to the pressure within the reactor. This state of affairs does exist when the reactor pressure is high relative to the atmosphere, since under these conditions choking occurs at the discharge aperture, and the stipulated proportionality between mass flow and pressure in the capacity immediately upstream of the orifice holds. Thus, assuming discharge through a hole caused by fracture of a cold duct close to the reactor shell, the reactor pressure would exceed the critical pressure for choked flow through an orifice until the pressure within the reactor had fallen to slightly less than two atmospheres, given by $(P_1)_e$, where

$$\frac{P_A}{(P_1)_e}=\left(\frac{2}{\gamma+1}\right)^{\frac{\gamma}{\gamma-1}} \quad . \quad . \quad . \quad . \quad (10)$$

in which P_A is the pressure of the atmosphere to which discharge takes place and γ is the ratio of the specific heats of the discharging gas. After this time continued proportionality between circuit pressure and mass discharge would not be possible. Thus, the time of stagnation can be obtained by replacing P_1 in equation 8 by $(P_1)_e$. The stagnation time would then be given by

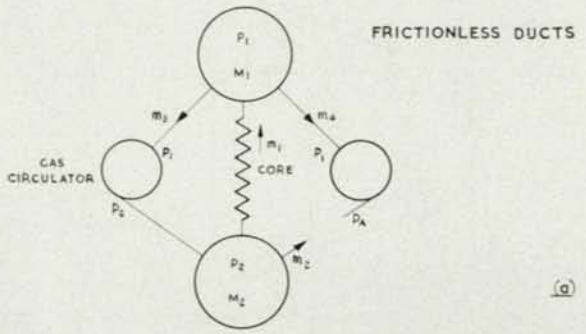
$$t_s=t_0 \log_e (P_1)_0/(P_1)_e, \quad . \quad . \quad (11)$$

and this would clearly be greatest when k_2 was zero, suggesting a complete blockage of the ducting in the fractured leg. The longest stagnation time under these conditions is therefore given by

$$(t_s)_{\max}=\frac{(M_1)_0 \log_e (P_1)_0/(P_1)_e}{(P_1)_0 k_1 (N-1)}. \quad . \quad . \quad (12)$$

Since it has been assumed that frictionless ducts are employed it is apparent that during the stagnation period the pressure ratio across the circulators remains at unity, and the value of k_1 can easily be obtained for this ratio from the relevant circulator characteristic curve.

6. *Fracture area to produce longest core flow stagnation period.* If the mass flows m_2 and m_3 are eliminated from equation 5 with m_4 equal to zero by the use of equation 6 and equation 13, i.e.



2—*Diagrammatic representation of reactor circuits*

$$m_2 = C_D A k_D P_2, \quad . \quad . \quad . \quad . \quad (13)$$

the resulting relationship, noting the equality of P_1 and P_2, will be

$$A = \frac{(N-1)\, k_1\, [1 + (P_1)_0\, (M_2)_0/(P_2)_0\, (M_1)_0]}{C_D\, k_D} \quad . \quad (14)$$

Ducts with friction

7. The foregoing treatment will give results close to the actual values when duct friction is small, but where duct friction might be appreciable the circulator working point for unit pressure ratio will give deliveries in excess of the quantities applicable to a system with duct frictions. The presence of friction demands under core flow stagnation conditions that the circulators operate at a pressure ratio in excess of unity, and thus with a mass flow less than that given by the intersection of the characteristic and the line for unit pressure ratio. Smaller circulator deliveries consequently lead to slower pressure decays in the capacity upstream of the circulators, resulting in longer stagnation times.

8. The circulator characteristic usually takes the form of a plot of a mass flow group $m_3(RT_3)/P_3$ against the pressure ratio, P_4/P_3, across the circulator, generally represented by the following:

$$P_4/P_3 = \phi\, [m_3 \sqrt{(RT_3)}/P_3] \quad . \quad . \quad (15)$$

in which the quantities involved are as indicated in Fig. 2b, while the duct flow equations, ignoring the effects of gas inertia, would be given by

$$m_3 = k_3 \sqrt{(P_1^2 - P_3^2)} \quad . \quad . \quad . \quad (16)$$

and

$$m_3 = k_4 \sqrt{(P_4^2 - P_2^2)} \quad . \quad . \quad . \quad (17)$$

9. It will be noticed from Fig. 2b that the reactor circuit has been reduced to one containing two capacities linked by the core resistance, and each further joined to the circulators by simple resistive paths, whereas in practice each flow path might be built up of a train of resistances and capacities. The justification for the use of the simple circuit of Fig. 2b is made in the Appendix, where the processes employed for the derivation of the single resistance and single capacity group equivalent to the resistance/capacity chain characteristic of a heat exchanger and hot duct path are outlined.

10. When core flow stagnation is assumed to exist, equality of P_1 and P_2 must again occur and under this condition equations 16 and 17 yield

$$m_3^2 = \frac{k_3^2\, k_4^2\, (P_4^2 - P_3^2)}{(k_3^2 + k_4^2)} \quad . \quad . \quad . \quad . \quad (18)$$

which can be arranged to become

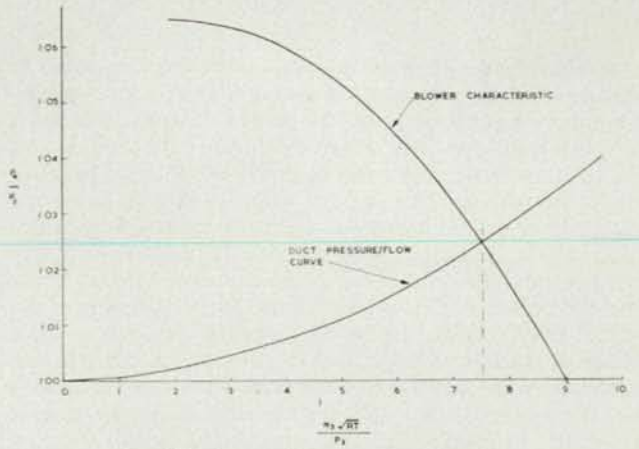
$$\frac{m_3^2\, RT_3}{P_3^2} = \frac{k_3^2\, k_4^2\, RT_3}{(k_3^2 + k_4^2)} \left[\left(\frac{P_4}{P_3} \right)^2 - 1 \right]. \quad . \quad (19)$$

It will be seen that equation 19, like that for the circulator characteristic 15, represents a relationship between $m_3\sqrt{(RT_3)}/P_3$ and P_4/P_3, implying that the solution of equations 15 and 19 must yield a value of P_4/P_3 which would apply over the complete time of stagnation, that is up to the cessation of sonic flow through the fracture opening.

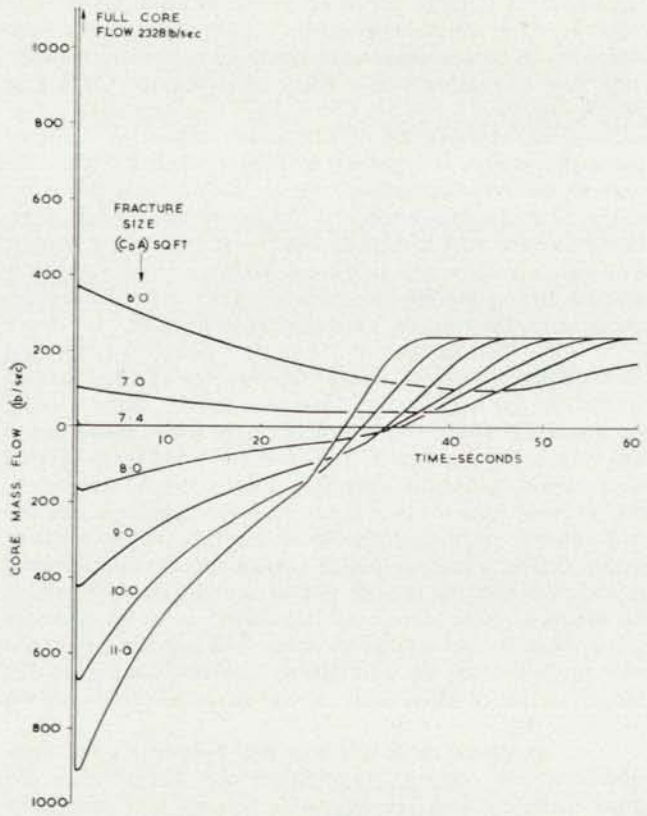
11. If the mass flow group corresponding to this constant circulator pressure ratio P_r during the stagnation period has the value Q, the mass flow through the circulator would be given by

$$m_3 = P_3\, Q/\sqrt{(RT_3)}. \quad . \quad . \quad . \quad (20)$$

During this same period it has been shown that P_4/P_3 has the constant value P_r, and by eliminating m_3 between equations 16 and 17 followed by division by P_1, noting that $P_1 = P_2$, it will be found that



3—*Circulator and duct pressure/flow curves*



4—*Core flow transient with varying fracture size*

$$P_3=[(k_3^2+k_4^2)/(k_3^2+k_4^2\,P_7^2)]^{\frac{1}{2}}P_1$$
$$=k_5P_1 \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (21)$$

The mass flow m_3 as given in equation 20 may now be written

$$m_3=k_5\,Q\,P_1/\sqrt{(RT_3)}, \quad . \quad . \quad . \quad (22)$$

and clearly if k_1 is replaced by $k_5Q/\sqrt{(RT_3)}$ in both equations 12 and 14, the longest stagnation time and the fracture area to bring this about are respectively obtained for the reactor circuit in which resistive flow paths have been included.

12. Transient core flow curves obtained by the use of an analogue machine are given in Fig. 4 for a Stage 1 reactor for which a parabolic circulator characteristic had been assumed of the form as shown in Fig. 3, and it was found that a fracture area close to 7·4 sq. ft (C_D unity) produced a core flow stagnation interval of approximately 34 sec. Application of the simple methods which have already been described to obtain the stagnation fracture area and corresponding period of stagnation gave rise to an area and time interval falling within a few percent of the analogue values.

POSITION OF DUCT FRACTURE

Duct with friction and complete open-ended fracture

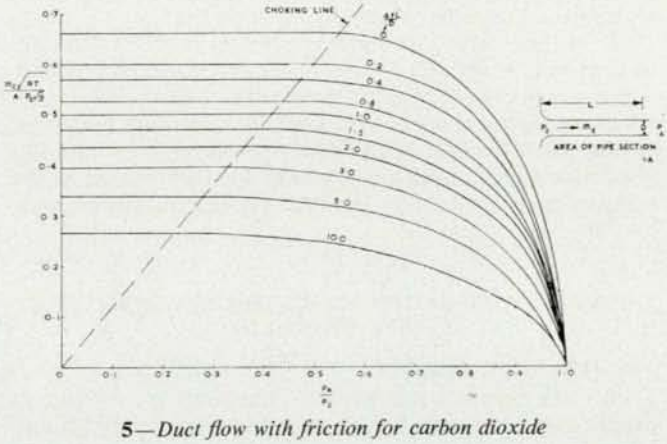
13. The foregoing work has considered the fracture to occur close to the pressure vessel such that the discharge from the pressure vessel is virtually through an orifice set in the vessel shell. Clearly, the same discharge rate could be produced through a length of fully open-ended duct attached to the vessel, provided the duct had the right degree of friction to attenuate the flow. The flow through such a duct would be given by a curve, such as one of those of the family shown in Fig. 5. It is clear that if core flow stagnation is exhibited it would persist until the pressure ratio P_A/P_2 reached a value corresponding to the intersection of the curve in question and the choking line.

14. The discharge through an orifice would take the same form as that for a frictionless duct, as exemplified by the curve for $4fL/D=0$ in Fig. 5, and it will be noted that here sonic flow ceases when P_A/P_2 is equal to 0·549, the flow being subsonic for pressure ratio greater than this value. This particular pressure ratio is greater than all other values for intersection with the choking line by curves for ducts with friction, and since for a given reactor system the same pressure decay curve must exist if stagnation occurs, irrespective of the discharge path from the pressure vessel, it follows that the orifice in the vessel wall will allow pressure to fall to the lowest value before sonic flow ceases. Hence, it is apparent that a full open-ended duct with friction cannot, due to the shorter pressure range over which sonic flow persists, produce a longer stagnation time than that for an orifice discharge occurring at the pressure vessel wall.

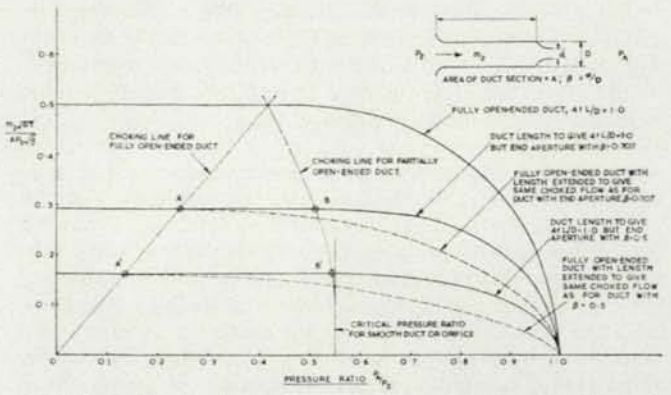
Duct with friction and partially open-ended fracture

15. In this case it is assumed that the duct is fractured in a manner such as to give an opening less than the full cross-sectional area, while as before, blockage of the fractured leg would have to occur to satisfy a criterion for maximum stagnation time. From Fig. 5 it is seen that a fully open-ended duct with friction chokes at a value of overall pressure ratio, P_A/P_2, less than that for a frictionless duct, in which case for carbon dioxide the ratio for the onset of choking is 0·549.

16. If a fully open-ended duct with friction is considered to be just choked it is clear that should the open end now be partially closed the choking state would not be relieved and choking would still exist, although at a smaller mass flow. In order to arrive at the condition where choking is again only just occurring at the now smaller duct outlet, clearly the upstream pressure P_2 would have to be reduced, if P_A is held constant, and at some new lower value of P_2 the desired state would be reached. Thus, a duct having friction and a fully open end would have a critical overall pressure ratio less than that for the same duct with the open end partially closed, although the mass flow in each case would not be the same.



5—*Duct flow with friction for carbon dioxide*



6—*Effect of incomplete duct severance on stagnation time interval*

If the first duct is extended in length, to further restrict flow, at a particular length a choked mass flow would occur equal to that of the shorter duct with the partially closed end, but due to the trend of the choking point with increase in $4fL/D$, as shown in Fig. 5, the longer duct would cease to be choked at a pressure ratio somewhat lower than that for the shorter duct, this state of affairs being exhibited on the calculated curves of Fig. 6 by the points A and B.

17. At this stage it could be said that, comparing ducts with friction having the same choked mass flow, a fully open-ended duct would produce a shorter stagnation time than would a shorter duct with the same friction factor but with a partially closed end. If the aperture at the end of the shorter duct is made smaller to restrict flow still further the point B, as shown in Fig. 6, will move to the right, to point B′ for example, since smaller flows will correspond to smaller duct pressure drops, allowing the upstream pressure P_2 to fall to a lower value before choking ceases. Extending the longer duct to provide choked flows in keeping with those of the partially closed duct makes point A for this duct move further to the left to A′, since $4fL/D$ will be increased. Thus, as the aperture in the end of the duct becomes smaller and the length of the plain duct is increased to give equality of flows, divergence of the points A and B occurs, as may be seen from the plots given in Fig. 6. When the aperture in the end of the duct is minute the pressure loss along the length of this duct will be insignificant, due to the very small mass flow, and the overall critical pressure ratio for the duct will be that for the aperture which would take the value of 0·549 for an orifice.

18. Clearly, then, the limiting pressure ratio for a length of duct with or without friction losses and irrespective of end opening size cannot be greater than that for an orifice, and since core flow stagnation time would be dependent upon this critical ratio, the time being longer the higher the ratio, discharge through an orifice as produced by duct fracture at the pressure vessel wall would produce the longest possible core flow stagnation time.

EFFECT OF DEVIATION FROM THE FRACTURE SIZE TO GIVE STAGNATION

Variation in core mass flow

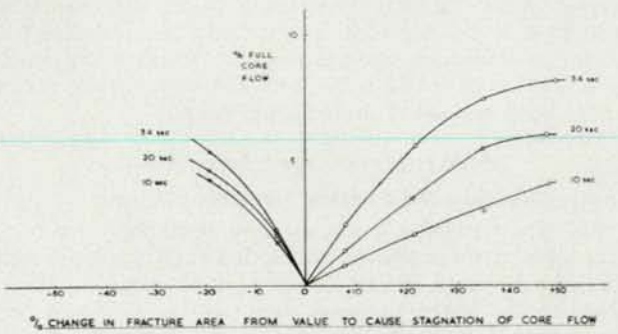
19. The pressure and gas flow transients for a Stage 1 reactor were investigated on an analogue computer for several sizes of duct fracture occurring at the pressure vessel wall. The core flow transients obtained are given in Fig. 4, from which it will be seen that with a discharge coefficient of unity, core flow stagnation would occur when the discharge area was 7·4 sq. ft.

20. The phenomenon of stagnation cannot be divorced from the period over which it occurs when its effect on reactor safety is concerned, for a short stagnation time might be acceptable, whereas a longer one could cause overheating of the fuel and might lead to serious consequences. The significance of small changes from the critical area was studied by computing minimum average mass flows over chosen time intervals for various sizes of fracture.

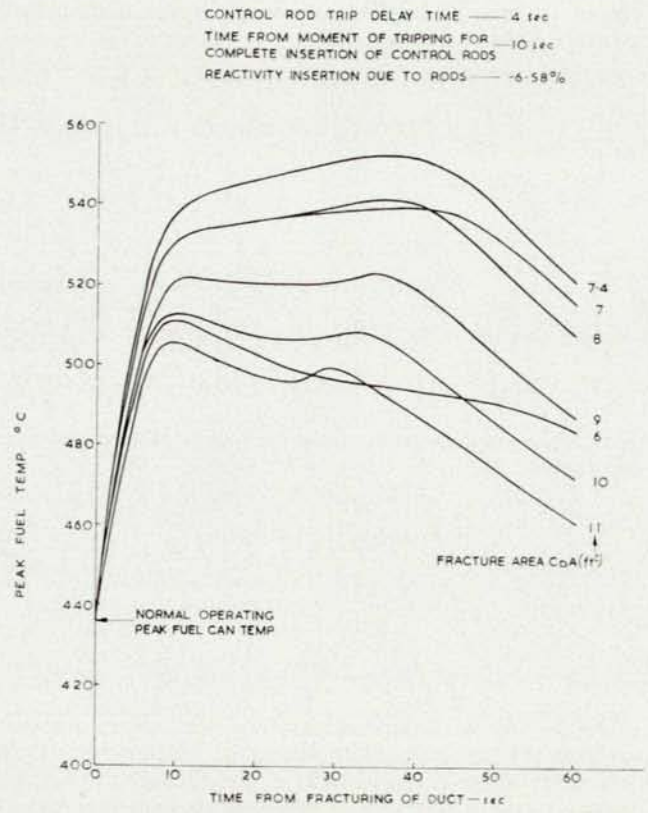
21. The fracture area of 7·4 sq. ft produces a stagnation time of approximately 34 sec, representing the period over which the reactor core would experience the worst cooling conditions. For the other transient curves where stagnation is not evident this particular time interval would be associated with minimum average core flow rates different from zero, and to investigate the core flow trend with discharge area the time interval in question was located in turn on the transient curves of Fig. 4 such as to give in each case a minimum integrated flow over the period; whether in the normal direction or reversed, flows were here considered as positive. From these integrated mass flows the mean flow rate for the chosen time interval could be determined; repeating the process for time intervals of 10 sec and 20 sec gave the results shown plotted in Fig. 7.

22. The sets of curves given in Fig. 7 show in some measure how fuel cooling conditions would change when the precise fracture area corresponding to core flow stagnation is not obtained. As an example, a fracture area having a value 10% less than that for core flow stagnation would give a minimum average core flow of approximately 3% of the normal operating full power value over a wide selection of time intervals, and this might in many circumstances provide adequate cooling for the fuel. The occurrence of a fracture area 10% in excess of the stagnation value, however, produces smaller minimum average flows with consequently poorer cooling properties.

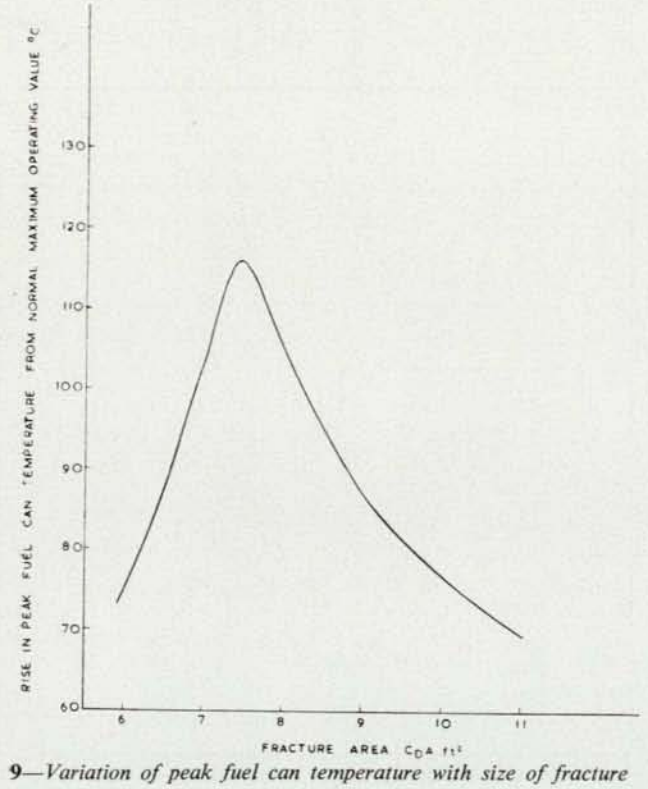
23. The lack of symmetry about the flow axis of the curves shown in Fig. 7 stems from the two forms of transient core flow possible, with the curve exhibiting stagnation serving as the dividing line. For fracture areas less than the size to give stagnation, the core flow will always be positive; that is in the direction normally produced by the gas circulators. For small deviations from the stagnation area, the core flow can remain



7—*Variation in minimum average core flow for several time intervals with fracture size*



8—*Variation of peak fuel can temperature with time during transients following fracture of a cold duct*



9—*Variation of peak fuel can temperature with size of fracture*

sensibly constant for some time. This results in the minimum average flow rates being sensibly constant irrespective of integration time base, provided the time base is not so long as to include much of the more rapid changes which occur at each end of the transient curve. This fact is drawn out in Fig. 7 by the closeness of the curves plotted for fracture areas less than the critical value.

24. Increasing the fracture area above the critical value causes core flow to reverse in direction very early in the transient, but since the core flow must ultimately return to the normal direction induced by the circulators, the flow will at some time be momentarily zero and integration straddling this cross-over point would lead to small summations and small average flow rates; particularly if the time base for the integration was short. Thus, during the depressurization transient, over some short-time intervals, at least, it might be expected that fracture areas marginally greater than the critical value would lead to cooling conditions poorer than would be obtained with areas smaller by similar margins.

Variation in peak fuel temperature

25. The incorporation of the core mass flows of Fig. 4 into a computer programme[1] capable of producing the fuel element temperature transients resulting from the rupture of the gas circuit gave rise to the set of curves of Fig. 8. Here, the peak fuel temperature under normal operating conditions is 436°C, and the initial rapid rise from this value is, apart from the fall in core mass flow rate, largely due to the delay in tripping the control rods followed by a further time delay before insertion of the rods is complete.

26. The fission heating pattern with time variation is in each case virtually the same, although relatively small effects will be apparent due to the influences of temperature changes on reactivity. As would be expected, the highest fuel temperature occurs under minimum cooling conditions when core flow stagnation arises at the fracture area of 7·4 sq. ft (C_D unity). A fracture area greater than the stagnation value would cause reversal of core flow, and consequently, in this instance, for a period of time hot gas would re-enter the fuel channels, and the cooling of the fuel would be impaired. Flow reversal is accommodated in the computer programme already referred to, and the overall effect of the flow variation leads to an unsymmetrical distribution of peak fuel can temperature with fracture area as can be seen from Fig. 9; deviations from the stagnation area giving smaller discharge openings, bringing about more rapid falls in peak temperature than would be obtained for similar increases in area.

27. The maximum peak temperature for the reactor considered amounted to 552°C and would be insufficiently high to jeopardize a magnox can, but it is conceivable that with higher fuel power ratings, core flow stagnation, or even small residual core flows for a period of time, might lead to overheating of the fuel elements.

CONCLUSIONS

28. The longest possible stagnation time may be simply obtained from a knowledge of the circuit friction losses, gas content distribution, and the circulator characteristic relating mass flow and pressure ratio. From these data the circulator operating point during the imposed core stagnation conditions can be found and the rate of discharge of the volume nominally above the core may be computed from the action of the

remaining circulators; it being assumed as a criterion for maximum stagnation time that the circulator in the fractured leg of the gas circuit is blocked.

29. Further, the longest stagnation time will be produced when duct fracture occurs at the pressure vessel wall, all times associated with fractures (whether complete or partial) occurring at other positions on the ducting being shorter. Although the precise stagnation state might prove troublesome from overheating of the fuel element it is possible that fracture areas differing only slightly in size from that to cause stagnation might in some circumstances provide acceptable gas flows.

APPENDIX

Derivation of equivalent circuit resistances

30. The treatments set out in the foregoing have dealt with reactor systems reduced to a simple form in which the gas capacities concerned were two in number, representing one capacity above and one below the core. These volumes were connected to the gas circulators by resistive paths while the core possessed no volume but exhibited resistance. In general, the reactor circuit would be more complex and the path from the plenums to the gas circulators would be via chains of volumes with resistive properties, as would be the case, for example, of the sequence of ducts and heat exchanger capacities coupled to the upper plenum. It was essential, if the methods already derived for calculating stagnation periods and corresponding fracture areas were to be of use, to determine the correct value of the resistance equivalent to that of the heat exchanger/hot duct chain to be used in the simple two-capacity reactor circuit.

Procedure

31. Consider the train of n capacities linked by resistive paths, the pressures instantaneously present in the capacities being $P_{1\to n}$ as shown in Fig. 10. For convenience the flows in the resistive paths will be arranged in the form

$$P_r - P_{r+1} = \frac{k_r^1 m_r^2}{P_r + P_{r+1}}, \quad . \quad . \quad . \quad . \quad (23)$$

the coefficient k_r^1 here being the reciprocal of the square of the coefficient formerly used.

32. If $(M_{1\to n})_0$ represents the initial masses of gas contained in the n volumes, the rates of change of pressure with time in the individual volumes can be found from equations of the form

$$\dot{P}_r = \frac{(P_r)_0}{(M_r)_0}\left[m_{r-1} - m_r\right] \quad . \quad . \quad . \quad (24)$$

in which $(P_{1\to n})_0$ are the initial pressures of the gas masses in the volumes and m_r, of course, represents the gas flow rate from the rth to the $(r+1)$th capacity as indicated in Fig. 10. The above equation implies that although there might be a temperature variation along the volume train, in each volume some mechanism acts to make conditions there isothermal.

33. If the overall pressure drop of the chain is small compared with the mean pressure in the system, the pressure summations of the type $(P_r + P_{r+1})$ could be replaced with little induced error by the sum of the end pressures $(P_1 + P_n)$.

Hence, the pressure drop across the various parts of the system could be written approximately as

$$P_1 - P_2 = k_1^1 m_1^2/(P_1 + P_n) \quad . \quad . \quad . \quad (25a)$$

$$P_2 - P_3 = k_2^1 m_2^2/(P_1 + P_n) \quad . \quad . \quad . \quad (25b)$$

. . .

$$P_{n-1} - P_n = \frac{k^1{}_{(n-1)}\, m^2{}_{(n-1)}}{P_1 + P_n} \quad . \quad . \quad (25(n\text{–}1))$$

Adding equations 25a, 25b ..... 25(n–1) gives

$$P_1 - P_n = \frac{1}{P_1 + P_n}\left[k_1^1 m_1^2 + k_2^1 m_2^2 + \ldots\ldots k_{n-1}^1 m_{n-1}^2\right] \quad (26)$$

Now, the equations shown in the form of 24 may be arranged to become

$$\dot{P}_1 (M_1)_0/(P_1)_0 = -m_1 \quad . \quad . \quad . \quad (27a)$$

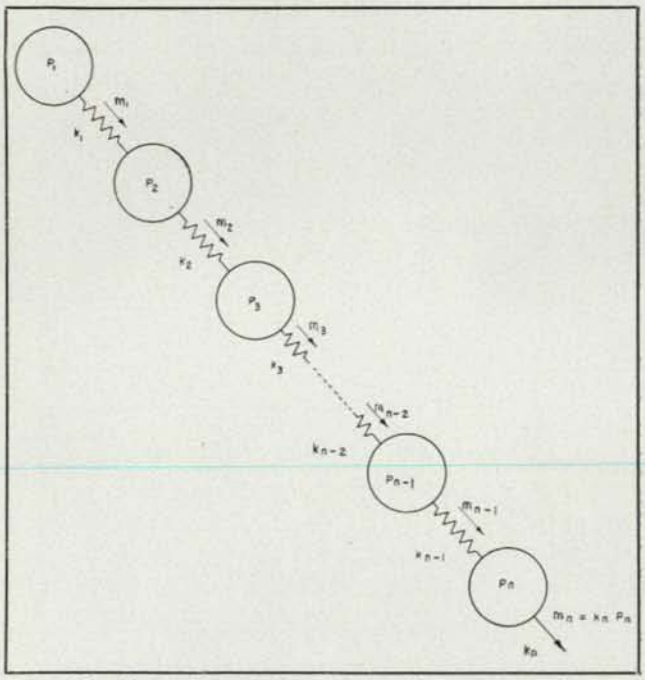
$$\dot{P}_2 (M_2)_0/(P_2)_0 = m - m_2 \quad . \quad . \quad . \quad (27b)$$

. . .

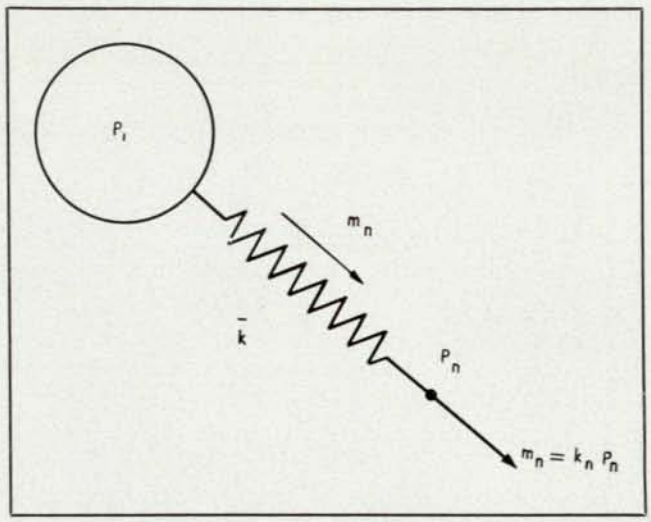
$$\dot{P}_n (M_n)_0/(P_n)_0 = m_{n-1} - m_n \quad . \quad . \quad (27n)$$

and if the first two equations are added, $-m_2$ will be obtained on the right-hand side, while adding the first three equations will yield $-m_3$ etc., until adding all the equations will give a value for $-m_n$. If both sides of the equations so derived including equation 27a are squared, the resulting equations will be

$$m_1^2 = \left[\frac{\dot{P}_1 (M_1)_0}{(P_1)_0}\right]^2 \quad . \quad . \quad . \quad . \quad (28a)$$



10—*Chain of* n *capacities connected by resistive paths*



11—*Single capacity and resistance circuit to give* P_1 *transient equivalent to that of an* n*-capacity chain*

$$m_2^2=\left[\frac{\dot{P}_1\ (M_1)_0}{(P_1)_0}+\frac{\dot{P}_2\ (M_2)_0}{(P_2)_0}\right]^2 \quad . \quad (28b)$$

$$\vdots$$

$$m_n^2=\left[\frac{\dot{P}_1\ (M_1)_0}{(P_1)_0}+\frac{\dot{P}_2\ (M_2)_0}{(P_2)_0}+\ldots\ldots+\frac{\dot{P}_n\ (M_n)_0}{(P_n)_0}\right]^2. \quad (28n)$$

34. If the pressure drop along the train of volumes and resistances always remains small in comparison with the datum pressure as the transient proceeds, as appears to be the case for fractures of a cold duct, each volume will to a close approximation undergo the same transient and any one of $\dot{P}_1, \dot{P}_2 \ldots\ldots \dot{P}_n$ could be closely represented by any of the others. If $\dot{P}_n$ is chosen to represent the pressure decay rate for all the volumes, equations 28a to 28n may be written

$$m_1^2=\dot{P}_n^2\left[\frac{(M_1)_0}{(P_1)_0}\right]^2 \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (29a)$$

$$m_2^2=\dot{P}_n^2\left[\frac{(M_1)_0}{(P_1)_0}+\frac{(M_2)_0}{(P_2)_0}\right]^2 \quad . \quad . \quad . \quad . \quad . \quad (29b)$$

$$\vdots$$

$$m_n^2=\dot{P}_n^2\left[\frac{(M_1)_0}{(P_1)_0}+\frac{(M_1)_0}{(P_2)_0}+\ldots\ldots+\frac{(M_n)_0}{(P_n)_0}\right]^2. \quad (29n)$$

Dividing equations 29a, 29b .... 29(n–1) in turn by equation 29n yields

$$m_1^2=m_n^2\left[\frac{(M_1)_0}{(P_1)_0}\right]^2 \div \left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)_0}\right]^2 \quad . \quad . \quad . \quad (30a)$$

$$m_2^2=m_n^2\left[\sum_{r=1}^{r=2}\frac{(M_r)_0}{(P_r)_0}\right]^2 \div \left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)_0}\right]^2 \quad . \quad . \quad (30b)$$

$$\vdots$$

$$m_{n-1}^2=m_n^2\left[\sum_{r=1}^{r=(n-1)}\frac{(M_r)_0}{(P_r)_0}\right]^2 \div \left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)_0}\right]^2 \quad (30(n-1))$$

and if $m_1^2, m_2^2 \ldots\ldots m_{n-1}^2$ are now eliminated from equation 26 by means of equations 30a, 30b ..... [30(n–1)] it will be found that

$$P_1-P_n=\frac{m_n^2}{P_1+P_n}\left\{k_1^1\left[\frac{(M_1)_0}{(P_1)_0}\right]^2+k_2^1\left[\sum_{r=1}^{r=2}\frac{(M_r)_0}{(P_r)_0}\right]^2+\ldots.\right.$$

$$\left.\ldots.k_{n-1}^1\left[\sum_{r=1}^{r=(n-1)}\frac{(M_r)_0}{(P_r)_0}\right]^2\right\}\div\left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)_0}\right]^2 \quad . \quad . \quad (31)$$

35. Now, if the long chain of gas volumes and resistances is to be replaced by the simple single resistance and single volume group as shown in Fig. 11 this might be visualized as lumping all the gas contents of the separate volumes into one to make a large capacity containing $\sum_{r=1}^{r=n}\left[(M_r)_0\right]$ initially coupled by one resistance to the point designated by n from which discharge of magnitude m_n takes place. The pressure transient exhibited by the single large capacity would now approximate to that of P_1 of the long chain if the flow equation

$$P_1-P_n=\bar{k}m_n^2/(P_1+P_n) \quad . \quad . \quad . \quad (32)$$

relating to the single capacity/resistance group is identified with equation 31. Thus, the equivalent resistance coefficient $\bar{k}$ would then be given by

$$\bar{k}=\left\{k_1^1\left[\frac{(M_r)_0}{(P_r)_0}\right]^2+k_2^1\left[\sum_{r=1}^{r=2}\frac{(M_r)_0}{(P_r)_0}\right]^2+\ldots.\right.$$

$$\left.\ldots.+k_{n-1}^1\left[\sum_{r=1}^{r=(n-1)}\frac{(M_r)_0}{(P_r)_0}\right]^2\right\}\div\left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)_0}\right]^2=$$

$$\sum_{s=1}^{s=(n-1)}\left\{k_s^1\left[\sum_{r=1}^{r=s}\frac{(M_r)_0}{(P_r)_0}\right]\right\}\div\left[\sum_{r=1}^{r=n}\frac{(M_r)_0}{(P_r)}\right]^2 \quad . \quad . \quad (33)$$

36. Since it has been assumed that at all times the pressures in the various parts of the chain differ only slightly one

from the other a further simplification can be made by assuming equality of the initial pressures to give

$$\bar{k}=\sum_{s=1}^{s=(n-1)}\left\{k_s^1\left[\sum_{r=1}^{r=s}(M_r)_0\right]^2\right\}\div\left[\sum_{r=1}^{r=n}(M_r)_0\right]^2 . \quad (34)$$

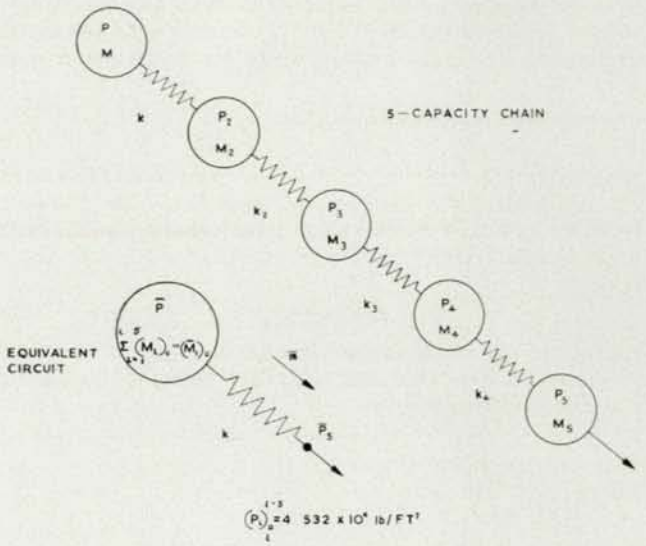
Thus, knowing the initial gas masses and linking resistances the resistance coefficient for the replacement single capacity/resistance group can be found quickly from a simple arithmetical computation.

Application of principle

37. It is proposed to show briefly the application of the method of derivation of the equivalent circuit to simple chains of volumes and resistances followed by some examples of the application of the principle to reactor circuits with comparisons between the estimated values of stagnation time and fracture area to cause core flow stagnation, and those values derived from the consideration of the more comprehensive reactor system which was subject to computer analysis.

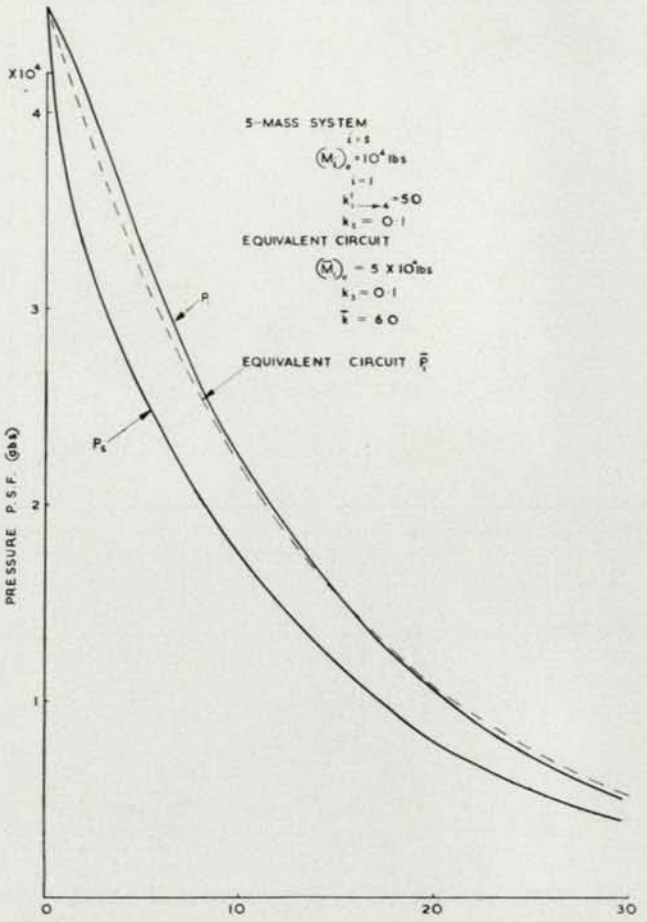
Capacity/resistance chain

38. The system considered here comprised five volumes joined into a chain by four resistive paths as shown in Fig. 12. It was assumed that the conditions in each volume were isothermal, and for the resistive paths, equations of the form

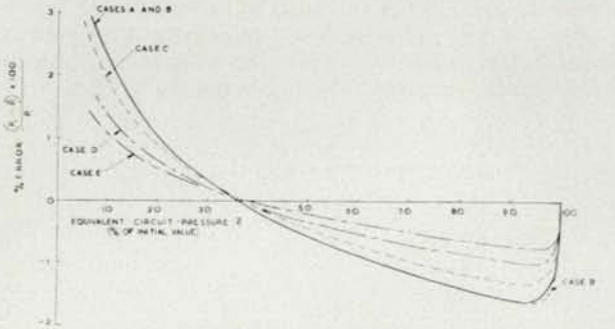


CASE	CIRCUIT DATA										
	GAS MASSES lbs					PRESSURE/FLOW COEFFICIENT SEC²/FT⁴				FT³/SEC	SEC²/FT⁶
	$(M_1)_0$	$(M_2)_0$	$(M_3)_0$	$(M_4)_0$	$(M_5)_0$	k_1^1	k_2	k_3	k_4	k_5	$\bar{k}$
A	10^4	10^4	10^4	10^4	10^4	10	10	10	10	0·1	12·0
B	$1·5\times10^4$	$1·25\times10^4$	10^4	$7·5\times10^3$	5×10^3	10	10	10	10	0·1	17·65
C	5×10^3	$7·5\times10^3$	10^4	$12·5\times10^4$	15×10^4	10	0	10	10	0·1	7·65
D	10^4	10^4	10^4	10^4	10^4	2	4	6	8	0·1	8·0
E	10^4	10^4	10^4	10^4	10^4	8	6	4	2	0·1	4·0

12—*Five-capacity chain and equivalent circuit*



13—*Pressure transients for 5-mass chain and equivalent circuit*



14—*Equivalent circuit pressure discrepancies during transient*

of 23 were employed with the discharge from the end volume being given by

$$m_5=k_5 P_5, \quad . \quad . \quad . \quad . \quad . \quad (35)$$

as would be the case if this outflow were the result of the action of a constant speed gas circulator.

39. In order to demonstrate the form taken by the transient Fig. 13 is provided, and here a train of initially equal gas masses of 10^4 lb each has been assumed with each resistance coefficient $k_{1\to4}^1$ having the value 50.

Application of equation 34 readily leads to

$$\bar{k}=\frac{50\times10^4\ (1+4+9+16)}{25\times10^4}=60. \quad . \quad . \quad (36)$$

The transient exhibited by $\bar{P}_1$ in the equivalent capacity/resistance group can be found by solution of equations

$$\bar{P}_1-\bar{P}_5=k\ \bar{m}_1^2/(\bar{P}_1+\bar{P}_5) \quad . \quad . \quad . \quad . \quad (37)$$

$$\bar{P}_1=-(\bar{P}_1)_0\ m_1/(\bar{M}_1)_0 \quad . \quad . \quad (38)$$

and

$$\bar{m}_1=k_5\ \bar{P}_5 \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (39)$$

relative to this simple flow path, and when $\bar{m}_1$ is eliminated between the equations in question it is found that

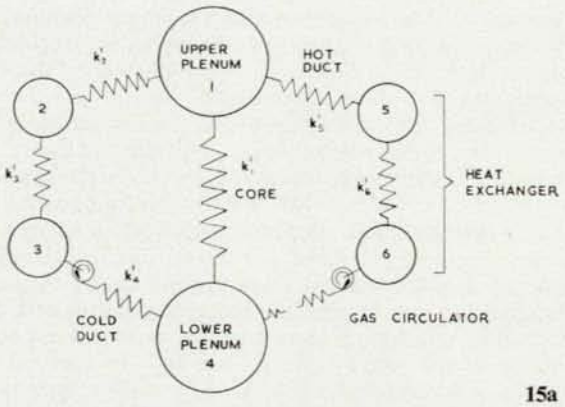
$$\bar{P}_1=-\bar{P}_1\ (P_1)_0\ k_5/(M_1)_0\ [1+\bar{k}\ k_5^2]^{\frac{1}{2}}, \quad . \quad (40)$$

which gives the desired transient as

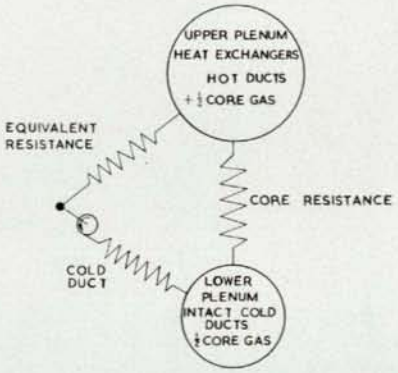
$$\log_0 \bar{P}_1/(\bar{P}_1)_0=-(P_1)_0\ k_5/(M_1)_0\ [1+\bar{k}\ k_5^2]^{\frac{1}{2}} \quad . \quad (41)$$

The transient corresponding to P_1 in the five-capacity chain was obtained by use of a digital computer.

40. The values of initial gas contents and connecting resistances have been chosen to give a relatively wide spread of pressure from $\bar{P}_1$ to P_5 for clarity of representation, the actual spread being approximately 20% of the highest pressure in the chain suggesting a relatively large pressure drop

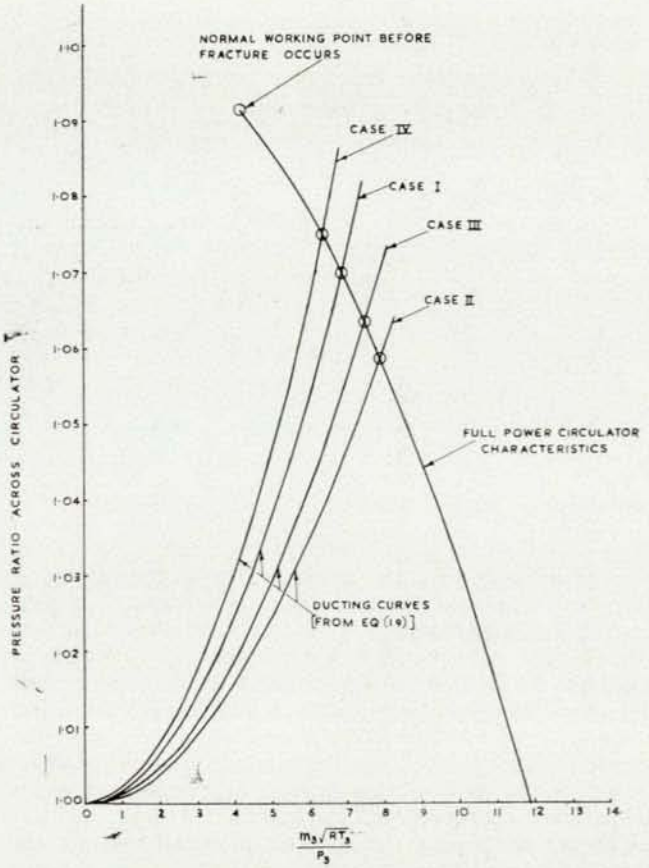


15a



15b

Reactor circuit representations



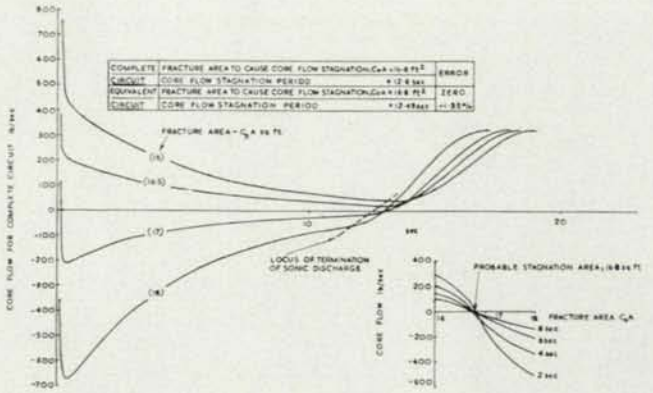
16—*Determination of circulator working point during period of core flow stagnation*

for a part-leg of a gas-cooled reactor. Nevertheless, even under these conditions the error between the transient for P_1 and that for P_1 of the equivalent group is not large, being for the most part less than $\pm5\%$ of P_1.

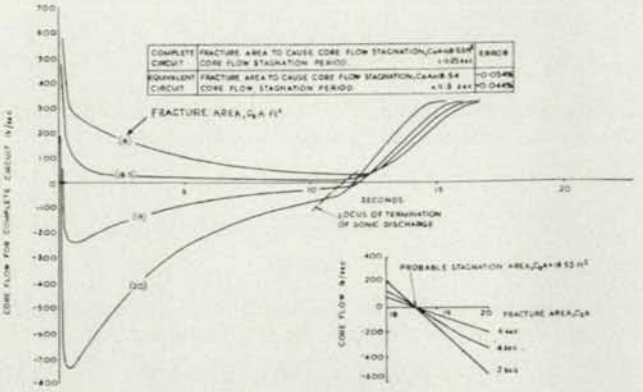
41. The aim of five additional computations which were made with various volume/resistance chains was to obtain a spread of pressure from P_1 to P_5 of about 5% of P_1 during each transient which would be more in keeping with present day gas-cooled reactor pressure drops, while the effects of having gas masses and resistance coefficients of widely different values with variations in sequence were also investigated. The relevant data for the five cases are set out in Fig. 12 together with the values employed for the equivalent resistance/capacity group in each case. The error in each case between the pressure in the equivalent combination and that of P_1 in the five-mass chain is given in Fig. 14. It will be seen that for most of each transient the error between the pressure of the gas mass in the equivalent group and that of the innermost mass of the chain is less than $\pm1\frac{1}{2}\%$ which must be considered as an adequately small error margin.

Reactor transients

42. Here, a reactor system, reduced in the first instance to that of Fig. 15a in which each heat exchanger has been replaced by two volumes with a coupling resistance, has been subject to analysis, and the core mass flow transients for four



17a—*Case* 1—*Core flow variation with size of duct fracture*

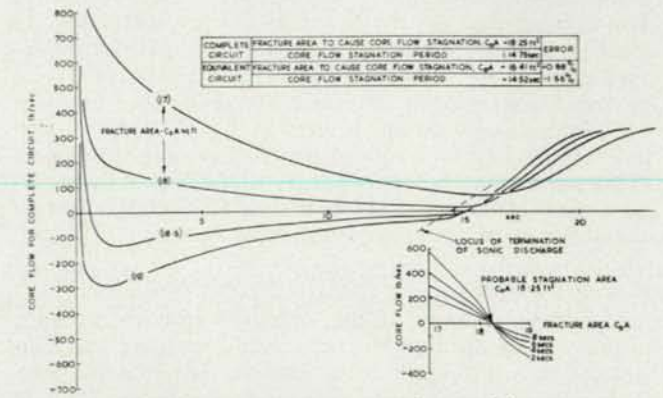


17c—*Case* 3—*Core flow variation with size of duct fracture*

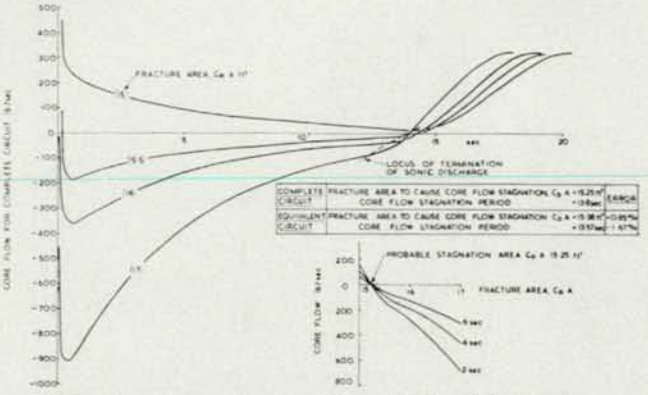
cases dealt with are given in Figs 17a, b, c, and d for various sizes of discharge area. The discharge areas so chosen were to give distributions of transients which would enable in each case an accurate estimation of the area to produce stagnation of core flow to be made. It was proposed for each case to maintain the same initial flow values with the same overall circuit initial pressure drop, and the same total gas volume and distribution of constant temperature around the circuit, the changes to be brought about being in one instance the interchange of the two volumes in each heat exchanger group while other changes rested solely with hot duct and heat exchanger resistances. These latter modifications did not influence the initial pressure drop from upper plenum to circulator inlet, but simply changed the manner in which this pressure drop was built up by varying only the initial pressure obtaining at inlet to the heat exchanger group. The data relevant to the four cases are given in Table 1 where the suffices for mass, volume and temperature are indicated in location in the capacities of Fig. 15a.

43. For each system chosen the equivalent circuit was reproduced and this entailed determining the value of the equivalent resistance to be used to join the gas circulators to the augmented gas mass above the core, this mass comprising the gas contents of all heat exchangers, hot ducts and the original upper plenum together with one-half the amount of gas held in the fuel channels. A representative three-mass chain to be transformed into the equivalent one-mass/one-resistance group would comprise a pair of heat exchanger capacities and coupling resistance together with a hot duct resistance, and an innermost gas mass made up of $1/(N-1)$th of the sum of the gas contents of the upper plenum, heat exchanger in the fractured leg and one-half of the fuel channel contents. Here, N indicates the total number of heat exchangers linked to the reactor vessel.

44. Having derived the flow coefficient for the equivalent flow path the longest possible stagnation time and the fracture area to produce this were calculated, by the methods already described, for each case. It will be noticed from Figs 17a, b, c, and d, where core flow stagnation times and corresponding fracture areas are tabulated for results obtained from the simple arithmetical process involving the equivalent circuit technique, and also for results obtained by means of a computer which accepted a more complex mathematical representation of the reactor circuit, that in each case the discrepancy in fracture area is less than 1%, while it appears that



17b—*Case* 2—*Core flow variation with size of duct fracture*



17d—*Case* 4—*Core flow variation with size of duct fracture*

Table 1

Case	Volume (ft³)		Mass of gas (lb)		Friction coefficients	
	V_2	V_3	M_2	M_3	k_2^1	k_3^1
1	$6{\cdot}235 \times 10^5$	$7{\cdot}724{\cdot}10^2$	5×10^3	10^3	55·85	55·11
2	$7{\cdot}724 \times 10^2$	$6{\cdot}235 \times 10^3$	$6{\cdot}194 \times 10^2$	$8{\cdot}073 \times 10^3$	55·85	55·11
3	$6{\cdot}235 \times 10^3$	$7{\cdot}724 \times 10^2$	5×10^3	10^3	92·66	18·29
4	$6{\cdot}235 \times 10^3$	$7{\cdot}724 \times 10^2$	5×10^3	10^3	18·70	92·26

$T_1 = 900°K$; $T_2 = 900°K$, $T_3 = 550°K$; $T_4 = 570°K$; $T_5 = T_2$; $T_6 = T_3$
$V_1 = 1{\cdot}231 \times 10^4$ ft³; $V_4 = 1{\cdot}485 \times 10^4$ ft³; $V_5 = V_2$; $V_6 = V_3$; $N = 8$
$M_1 = 10^4$ lb; $M_4 = 2 \times 10^4$ lb; $M_5 = M_2$; $M_6 = M_3$; $(m_1)_o = 8000$ lb/sec
$k_1^1 = 3{\cdot}404$; $k_4^1 = 59{\cdot}34$; $k_5^1 = k_2^1$; $k_6^1 = k_3^1$

the stagnation period can be estimated to within 2%. The proximity of the pairs of area and time values suggest that the simple approach to the derivation of longest core stagnation time and corresponding fracture area is adequately accurate.

45. The curves submitted in Fig. 16 are of interest in that they show the circulator working point during the four stagnation periods, and also the working point of the circulator under the steady conditions obtaining prior to duct fracture. It should be remembered that in each case the circuit pressure drop is initially the same, the only differences between the cases being due to either a change in partition of heat exchanger volumes or heat exchanger and hot duct resistance. The wide spread of circulator working point indicates the influence of the partitioning of gas mass and path resistance, and shows the need for care in the choice of sub-division and overall representation of the reactor circuit and its flow properties.

REFERENCE

1. Edwards, J. E., and Collins, J. E., 'Fractured duct temperature transient program, Wolf Trap,' AHSB (S) M103

3 Application of the safety limitation against depressurization to the Calder and Chapelcross reactors

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This Paper is the third of five to be presented at a half-day symposium of the Society on 11 November

The calculated temperature rise following a fault in the pressure vessel giving depressurization is incorporated in the fuel element temperature control criterion used on the Calder and Chapelcross reactors. The basis of this criterion and its practical application to reactor control are described.

INTRODUCTION

WITHIN any overall limitation set by the bulk outlet gas temperature, the thermal power output for a given design of reactor operating at its maximum pumping power is controlled by the maximum fuel element temperature. Two limitations are imposed on the maximum can surface temperature: the first is concerned with the integrity of the can and its performance, and thus is determined essentially by mechanical strength and oxidation characteristics; the second is related to the temperature excursion during a fault when the limiting value is the ignition temperature of the can material. The maximum temperature of the uranium fuel is also limited, in this case by the allotropic change points of the uranium.

2. At the time when Calder reactors were designed, the available information on the chosen canning material suggested that the limiting can surface temperature should be about 400°C. After choosing certain design parameters, calculation showed that the design heat output of 180 MW could be achieved if each channel was allowed a mass flow needed to make the maximum can surface temperature 408°C.[1] This temperature was adopted as the limiting value for operation and the reactors were controlled to this 'nominal maximum' limit by graphical assessment of temperatures measured in channels judged to be representative of the complete temperature distribution, within the operational restrictions existing at that time on the loading of thermocouples. An additional assessment was made to take account of measured 'hot channel' factors and a limit of 425°C was placed on this 'assessed maximum' value. Following a reassessment of fuel element performance, each of the above limits was raised by 10degC.

3. Additional information about the properties of the canning material—magnox—had shown that the original limitation of about 400°C on maximum surface temperature could be raised by nearly 100 degC. This might suggest that the original criterion could have been applied with a new upper limit of about 500°C, and the reactors controlled on the basis of a prediction of maximum temperature in each channel normalized to the available measurements. However, such a drastic increase in the limiting temperature was clearly not practicable because, although calculation of the controlled and uncontrolled consequences of certain fault conditions had shown that the reactors could withstand the worst credible of these faults when operating under the original temperature limits, they would not necessarily do so if this limit was raised without reference to channel rating. A similar problem of reactor safety was often raised when proposals were made to irradiate experimental fuel at elevated can temperatures. Clearly it was desirable to define the most arduous fault condition which reactor loadings should withstand, and from this to formulate a quantitative criterion for the operating conditions of any type of fuel element.

4. Preliminary consideration of credible incidents showed that only the following cases required further assessment:

(*a*) depressurization from top duct fracture and simultaneous failure of blowers;
(*b*) depressurization from bottom duct fracture and simultaneous failure of blowers;
(*c*) sudden increase in reactivity at full power; and
(*d*) sudden increase in inlet temperature at full power.

Wall[2] has shown that with a fractured bottom duct it is possible for stagnation to occur in the core with particular combinations of hole size and discharge coefficient, and, although the initial loss of coolant may be less rapid than with other forms of fracture, the initial temperature transient is more severe. Other calculations for conditions (*c*) and (*d*) confirmed that the temperature transient was less restrictive than with case (*b*). The temperature control criterion is therefore based on considerations of bottom duct failure.

OBJECTIVE AND SIGNIFICANCE OF TEMPERATURE CRITERION

5. The objective of the temperature control criterion is to avoid widespread can melting during the temperature transient following bottom duct failure. It is assumed acceptable to have some low probability of ignition of a single channel, or small number of channels, provided that the risk of fire in a single channel spreading to neighbouring channels is minimized by specifying a maximum permissible value for the rate of release of stored energy in the graphite sufficiently below its specific heat; under these conditions the intervening graphite provides a sufficiently large thermal sink to isolate

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the fire to a single channel. Acceptance of this risk is consistent with the possibility that increased fuel element temperatures (even conceivably to the point of ignition of the magnox can) may arise during steady state operation, from reduction of flow caused by cracked graphite sleeves or by partial blockage by graphite or fuel element debris. Rigorous inspection and fuel loading procedures are applied to minimize the risk of blockage, and irradiation limits based on measurements of bow are in operation to reduce the probability of fuel element fins touching the channel walls. Despite the most careful precautions, however, it is impossible to eliminate completely the risk of restriction in flow in isolated channels. It is unrealistic to attempt to specify steady state operating conditions that take account of the possibility of channel blockage when its incidence cannot be specified, although it may be argued that a reasonably conservative safety criterion should be adopted as a consequence.

6. The problem of specifying a 'safe' temperature distribution is a statistical one for which a number of alternative approaches can be adopted. The one chosen specifies that the distribution of fuel element temperatures in the reactor under normal operating conditions shall be such that in the initial transient following a bottom duct failure, the probability of can temperature exceeding the ignition point of magnox shall be less than 0·1 at a 90% confidence level. The ignition temperature of magnox depends on the air and moisture content of the carbon dioxide coolant and on the rate of rise of temperature. For the Calder and Chapelcross reactors, a conservative value of 630°C is currently used pending further assessments. The significance of this form of statement will now be considered.

7. The numerical value of probability adopted must be based on two conflicting considerations:

(*a*) our present knowledge of the distribution of the variables involved is such that any attempt to define low probability limits could be considerably in error and to make adequate allowance for this could lead to an economic penalty arising from inordinately low temperature limits. If a high probability is acceptable then, even if as is done here an overall normal distribution is assumed, the model should be reasonably valid;

(*b*) the probability should be low enough to give an acceptable degree of protection.

8. As the probability of ignition depends on both the probability of occurrence of the defined fault, and the probability that, if it does occur, the ignition temperature of magnox will be exceeded, it is considered that an overall probability of 1 in 10 for the reactor is reasonable. It may be noted that changing the probability from 0·1 to 0·01 would reduce the permitted can temperatures by 6–7 degC. As will be seen in later sections, the errors on the model are estimated on the basis of a separate normalization for each zone of the reactor.

9. The concept of confidence is based on the accuracy with which the prediction of peak transient temperature and its associated uncertainty is known. If the variability of this parameter could be represented by a normal distribution, and its mean and standard deviation were known exactly, then the probability of the parameter exceeding a given number of standard deviations above the mean could be exactly specified. In the present case, however, the mean and standard deviation of the maximum steady state channel temperatures are not known precisely as they have been determined from a relatively small number of results. Thus it is no longer possible to determine exactly the temperature limit which will be exceeded by the desired probability. However, it is possible to define a limit with a certain degree of confidence which is a function of the measured values of standard deviation and the number of results from which the latter are derived. The choice of the degree of confidence used in the present criterion, i.e. 90%, does not impose too great a restriction on the operating limits, but ensures that they are reasonably realistic for the steady state temperatures. Even then they will be pessimistic because a deliberately high value for the transient rise has been assumed so that the probability of this component contributing to the overall error is negligible.

10. The information required before limits can be set on the steady state temperatures of instrumented channels is:

(*a*) the steady state temperature distribution (i.e. the maximum can temperature in each channel in the reactor) together with its associated uncertainty;

(*b*) the transient temperature rise in each channel following a bottom duct failure.

THE STEADY STATE TEMPERATURE DISTRIBUTION

11. With the Calder and Chapelcross reactors which are refuelled off-load, there is a considerable economic penalty if the reactor must be shut down to make good a deficiency in the positioning of absorbers. A method of predicting fuel element temperatures for a given absorber pattern is essential and it is desirable that this method should not depend solely upon a knowledge of the temperatures in instrumented channels. The programme FTD2[3] is used at Calder and Chapelcross for this purpose, although it is not claimed that alternative programmes could not be used equally satisfactorily. One obvious disadvantage is that this programme is two-dimensional only, although because of varying control rod positions, the problem is essentially three-dimensional. However, the problems of devising averaging techniques for flux to determine the effect of rod insertion on radial flux distributions were thought to be capable of solution more readily than the formulation of a three-dimensional programme; although this latter problem has been solved recently, the greatly increased cost of the more complicated programme is not thought to justify its use in routine practice.

12. FTD2 solves the two-group diffusion equations by a finite difference method using a mesh on interstitial points of the fuel lattice, so that the cross-section of the reactor is divided up into square regions each with a fuel channel at the centre. Up to 100 different regions can be used, including fuel, reflector, and absorber regions, each with the appropriate lattice constants. The power distribution is calculated from the flux distribution using a different power per unit flux factor γ for each different core region, the powers being normalized to give the specified total power output for the reactor. The maximum can temperature in every channel is calculated from the relation

$$T_{sm} - T_i = \text{const.} \frac{K}{M} \frac{\phi\gamma}{\Sigma\phi\gamma} P$$

where T_{sm} is the maximum can temperature in the channel, T_i = the reactor inlet temperature, P the reactor thermal

power, K a function of the Stanton number in the channel, M the channel mass flow, ϕ the thermal flux in the channel, and $\sum\phi\gamma$ the sum of $\phi\gamma$ over all the fuelled channels. The validity of the programme for flux calculations was established by comparison with measurements made during commissioning and with the reactor at power;[4] in general the discrepancy between measured and predicted flux is less than 5%.

13. Early predictions of maximum can temperature using design flow data and heat transfer data from experimental rigs showed poor agreement with measured values. Attempts to improve the prediction were made using information obtained from reactor measurements. A single parameter (K/M in the above equation) was obtained for each gag from the measured maximum channel temperature and predicted flux. To obtain the best values of this parameter, comparisons of predicted and measured values of T_{sm} were made over a large number of reactor charges, applying suitable corrections to the measured T_{sm} to allow for the difference in value with control rod position. (FTD2 is used to calculate T_{sm} for fully withdrawn control rods because the provision for dealing with partially-inserted control rods included in the programme still requires empirical adjustments, as again it is a two-dimensional calculation of a three-dimensional problem.) After applying a simple zonal normalization (necessary because of errors in the predicted flux shape) best average values of (measured–predicted) T_{sm} were obtained for each gag for which measurements were available, and hence values of K/M to give the best fit of predicted to measured T_{sm}. Values of K/M for those gags for which no measurements were available, were interpolated from smooth curves of K/M against M, derived from channel gas flows measured during commissioning.

14. The original correlations of temperatures were made using measurements from channels immediately surrounding X-holes, these being the only instrumented channels. When it became possible to load thermocouples in other positions in charge pans and the mass of practical data increased, three systematic effects were recognized which affected the steady state temperature distribution:

(*a*) *Quadrantal temperature asymmetry*. Both mass flow and temperature measurements indicated that two stable mass flow patterns can exist and give rise to quadrantal asymmetry over defined areas; either pattern can be induced by bringing up the blowers in a defined manner. The area of the reactor in each quadrant which is affected by the asymmetry, and the magnitude of the can temperature change between the two patterns has been established fairly accurately. Part of this area must have 2 degC $\pm$2 degC, and part 5 degC $\pm$2 degC added to the predicted temperatures for two diametrically-opposite quadrants; in the opposite quadrants these values must be subtracted.

(*b*) *Gas and heat leakage*. In the unsleeved reactors coolant gas leaks through moderator gaps from the fuel channels into the X-holes so that the mass flow in each channel falls in steps with distance up the channel. The relatively cool gas flowing up the X-holes then removes heat preferentially from adjacent inner channels. The effect has been determined from a combination of detailed theoretical treatment containing, of necessity, several assumptions, and analysis of temperature measurements in inner and outer channels. It amounts to about 10 degC in T_{sm}. In sleeved reactors the effect is negligible.

(*c*) *Diagrid effect*. Commissioning flow measurements indicated that, for nominally identical channels the flow decreased as the diagrid was crossed in the radially-outward direction, the effect decreasing with distance from the centre of the reactor. A detailed pattern of this effect was obtained from an analysis of other commissioning results in which the flow was measured in every channel in one quadrant of a reactor. The pattern has been checked by analysis of temperature measurements. In the positions most effected, T_{sm} changes by about 15 degC.

15. These revisions to the reactor flow model enable the maximum can temperatures to be predicted with equal accuracy over the whole core. After normalization the overall values shown in Table 1 were obtained.

Table 1: *Errors of predicted temperatures*

Zone	Unsleeved reactors		Sleeved reactors	
	Error (S.D.) (degC)	No. of readings	Error (S.D.) (degC)	No. of readings
A	8·8	331	9·4	87
B	8·0	506	8·6	218
C	8·5	921	6·8	222

The weighted mean standard deviation is $\pm$8·3 degC. This error is applicable only to the variations in steady state temperatures normally obtained; it includes errors of measurement and can-to-can variations, as well as errors in the model, but does not take into account the possibility of increases in temperature caused by coolant leakage through a cracked sleeve, by partial blockage from graphite or fuel element debris, or by bowing of fuel elements resulting in local heat transfer changes.

TRANSIENT CALCULATION

16. The temperature transients following depressurization and the associated theoretical and experimental studies are considered in detail in companion Papers by Wilson and Dodds,[5] Wall,[2] and Moore.[6] It is intended in this section to discuss the calculation only in sufficient detail to show the way in which the results are used in the determination of operating limits on fuel element temperatures.

17. Following fracture of the bottom duct there is rapid loss of pressure in the reactor with severe reduction in coolant flow through the fuel element channels. The delay between instant of fracture and the time at which the control rods are tripped, depends on the transient response of the initiating reactor parameter and on the characteristics of the reactor safety circuits. Fuel element temperatures rise to a maximum, determined by the balance between the heat production from delayed neutron and fission product sources and the heat loss by radiation to the graphite moderator and convection in the reduced gas flow. Once this equilibrium is established, fuel element temperatures fall as the heat production in the elements decays. Eventually, increasing graphite temperatures and oxidation of the graphite may cause oxidative runaway conditions with melting of fuel element cans, unless forced cooling by one or more main blowers can be restored; this, however, requires consideration of factors beyond the scope of the present Paper. The fuel element temperature control criterion is primarily concerned with the avoidance of widespread melting of fuel elements during the initial transient.

18. Some of the assumptions made in the calculation of the initial transient rise have been made deliberately pessimistic. Thus, following the work of Wall,[2] it is assumed that after an initial reduction of coolant flow through the core to zero in 0·5 sec there is a period of flow stagnation for 34 sec. To achieve this it is assumed that:

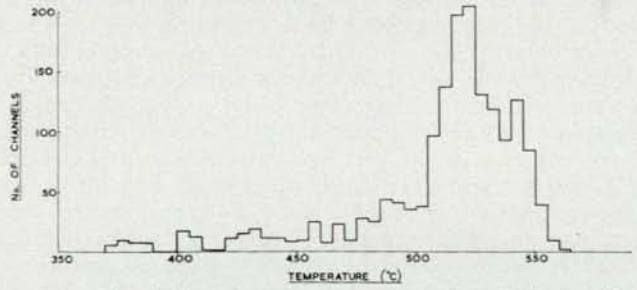
(*a*) the blowers remain at full speed;
(*b*) the circuit containing the fracture is blocked, preventing the blower in that circuit discharging from the space above the core;
(*c*) the fracture size is 7·4 sq. ft with a discharge coefficient of unity.

The first assumption is effectively correct only for the blower in the affected circuit, because under the conditions postulated that blower will be operating near to its free running point, and the other two assumptions are also obviously pessimistic. Accepting this model, the initial rate of loss of pressure is about 5 lb/in² sec and the maximum temperature rises initially at a rate of about 20 degC/sec. The reactor safety circuits which will respond quickly to this loss of coolant include: (*a*) rate-of-change of pressure; (*b*) out-of-balance flow; (*c*) fuel element can temperature; and (*d*) blower over-speed.

19. The rate-of-change of pressure trip set at 5 lb/in²min will trip the control rods some 2 sec after the duct fracture occurs.[7,8] With the Bailey temperature trip amplifiers set at a 10 degC margin to trip (the normal setting at steady power) the maximum trip delay time is about $3\frac{1}{2}$ sec.[5] To determine the delay time for a blower over-speed it is necessary to know the blower characteristic at the free running point outside the normal range. This information is not readily available, but calculations based on an extrapolation of the operating characteristic beyond 940 rev/min lead to an estimate that the trip delay time could be as high as $5\frac{1}{2}$ sec.

20. To give an added margin of safety in applying the temperature criterion, it is assumed that the control rods will be actuated by the second fastest independent trip circuit. The rate-of-change of pressure and out-of-balance flow trips cannot be considered as independent safety trips because they are based on the same mechanical principles of operation and rely on the same compressed air supply. Hence the delay time used in the transient calculation is that associated with the fuel element can temperature trip. It may be noted that a change of 1 sec in the trip time produces a 9 degC change in the transient temperature rise.[5]

21. The transient temperature rise is directly proportional to channel power but is not greatly affected by a change in channel mass flow at constant rating; it is, in fact, proportional to the peak rating in the channel over the range of ratings and control rod insertions met in operation of these reactors (see Table 3, ref. 5). This linear dependence of transient temperature rise on rating enables the transient can temperature in each channel to be calculated by a simple extension to the FTD2 programme used to predict the steady state temperature distribution. A typical distribution of calculated maximum can temperatures during a transient is shown in Fig. 1.



1—*Typical distribution of maximum can temperatures during transient*

CALCULATION OF PERMITTED TEMPERATURES

22. From the FTD2 prediction of peak transient temperatures T_t, that temperature T^* must be found which will be exceeded with a total probability of only 0·1 at the 90% confidence level. For this the following result is used: the temperature T which is exceeded with a probability α at a confidence level $(1-\beta)$ is given by

$$T=\bar{t}+\sigma\left[K_\alpha+K_\beta\sqrt{\left(\frac{1}{n}+\frac{K_\alpha^2}{2(N-1)}\right)}\right] \quad . \quad . \quad . \quad . \quad (1)$$

where $\bar{t}$ is the normalized value of the temperature, K_α the normal deviate corresponding to α, K_β the normal deviate corresponding to β, n the number of measurements used in normalizing the model, and N the number of measurements used in estimating the error σ.
As N is large, this equation can be reduced to:

$$T=\bar{t}+\sigma\left[K_\alpha+\frac{K_\beta}{\sqrt{n}}\right] \quad . \quad . \quad . \quad . \quad (2)$$

23. An estimate is made of T^* and from equation 2, the probability of T_t exceeding T^* is found. The estimate is revised if necessary, until the sum of the probabilities for all channels is 0·1. The quantity $(630-T^*)$ degC is then added to the predicted steady state maximum can temperature for each instrumented channel to give the permitted maximum can temperature appropriate to the reactor power and control rod insertion assumed in the FTD2 predictions. From a knowledge of the axial temperature distribution, the permitted temperature for each installed thermocouple can then be derived. Provision must be made for other possible operating conditions, either by a complete recalculation, or by means of simplifying, pessimistic assumptions about the effect of changes in reactor power or control rod insertion. Finally, a table can be constructed of average permitted temperature for the thermocouples in each zone against control rod insertion and reactor power.

24. After a major discharge and before temperature readings have been evaluated, it is necessary to rely solely on the predicted flux distribution to give the channel ratings. To allow for the effect of errors, the permitted limits as evaluated in the preceding paragraph are reduced by 10 degC. As soon as conditions are suitable, the temperature predictions are normalized to the measurements, and revised values of permitted limits calculated. Insertion of control rods alters the flux and temperature distributions and corrections to the permitted limits must be made. Away from the control rods, the changes in steady state temperatures are known for the thermocouple channels, and from these, contours of the temperature changes can be plotted. Since the coolant flows remain unaltered, the temperature changes also indicate the changes in channel power and hence in transient rise. By combining the changes in steady state temperatures and transient rises, the corrections to the permitted limits can be derived for all fuel channels away from the inserted control

rods. Close to these rods the changes in temperature and flux vary very rapidly and in general there are no thermocouples. The effects of rod insertion to be considered are:

(*a*) an increase in transient rise due to the change in the axial flux shape;
(*b*) a decrease in transient rise due to the radial flux depression; and
(*c*) a decrease in steady state temperature due to the radial flux depression.

It has been shown from a study of commissioning measurements that the decrease in transient rise (*b*) is greater than the increase (*a*), and so extension of the contours of temperature and flux changes to the regions near the control rods will be pessimistic.

ANALYSIS OF MEASURED TEMPERATURES

25. Fuel element thermocouples are loaded into the reactor core for two main purposes:

(*a*) to provide an assessment of the maximum can temperature in an unbiased selection of channels;
(*b*) to provide experimental measurements of anomalous temperatures in channels affected by some feature of the reactor design or loading—e.g. diagrid, or the proximity of control rods, absorber cartridges, experimental enriched fuel elements, etc.

A temperature criterion based on temperatures attained in a fault transient requires a reliable model for temperature prediction, but this can only be guaranteed after detailed comparison with many in-pile measurements. As confidence in the model grows, fewer thermocouple elements need be loaded, giving a reduction in costs of materials and in fuel handling time. Since the introduction of the new temperature criterion it has been possible to reduce the number of instrumented channels necessary for control of the reactor from 52 to 24. In practice, the 24 is taken as a minimum number and additional channels are instrumented to allow for any loss of thermocouples which cannot immediately be replaced. Between six and twelve thermocouple channels are loaded in each of the three zones, with at least two channels on each monitored radius. These control thermocouples are not loaded in charge pans containing control rods, and the loading pattern is chosen so that the sense of the quadrantal effect can be determined. Usually each channel contains two thermocouple elements loaded in positions 3–6, with most loaded in the hottest positions 4 or 5.

26. In analysing the fuel element temperatures the obvious first step is to check that no indicated temperature exceeds the limit of 495°C imposed from compatibility considerations (see later section). The average of the thermocouple readings for each zone is obtained and subtracted from the average permitted temperature for the actual reactor power and control rod insertion. This difference must be positive for each zone and its value gives the number of degrees by which the fuel element temperatures (more strictly, the peak transient temperature) can be increased without exceeding the specified probability of ignition in the event of a bottom duct failure.

27. Finally, the measured temperature should not exceed the predicted temperature at full power by more than 25 degC (approx. 3 standard deviations). A difference of this magnitude should occur by chance only once in 40 reactor charges, and could be caused by a partially-blocked channel, an error in loading, or inadequacies in the model used for prediction. If this limit is reached, a technical reappraisal is made of the transient temperature distribution, with corrections if necessary to take account of the contribution of these channels to the overall probability. If, on the other hand, a predicted temperature exceeds the measured temperature by more than 25 degC (which may well be due to a faulty thermocouple), this thermocouple is simply excluded from assessments of whether the criterion is satisfied.

IMPLICATIONS OF THE TEMPERATURE CRITERION

28. Fuel element temperature criteria cannot be applied without consideration of the consequences on the performance of all reactor components.

Fuel

29. Maximum can temperatures based on transient considerations could, in principle, range from values of about 440°C at the centre of the reactor to about 500°C towards the periphery, with corresponding central uranium temperatures of about 560°C throughout the reactor, i.e. 100 degC below the α–β transition temperature. Although the limitation imposed by outlet temperature and the practical difficulties of increasing can temperatures at the periphery of the reactor reduces the can temperature range to 400–480°C, this represents a substantial increase over the design values referred to in the introduction. Possible temperature-induced changes in factors such as the creep ductility, grain instability, creep rates, and fatigue resistance of magnox, and creep properties and swelling of uranium are reflected only in the economics of reactor operation and not in the safety of operation. The main development programme carried out by the AEA to ensure the endurance of fuel elements for the civil nuclear power stations has been reported,[9] and the conclusions apply to fuel operating under conditions allowed by the new criterion in Calder and Chapelcross reactors.

30. There is no possibility of interaction between the magnox can and uranium at the increased operating temperatures since the two materials are compatible at all temperatures in the solid phase.[10] Resistance to oxidation of magnox in carbon dioxide is satisfactory up to about 550°C and magnox and graphite are compatible at all temperatures in the range likely to be experienced in the reactor. With increase in temperature, reduction in creep resistance of magnox AL80 could lead to fin deformation in the flow of coolant gas. However, examination of a number of thermocouple elements which had operated for several months at temperatures of about 480°C showed no sign of increased fin distortion on the elements.

31. A possible compatibility problem on thermocouple elements is the reaction between magnox and the stainless steel of the thermocouple sheath or of the binding wire. Laboratory tests have shown that no reaction occurs at 500°C provided that the surface layer on the thermocouple sheath is not removed, but that magnesium/nickel compounds form in the magnox at higher temperatures. It is considered that there is no magnox/stainless steel incompatibility which is likely to hazard the reactor at temperatures of up to 500°C, and, allowing 5 degC for possible error in instrumentation, the maximum operating temperature has been specified as 495°C.

32. A reaction between a magnox can and an incompatible material would cause a fuel element failure characterized

by a rapidly rising signal on the burst cartridge detection gear. The BCDG signal would increase at a rate of several thousand counts per hour and early shut-down of the reactor would be necessary. There would be an economic penalty in this occurrence, but no serious safety problem, since the defect in the can would be localized and there would be insufficient oxidation of the surface of the uranium bar to reduce its mechanical strength significantly, or to cause high uranium temperatures.

Pressure Vessel

33. The ideal radial temperature distribution for the original criterion was that attempted in the design, i.e. uniform. Difficulties in matching flux and flow and the discrete nature of both the gagging and absorber patterns caused considerable variations from this ideal distribution. Since the new temperature criterion is based upon a probability of exceeding the magnox ignition temperature following a reactor fault, and because the transient rise varies linearly with channel power, then if it is desired to have an equal probability of ignition for each channel in the reactor, the ideal steady state temperature distribution should be inversely related to the channel power distribution provided that no other limitations apply. The original unflattened flux distribution was adjusted to one which would more nearly comply with this ideal using the same gagging pattern. The resultant thermal output of the reactor was higher than that obtained with the original criterion, and in fact output was limited by outlet temperature. The limit on gas outlet temperature (345°C) is in reality a limit on the pressure vessel based on stress rupture considerations.

Graphite

34. A consequence of the increased fuel element temperatures and reactor power following the introduction of the new temperature criterion is an increase in temperature of the graphite moderator. At the graphite temperatures of up to about 400°C encountered in normal operation of the Calder and Chapelcross reactors the graphite–carbon dioxide reaction is of negligible importance. Limiting graphite temperatures are set by the need to prevent oxidative runaway of the graphite following circuit failure with substantial ingress of air. The severity of this long-term temperature transient is mainly dependent on the air-reaction rate of the graphite, and the increase of about 10 degC maximum graphite temperature when the new temperature criterion was introduced makes only a small reduction in the time available for restoration of main blowers to control the temperature transient.

CONCLUSIONS

35. The introduction of a temperature control criterion based on a limitation against ignition of magnox following failure of a bottom duct has contributed to the improved performance of the Calder and Chapelcross reactors during the past two years. Although the maximum possible increase in thermal power has not been achieved, because the operating limit on the outlet duct temperature has been reached well before the limit on fuel element temperatures, the margin in hand on fuel temperatures has enabled the reactor power to be maintained at a consistently high value for much longer periods before absorber retrims are necessary. This has given a direct improvement in load factor.

36. Improved knowledge of the temperature distribution in the reactor, based on many experimental measurements, and on computer calculations of temperatures in uninstrumented channels under steady state and fault transient conditions, leads to greater confidence in operation. Similar calculations are performed for the many experimental fuel elements being irradiated in the Calder and Chapelcross reactors as part of the AEA fuel element development programme. A satisfactory safety analysis for some of these advanced designs operating at high temperatures and ratings would not be possible without the application of the temperature control criterion described in this Paper.

37. Potentially the maximum permitted fuel element temperatures could be increased further if required. Reasonably pessimistic parameters have been used in the transient calculations. Not only does the model assume the particular form of fracture and combination of blocked duct to give stagnation of coolant in the core, but the assumption is made that the fastest acting trip is inoperative. Other pessimistic assumptions are made in the treatment of trip delay times, fission product heating, thermal capacity, axial conduction of heat in the element and in the calculation of emissivity of the can. Further refinements in the philosophy of this type of temperature criterion are discussed by Leslie.[11]

ACKNOWLEDGEMENTS

38. The Authors are grateful to Dr J. M. Hill, Managing Director, Production Group, UKAEA, for permission to publish the Paper. Several of their colleagues are associated in the work described and the contributions of Messrs D. S. Briggs, P. B. Fay, and N. J. Brow, in particular, are acknowledged.

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4 Studies of temperature transients in Calder and Chapelcross reactors following assumed depressurization

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This Paper is the fourth of five to be presented at a half-day symposium of the Society on 11 November

In order to specify permissible operating temperatures for fuel elements in a magnox reactor, a knowledge is required of the transient suffered in the worst credible fault. Because of safety and technical considerations the information which can be obtained from direct experiment is both limited and costly. Theoretical predictions are therefore necessary, moreover the complexity of the problem almost demands computer study. However, such studies in addition to providing the required answer (usually based on pessimistic assumptions) demonstrate the relative importance of uncertainties in items of input data and indicate those areas in which judicious (and perhaps indirect) experiment might be profitable.

INTRODUCTION

THIS Paper investigates the fuel element can temperature excursions following a bottom (cold) duct fracture. In such a fault it is assumed that the reactor at power suffers a rapid depressurization with drastic reduction in core coolant flow. The reactor is shut down by tripping the control rods after a delay which is characteristic of the safety circuits. With the reduction in coolant flow, fuel element temperatures rise to reach a maximum determined by the rate of heat loss by radiation and convection and the falling rate of heat input from delayed neutron and fission product sources. Fig. 4, which is discussed fully below, indicates the form of such a temperature transient based on a coolant flow–time relationship postulated by Wall.[1] Attention is focused exclusively on the short-term transient which occurs within minutes of depressurization. The long-term transient, which occurs on a time scale of hours being controlled primarily by graphite oxidation, is not considered, although it may occur for some of the assumed conditions.

2. For various reasons, mainly historical and economic, the investigations involved the use of three computer programmes, two digital, the other analogue. Only the main aspects of these are described here. Emphasis is placed on the demonstration of the consequences of varying temperature sensitive parameters, rather than of the methods of calculation.

DESCRIPTION OF COMPUTER PROGRAMMES

Stab

3. This programme, written for the IBM 704 computer was used in its two-dimensional (r, z) form as described by Curtis *et al.*,[2] but with modifications to incorporate

(*a*) fission product heating, with a single time constant to define decay, and

(*b*) relinearization, after each time step, of the radiation coefficient determining radiative heat transfer from the can to moderator.

Ten radial and seventeen axial mesh points were used to give spatial representation. At each axial mesh point a can, two fuel, and three moderator regions were represented in the seven equations governing heat generation and transfer. In the neutronics a single prompt and six delayed neutron groups are treated to achieve reactor kinetics.

Fidax

4. Written for the Mercury computer, specifically to consider depressurization faults, FIDAX is a one-dimensional axial reactor model developed by the GEC[3] which solves neutron flux, heat transfer, xenon, etc., equations for two reactor channels. One channel represents the reactor radially averaged conditions and the other is available as a highest rated or flattened region channel. Fission product heating is assumed to depend only on the initial flux profile and decay is represented by four groups. Fission heat generated during the trip period is calculated using point neutron kinetics with two delayed neutron groups. At each mesh point a can, two fuel, and two moderator regions are considered in the heat transfer equations. Ten axial mesh points were used in the FIDAX work discussed below.

The one-dimensional analogue

5. This programme written for the PACE computer was used in the form described by Wilson and Proctor.[4] A fuel, a can, and two moderator regions are treated in the equations for heat generation and transfer. Fission product heating is determined by two groups whilst the neutronics gives an approximation to axial kinetics, with two delayed neutron groups. Five axial mesh points were used to simulate channel behaviour. The majority of the investigations were carried out on the analogue model by making perturbations to a standard set of assumed data. This standard set of data is given in Table 1. (The STAB results, without exception, were obtained using the quoted coolant flow–time relationship.) Of the three models only FIDAX is capable of considering the long-term transient, although this facility was not required for the work described in the present Paper. In all three models axial heat transfer in the solids is ignored.

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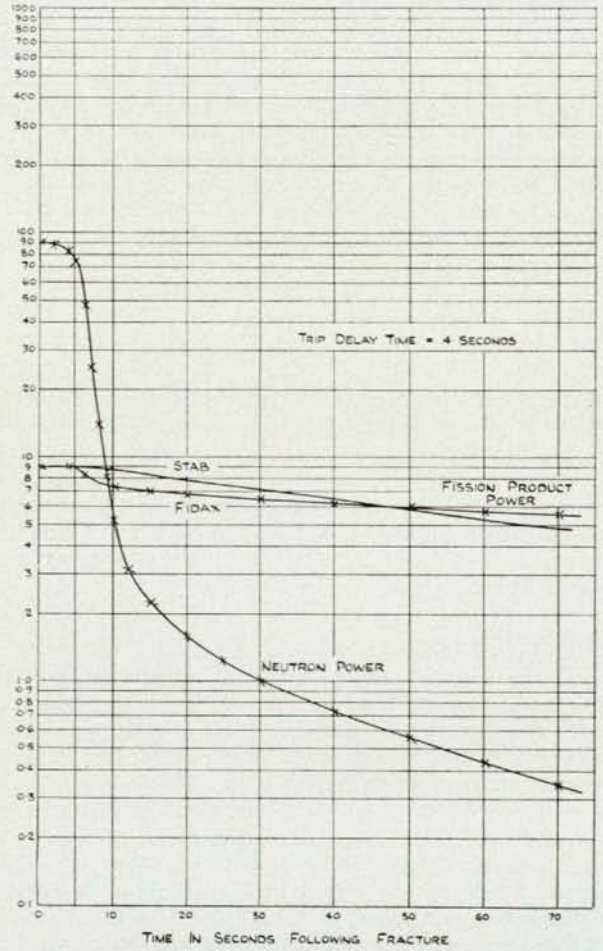
Table 1: *Standard data for analogue calculations*

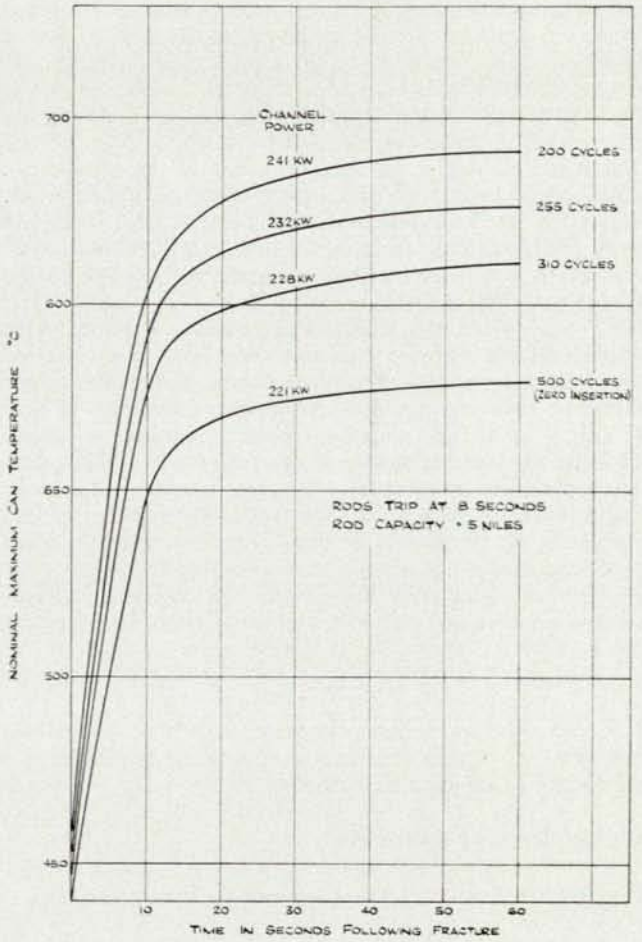
Fuel temperature coefficient, −1·0 mN/degC
Moderator temperature coefficient (maximum), +2·5 mN/degC
Can emissivity, 0·5
Graphite emissivity, 0·76
Fission product heating (steady state), 0·1 × neutron power
Step change in relative mass flow, 1·0 to 0·005
Rod worth, 6·58 niles
Trip delay time, 8 sec
Mean rating, 2·9 MW/te
Coolant temperature rise, 200 degC
Channel power, 220 kW
Initial flux shape determined by control rods at 0·33 insertion
Full flow to 0·5% in 1 sec with subsequent constant flow

COMPARISON OF THE MODELS

6. In addition to the inherent differences in scale of representation and in the methods of solution of the associated equations, two obvious differences exist between the three models discussed above. The major obvious difference is in the representation of fission product decay heating, for which agreement is, in principle, only possible over a limited period of time. This is demonstrated in Fig. 1 in which the STAB and FIDAX assumed forms of decay heating are compared. By



1—*Variation of power following shut-down*



2—*Typical STAB transient predictions*

virtue of its four-group representation FIDAX attains an almost perfect fit to the UKAEA recommended data, whereas STAB with its single group is relatively high up to 50 sec and low for times exceeding that value. Significant underestimation occurs in STAB for times greater than about 80 sec. The STAB calculations were not taken beyond 60 sec. In the case of the one-dimensional analogue, the fit of the fission product decay heating is closer to that of FIDAX than STAB over the first few minutes. In Fig. 1, the variations with time of the neutron and fission product powers are compared, at a trip delay time of 4 sec. This shows clearly the dominance of the neutron heating at times close to depressurization for which small deviations in the assumed fission product decay heating are apparently tolerable. Such is indicated by Fig. 2, in which a typical set of transients (obtained at a constant flow of 0·5% following an initial flow run-down period of 0·5 sec) demonstrate the relatively small contribution from fission product heating to the magnitude of the temperature excursions. This is confirmed below where the effect on transient behaviour of varying the assumed value of steady state fission product heating is discussed.

7. The remaining obvious difference relates to the representation of control rod capacity to shut-down, and its associated time of insertion. Both the dynamics of the control

rod drop and the control rod calibration were incorporated in the STAB model, thus whatever the steady state degree of insertion from which the control rods were tripped, the correct reactivity was inserted and on the proper time scale. In both FIDAX and the analogue this was not the case; the full reactivity worth was assumed to be inserted on the trip and at the limiting insertion time, whatever the control rod position associated with the assumed steady state flux profile. The effect of such differences is discussed below in the appropriate sections and is shown to be small for the control rod worth and insertion times assumed appropriate to the Calder and Chapelcross reactors.

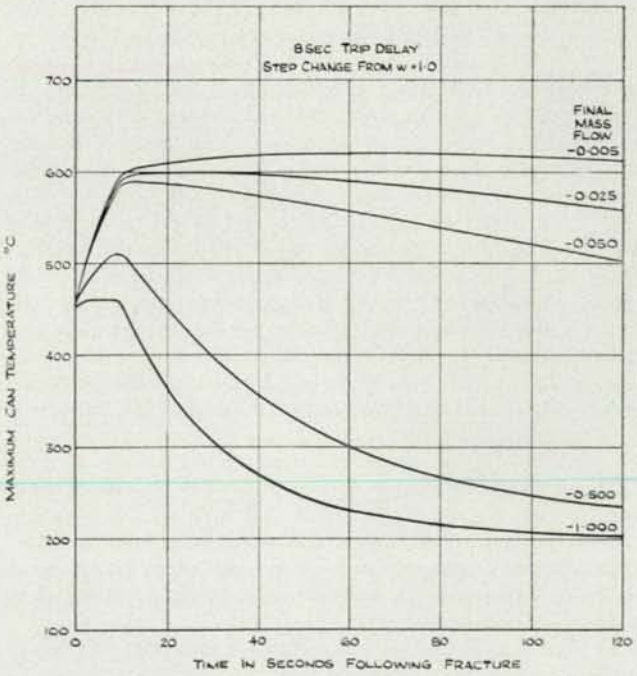
8. For effectively standard conditions the predictive abilities of the three programmes have been compared for times up to 1 min after assumed duct fracture. The greatest difference between predictions in this time interval was 12 degC, associated with an absolute change of about 150 degC in nominal maximum can temperature. The accuracy of the STAB programme is preferred because of the more realistic reactor model assumed, weighted by the previous success of this programme in simulating temperature changes experienced during blower run-down experiments.[5] The equivalence of the three programmes for difference calculations is assumed and demonstrated below where comparisons are available.

RESULTS

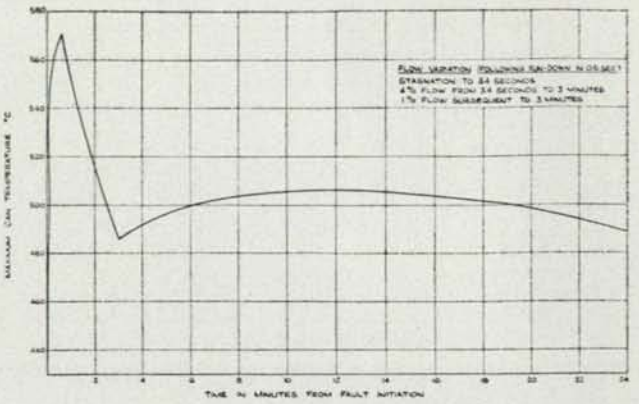
9. This section considers the effects on the temperature transient of varying the values of selected parameters, a number of which are time dependent.

Coolant flow–time relationship

10. The effect of varying the value of the final flow beyond the 1 sec run-down period was determined on the analogue and



3—*Variation in final mass flow*



4—*Effect of substantial flow after stagnation*

shown for reverse flows in Fig. 3. In the range of final relative flows less than 0·005, the change in peak can temperature was found to be independent of the direction (positive or negative) of the flow. Because of limitations in the accuracy of the null position of the servo-multipliers in the flow calculations of the analogue, the effect of continued flow stagnation beyond the linear run-down period was examined in three FIDAX runs, assuming the data of Table 1. For flows of 0·5% and 1% full flow, reductions in the transient rise by 5 degC and 9 degC respectively were found relative to the value for stagnation. (Here and subsequently, the transient rise is taken to be the change in nominal maximum can temperature between the steady state and transient conditions. The transient rise does not necessarily refer to a fixed axial location in the channel.)

11. Wall[1] has shown that for assumed conditions of duct fracture, an initial run-down from full to zero flow in 0·5 sec would be followed by core flow stagnation for a maximum period of about 34 sec. The flow would then rise to about 10% of the full flow value, consistent with the assumption that the circulators remain at full speed. In fact the circulators must decelerate, the flow falling ultimately to that value (about 1%) delivered by three pony motors. There is considerable difficulty in assessing circulator run-down and flow behaviour under the conditions of the assumed fault. As an approximation, it is assumed that following the period of stagnation, 4% of the full flow exists for 3 min, falling immediately to and thereafter remaining at 1%. The effect of imposing this flow relationship, investigated on FIDAX and shown in Fig. 4, is to limit the transient rise associated with continued flow stagnation to its value at 34 sec.

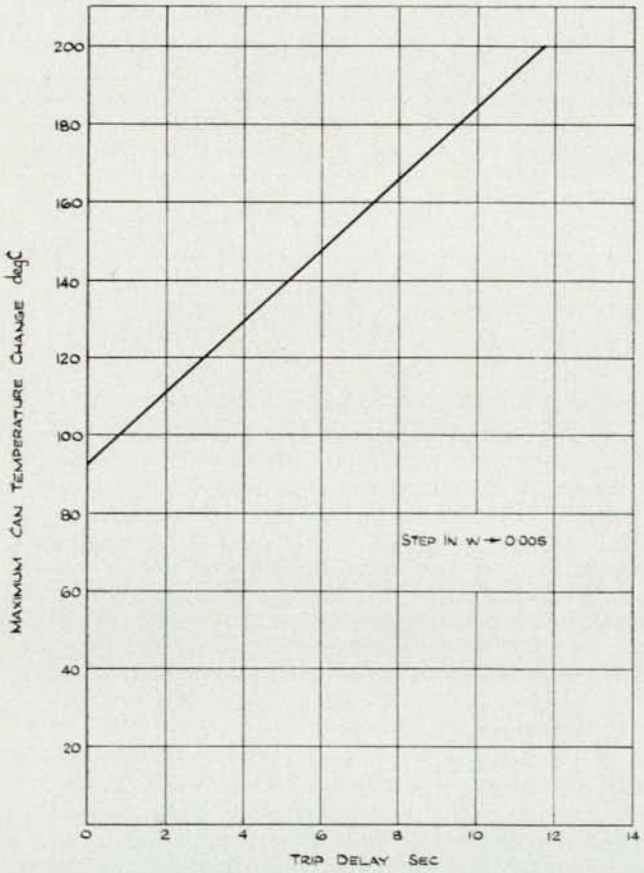
Trip delay time

12. The variation of transient rise with trip delay time was examined on the analogue, the results being depicted in Fig. 5. The linearity of the transient rise with trip delay time has been confirmed using FIDAX. Moreover, for fuel elements markedly different from Calder's and for peak ratings in excess of 10 MW/te (associated with enrichment), the same behaviour was observed. The slope was found to be proportional to the peak point rating in the channel, and for Calder elements 2 degC per second trip delay per MW/te. In Table 2 the details of a number of STAB runs are given, runs 2 and 5, and 6 and 8 respectively differing only in trip delay time. The differences in transient rise between these runs with respect to

Table 2: *STAB results for the centre channel*

Run No.	Rod insertion *	Rod insertion (cycles)	Trip delay (sec)	Rod worth %	Can weight (g/cm)	Final nominal peak can temp. † (°C)	Rise in nominal peak can temp. (degC)	Axial position of nominal peak can temp. Initial‡	Axial position of nominal peak can temp. Final‡	Channel power (kW)	Nominal max. rating§ (MW/te)	Axial position of max. rating‡	Other details
1	0	500	8	5·0	10·94	580	144	0·75	0·56	221	3·74	0·50	Unsleeved
2	0·36	310	8	5·0	10·94	612	165	0·50	0·44	228	4·40	0·38	,,
3	0·46	255	8	5·0	10·94	628	175	0·44	0·38	232	4·72	0·31	,,
4	0·57	200	8	5·0	10·94	643	185	0·38	0·31	239	4·95	0·31	,,
5	0·36	310	4	5·0	10·94	577	130	0·50	0·44	228	4·40	0·38	,,
6	0·57	200	8	6·58	10·94	664	189	0·38	0·31	241	5·38	0·25	,,
7	0·31	332	8	6·58	10·94	610	161	0·56	0·44	228	4·40	0·38	,,
8	0·57	200	4	6·58	10·94	624	149	0·38	0·31	241	5·38	0·25	,,
9	0·57	200	4	6·58	9·75	629	154	0·38	0·31	241	5·38	0·25	,,
10	0·57	200	4	6·58	7·7	638	163	0·38	0·31	241	5·38	0·25	,,
11	0·57	200	4	6·58	5·47	650	175	0·38	0·31	241	5·38	0·25	,,
12	0·36	310	8	5·0	10·94	612	166	0·56	0·44	228	4·39	0·38	Sleeved
13	0·57	200	4	6·58	7·7	634	153	0·44	0·37	257	5·26	0·25	Unsleeved 3000 MWd/te

Notes * Rod insertion is given as proportion of core height from top of fuel.
† In all runs the final nominal peak can temperature occurred in the centre channel.
‡ Axial locations of peak can temperature and maximum rating are given as proportion of core height from bottom of fuel.
§ Heat generated in fuel and moderator per tonne of fuel.



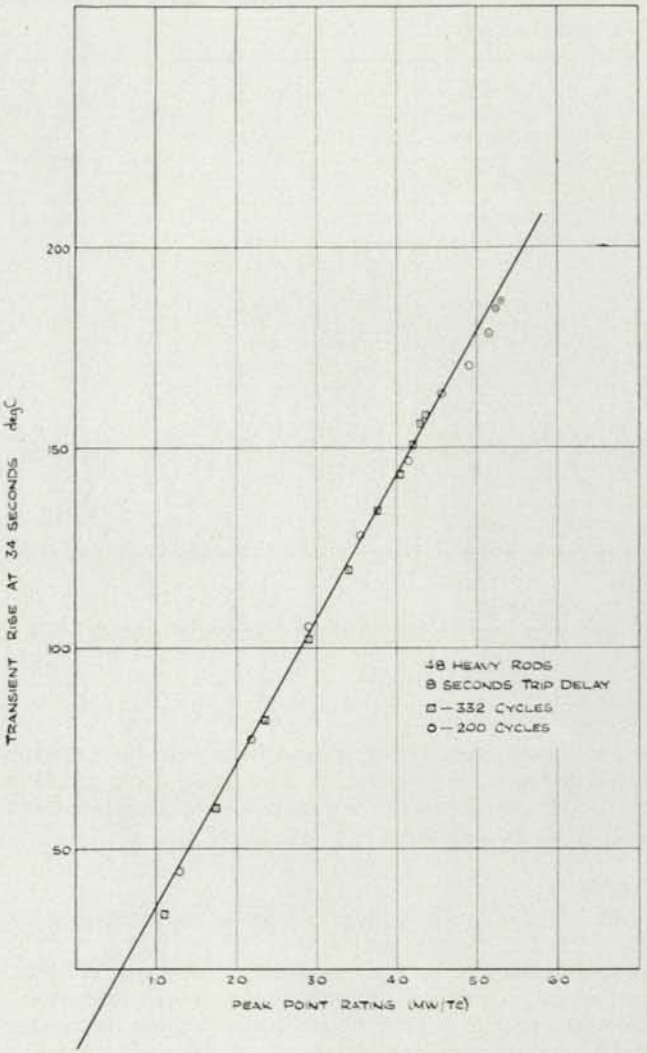
5—*Variation of trip delay*

trip delay and peak rating are consistent with the analogue result of Fig. 5. The method for determining the appropriate trip delay times for use on the Calder and Chapelcross reactors is given in the Appendix.

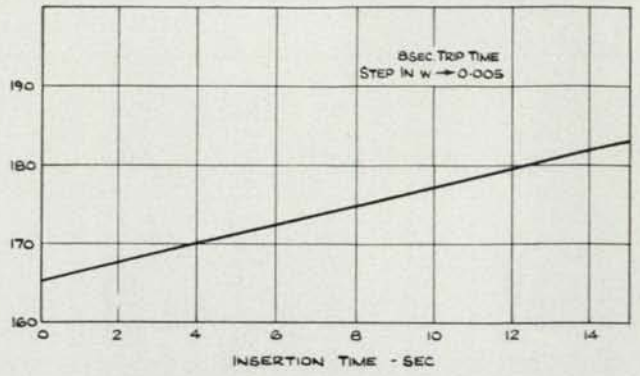
Rating

13. Undoubtedly the most surprising result obtained in the series of investigations is that shown in Fig. 6. The proportionality of the STAB predicted transient rise, at 34 sec, to the peak rating in the channel is shown for two distorted axial flux profiles at each of ten channels along the reactor radius. It must be emphasized that the position of peak rating did not coincide with the position of maximum can temperature either in the steady state or in the transient condition. Table 2 gives the relative positions of these maxima, the transient data here referring to values at 60 sec. The proportionality of peak rating and the transient rise at 34 sec is not a direct consequence of the dominance of heat input over heat losses in that period. Had this been the case, proportionality of temperature change with rating would have resulted at each axial mesh point; this was certainly not so. Moreover, FIDAX calculations have shown that even when neutron power is negligible and heat loss is comparable with heat input, the proportionality still exists.

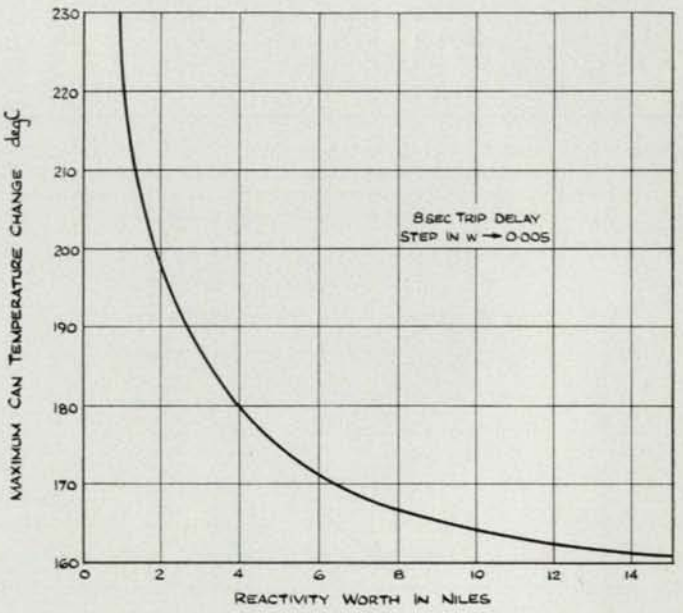
14. The channel conditions to which the results of Fig. 6 apply cover the range of Stanton number, mass flow, and channel power normally met. Analogue predictions of the effect of varying these quantities separately are substantially in agreement with the STAB results. The results shown in Fig. 6 were found to apply to all axial flux profiles, and so it follows that the transient rise is inversely proportional to the axial form factor α_z. This result has proved most convenient in practical application, the variation of α_z with control rod position being well known.



6—*Variation of peak point rating*



7—*Variation of control rod insertion time*



8—*Variation of rod worth*

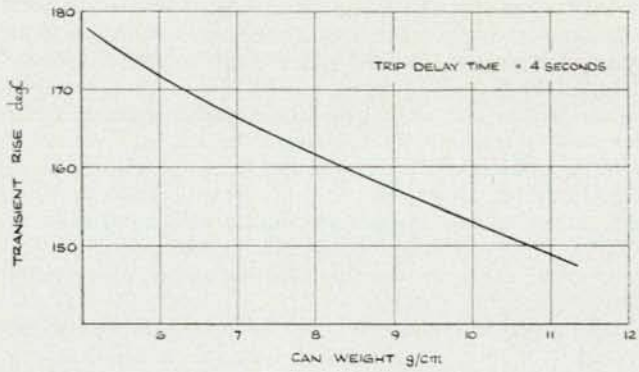
Control rods

15. The variation of transient rise with rod insertion time, obtained from an analogue study, is shown in Fig. 7. The assumed rod worth was 6·58 niles and the trip delay time 8 sec. For a trip delay time of 3 sec this is equivalent to about 0·5 degC per second insertion time at normal channel ratings. With the strict control exercised through control rod drop tests to ensure acceptable insertion times, such a variation is of no practical consequence. The analogue was also used to investigate the variation of transient rise with control rod worth, a constant axial flux shape being assumed. The results are shown in Fig. 8 for an 8 sec trip delay time, indicating a change of 1·3 degC per nile at a worth of 10 niles and 4·4 degC at a worth of 5 niles. There is a difference of 4 degC in transient rise between STAB runs 1 and 7 of Table 2. These have the same peak rating but total rod worths of 5 and 6·58 niles and rod positions of 310 and 332 cycles respectively. The corresponding residual capacities to shutdown are 4·3 and 5·7 niles respectively. From Fig. 8 the analogue prediction of the temperature difference for these capacities is 5 degC, agreeing well with the STAB result.

16. Correcting the data of Fig. 8 to a 3 sec trip delay time, for control rods fully withdrawn the reduction in transient rise for a change in capacity of 6·58 to 5·7 niles is 1 degC. The corresponding difference for the respective residual capacities with rods at 200 cycles (0·57 insertion) is 3 degC. Account is taken of this difference and its variation in the application of the transient predictions. With one exception rod patterns on the Calder and Chapelcross reactors are identical, 48 rods holding 6·58 niles. The exception, Chapelcross reactor 4, has a 40 rod pattern worth 5·7 niles.

Can thermal capacity

17. The variation of transient rise with can thermal capacity was determined on STAB in runs 8 to 11 inclusive of Table 2. The results are depicted in Fig. 9, showing a nearly linear relationship, the slope being about 4 degC/g cm at a peak rating of 5·38 MW/te. Reducing the can thermal capacity increases the transient rise, through two effects.



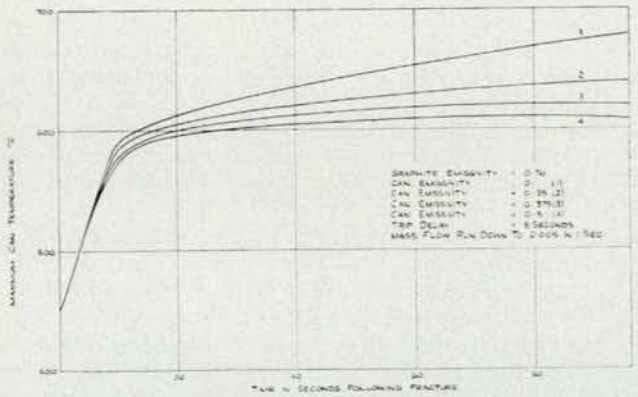
9—*Variation of can thermal capacity*

First, the consequent overall reduction in thermal capacity increases the rate of temperature rise of the fuel element assembly. Secondly, and apparently more importantly, the steady state mean assembly temperature increases relative to that of the can. (The steady state uranium and can temperatures of course do not change.) Although the respective weights of uranium and can are markedly different, the thermal capacities are comparable. Relaxation of the steady state temperature is a significant component of the transient rise; for example, at a rating of 5 MW/te even if heat generation subsequent to the instant of fracture could be ignored, a transient rise of about 35 degC would result. The value 7·7 g/cm in Table 2 corresponds to a bare Calder can with no end fittings or brace attachments. This value is assumed in the calculation of transient temperatures for reactor control purposes.

18. The specific heats of magnox and uranium both increase with temperature. However, in none of the three computer programmes used was the thermal capacity of the fuel element assembly allowed to vary during the transient calculation, steady state values being assumed. The effect of this is to overestimate the transient rise by about 4 degC.

Thermal radiation coefficient

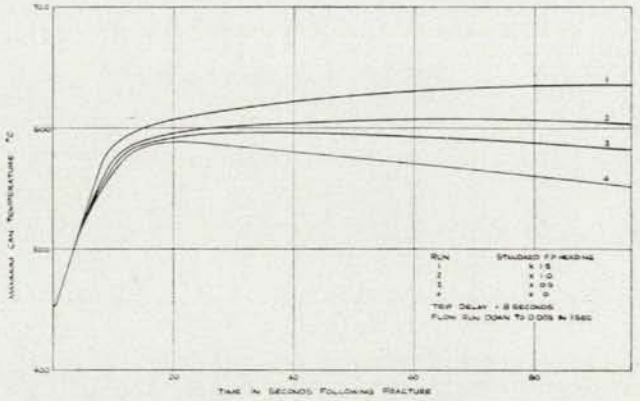
19. The graphite emissivity assumed in all the transient calculations is 0·76. The effect of varying the can emissivity in the range zero to 0·5 was examined on the analogue, the results being shown in Fig. 10. Reducing the emissivity from 0·5 to zero at a peak rating of 4·4 MW/te increases the transient by 28 degC at 34 sec, and 44 degC at 60 sec for a trip delay time of 8 sec. The corresponding figures corrected to a trip delay time of 3 sec are 11 degC and 17 degC respectively. The emissivity assumed for a bare Calder can is 0·5, implying a material emissivity of 0·18. The increase effected by the brace assembly is ignored. Except in this particular analogue investigation of the effect of varying can emissivity, a value of 0·5 was assumed in the transient calculations.



10—*Variation of can emissivity*

Fission product heating

20. The value for steady state fission product heating currently recommended for safety calculations is 9·1% of the total heating. The consequences of varying the assumed value in the range zero to about 14% were determined on the analogue, the results being shown in Fig. 11. Increasing the assumed fission product heating from zero to 9·1% increases the transient rise by 12 degC at 34 sec and by 17 degC at 60 sec, for a trip delay time of 8 sec. Corrected to a trip delay time of 3 sec, the corresponding figures are 5 degC and 7 degC respectively. It is apparent from these values and the form of Fig. 11 that for the purpose of determining transient temperatures within a few minutes of depressurization the representation of fission product heating is not critical.



11—*Variation of fission product heating*

Irradiation

21. The effect of increasing fuel irradiation was examined on STAB and is shown in the differences between run 10 and run 13 of Table 2. Run 13 is effectively a repeat of run 10 but at assumed conditions associated with an irradiation of 3000 MWd/te. The significant changes in parameters with respect to those of zero irradiation were in the fuel temperature coefficient from −1·0 to −0·4 mN/degC, in mean moderator temperature coefficient from −3 to 17 mN/degC and in delayed neutron fraction from 0·7% to 0·429%. The effect of the increased moderator temperature coefficient is on the steady state conditions only, reducing the ratio of peak to mean rating by about 8%. The increased (less negative) fuel coefficient must tend to increase temperature excursions, whereas the reduced delayed neutron fraction must have the opposite effect by increasing the rapidity of the drop in power following shut-down. Standardized to a peak rating of 5·8 MW/te, the transient rise is 7 degC lower at 3000 MWd/te than at zero irradiation, indicating the dominance of the

reduced delayed neutron fraction. Standardized to constant channel power, the reduction in transient rise is considerably greater, being 18 degC at 220 kW, because of the reduced steady state ratio of peak to mean rating.

22. A reduced thermal conductivity of uranium at high irradiation was not included in the STAB investigation. The effect of this is to increase, in the steady state condition, the mean fuel element assembly temperature relative to that of the can, thereby increasing the transient rise in a 220 kW channel by about 3 degC. This is more than offset by the reductions discussed above.

APPLICATION OF TRANSIENT CALCULATIONS

23. Accepting the results of the STAB calculations in Table 2, and in particular those shown also in Fig. 6, corrections have been applied to determine the transient rise for Calder fuel elements and its variation with control rod insertion, at a constant channel power of 220 kW. The corrected data relate to assumed conditions of bottom duct fracture, the more important of which have been discussed above, under their respective headings.

Table 3: *Transient rise for Calder fuel elements for a 220 kW channel*

Control rod position		Trip delay time (sec)	Transient rise (degC)
*Cycles	Relative insertion		
500	0	3·62	122
450	0·1	3·59	122
400	0·19	3·53	125
350	0·28	3·45	132
300	0·38	3·36	140
250	0·47	3·30	147
200	0·57	3·30	152

* A cycle is a local unit of control rod insertion.

For example, Table 3 shows the corrected figures corresponding to the trip delay data of Table 4 of the Appendix. The initial rate of rise of can temperature, α, associated with channels housing thermocouples serving the Bailey trip amplifiers, was allowed to vary with control rod insertion, the channel power being kept constant at an assumed maximum of 180 kW. It is this variation of α which gives rise (see Appendix) to the varying trip delay time of Table 3. The transient rise figures of Table 3 refer to a channel power of 220 kW and for other channel powers the appropriate transient rises are found by direct proportionality. The transient rise figures of Table 3 may be taken to apply equally to sleeved or unsleeved reactors since the computed difference (see STAB runs 2 and 12 of Table 2) was 1 degC for a trip delay time of 8 sec.

APPENDIX

Calculation of the trip delay time

24. The trip delay time is defined as the time interval between fracture and loss of electrical supply at the control rod mechanisms. Trips can be initiated by a number of independent sensors, and it is assumed that the appropriate trip delay time to use in safety calculations is the second most swiftly acting one. The minimum trip delay time is that for a trip initiated by rate of change of coolant pressure, which for the assumed conditions of duct fracture would not exceed 2·1 sec. The second shortest trip delay time is that determined by the Bailey temperature trip amplifiers. The components of the delay time associated with such a trip are: a variable part, t, being a function of τ_1, τ_2, the can thermocouple and Bailey amplifier input circuit time constants respectively; α, the initial rate of rise of can temperature; and a fixed part, being the sum of t_1, the relay drop out time of the Bailey amplifier output circuit, and t_2, the response time of the safety circuit guard line.

25. Since the initial rate of rise of can temperature shown in Fig. 2 may be taken as linear, the variable part t may be defined by the equation

$$\gamma = \alpha\left[t-(\tau_1+\tau_2)\ (1-e^{-t/\tau_2})+\frac{\tau_1^2}{\tau_1-\tau_2}\ (e^{-t/\tau_1}-e^{-t/\tau_2})\right]$$

where γ is the temperature margin to trip of the Bailey amplifiers. The total trip delay time t_3 is then given by

$$t_3 = t + t_1 + t_2.$$

For example, assuming the data of Table 4, the trip delay time is 3·62 sec. The value of α, dictated by the steady state heat rating adjacent to the thermocouples serving the Bailey amplifiers, was shown from STAB results to be about 4·5 degC/sec per MW/te.

Table 4: *Trip delay data for an excess can temperature trip*

Bailey amplifier margin to trip, γ	10 degC
Initial rate of rise of can temperature, α	13·7 degC/sec
Thermocouple time constant, τ	2 sec
Bailey amplifier input circuit time constant, τ	0·4 sec
Bailey amplifier output circuit response time, t_1	0·17 sec
Safety circuit guard line response time, t_2	1·05 sec

Increasing the margin to trip of the Bailey amplifiers from 10 degC to 15 degC increases the trip delay time by about 0·5 sec.

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5 Chance of a channel fire immediately following depressurization of a Stage 1 reactor

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This Paper is the last of five to be presented at a half-day symposium of the Society on 11 November

Consideration is given to the extension of the probability approach as applied to the primary transient following a circuit breach on a Stage 1 reactor. It is shown that a considerable factor of pessimism can be introduced by taking parameters with 'extreme' or 'boundary' values. The suggestion is made that Stage 1 reactors be operated in such a way that there is a 0·1 probability for a channel fire using mean parameter values with associated random errors and assigning equal probability to the size of fracture.

INTRODUCTION

ONE of the accidents considered in assessing reactor safety is that of rapid depressurization due to a circuit breach. Current reactor safety assessments call for an estimate of the likelihood of a channel fire immediately after the breach[1] either by an appeal to intuition or by determination of the probability. The intuitive approach employs subjective judgement in fixing parameter values affecting the temperature transient; the second approach assumes a random error associated with some of the parameters whilst giving intuitive values to others.

2. In the initial stages of reactor development the appeal to intuition usually involving extreme parameter values is no doubt the most prudent course. As the reactor system becomes better understood it is logical to refine these techniques, as has been done for the Calder and Chapelcross reactors[1] where the probability of a reactor fire is deduced. Further development is possible and it is with this that this Paper is concerned.

Major parameters affecting the chance of magnox ignition

3. The main parameters concerned are: core flow, trip delay time, trip sensor error, value of negative reactivity insertion, emissivity, can ignition temperature, fission product power, fuel element initial temperature, fuel element power, uranium specific heat, uranium conductivity, fuel-to-can conductivity, magnox thermal capacity, graphite temperature, graphite conductivity, and graphite specific heat. Associated with each parameter is an uncertainty about its value; the intuitive approach when fixing parameter values uses 'extreme' values. Typically on fission product heating some 30% is added to the mean, 10% being taken as the standard deviation; obviously this addition to the mean depends on the individual's judgement, some may put on more, others less. A system apparently safe with say a 20% addition to the mean fission product heat value could be transformed into an apparently unsafe one by the choice of a 30% addition. Similar observations can be made on the other parameters.

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COMBINATION OF RANDOM ERROR ON THE PARAMETERS

4. Before considering the above it is worth while following through the probability approach in the case where only one parameter has a random error. For the purpose of illustration we suppose that this particular parameter is the initial magnox temperature, the mean value being θ and the standard deviation σ_θ. For the particular fuel element in the channel we calculate the transient temperature rise θ_t. The number of standard deviations n when added to the sum of θ and θ_t to just reach the magnox ignition temperature is then determined, hence the probability p of this fuel element igniting can be found.

5. Turning now to the combination of the parameters we shall suppose that mean values and their standard deviation can be assigned to each of them apart from the core flow which will be dealt with separately. In the case of the insertion of control rods we assume a binomial distribution associated with non-entry of some of the control rods, and evaluate the standard deviation. Pessimistically, the loss of reactivity could be linked with those rods controlling the maximum amount of reactivity.

6. The transient rise is now calculated using the mean parameter values; one parameter at a time is changed in value and the transient is again evaluated, hence the change in the transient rise may be found as a function of the standard deviation for each parameter; thus the combined standard deviation for all the parameters can be simply evaluated if the assumption is made that the effect of each parameter is independent. This is considered further in the Appendix.

7. To illustrate the approach, it is proposed to consider a single channel in a hypothetical reactor where some of the parameters have been treated as suggested above, the other parameters having been assigned 'extreme' values.

Can starting temperature, 475°C (we assume that only one fuel element need be considered in the channel, the contribution to the probability from other elements being small)

Mean magnox ignition temperature, 640°C

Transient rise with mean parameters, 100 degC (calculated on the basis of the critical hole size)

Change in transient rise due to one standard deviation change in fission product power, 5 degC

Table 1

Hole size	Sheath temp. rise (degC)	Temp. rise +500 (°C)	640–(temp. rise + 500) (°C)	n	p
5–5·5	64	564	76	5·06	
5·5–6·0	75	575	65	4·33	0·000008
6·0–6·5	87·5	587·5	52·5	3·5	0·000232
6·5–7·0	102·5	602·5	37·5	2·5	0·006210
7·0–7·5	116	616	24	1·6	0·054799
7·5–8·0	116	616	24	1·6	0·054799
8·0–8·5	105	605	35	2·33	0·009903
8·5–9·0	94·5	594·5	45·5	3·03	0·001222
9·0–9·5	87	587	53	3·53	0·000208
9·5–10·0	81	581	59	3·93	0·000042
10·0–10·5	77	577	63	4·2	0·000013
10·5–11·0	73	573	67	4·46	0·000008
				Sum	0·127444

Change in transient rise due to one standard deviation change in trip delay, 2·5degC
Change in transient rise due to one standard deviation change in emissivity, 2·5degC
Change in transient rise due to one standard deviation change in fuel power, 5·0degC
Change in transient rise due to one standard deviation in value of negative reactivity insertion, 4·0degC
Standard deviation on starting temperature, 10degC
Standard deviation on magnox ignition temperature, 10degC

From the above the combined standard deviation is 16·7degC. Hence the probability for this particular fuel element igniting can be obtained:

$$100 + 475 + (n \times 16{\cdot}7) = 640.$$
$$n = 3{\cdot}89$$

or
$$p = 0{\cdot}0005.$$

8. It is interesting to compare the result obtained by taking 'extreme' values for the above parameters, except the can starting temperature and the magnox ignition temperature, the 'extreme' value being obtained by the addition of three standard deviations. The transient rise therefore becomes 157degC, and

$$157 + 475 + (n \times 10) = 640$$
$$n = 0{\cdot}8$$
$$p = 0{\cdot}36.$$

Alternatively if 'extreme' values have been obtained by the addition of two standard deviations, the value of p becomes 0·0035. We see therefore that the intuitive approach can give a variety of results and for the case considered introduces undue pessimism.

VARIATION IN TEMPERATURE RISE WITH CORE FLOW

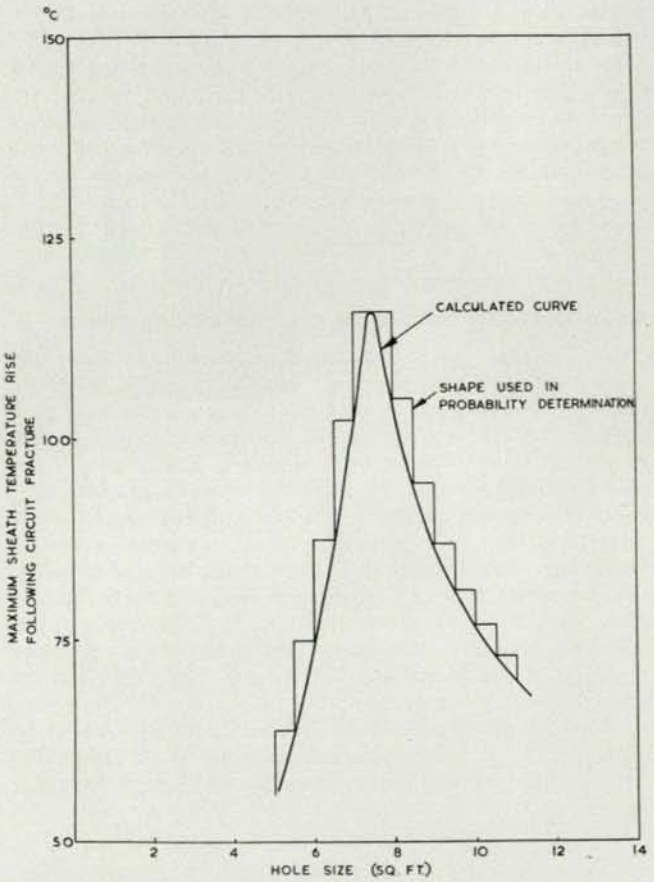
9. Currently assessments are carried out with the assumption that the circuit breach is of such a size as to give stagnation conditions. It is well known that the hole size to give these conditions is highly critical; it is therefore pessimistic not to take account of the possibility that fractures can occur which do not have this particular hole size. A somewhat simplified model has been used in examining how the fuel sheath temperature rise varies with hole size; the model considers the case where a fracture occurs at the connexion of a lower duct to the pressure vessel and supposes that the gas discharge is only from the pressure vessel.[2] Results from such a calculation are shown in Fig. 1.

10. For the particular model used the chance of a channel fire can be found if the probability of a particular hole size is known. In this example it will be assumed that there is an equal chance of any hole size up to the full duct area and that the combined standard deviation associated with the other parameters has a value of 15degC. Half square foot steps will be taken for the hole size and the can starting temperature assumed to be 500°C. Values for the transient rise can be obtained from Fig. 1, and Table 1 drawn up.

Taking the full duct area as 16 sq. ft one obtains a value for the combined probability of 0·004 whereas the corresponding figure with the assumption that all fractures have the critical hole size is 0·055. It is seen therefore that the critical hole approach introduces a fair degree of pessimism.

PERMISSIVE CHANCE OF CAN IGNITION

11. On the Calder and Chapelcross reactors the permissive probability of a channel fire is 0·1. In evaluating these probabilities, extreme values are used for some of the parameters



1—*Maximum sheath temperature rise in a particular channel*

and the breach is assumed to have the critical size; in fact therefore the chance of a fire for the 'as designed' elements is likely to be considerably less than the above figure. Although subjective judgement can be eliminated by using mean parameter values with associated random errors it is still necessary to gauge the permissive chance of can ignition. A factor to be borne in mind besides the human and political problems is the probability of having elements in the core not in the designed state as typified by elements damaged in loading. In the case of these 'rogue' elements, if these ignite regardless of reasonable limitations on power and temperature, there is little point in severely limiting the conditions of operation for the 'as designed' elements. A further consideration is the likelihood of a circuit breach, expert opinion holding the view that this is a highly improbable event. Consideration of all these factors leads to the suggestion that Stage 1 reactors may be permitted to work to a probability of 0·1 for a channel fire using mean parameter values with associated random errors and assigning equal probability to the size of fracture.

APPENDIX

Combination of random errors

12. The transient rise is a function of the parameters on p. 211, or

$$T=f(\epsilon, t, \ldots\ldots)$$

where T is transient rise, ϵ the emissivity, and t the trip delay.

$$dT=\frac{\partial f}{\partial \epsilon}d\epsilon+\frac{\partial f}{\partial t}dt \ldots\ldots$$

$$dT^2=\left(\frac{\partial f}{\partial t}\right)^2 d\epsilon^2+\left(\frac{\partial f}{\partial t}\right)^2 dt^2+2\frac{\partial f}{\partial \epsilon}\cdot\frac{\partial f}{\partial t}d\epsilon\, dt+\ldots\ldots$$

$$\epsilon(dT)^2=\text{variance } T=\left(\frac{\partial f}{\partial \epsilon}\right)^2\text{variance } \epsilon+\left(\frac{\partial f}{\partial t}\right)^2\text{variance } t$$

$$+2\frac{\partial f}{\partial \epsilon}\cdot\frac{\partial f}{\partial t}\text{covariance }(\epsilon, t)\, t \ldots\ldots$$

If the parameters are independent then the last term in the above is zero and therefore,

$$\sigma_T^2=\sigma_\epsilon^2+\sigma_t^2+\ \ldots\ldots$$

where σ_T^2 is standard deviation on transient rise, σ_ϵ the change in transient rise due to one standard deviation change in emissivity, and σ_t the change in transient rise due to one standard deviation change in trip delay.

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Dounreay Experimental Reactor Establishment highly active plutonium examination and reprocessing caves

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Correspondence on this Paper is invited

INTRODUCTION

THE future of the fast reactor project in the UK is closely linked to the use of plutonium as a fuel. The UKAEA Reactor Group has embarked on a major development programme to investigate the behaviour of suitable fuels containing plutonium in the Dounreay fast reactor and to test designs of fuel pins and sub-assemblies.

2. The Dounreay site already includes a set of gamma shielded caves[1] which have operated for more than four years, assisting in the investigation of a wide range of irradiated uranium fuels and other materials. These caves will continue to operate for many more years and are heavily committed. The examination of plutonium fuels, especially in the form of subassemblies, requires a different standard of containment for reasons both technological and concerned with safety. The experience gained from the gamma shielded caves has proved very useful in the design of this new facility.

The handling problem

3. A single sub-assembly consists of a large number of single pins (77 in the first experiment) in close array together with a wrapper and end fittings. Approximately 5 kg of fuel, oxide, carbide, or cermet, are present in each sub-assembly. The fuel in these tests has a power rating of about 200 W/g, so that during operation, a sub-assembly is emitting about 1 MW. Immediately following shut-down of the reactor, this power drops to 6% of the total, due to fission product heating, so that the sub-assembly cannot be removed quickly from its cooling environment. In fact, a period of 50 days' cooling is necessary to reduce this power to below 1 kW. The gamma activity at this stage is of the order of 2×10^5 MeVc.

4. On removal from the reactor the sub-assembly will be coated with sodium–potassium alloy coolant, which will fire on exposure to air or react vigorously with moisture. Transfer and subsequent handling must be achieved under inert conditions.

5. The concentrations of plutonium in the air which workers breathe is stringently controlled. There is a limit of 2×10^{-12} μc/ml which can only be maintained by complete containment of specimens within sealed caves. Many plutonium compounds are chemically active in air or moist gases. To avoid damaging the evidence of the experiments by reactions which could lead to fuel cracking or expansions, a dry inert atmosphere is essential.

EXAMINATION REQUIREMENTS

Metallurgy and engineering

6. The first information required is whether or not the complete assembly has retained its overall and detailed geometry. This will be obtained from direct measurement and from examination of a large number of X-ray photographs taken from many angles. Next, there is the possibility that the sub-assembly has acted as a trap for any impurities present in the liquid metal coolant. Following this, it will be necessary to find out whether any rubbing or corrosion has occurred.

7. Investigation of fuel behaviour will cover many points including fission product gas release; migration of plutonium and fission products, swelling of the fuel, and interactions between the fuel and its canning material, and considerable post-irradiation testing of canning materials.

Chemical cells

8. Some of the pins from an irradiated sub-assembly will not be required for detailed examination. These can be used for chemical reprocessing trials. The fuels being tested are all quite different from those being used in reactors at present so it is necessary to investigate and prove economic and safe routes for the recovering of the plutonium.

9. Finally, as many of the investigations require supporting evidence from chemical analysis, small shielded caves are necessary for sampling and preparation for the analyses. These caves must be free from cross contamination from the other major caves.

DESCRIPTION OF THE FACILITY

10. The basis of design was to provide facilities for the metallurgical, chemical, and analytical examination of irradiated mixed plutonium and uranium fuels, with complete radiological protection against alpha, beta, and gamma radiation, and provision in the Metallurgical Section for all operations to be carried out in an inert atmosphere.

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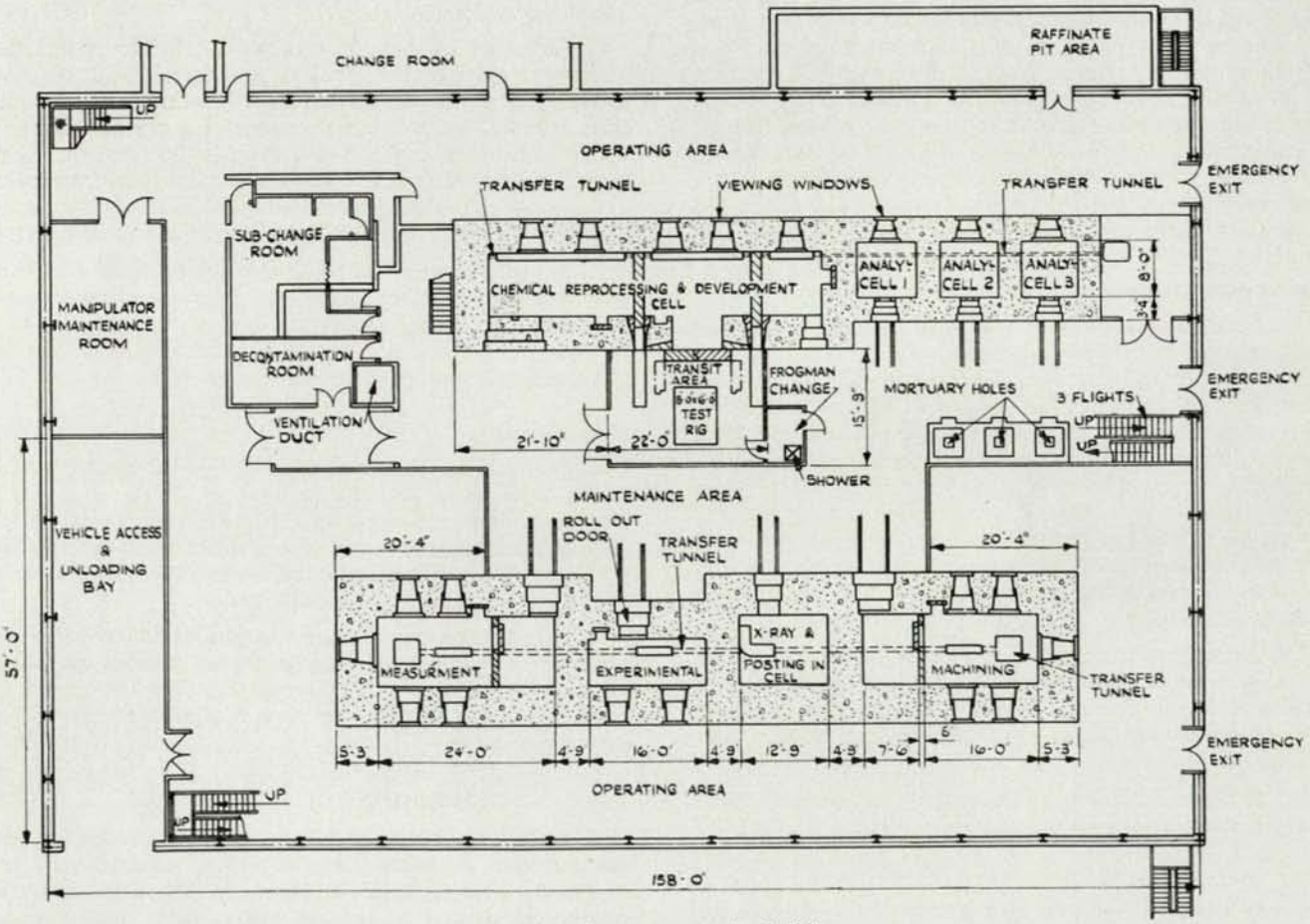
1—*View of main examination building*

11. The complete facility consists of the main examination building and a services annexe adjacent to the access and services corridor of the Chemical and Metallurgical Group of DERE. The main building shown in Fig. 1 accommodates three suites of caves for metallurgical, chemical, and analytical work, and the annexe contains the supporting services peculiar to the main building.

Main building

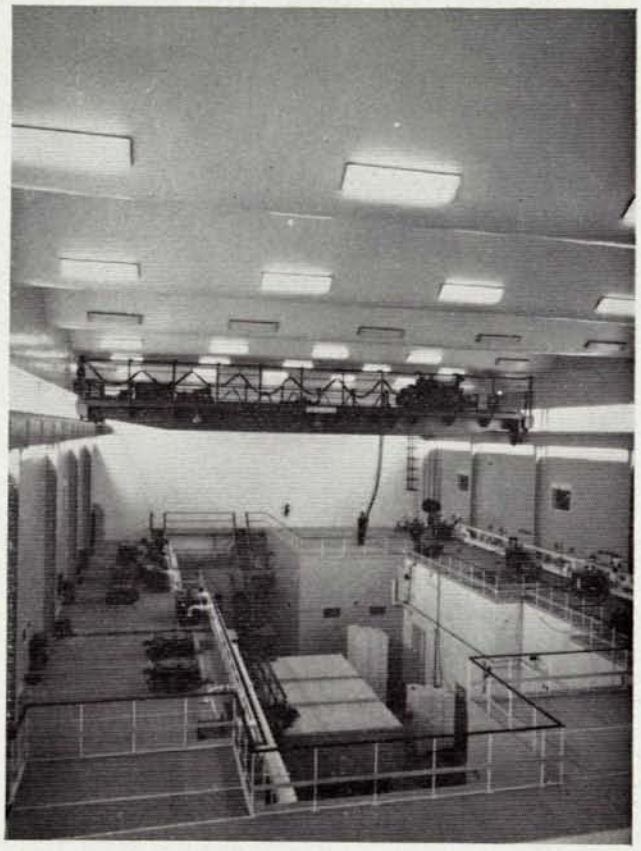
12. The main building, which is 158 ft long, 105 ft wide, and 50 ft high, consists of a steel-framed structure, with the outer walls constructed in 11 in. cavity brickwork which, together with the vermiculite concrete barrel arch roof, is designed to provide secondary containment in the event of an accidental release of plutonium from the cell system which forms the primary containment. The whole of the internal surface in the operating and active areas is treated with a smooth plaster, gloss-painted finish. A particular feature of the barrel arch roof design is the absence of protruding



2—*Plan of main examination building*

structural members, giving a smooth underside which will facilitate cleaning and decontamination.

13. The caves are arranged in two parallel lines as shown in Fig. 2, the space between being utilized for access to the caves and an active maintenance area. Personnel access to the maintenance area is given through a sub-changeroom, and plant access through an airlock. Adjacent to the airlock is an equipment decontamination room. This whole area, including the roof of the caves illustrated in Fig. 3, is served by a 25 ton overhead electric travelling crane. This crane also serves the vehicle loading bay at the west end of the building. A manipulator maintenance bay is also provided at the west end. As shown in Fig. 4, the area between the caves and the outer walls of the building on the north and south sides provides space for the cave operating areas, services corridors, offices and small laboratories.

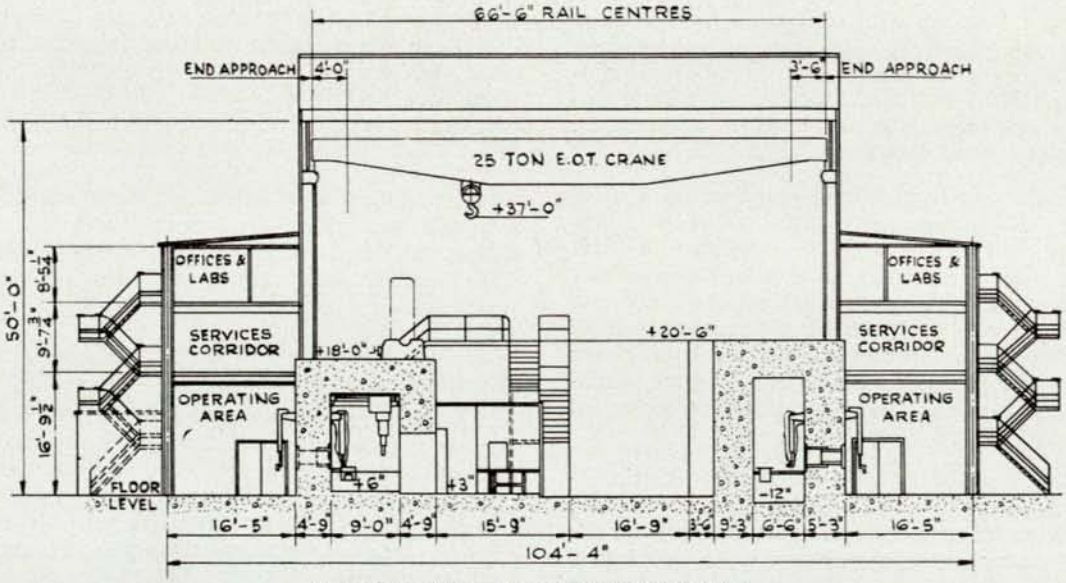


3—*Active maintenance area and cave roofs*

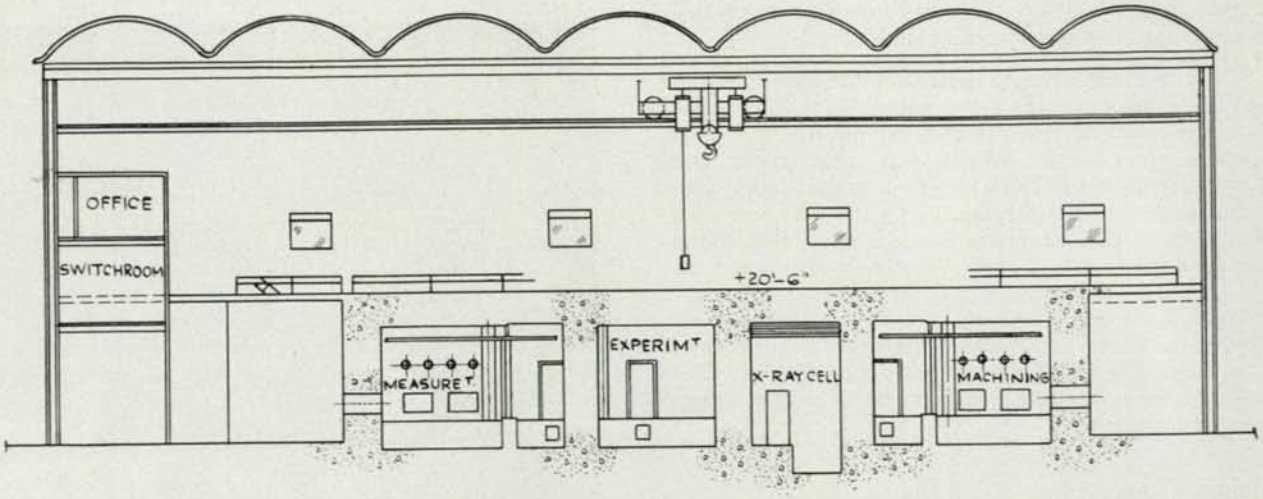
Metallurgical caves

14. The metallurgical suite comprises machining, X-ray, experimental, and measurement caves. A sectioned view through this group of caves is shown in Fig. 5. With the exception of the X-ray cave, all these caves are designed for operation in an argon atmosphere at a pressure of $-\frac{1}{4}$ in. water gauge.

15. The geometry of the caves was evolved, from a consideration of examination requirements, to utilize the reach of standard master slave manipulators, and to avoid blind regions. For these reasons the strong working bench has been made the lower seal of the caves. Furthermore, by placing windows on three sides of the main caves, every position is within reach of the manipulators and in sight of a window. Such a layout economizes in shielding and cell volume.



4—*Cross-section of main examination building*



5—Sectional elevation of main examination building

Machining cave

16. The machining cave consists of a reinforced concrete cell 24 ft long, 10 ft wide, and 16 ft high, with 5 ft 3 in. thick walls. A 1 in. thick steel deck is fitted just below the observation windows, to provide a working bench for direct mounting of machines and equipment. The walls and roof are lined with stainless steel sheet, fully welded, and together with the bench, form a gas-tight cell for the containment of alpha activity and the inert gas atmosphere.

17. Within the cave, a 6 in. thick steel partition and an argomatically operated sliding door are provided, to give an area 7 ft long in which maintenance can be carried out. Access to the cave is achieved by rolling out a concrete shield plug, after which entry can be made through a newt suit port, or by removing a large gas-tight steel panel. This panel is fitted with an observation window. Five viewing windows are provided. These are of lead glass, mounted in epoxy-resin-lined steel boxes filled with zinc bromide. To prevent disturbance of the zinc bromide at high radiation levels, 10 in. ceria-stabilized glass blocks are fitted inside the cave.

18. A gas-tight posting port is mounted on the roof of each cave. All fuel elements and material, which are transported by flask, will be introduced into the caves by this means. In the base of each cave, a lead-shielded mortuary hole is provided for convenient storage of a fuel element, and provision is made for cooling the element if required. Sodium lighting is provided, at both roof and window level. Power and instrument plug and socket connexions are fitted at suitable points on the bench, and these and other services are fed through the cave wall below bench level.

19. The cave is served by a 750 lb capacity electrically-operated power manipulator and a 3300 lb capacity travelling crane. Above each window, two tubes are provided for the installation of master slave manipulators. Sealing of these is achieved by encasing in pvc sleeves, fixed to the wrist at the slave end of the manipulator and to the mounting tube on the operating face. A second pvc sleeve encases the master end.

20. The initial equipment provided in this cave consists of: large and small viewing jigs together with an externally-operated periscope and camera; gamma scanning apparatus, power-operated bench saw, lathe, milling machine, welding jig, can piercing and gas release apparatus, accountancy balance, and a small decontamination tank.

X-ray cave

21. The X-ray cave has been treated as a separate problem. X-ray heads and their ancillary equipment need regular attention and adjustment, so that easy access is important. To accomplish this the transfer tunnel through the cave is used as a glove box to contain all activity. Radiography of specimens can be effected without contaminating the cave or equipment. Whenever the radiation specimens are not in the tunnel, entry is possible via the rear door.

22. The cave is 12 ft long, 10 ft wide, and 16 ft high. A stainless steel glove box is included in this cave to give access to the tunnel. This provides a ready means of posting into the other caves such items as tools, replacements, and materials.

Experimental cave

23. The experimental cave is 16 ft long, 6 ft 6 in. wide, and 16 ft high, and is fitted with two viewing windows. Its design and construction are similar in detail to the machining cave, except that no maintenance area or power manipulators are included. A 3300 lb capacity remotely-operated crane is installed, but no metallurgical equipment is included in the initial installation.

Measurement cave

24. The measurement cave is similar to the machining cave, except that a crane is not provided. The bench equipment installed consists of small viewing jigs with an externally-operated periscope and camera; leak detection apparatus, 1500°C electric furnace, 2000°C electric vacuum furnace, thermal comparator, calorimeter, high temperature thermal conductivity measuring apparatus, density measuring equipment, accountancy balance, and a small decontamination tank.

Cave services

25. The metallurgical caves are interlinked below bench level by a 12 in. by 8 in. transfer tunnel. The tunnel is connected to each cave except the X-ray cave by a gas-tight hatch. Mounted in the tunnel is a winch-operated trolley for the inter-cave transfer of fuel elements and other materials, the movement of the trolley being controlled from a central point on the cave operating face. Provision is made on the trolley for argon cooling of the fuel element.

26. Remotely-operated shield blocks are incorporated in the tunnel at each cave wall, to allow sectional isolation when required. There is also a 4 in. diameter argomatically-operated posting-tube system for the quick cave-to-cave transfer of small specimens. The carrying cartridges are actuated by medium-pressure argon, which exhausts into the caves.

27. Each cave is fitted with in-cell listening equipment, consisting of a portable microphone within the cave, connected to a loudspeaker unit mounted on the console outside. An intercom installation gives communication between the operating face, the cave roof and the maintenance area. The caves are equipped with a fire detector system, giving indication at the cave face, and also connected to the central alarm panel, which is integrated into the site system.

28. An argon fire-fighting installation is provided as a safety measure when the caves are being operated with atmospheric air, and also to provide an emergency supply should there be an accidental inleakage of air when operating in argon. As an additional safeguard, bags containing dry powder extinguishant are provided inside the caves. All the services on the cave operating faces are concealed behind steel panelling with removable doors, to give easy access for maintenance.

Chemical and analytical caves

Chemical caves

29. The chemical caves consist basically of one reinforced concrete cave 46 ft long, 9 ft wide, and 13 ft high with 4 ft 9 in. thick walls, divided by 21 in. thick cast iron roll-out doors into three cells 20 ft, 15 ft, and 8 ft long respectively. Access to the caves is through a 7 ft wide, 10 ft high doorway in the central cave. This opening is shielded by 19 in. thick steel doors.

30. The transit area immediately adjacent to the access door is enclosed by a steel cubicle 22 ft long and 15 ft wide. This cubicle is equipped with frog suit change, and control facilities. The object of the transit area cubicle is to minimize the spread of contamination when the cave access doors are open and to provide a posting area for the chemical rigs. The caves are lined throughout with stainless steel sheet fabricated to the same standards as the metallurgical caves.

31. Two viewing windows are provided in the operating face of the two larger caves and one window in the smaller cave. Provision is made over each window for the installation of two master slave manipulators. Sodium lighting is provided at roof and window level. A 12 in. wide shelf is fitted in the caves below the window, on which are mounted plug and socket connexions for the instruments and electrical equipment. Underneath the shelf an inter-cave transfer tunnel is installed, similar to that provided in the metallurgical caves. Posting ports are provided in the roof, and each cave has a mortuary hole in the floor. One power manipulator is installed, to serve the requirements of all three caves. The three caves are connected to an active extract ventilation system which discharges via absolute filters and a carbon bed to the existing chimney stack, and to the active effluent monitoring and hold-up tanks in the raffinate pit adjacent to the main building.

Analytical caves

32. The analytical caves are a continuation of the chemical caves, and consist of three identical caves, 8 ft square and 16 ft high, with 3 ft 4 in. thick walls. Each cave is lined with stainless steel sheet which, together with the bench just below window level, forms an alpha-tight box; a removable panel and a newt suit port is incorporated in the rear face opposite a roll-out concrete door. A viewing window is provided in each cave, and provision is made for the introduction of master slave manipulators. Sodium lighting is installed in the roof of the caves. The lower part of the caves is connected into the chemical caves extract ventilation system.

33. The transfer tunnel system in the chemical caves also serves the analytical caves, and inactive materials can be introduced through a glove box located at the end of the analytical line. Active material can be posted in and out of each cave by means of a posting-port on the roof. Each cave is connected to the active drainage system. All services on the operating face are concealed behind a steel panel fitted with access doors.

6—*Cave operating area*

Cave Services

34. An intercom system similar to that supplied for the metallurgical caves is provided for both suites of caves, and in-cell listening equipment is installed in the chemical caves.

35. Fire detection equipment is fitted, and connected to the central alarm panel, and the analytical caves are equipped with a carbon dioxide fire-fighting system, but none is fitted in the chemical caves, where it is intended that the portable rigs would include a suitable system.

Operating area

36. Fig. 6 illustrates the operating area. The concealed services are taken into a console, which extends the full length of the cave faces. All the permanent controls, plugs, etc., are mounted in this console. Fixed equipment in this area has been kept to the minimum, in order to give maximum freedom of movement to the operators. No windows are incorporated in the outer walls, so that lighting conditions can be readily controlled.

Service corridors

37. The space above the operating area in the north and south sides of the building has been allocated to all the necessary services, control cubicles, and ancillary equipment associated with the caves. The electrical distribution panels and switchgear are housed at the west end.

Offices and laboratories

38. The area above the services corridors is divided into a number of offices and four small laboratories. Each laboratory is fitted with a bench, sink, and normal services.

Effluent annexe

39. The effluent annexe is located on the north side of the building, with access from the operating area. Housed in a concrete shielded pit are stainless steel tanks for the reception of low active arisings from the chemical and analytical caves, and also stainless steel slab tanks for the highly active raffinates arising from the chemical processing experiments. Provision is made for sampling and monitoring of the contents of these tanks, and for disposal by means of ejectors to the high and low active group drain system.

Heating and ventilation

40. Filtered heated air is distributed to the operating areas at a slightly positive pressure, and at a rate which gives approximately ten changes per hour. Some of this air is directed from the operating area to the services corridors and the office gallery above, while the remainder is exhausted into the maintenance area. Air is extracted from the maintenance area into the existing group extract and chimney system.

Services annexe

41. The services annexe building is approximately 100 ft long, 100 ft wide, and 30 ft high, of steel-framed construction, with the main section clad in asbestos cement sheeting, and the changeroom, tea bar, and stand-by electrical supply rooms of brick construction. The upper floor of the main section comprises an operational stores area and some office accommodation, and the ground floor is divided between a workshop, test rig area, and the argon purification plant.

42. The argon purification plant is designed to handle 30 000 cu. ft/h of gas containing 250 ppm oxygen, 15 ppm water, and over 10% nitrogen and reducing these impurities to 100 ppm oxygen, 10 ppm water, and 10% nitrogen. The plant is connected to the metallurgical caves by a ring main with individual pressure control to each cave, and on the return leg, each cave is connected through an absolute filter and carbon bed.

43. A changeroom and tea bar for about 40 men abuts on to the main building, and is designed so that direct access can be obtained without using the group facilities. Housed over the changeroom is the standby electrical equipment required to maintain essential power in the event of a failure of the public electricity supply. The equipment consists of a 75 kW motor alternator set, operated by batteries, connected by automatic change-over gear to certain essential items, including the chemical caves extract fan, manipulators, argon plant blowers and argon cooling equipment. Comfort conditions are maintained in the services annexe by means of unit heaters and an extract ventilation system is provided in the argon plant room.

Completion of the facility

44. The metallurgical caves are being commissioned. When this work is complete a period of extensive handling trials is planned. During this period it is likely that minor snags and faults, so difficult to forecast in such a complex facility, will appear. Furthermore, the trials will serve as training to the operational staff. On completion, the facility will handle all the experiments containing plutonium which are already being irradiated in the Dounreay fast reactor.

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DISCUSSION on:

Replanning Britain's nuclear power programme*

by Mary Goldring

Mr J. A. Jukes (*Economic Adviser to the UKAEA*) agreed with Miss Goldring that cost figures were guide lines only and that trouble arose through people reading too much into them. He said that the Authority assumed a long term price for uranium of $7/lb, very similar to the price in the EURATOM Report.

28. He did not know what the lecturer meant by physical and technical obsolescence. A nuclear station had very low running costs and once built was not made obsolescent by the following stations. The CEGB was expanding its capacity each year and had to decide on the best methods to use at any one time despite the fact that there might be a better station in two or three years' time. Miss Goldring had not appreciated the savings from replication of design. To build a few small stations of different design and type would cost a great deal and would probably be quite uncompetitive.

Mr Holt said that estimates at the Melbourne Conference of the nuclear 'gap' in 1980 were between 20 000 and 30 000 MW(e). That required about 2000 MW/year from 1969.

30. It was true that Bradwell and Berkeley were uncompetitive with coal, but the better fuel elements and graphite, and the extensive reactor development available today were the result of the 1956 determinations which produced these stations. Did Miss Goldring accept Sir Leonard Owen's reassessment of the 1956 position as given in his recent lecture in Rugby?

Mr E. N. Shaw (*Managing Editor of* Nuclear Engineering) said he thought Miss Goldring's subject was a political one and it was a pity such a discussion had never taken place in the House of Commons. The Minister of Power or the Government were responsible for giving the essential *raison d'être* of the Atomic Energy Programme and that had been lacking. It was not good enough to define the objective in terms of the economic power generation when arbitrary and different terms of reference were used by Government authorities, when the ground rules were continually changed, and when the principal purpose might well be neglected. Lack of direction had been evident in the previous year's power shortage.

32. If they had sufficient carbonaceous fuels for immediate needs and did not find that there was a ceiling which would be very costly to exceed, it was surely more appropriate to base rate of construction on the rate of technical progress, while maintaining a healthy and profitable manufacturing capability.

33. When producing plutonium and U^{235} for the bomb, the UKAEA had had tremendous drive, morale and technical competence. Now it was a Parkinsonian organization with low morale and output.

34. The prosperity of the industries supporting the programme should not be so dependent on the placing of single orders. Referring to the consortia concept he said it was necessary to distinguish between a group of manufacturing companies and the architect engineering companies. The CEGB had need of the latter, either within itself or outside, which should also, independently, negotiate and contract for overseas orders as well as diversify into non-nuclear activities.

35. He thought Miss Goldring had implied that the UKAEA should reorganize its high research and development competence into productive fields. Perhaps the Trend Report had also implied that. The fuel production side should be independent. The Research Group could become a national laboratory which, he reminded Miss Goldring, would be devoted not purely to physics. Overstaffing in the Reactor Group could be admitted, but the steam generating heavy water reactor prototype (SGHWR) and the prototype fast reactor (PFR) were major tasks. He suggested other capital plant could be constructed by the UKAEA.

36. The present fast reactor developments were going on wholly within the UKAEA and it might be proper for the Government to create architect engineering companies. He thought the manufacturers would be better served in the long term by releasing their private architect engineering organizations.

37. He urged that the prototype fast reactor be given immediate Treasury approval; that the UKAEA be asked to examine a pressure vessel water reactor; the CEGB be instructed to build nuclear power stations when a design showed material improvement over the last; and the differences in capital cost between such a reactor and a conventional station be paid by a direct Treasury grant. Repeat orders would have to be justified on a basis of cost per kilowatt hour using the same ground rules as for conventional stations.

Mr Fletcher (*UKAEA, Harwell*) said he lived in an area threatened with a 2000 MW coal fired station. Did Miss Goldring think that any account should be taken of the side effects of coal fired stations? Corrosion damage had been estimated to cost about £1/ton. Did she think the siting

* The lecture was published in the January 1964 issue of the *Journal*, pp. 11–15.

policy was moving at the right rate, or too slowly? If too slowly, would this prejudice comparison with competitors in overseas markets?

Mr Campbell (*Reactor Group, UKAEA, Risley*) said that the capacity of the prototype SGHWR was 100 MW. The length of channel was specified by the steam quality emerging at the top. The channel length must be true in the prototype and 100 MW required 112 channels. A prototype smaller than this brought such different problems from those of a large reactor that nothing would be learned by it.

40. He thought the choice of AGR was the right decision. It was a continuation of the type of plant they had started with and gave modern steam conditions.

Dr J. M. Kay (*Director of Research & Development, Tube Investments Ltd*) thought Miss Goldring had confused the UKAEA's research with its reactor design. One had to recognize that the Authority had three functions: research, fuel production, and reactor design. It might seem more logical to separate them and envisage a research organization at Harwell and a production organization based on Springfields and Windscale which, because of its close military connexion, would have to remain a strictly Government organization.

42. But was it right for the UKAEA to continue reactor design and construction when the CEGB were the customer and the consortia had not enough work? He asked Miss Goldring whether she really thought that the design and development, and therefore the determining of technical policy, should be separate from the commercial responsibility. He thought that wrong. Serious thought should be given to integrating the present reactor division of the Authority with the engineering departments of the CEGB.

Dr Haigh (*Director, Berkeley Laboratories, CEGB*) said he was disturbed by Miss Goldring's remark that the Authority should do work which might equally well be done by the electricity authorities. He asked what one was to do with a research organization that had done its job. It was wrong to hand over to it work which was not being done as well as it should be elsewhere. One would not hand over aircraft research to a gas turbine establishment or the work of the Medical Research Council to a private drug company. His own view was that the organization should be pulled to pieces; 'get in another bunch of fellows and start again.'

Author's Reply

Miss Goldring, in reply to Mr Jukes, said that one assumed that second generation stations would have lower capital costs, and one had to remember the interests of the electricity consumer. Sir Christopher Hinton had estimated the extra capital burden of the nuclear programme as £360 million and the extra running cost as £20 million a year.

45. She and the others like her were very sceptical about costs where there were so many. Bradwell and Berkeley were notorious examples. Three or four years ago the costs were given as £140/kW. The last final figure was £177/kW, but on the way to the official opening at Bradwell, a slightly higher figure had been handed to correspondents.

46. Mr Verity's paper gave Oldbury's capital cost as £108/kW. Hansard gave it as £111 6*s*. She asked who was to be believed.

47. About the energy gap, she said energy experts had told her that there would be no serious rise in price of oil or coal for the next twenty years. Due to the modernizing of the American coal industry and the piping of natural gas, coal could be dumped in Western Europe at very competitive prices which would result in stable coal and oil prices.

48. Replying to Mr Shaw, Miss Goldring said that she thought two political programmes had got tangled together. There was the nuclear programme on the one hand, and the Treasury campaign for a commercial return on capital employed by the nationalized industries on the other. The CEGB had, as a result, put up its capital charges.

49. She thought architect engineering companies would be in danger of not picking a winner. Nothing succeeded like success and if the CEGB ordered an AGR, people who had gone for other systems would have difficulty in finding buyers.

50. Replying to Mr Fletcher, she said she had not studied siting policy carefully, but thought the safety requirements for a new type of reactor would have to be very tight.

51. She said in reply to Mr Campbell, that if experiments had to involve large power stations, the number of experiments they dared do would be very small. It was surely not beyond the ingenuity of engineers to find ways of doing small scale tests.

52. Dealing with Dr Kay's points, she said she was against carving up the UKAEA. It was a splendid and successful organization and it was essential to separate experimental design from those who were to buy commercial stations. The UKAEA must experiment though she recognized the present strong differences between the UKAEA and the CEGB. She said that Britain should experiment with fast reactors but that the CEGB should not at present order a fast reactor—not even a 200 MW one.

53. In reply to Mr Haigh, she said that Lord Hailsham had written recently of the difficulties of dealing with research organizations when their steam ran out. She thought opinion was moving towards concentrating research in the universities where people were continually changing and resources could be easily switched. However, the high cost of equipment ran counter to this idea, for a school would be established only where the equipment was.

54. She did not know whether the solution lay in a fairly large Government department—a Ministry of Technology—in which there were representatives of the UKAEA, the RAE and the DSIR, so that programmes could be moved backwards and forwards.

DISCUSSION on:

The spectral shift control reactor*

by M. C. Edlund

The Author, in introducing his Paper, said that the principal reason for attempting to develop the spectral shift reactor was to try to capitalize on the superior neutron economy of the U^{233}–thorium fuel cycle. That cycle offered the potential of the lowest fuel cost in the SSCR, primarily because of the high value of η, the neutron regeneration factor, which made it possible to obtain conversion ratios of about nine-tenths with reasonably large burn-ups, so that the contribution of fabrication and re-processing cost to the total generating cost would be kept to a minimum.

2. He pointed out that he was talking about a U^{235} make-up for the particular cycle of something not exceeding about 0·1 g/MWd. That should be compared with the U^{235} consumed in a pure burner. If the U^{235} was simply burnt with no conversion the figure would be more like 1·23 to 1·25 g/MWd. What they hoped to achieve, therefore, was a reactor which would give burn-ups of about 30 000 MWd/t with a conversion ratio of nine-tenths or better, which would then reduce the U^{235} requirement by roughly a factor of 10 below those for a pure burner.

3. The thorium fuel cycle could be started with either highly enriched uranium or plutonium, the choice depending on the availability and the relative prices of the two fuels. At the present time it was not clear which was the better choice for the USA, first because they did not know enough about the physics of the plutonium isotopes in the resonance region to make an accurate estimate of the grammes/MWd required for make-up, and secondly because they had no idea what it would be selling for in the next ten years. As might be known, Congress would probably pass a private ownership Bill within the next three years, and it was very likely that within ten years there would be a relatively free market for nuclear fuels, so that it was difficult to predict the relative market prices of isotopes because there were too many imponderables involved. All sorts of calculations could be made and argued about.

4. They had reached a point, he believed, where they had a good knowledge of the physics of the system and they were now concentrating on determining what the other components of the fuel cost were likely to be. The main problem, in his opinion, was that of the economical re-processing of the spent cores. Even after having seen the start of the new re-processing plant by Nuclear Fuel Services in western New York, and even after some experience with the USAEC's re-processing plants, considerable uncertainty remained as to what these re-processing plants really cost. It was known that they were going to be expensive, but if there was a large enough throughput the cost could be cheap. The real question was whether it was a matter of throughputs as small as 0·5 or 0·3 ton/day or whether 1 ton or more a day would be necessary to achieve the sort of costs likely to make the scheme attractive. That was the principal economic problem.

5. As he had indicated in the Paper, they were attempting to find a solution to the interim problem, the problem of how to make this cycle economic with, say, three reactors generating between 1500 and 2000 MW(e). To do that they had decided not to recycle the thorium. What this would permit them to do was shown in Fig. 15 of the Paper. They would be able to clean up the material and remove any decay products of U^{232}, by a technique which did away with the need for completely remote operation behind a shielded wall. If this whole cycle was successful, then at some point, perhaps at 1 ton/day, it would become economic to go to a remote plant and recycle this material at that plant.

6. Thorium now cost about $15/kg in the form of nitrate crystals. By storing this for 10–12 years the thorium-228, which was the precursor of radioisotopes which emit high energy gamma rays, would decay sufficiently to be used even in this type of cycle. Storing it for 10–12 years would cost, they thought, about one-third of $15/kg, say $5/kg, so that it would still make economic sense.

7. The mixed thoria–urania powder was then put into fuel rods by compaction. They had a pilot line in operation using this process and good results had been achieved with this technique of uniformity of loading of the rod. There should be no appreciable difference in the operating performance of a rod of this type as opposed to the more conventional pellet core. Numerous experiments had been done on the impacted fuel and they believed that the problem was essentially solved. The process for making the mixed thoria–urania powder was quite cheap and the equipment relatively small and uncomplicated. They were now in process of arriving at cost figures on their pilot plant and the chemical engineers said that even at a relatively low throughput, say 1500–2000 MW(e), the costs should be around $1/lb.

8. He wished to give an up-to-date version of costs. The original figures had been arrived at about two years ago and since then they had examined them in greater detail. With some laboratory and development experience behind them,

* The Paper was published in the January 1963 issue of the *Journal*, pp. 14–22.

he had been able to modify the figures. The price for zircaloy would come down. At the present time they were obtaining it for about $12/kg, but the zirconium tubing people claimed that a further 35% reduction could be expected, so that in Table 1 of the Paper instead of $22/kg the figure should be more like $8 or 9/kg. The overheads for the fabrication plant and general administrative expenses would be lower than the figure of $40 given in the Table. Their development work indicated that they would be below this, perhaps by as much as $10.

9. Unfortunately, however, the reprocessing estimate had been the idiot's choice of four studies made over a period of years at the Oak Ridge National Laboratory and the MIT practice school. On looking at that information it had been found that there was a spread of 2½ times between the lowest and the highest estimated costs, and he became a chemical engineer for two days and 'went down the middle'. They now felt that the figure of $21 given in Table 1 should be more like $40. They expected to be close to the total figure in the Table, $113, but the distribution was going to be somewhat different.

10. Table 2 was a reminder of the characteristics of the reactor which was analysed in the Paper. It was a reactor of about 400 MW(e). The metal/water ratio was somewhat different from that for which they had done the experiments, but they had done experiments at this value also.

11. Using those numbers, they had in Table 5 estimated first cycle costs at about 1·4 mill/kWh, with a possible equilibrium cost of about 1·2 mill/kWh. That had been the final conclusion of his Paper, written more than a year previously. He wished to bring the position a little more up to date. First, the fuel inventory charge in the calculation had been based on an interest rate of 4·75%. With the impending private ownership of fuel in the USA it might be necessary to face interest rates as high as 12%. The rate was certainly going to be variable, because different utilities would approach the problem in different ways. They expected that some utilities would want to own their own fuel, while others would lease it from some leasing company which could perhaps get by with 6% and still make a handsome profit. The figure, therefore, might be anything from 6% to 12%. Some utilities would want to buy the material and pay 12%; they made a 6% net return on their investment, so that there was a big incentive to obtain as much as possible.

12. At 0·5 mill/kWh the fuel inventory charge would be too high, and so they were now working on designs in which they were going to attempt to increase the specific power by about a factor of 2. Preliminary work indicated that there were several ways of doing this. One way was to use a diluted fuel, and it appeared that it would be possible to cut the inventory charge approximately in half—i.e. to double the specific power—without too much of a penalty in the fuel burn-up. It seemed that doubling the specific power reduced the conversion ratio by 4%, but that would be more than offset by the reduction in the inventory cost.

13. To bring the information up to date on developments on the spectral shift reactor in the USA, about three weeks previously the USAEC had issued an invitation to the utility industry to build one of these, with $30 000 000 support. Bids were to be received by 15 April and it was planned to start the project by July. Four years of construction and testing were scheduled, so that the first spectral shift reactor in the USA should be running in the last half of 1968.

14. The AEC were supporting spectral shift reactor development by subsidizing the construction of a power plant in order to develop the thorium fuel cycle. There was no need to build a reactor to prove the physics as such and it was essentially an ordinary PWR. There was a possibility that the thorium fuel cycle development programme might be accelerated by including U^{233} in the first core charge. His company had a modest programme in being and they were also carrying out work for the AEC. They would be doing physics experiments on U^{233} and they would attempt to develop improved cores and to carry out further radiation testing of those improved cores.

Mr D. R. Berridge (*Nuclear Plant Design Engineer, CEGB*) said that the spectral shift reactor described by the Author had a number of very attractive features. Not the least of these was that it was not a completely novel reactor type but was based on the PWR, which had probably had more proving than any other single reactor type in existence, in one form or another. The Author had taken this concept and added to it two innovations which had not in any way reduced the soundness of the basic design but which should materially improve its economics. It seemed to them that sufficient basic work had been done on the spectral shift feature to prove it beyond all reasonable doubt, and he was pleased to find that this was being firmly backed for development in the USA.

16. The second feature, the use of the U^{233}–thorium cycle, was clearly the most exciting proposal, and this seemed to promise very real reductions in fuel cycle costs and should enable national power programmes to be run with quite small feed enriched fissile material. It was clear that there were many problems to be overcome not only in reactor physics but also in fuel processing and fabrication. There was a real incentive to overcome those difficulties, however, and people in Great Britain would watch with great interest the progress made.

17. He noticed that the optimum fuel irradiation was high, being about 30 000 MWd/t or 1000 reactor-days operation. It must be an attractive feature to any operator to be able to run a plant for two or three years without any need to move the fuel, and presumably fuel-handling could be done during a period of reactor shut-down for inspection and maintenance of plant. Perhaps the Author would comment on how long it would take to refuel a reactor of, say 500 MW or larger.

18. This large figure of 30 000 MWd/t for average fuel irradiation seemed to be higher than anything for which there were statistically significant rates in Great Britain. Would the Author comment on the amount of backing information which existed at the moment to support such a large irradiation with a fuel element of the type and construction which he had in mind? It was improbable that he had everything 'all buttoned up', but it would be interesting to know how far he had gone.

19. Pending the proving of the thorium cycle it would clearly be possible to operate a spectral shift reactor on a U^{235}–U^{238} cycle or a plutonium–uranium cycle. It would be interesting to have the Author's comments on the attraction of such a reactor in relation to the soluble poison shim control on the PWR and to have his comments on the factors involved

if a spectral shift reactor were operated initially on the U^{235}–U^{238} system and subsequently converted to the thorium cycle.

20. The Author had not made much reference to reactor safety, and presumably the fault behaviour of the SSCR was generally similar to that of the PWR; but in the event of a rupture of a cooling circuit, especially in the early life of the core, it would be calamitous to flood the system with unpoisoned light water. Presumably there were ways round this.

21. Mr Berridge did not understand from the Paper on what basis the Author derived the value of about \$15/g for U^{233}, and if he would elaborate on that it would be helpful.

Dr Edlund said in reply that the history of the refuelling time was not good. That, of course, was due to the fact that they had only the history of the first plant, and the refuelling time had been established not by the actual physical time taken to refuel but by other difficulties. They believed that they could refuel a large water reactor of this kind, a complete core, in no more than four weeks. Some people felt that the time might be reduced to two weeks at some future stage in development, but he thought that four weeks was a reasonable estimate. The operating people felt that a figure of four to six weeks was a good estimate and he would rely on their judgement.

23. It was true that the MWd/t irradiation was considerably higher than anything that had been proposed so far for actual operation. There was quite a variety of experimental information that led them to believe that 100 000 MWd/t could possibly be achieved with these fuels. That in itself, however, was not sufficient. They did not possess as much statistical information as they would like to have, but from the irradiation experience which they had, they had taken thorium oxide up to 50 000 in some experiments, and a good extrapolation of this indicated that 30 000 should not give any trouble.

24. The next question was on the operation of the reactor with low-enriched uranium oxide fuel as distinct from the more conventional use of soluble poison shim control rods. They had some information on this. First, comparing an SSC reactor with a batch-loading reactor. The SSC reactor could be designed with good design margins for a peaking factor of 2·1. The conventional PWR with shim control and control rods had been considered as a typical approach to this. These designs had peaking factors of about 3·2. There was some question of the extent to which this peaking factor could be reduced through experience.

25. The next point was that soluble poison shim control plant must be shuffled, and a three-cycle shuffle seemed to be the way to go about it. This meant that the core would be reloaded on a schedule of about three times as often as the batch plant, which would mean a reloading period of between 9 and 12 months, which would, of course, reduce plant availability.

26. The fuel cost for the SSCR on the low-enriched cycle was about 0·15 mill/kWh lower than the fuel cost for the soluble poison plant. The soluble poison plant had a somewhat larger capital cost in pressure vessels and would probably need an additional loop, so that there was another heat exchanger and larger containment. This additional capital cost roughly offset the cost of heavy water, so that the final net figure was about 0·15 on fuel plus an evaluation for improved plant availability. This plant availability could be worth anything from 0·1 to 0·2 mill/kWh, depending on the system and the requirements. Even on the low-enriched cycle, perhaps a figure as low as 0·25 mill/kWh could be obtained.

27. The next question concerned the building of a sizeable soluble poison PWR and its subsequent conversion to spectral shift. There were several choices. A 500 MW unit could be built—a more or less conventional PWR—and later converted to SSC if sufficient steam separating capacity were allowed for in the heat exchanger, but with the same primary coolant flow, about 50% more power could be obtained. In the USA he did not believe that anyone would build-in that much excess turbine capacity, and so the idea of converting should perhaps not be the correct approach and one should think of it as a back-up. If, for example, next year an enormous amount of really cheap uranium ore was discovered, neutron economy would not be very important and one would want to run the plant in the cheapest way. In those circumstances one could retrieve, but at some loss in power.

28. He did not think that there was any appreciable difference in the safety of the two types of plant. They had about the same power coefficients, and in fact their negative power coefficients and their temperature coefficients were somewhat larger. On the problem mentioned by Mr Berridge, they would use boric acid water for emergency cooling under a condition of primary loop rupture.

29. The last question was how the value of U^{233} given in the Paper had been obtained. What they had done had been to take each cycle, starting with a fresh batch of U^{235}, and on the next cycle ask themselves whether to buy U^{235} at \$12/g or recycle the material. He would first assume that there was no difference in total process costs, costs of fabrication, and reprocessing. It was then merely a question of writing down a balance—so many grams of 233 plus so many grams of 235 equalled whatever charge of 235 was required to give the same total energy output. It was not, however, quite so simple as that, because the fabrication cost might be somewhat different and the value was a function of the ratio of the fabrication cost for U^{233} fuels to the cost for U^{235} fuels. They had used a factor of 1·3 to arrive at the figure of \$15. With a factor of 1 it would be about \$16½.

Mr D. R. Smith (*Nuclear Power Group*) observed that the principle of spectral shift was striking by its basic simplicity, and like so many original ideas was so obvious, once postulated, that it made one wonder why it had not been thought of earlier. During the past few years his company had carried out design studies of spectral shift reactors for both power generation and marine propulsion, and it was against that background that he wished to comment.

31. The good radial form factor of the spectral shift reactor was important in respect of fuel inventory, i.e. the cost of the initial charge of fuel. The importance of fuel inventory had been obscured by the method of financing available in the USA, through the so called 'use charge'. The use charge represented a powerful subsidy, because the operator did not have to raise the capital for the fuel inventory; the use charge of 4¾%/year was appreciably lower than current interest rates; and it was levied on the average instead of the initial U^{235} content of the reactor.

32. The extent of this subsidy was apparent from Table 8, which showed the fuel costs of the spectral shift reactor

Table 8: *Spectral shift control reactor fuel costs*

Net electrical output (MW)	407	
Fuel cycle (full power days)	1000	
Fuel hold-up (days)	740	
Weight of oxide (kg)	45 000	
Initial U^{235} (kg)	1638	
Reject U^{235} (kg)	537	
Fuel cost ground rules*	UK	US
U^{235} inventory charge (kg)	1638	1088
Value of fuel in core (£/kW)	23	16
Value of fuel hold-up (£/kW)	17	12
Total value of fuel	40	28
Fuel inventory (d/kWh)	0·105	0·045
Fuel replacement (d/kWh)	0·075	0·075
Total fuel cost (d/kWh)	0·18	0·12

* UK—7·5% p.a. on initial U^{235}
US—4·75% p.a. on average U^{235}

described in Dr Edlund's Paper, calculated for both British and American ground rules. On British ground rules the operator had to find £40/kw for the initial fuel charge and hold-up, and the interest on that amounted to 0·1*d.*/kWh on the fuel cost, or 0·06*d.*/kWh greater than on American ground rules. The contribution of £17/kW from fuel hold-up was also noteworthy.

33. A high fuel inventory was not peculiar to the spectral shift reactor; it would be higher still for a first generation PWR as typified by Yankee. It did, however, emphasize the importance of good form factors and low enrichment. The Paper put forward claims in those respects for the spectral shift reactor, but in his opinion did less than justice to the PWR. Fuel shuffling procedures, though not the one indicated in the Paper, could result in PWR form factors comparable with the SSCR.

34. The initial enrichment of a multi-batch cycle PWR was about 3·0% for 30 000 MWd/te fuel life, compared with 4·0% for the single batch cycle spectral shift reactor. This was because the excess reactivity required to compensate for fuel burn-up was less in the multi-batch cycle because of the shorter time between refuelling.

35. More frequent refuelling also reduced fuel hold-up which was inherently greatest for single batch cycles and least for continuous (on-load) refuelling.

36. The multi-batch PWR thus appeared to have a lower enrichment, and a smaller fuel hold-up than the spectral shift reactor. The fuel inventory of the PWR would therefore be significantly smaller.

37. He asked for Dr Edlund's views on the feasibility of zircaloy fuel cladding under PWR operating conditions. An examination of the (admittedly scanty) experimental data suggested strongly that the rate of hydriding, particularly of defect cans, would be prohibitive.

38. Would Dr Edlund give some indication of how he proposed to cope with the tritium health hazard during refuelling, particularly with regard to the removal of defect fuel before the end of the core life?

Dr Edlund, in reply, said that Mr Smith had presented some interesting figures, but it was difficult to understand how his money figures came to be so large. To take a simple example, there was a loading of 1600 kg of U^{235} in the core. He would assume that that fuel remained constant over the life of the plant and there was no decrease in the cost or value of the fuel. He would have to spend about $20 million. In addition to this he would assume that he had to buy one more core. At the most that would mean $40 million, which was a little under $100/kW. To use the actual out-of-pile figure, which gave a factor of about 1·3, his investment was $26 million for 400 MW, which worked out at $60/kW, or about half the figure shown in Mr Smith's Table.* Mr Smith was certainly right in saying, however, that the fuel inventory problem was a very major one and it would lead them to press these designs to much higher specific powers than anything that had been built today.

40. On the question of form factor, admittedly there were many people, including his own company, who were doing physics calculations to see what they might be able to do with various types of fuelling, starting with the roundelay methods. This, unfortunately, was a function of the size of the core. With regard to the 1·25 form factor, they had had numbers such as that for relatively small cores. There was, however, a very serious power distribution stability problem, namely that when shuffling the fuel around it was necessary to ensure the maintenance of very close symmetry on reactivity differences. A small shift from one quadrant to another could give power tilts which were quite significant and these had been observed experimentally. Their own best calculations for the three zone out–in shuffle approach for a 1000 MW core gave a figure of 1·5 to 1·6, but again it was a question of comparing something that had not been proven with a design number. His own calculations would give a figure around 1·6, but he did not know of anyone offering three zone out–in shuffled cores with a guarantee of a form factor less than 3·2.

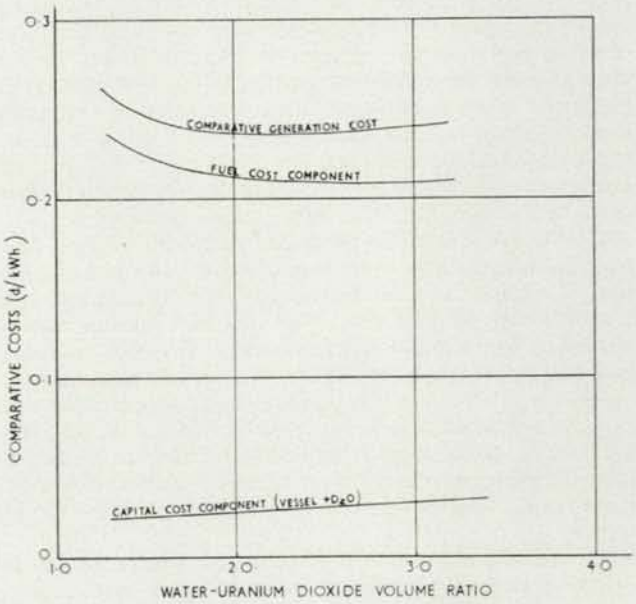
41. The next question concerned zirconium hydride. That had been rather a major problem for many years in the utilization of zircaloy, but the picture had been cleared up quite largely during the past year. They now had data on rates of zirconium hydriding at temperatures up to 680°F, and they had performed an extensive series of mechanical tests on highly hydrided zirconium tubing. The results of their own tests, as well as work done by other laboratories in the USA, led them to state that zirconium could certainly be used up to 670–680°F for periods of two or three years. In fact the AEC had accepted this position and would not put more money into it.

42. On the question of refuelling with a large number of defective elements, it should be emphasized that a single defect did not close the plant down; on-stream clean-up handled 1% of the fission products being released continuously. If, however, it was desired to shut down before reaching the end of life, there were two important points which should be mentioned. First, by using borate with the mixture the problem of temperature coefficients could be overcome—it was planned to do this in any case in normal operation—and secondly the tritium build-up did not constitute a major health hazard. It took a long time to build up a large amount of tritium and there were special precautions which could be taken. That had been looked into very carefully.

* Dr Edlund writes: I referred to the case of one hold-up core for three reactors. I did not include fabrication costs as Mr Smith did. If I did, the cost per kilowatt would be $80.—EDITOR.

Mr P. Steward (*Nuclear Power Group*) raised a point regarding the choice of water/UO_2 volume ratio in the proposed 400 MW(e) SSCR. Previous design investigations in the USA, he said, and particularly that made by the Stone and Webster Engineering Corporation, selected for their reference design a water/UO_2 volume ratio of 1·4, whereas a more open lattice would appear to give substantial savings in both fuel and generation costs. This point was illustrated by Fig. 19, which showed the variation of the comparative capital, fuel, and generation costs with the water/UO_2 volume ratio. The upper curve on the graph showed that a net saving in generation cost of 0·015*d.*/kWh, equivalent to a capital cost difference of £5/kW, could be achieved by increasing the water/UO_2 volume from 1·4 to 2, corresponding to the point of minimum generation costs. This reduction in generation costs was relatively insensitive to both the costing ground rules and the credit assumed for the reject plutonium.

44. He would therefore like to ask the Author whether the adoption of tight lattices in 450 MW(e) SSCRs utilizing a low-enriched uranium fuel cycle was in any way influenced by the maximum size of reactor pressure vessel that could be fabricated at present.



19—*Variation of comparative generation costs with water–uranium dioxide volume ratio for an SSCR*

Dr Edlund replied that in effect the question was whether the apparently tighter water/metal ratios in the USA which had been discussed were determined by the pressure vessel fabrication problem. He would say that that had probably been so until two years ago, but that today, when they were putting themselves in a position to build considerably larger vessels, he thought at least for powers up to 600 MW(e), that was not the determining condition. Exactly why there had been such low water/metal ratios he did not know, but he agreed that the optimum was closer to 2. The reference design in his Paper was about three years old, and they were now designing in the range of about 1·8 to 1·9.

Mr J. Fell (*UKAEA, Winfrith*), many of whose points had been covered by previous speakers, confined himself to one brief one. One of the difficulties of the SSCR was that it was applied to make use of a large fraction of the total energy of the fuel; for example the total energy obtained in one case was about 15 MWd/g compared with a figure of about 1·2 in the conventional case. Although this was an encouraging improvement, it fell short of what should be possible, and he would like the Author to comment on a recent proposal presented to the AEC for a seed and blanket PWR using thorium as a fertile material and a moveable feed for reactivity control. It had been suggested that such a system could achieve a conversion factor in the neighbourhood of 0·95 to 1·05. On the basis of any simple physics argument one would expect the SSCR to have had a somewhat better potential in this respect than the seed and blanket PWR. In the Author's view, had the attempt to obtain optimum generation costs meant that the proposal had under-emphasized the fuel utilization potential of this system?

Dr Edlund replied that the answer to the last question was, yes. He was perhaps not in the best position to comment on the seed and blanket reactor because he had views which were quite different from what had been publicized with respect to that concept. First, that reactor would not breed under economic conditions at any time in the near future. In order even to approach a conversion ratio of 1 the fuel lifetime for the seed was such that the fabrication costs were enormous. At the present time a study was being undertaken by the Oak Ridge National Laboratory to evaluate the seed and blanket and high-temperature gas-cooled concepts and the organically-moderated heavy-water-cooled concept and he understood that the results were to be published in the spring; but he had heard some preliminary results, and apparently the economic optimum for the seed blanket was in the range of a 0·85 to 0·9 conversion ratio at the present time and the fuel cost seemed to be in the region of $2\frac{1}{2}$–3 mill/kWh.

Capt. H. F. Atkins congratulated the Author wholeheartedly on his choice of the name 'spectral shift'. To Capt. Atkins it conjured up delicious visions of the ghost of Hamlet's father flitting across the battlements, of Banquo at the banquet and of Beckford's historic steeplechase through a churchyard with all the riders dressed in nightshirts. Its second-hand sales value was shown by the UKAEA rushing to develop the Vulcain spectral shift reactor where Euratom, ENEA, and even Vickers (in the person of the speaker) had feared to tread.

49. Presumably M. Maldague's calculations had been based on the work of Babcock and Wilcox, so that the Author might be able to explain how the estimate had been reached of $3\frac{1}{2}\%$ enrichment for a 60 MW(t) reactor, less than the Author's for 1516 MW(t). Could the Author suggest how the AEA could have believed that figure, much less made an agreement with Belgo–Nucléaire based on it? In the House of Lords that afternoon, Lord Bessborough had had to reveal that by the end of that year £5 million of the taxpayers' money would have been thrown away on the project. Could not the Author have sold Great Britain the information for less than that?

50. The AEA, it might be recalled, had thrown out a much more elegant form of spectral shift control, more prosaically called 'f and p' control, as proposed long ago in the Rolls-Royce steam-cooled reactor, moderated by heavy or light water or, if desired, by a mixture which, if one were bent on making things complicated, could readily be varied. The moderator was at atmospheric pressure and below 180°F, and could be removed selectively from a number of tubes dispersed throughout the lattice. It gave all the advantages and more of the other form of spectral shift reactor, without any control rods and their very complicated mechanisms.

51. The ideas of Rolls-Royce were, of course, years ahead of that now. The only hope for Great Britain's merchant ships was for the Admiralty, Navy Board, or Second Under-Secretary of State, Defence Committee (Maritime) to order the Rolls reactor for surface warships. It would be cheap enough for the Merchant Navy too.

52. In the small-size reactor required for a modern ship the effect of variations in neutron leakage due to spectral shift was very marked. Perhaps the Author would comment on the application of spectral shift to a reactor of about 60 MW(t) to give 20 000 shaft-hp for a ship.

53. To the speaker this mixture of water seemed a physicist's dream and an engineer's nightmare. It might be all right on shore, with physicists galore at beck and call, but in a ship how would the chief know what precise mixture was in the core at any moment except by the resultant reactivity? How could he be sure that the mixture was the same in every nook and cranny of the core? When steam generator tubes leaked under way, heavy water would be lost to the secondary circuit, so that many millions of dollars would be lost, and much extra capital carried in heavy water. If, however, a tube leaked the other way during shut-down the reactor might be brought critical with far greater excess reactivity than expected owing to the greater proportion of light water. Would the chief have to try to check first with a spectroscope or other device? No doubt he would be given tables to show just what the reactivity of the core was going to be with all possible mixtures throughout its life history, but would the physicists really know themselves?

54. The saddest thing about the spectral shift was that it was tied to the pressurized water reactor. 'Tarting up' that old lady in a spectral shift could not disguise her ponderous figure, or dispense with costly surgery to cut out her blemishes, or heat treatment to alleviate her built-in stresses; nor could it modernize her into the twentieth century with regard to the steam that she produced. If she were consolidated she became even stouter. The danger of her high blood pressure was not just that of a minor burst, but of the catastrophic brittle failure of her whole carcass. It was to be hoped that no more nuclear submarines were lost. He hoped that *Thresher* had dived too deep, but a burst reactor pressure vessel would equally sink her without trace. The wild stories of sabotage in *Comet Is* would be recalled until the Navy had recovered enough pieces of a pressure cabin.

55. Boilermakers could be trusted not to make square holes in a reactor pressure vessel, but the knowledge available of radiation damage was still slight, and the thicker the steel the worse was the problem. If the failure of a reactor pressure vessel was regarded as incredible, why were costly precautions necessary and why were there sighs of relief on going over to concrete?

56. The remedy was simple: to adopt a pressure tube design in which the failure of any part at all could be catered for with safety.

57. Finally, he hoped that the Author would not evade his questions because they might be loaded. He was not gunning for the Author and vital questions were bound to be loaded. 'Spike my guns quickly,' Capt. Atkins said, 'so that if I do fire they will burst in my face whilst you, Sir, may have got out of range again.'

Dr Edlund, in reply, said that he could not compete with Shakespeare, but certain allegations had been made with which he would try to deal. Dr Maldague had done his work independently of the work of Babcock and Wilcox. They had had some brief conversations about this many years ago, but after very brief study, lasting about two hours, they had decided that spectral shift should not go on a ship at all and it had never been considered in his company. After another three or four hours' study they arrived at the conclusion that if it were to be attractive it would have to be in very large power plants, so that he did not consider that it was sensible to consider these complications—and they were complications, as Capt. Atkins had so well pointed out—for very small reactors or for marine application. They would have been delighted to sell that information for very little money.

59. On the question of brittle failure of the pressure vessels, a large number had been operating and no failure had yet been observed. The question of what sank the atomic submarine was an open one, but there was no evidence that these pressure vessels were going to fail.

60. As to whether the old lady could be dressed up enough to be sold, several companies were making a good job of it in the USA and he wished that his was one of them. He thought it would be just a matter of time before these various commercial questions were settled, and he suspected that different answers would be found in different parts of the world.

61. In reply to a speaker who asked about the fluoride volatility process, the Author said that so far as they knew the fluoride volatility process might prove attractive, but it had not yet been developed to a stage at which it was possible to talk about relative costs. It might be necessary to abandon the thorium altogether if that process were used; he had not heard anyone suggest a way of recovering it.

62. In reply to another speaker, who asked whether the Author could reproduce his Fig. 15 if he started with a fair amount of plutonium, the Author said that the plutonium would be stripped out and recycled.

DISCUSSION on:

A method for the economic assessment of nuclear power programmes*

by C. E. Iliffe and J. K. Wright

THE discussion was opened by **Mr L. Grainger** (*Richard Thomas & Baldwins*). He illustrated a method of economic assessment of nuclear power programmes using what he had called the cumulative cost curve, the main advantage of which was that the judgement about factors such as station life-time could be left until last. He showed that with a 50 000 MW programme the comparison between nuclear and conventional power after 20 years with interest would be about £9500 M and £11 500 M cumulative cost. Excluding interest, the corresponding figures were about £6000 M and £8000 M. After 30 years the comparison would be, with interest, £25 000 M and £38 000 M and without interest £11 100 M and £20 000 M.

2. He hoped that future studies would take station life-time, interest rate and load factor as variables and that the Authors would say how their model could cope with inflation. Such studies, however, could never be more than a guide to senior people to take decisions.

3. A contribution from **M. J. Gaussens** (*Chargé des Etudes Economiques Générales, Commissariat à l'Energie Atomique, Fontenay-aux-Roses*) was read by Madame Kinsky who said that in France the problem of selecting the most suitable reactors for the long term French electricity supply was under investigation. Originally, they had used equivalence curves to define a plutonium price which equalized the balance account of the thermal reactor with that of the breeder reactor. These curves were soon found to be unsatisfactory as they did not take into account plutonium production and consumption on a programme scale. They had therefore been led to adopt their present model. Electricity demand was assumed for the next 30 to 40 years. A linear programming method had been adopted which optimized the total expenditure incurred over the period concerned; it was assessed by the present worth method. In the study no particular value was taken for plutonium at the start; it was obtained from the optimum programme by considering the associated 'dual programme' which gave the plutonium marginal values from which plutonium prices were deduced. Some of the earlier work in this field was published for the first time in 1960. The model chosen had many points in common with the paper under discussion. Both discounted expenditure and neither took plutonium prices into account in their study of the nuclear programme optimum policy. Following talks with the Authors however, they had started to adjust their model so as to express capacity in 500 MW units instead of continuous values. It would probably be interesting to extend the range of plutonium market study to both countries rather than to define a plutonium price within the boundaries of a country and they hoped to continue their co-operation with the Authors in this field.

Mr J. L. Gillams (*UKAEA, London*) said that the discount principle was a familiar one which provided an important refinement to economic assessments if and when the other main parameters were known. However, the Authors were less deterred than he would be by the need to make a great many assumptions about the future. It seemed to him that the method made it impossible to assess the merits of thermal reactors until cost and performance data were found for plutonium burning reactors of the future. The algebraic proof that the discounted unit cost was independent of any value assigned to plutonium was no more than a truism when the value assigned was constant over time, and failed to cover the case in which the value varied with time. A change in the plutonium valuation might equally well be intruded in the closed cycle through the overseas market since there could well be very substantial differences between one country and another in interest charges and re-processing costs and this could initiate trade both for the exporting and importing markets. They had been shown a curve for a breeder reactor which gave a plutonium price of £8/g for each 0·1*d.*/u.s.o. differential between fast and thermal reactors. In this event, would it not be cheaper to start up new fast reactors on U^{235} rather than plutonium. He thought that the model would have to be a good deal more complicated before it fully reflected the future possible realities.

Mr R. E. Strong (*UKAEA, Risley*) suggested that the discount method used by the Authors could be employed for assessing the value of U^{238}. He pointed out the danger of the present notion that the whole value of uranium lay in U^{235}. At concentrations of interest for fast reactors and advanced thermal reactors, the cost was substantially constant but in the area around natural it fell away sharply.

Mr K. L. Stretch (*C.A.T., Birmingham*) agreed entirely with the need to use the discount technique and the type of model proposed by the Authors, but thought that it needed refining. There was no economic technique however which would obviate the sudden drop in price of a by-product and he saw danger in any distinction between main products and by-products. He was critical of the 'closed system' approach and pointed out that there could be good reasons for dumping plutonium, even temporarily, but this decision required some judgement about the value of plutonium in 20 years' time and the method proposed did not help in this. Mr Gillams pointed out that the decision to dump also required an analysis of plutonium as a fuel for thermal reactors.

* The Paper was published in the January 1963 issue of the *Journal*, pp. 41–48.

7. In a written contribution **Mr D. Rudd** (*Humphreys & Glasgow Ltd.*) drew attention to the fact that a kilowatt of power is worth more during the winter 'on-peak' consumption than for summer 'off-peak' consumption, as reflected in the annual load factor. He argued that the discounted unit cost of a pump storage station could be infinite although the station might be a good economic proposition. Further, he stated that it is not generally known or agreed how the capital cost of a station ought to be spread over its life when its load factor varies, whilst the life itself could be determined from economic considerations.

Leicester Discussion*

8. In opening the discussion, **Mr V. White** (*English Electric Co.*) suggested that the 20 year station lifetime used in the assessment should be extended, particularly for future fast reactors. Other speakers (seven in all) asked for information on the effects of interest rates, load factor, and cost of storage of radioactive material.

Authors' Reply

The Authors, in reply, welcomed Mr Grainger's emphasis on cumulative cost; when interest was taken into account, it corresponded to what they had termed total discounted expenditure. It was this quantity which could ultimately be compared with costs of development. They had adopted discounted unit cost as their criterion rather than total discounted expenditure, mainly because it was relatively insensitive to assumptions on size of power programme. They could already vary station lifetime, interest rate, and load factor in their method and results for different rates of interest had recently been published.[1] It was unnecessary to pay regard to inflation provided that a real rate of interest r was used and not a monetary rate, r'. Following Rudd,[2] if inflation were a fraction i per annum, then

$$(1+r)=(1+r')/(1+i).$$

10. They were grateful for M. Gaussens' highly informative contribution. They had derived great benefit from discussions with him and his colleagues in the CEA and were reassured by the large measure of agreement revealed. They were pleased to acknowledge the pioneering paper published by Andriot and Gaussens in 1960, and they too hoped that collaboration in this field would continue.

11. In reply to Mr Gillams, they saw no disadvantage in their method requiring many assumptions about the future since it was on these that decisions depended. Moreover, with electronic computation, it was easy to see the effect that variations in the assumptions produced. In assessing thermal reactors by their method, it was not essential to refer to future plutonium-feed reactors unless economic benefit from plutonium was to be allowed for, and then such reference was surely inescapable. That the discounted unit cost was independent of any constant value assigned to plutonium they agreed was a truism, although it did not seem to be widely appreciated. The same must be true of a variable value of plutonium since any effect it had on discounted unit cost could only imply that the fuel processing organization was subsidizing the electricity consumer or vice-versa, a starting-point that the Authors wholly rejected. They did not exclude the possibility of international trade in plutonium. At the present time, however, they thought it unrealistic to postulate trade of the size of the several tonnes per year that the UK civil stations would be producing, even if its direction could be predicted. They agreed that replacement of the thermal reactor by an all-uranium feed fast reactor would give a lower plutonium price for each 0·1*d.*/u.s.o. differential between it and the plutonium feed fast reactor. A recent study gave a figure of only £4½/g per 0·1*d.*/u.s.o. differential. However, this low price did not necessarily mean that the thermal reactor was the less economic since the differential unit cost between the two fast reactors could hardly be less than one-thirtieth of the U^{235} price in £/g, leading to a plutonium price, in absolute terms, 1½ times that of U^{235}.

12. Mr Strong's suggestion that the method could be used to assess a price for U^{238} was both interesting and practicable. It was only necessary to specify how the deficiency in U^{238} arising from sales of it was to be made good, such as by importing natural uranium or fossil fuel, compute the consequent increase in expenditure and derive a selling price of U^{238} to offset it. The variation in U^{235} price with concentration only affected the data fed into an assessment, and not the method itself.

13. There was no difficulty in applying the method to Mr Stretch's problem of whether or not to dump plutonium. In fact, applications had already been made in which plutonium was either dumped or burnt in thermal reactors, in each case either indefinitely or until the introduction of fast reactors. The discounted unit costs were then compared.

14. They accepted Mr Rudd's distinction between units of electricity generated during 'on-peak' and 'off-peak' periods, but this affected the load factor data used rather than the method of assessment. The pumped storage station raised no difficulty since the method made no restriction on costs, output or load factor of any station. Whilst the way in which the capital cost of a station was spread over its life affected the variation in annual costs, it did not affect the discounted total. It was unlikely that the life of a nuclear station would be determined by economic considerations.

15. Mr White's query on the station lifetime to be adopted affected only the data fed into the method and not the method itself. The effect of extending the lifetime had not so far been studied but could readily be so. Similar considerations applied to load factor, storage costs, and interest rates but, as mentioned above, results of varying the latter parameter had already been published.

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* The Paper was discussed at a meeting on 9 January, 1964, of the East Midland Branch.

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