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ASPECTS OF SAFETY AND RELIABILITY FOR FUSION MAGNET SYSTEMS

FIRST ANNUAL REPORT

J. POWELL, Editor

January 15, 1976

FUSION TECHNOLOGY GROUP
DEPARTMENT OF APPLIED SCIENCE

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I OVERVIEW

I-1 Introduction

In general, there appears to be no fundamental reasons why CTR magnet systems cannot achieve the degree of safety and reliability required for large central station electric power plants. A variety of engineered protective systems can be installed to assure that accidents involving magnet disruption are very unlikely. These safety systems can be both active and passive in nature, and serve to isolate and limit the effects of any potential accident initiator.

In this report, general systems aspects of fusion magnet safety are examined first, followed by specific detailed analyses covering structural, thermal, electrical, and other aspects of fusion magnet safety. The design examples chosen for analysis are illustrative and are not intended to be definitive, since fusion magnet designs are rapidly evolving.

Analyses of the safety aspects of CTR magnet systems are at an early stage, because of the complex nature of such systems, involving interrelated mechanical, electrical, thermal, and hydraulic aspects that are difficult to analyze. Considerable simplification must be often made for analysis. In addition, CTR magnet designs are only beginning to be detailed enough for meaningful safety analyses.

Magnet safety analyses at BNL have concentrated on the TF coil systems for Tokamak reactors, partly because of limited time and manpower, and partly because more design information exists for these systems. Future BNL safety analyses will cover other parts of Tokamak magnet systems (e.g., the OH and divertor coils), and other CTR magnetic confinement concepts.

The BNL work described in this report has investigated a number of potential generic problems relating to magnet safety to see if there are design approaches that will eliminate or mitigate such problems. It has not been the intent of this work to assess the safety and reliability of fusion magnet designs. Definitive assessments will require more detailed designs and the development of improved analysis methods. Such assessments will probably be required before EPR-1 begins construction, and improved various analytical techniques are needed for this task.

Included here is a comprehensive collection of design and operating data relating to the safety of existing superconducting magnet systems. In some respects, this experience will be relevant because certain similar problems can be expected. It can also be significant by showing that in some respects CTR magnet systems have limited experience to draw on and are a large extrapolation from present knowledge.

The remainder of the overview, lists the main conclusions developed from the work to date. These should be regarded as initial steps. Since this study has concentrated on examining potential safety concerns, it may tend to overemphasize the problems of fusion magnets. In fact, many aspects of fusion magnets are well developed and are consistent with good safety practice. A short summary of the findings of this study is given in Table I-1.

TABLE I.1

SUMMARY OF FINDINGS ON CTR MAGNET SAFETY AND RELIABILITY

ELECTRICAL

- . HELIUM IS WEAK LINK IN COILS WITH POOL COOLING - VOLTAGE WITHSTAND CAPABILITY FOR COILS DURING QUENCHES IS $\leq$ 1000 VOLTS.
- . RADIATION EFFECTS [H_2 EVOLUTION FROM SOLID DIELECTRICS, DIRECT IONIZATION IN He] SERIOUSLY REDUCE DIELECTRIC STRENGTH.
- . RIGID METAL TF COIL CONSTRUCTIONS (E.G., UWMAK BOLTED SS DISC, ORNL WELDED SS CONDUITS) FORM EQUIPOTENTIAL SURFACES PERMITTING BREAKDOWN BETWEEN WIDELY SEPARATED CONDUCTORS.
- . ORNL TF COIL CONSTRUCTION LIMITS VOLTAGE WITHSTAND CAPABILITY TO $\sim$ 500 VOLTS IF CRACKS APPEAR IN GLUE BETWEEN THE CONDUCTOR JACKET AND THE SS WELDED CONDUITS.
- . ELECTRICAL BREAKDOWN IS POSSIBLE IN COILS DURING QUENCHES WITH RELATIVELY SMALL TEMPERATURE INHOMOGENEITIES.
- . SECONDARY ARCS SHOULD BE DELIBERATELY GENERATED IN A TF COIL IF A PRIMARY ARC IS DETECTED, SO AS TO MINIMIZE THE AMOUNT OF ENERGY LOCALLY DEPOSITED.

STRUCTURAL

- . RELATIVELY MODEST TEMPERATURE INHOMOGENEITIES DURING QUENCHES CAN CAUSE LOCAL PLASTIC DEFORMATION AND/OR FAILURE OF MAGNET REINFORCEMENT AND/OR CONDUCTORS.
- . RELATIVELY LARGE FAILED REGIONS (E.G., 30% OF THE RADIAL THICKNESS) CAN OCCUR IN UWMAK TF COILS WITHOUT COIL DISRUPTION, BUT SUBSTANTIAL PLASTIC DEFORMATION OCCURS.
- . SMALL CRACKS (E.G., IN THE ORDER OF 1 CM.) IN THE UWMAK DISCS DO NOT APPEAR TO PROPAGATE [UNDER ELASTIC, HOMOGENIZED APPROXIMATIONS], NEGLECTING CYCLING EFFECTS.
- . UWMAK TF COILS HAVE ACCEPTABLY LOW STRESSES DURING STRONG EARTHQUAKES [FOR IN PLANE LOADINGS AND HOMOGENIZED APPROXIMATIONS].
- . PRESENT STRUCTURAL ANALYSIS CAPABILITY MUST BE GREATLY IMPROVED TO ADEQUATELY ANALYZE CTR MAGNETS. HOMOGENIZED STRUCTURE, NEGLECT OF INTERLAMINAR SLIP, SIMPLIFIED FRACTURE THEORY, ETC., CAN CAUSE SERIOUS ERRORS, ESPECIALLY FOR FAILURE EVALUATION.
- . ERRORS IN EXISTING STRUCTURE CODES HAVE BEEN FOUND AND MANY ASPECTS REMAIN TO BE VERIFIED.
- . DEVELOPMENT OF A BNL 3D ELASTIC-PLASTIC LARGE DEFORMATION CODE (NFAP) IS WELL UNDERWAY.
- . DEVELOPMENT OF A SPECIAL ELASTIC-PLASTIC ISOPARAMETRIC ELEMENT FOR COMPLEX HETEROGENEOUS CTR MAGNETS IS NEEDED.
- . CONSTITUTIVE RELATIONS FOR COMPLEX STRUCTURES AND MATERIAL PROPERTY DATA ARE VERY INADEQUATE.

QUENCH SITUATIONS AND ENERGY REMOVAL

- . BREAKDOWN VOLTAGE IS TOO LOW TO PERMIT SIGNIFICANT ENERGY REMOVAL DURING QUENCHES OF LARGE TF COILS UNLESS MUCH HIGHER CONDUCTOR CURRENTS (E.G., 10^5 A) OR VERY LARGE NUMBERS OF SUBDIVISIONS ARE USED (E.G., 34 IN A UWMAK TF COIL).
- . LARGE TEMPERATURE INHOMOGENEITIES WILL OCCUR DURING A TF COIL QUENCH, LEADING TO ELECTRICAL BREAKDOWN AND MECHANICAL DAMAGE, UNLESS THE COIL IS DRIVEN NORMAL EVERYWHERE EARLY IN A QUENCH. DUMPING OF LIQUID HELIUM AND PRESSURIZATION WITH WARM HELIUM IS PROBABLY THE BEST METHOD FOR THIS. UNIFORM CU/AMP AND THERMAL CAPACITY/AMP ARE ALSO NECESSARY TO AVOID TEMPERATURE INHOMOGENEITIES.

DETECTION AND MONITORING

- . MONITORING OF HUNDREDS OF DETECTION POINTS APPEARS NECESSARY FOR LARGE MAGNETS. SEVERAL REDUNDANT DETECTORS/LOCATION WILL BE REQUIRED TO ENSURE RELIABILITY. AGREEMENT OF TWO DETECTORS WILL PROBABLY BE NECESSARY TO AVOID FALSE ALARMS.
- . DETECTION OF LOCALIZED REGIONS WHERE CONDUCTOR TEMPERATURE IS MONOTONICALLY INCREASING (DUE TO COOLANT CHANNEL BLOCKAGE, ETC.) APPEARS THE HARDEST DETECTION PROBLEM.
- . COMPUTER MONITORING AND DECISION ON MAGNET SHUTDOWN APPEAR NECESSARY.

REQUIREMENTS ON SAFETY AND RELIABILITY

- . EXPERIENCE ON EXISTING LARGE SC MAGNETS IS NOT SUFFICIENT TO ADEQUATELY PREDICT FAILURE RATES IN LARGE CTR MAGNETS. THE TECHNOLOGY OF EXISTING MAGNETS IS MUCH SIMPLER AND OPERATING EXPERIENCE TOO LIMITED. A NUMBER OF FAILURES HAVE OCCURRED IN EXISTING SC MAGNETS, THOUGH MORE EXTENSIVE ENGINEERING MIGHT HAVE PREVENTED THEM.

- . EXTREMELY LOW FAILURE RATES WILL BE REQUIRED FOR CTR MAGNETS IN POWER REACTORS. JUDGMENTS AS TO FAILURE RATE WILL HAVE TO BE BASED ON ANALYSIS SINCE RELEVANT EXPERIENCE WILL BE INSUFFICIENT FOR MANY YEARS.
- . A THICK EXTERNAL COIL CASE FOR TF COILS APPEARS VERY DESIRABLE TO CONTAIN THE COIL IF THE SC WINDING FAILS AND TO ISOLATE THE SC WINDING FROM EXTERNAL ACCIDENTS (E.G., BLANKET ACCIDENTS).
- . A VERY LARGE NUMBER OF ACCIDENT PATHWAYS CAN BE ENVISAGED FOR CTR MAGNETS, AND IT DOES NOT APPEAR FEASIBLE TO QUANTITATIVELY ESTIMATE ACCIDENT PROBABILITIES, AT LEAST FOR THE NEXT DECADE. ONLY QUALITATIVE JUDGMENTS WILL BE POSSIBLE.
- . THE PRIMARY CONTAINMENT FOR CTR REACTORS ENVISAGED IN REFERENCE DESIGNS APPEARS ADEQUATE TO CONTAIN A MAGNET ACCIDENT.

I-2 CONCLUSIONS

I-2.1 Applicability of Experience on Existing SC Magnets

1. Based on present superconducting magnet experience, there appear to be no fundamental reasons that would prevent the design and construction of safe and reliable CTR magnets. However, existing experience appears to be too limited, and the technology too different, to directly base predictions as to failure rates for CTR magnets. For example, the largest accumulated operating period for an existing magnet system is 15,000 hours for ANL's 12-foot bubble chamber magnet. This operating time has been accumulated in the last six years. Magnets in CTR reactors must operate at high plant factors, approaching 100% for 30 years.

2. A substantial number of failures have occurred in existing magnet systems, caused by a variety of reasons, e.g., burned power leads, hot spots, electrical breakdown, etc., and a comprehensive collection of such data is summarized in this report. Continued collection and evaluation of such failure statistics should help to some degree the designers of CTR magnet systems to avoid similar failures. Present fault detection and protection systems for existing magnets are not adequate for CTR magnets, in that they are generally manually controlled, and are relatively simple. CTR fault detection and energy removal systems will

probably have to be computer controlled, will monitor many more points, and will have to be very reliable.

3. CTR reactor magnet systems represent a large extrapolation over existing superconducting magnet systems with regard to both technology and degree of reliability required. In terms of technology, for example, TF coils for Tokamak commercial reactors will have approximately 100 times more stored magnetic energy than the largest existing superconducting magnet system, higher structural stresses, higher fields, more difficult geometry, and instead of operating in the DC mode, will be subjected to many thousands of cycles involving large pulsed magnetic fields and force loadings. Further, the time between failures for TF magnets will have to be considerably longer than the accumulated operating time on the relatively simple superconducting systems that have been built.

4. As a result of the above, it does not seem possible to directly base predictions about the safety and reliability performance of future CTR magnet systems on experience with existing superconducting magnet systems - the extrapolations involved appear too great. For a considerable time to come, it seems likely that predictions about the safety and reliability performance of CTR reactor magnet systems will have to be based on analytical methods.

I-2.2 Safety Detail in Present Fusion Magnet Designs

5. In general, to date, CTR magnet designs have not specified safety systems and procedures, except to say that provision will be made for removing stored energy in the event of accident. Safety analysis work at BNL, however, indicates that safety considerations can have very important effects on what options are acceptable in CTR magnet design. It thus seems necessary to realistically investigate safety systems and problems as an integral part of magnet design, rather than to design magnets and then attempt to retrofit safety systems to given designs.

6. CTR magnet designs are not detailed enough, nor is basic information adequate in many respects, to permit one to make meaningful quantitative assessments of magnet safety and reliability performance. For some time to come, only qualitative safety judgments will be possible for various design approaches. Achievement of even this capability, however, will require much more analysis than has been carried out to date. The many possible accident pathways will have to be specified and compared for each design approach, and the consequences of different accident initiating events assessed using a variety of structural, heat transfer, and electrical analyses.

I-2.3 Accident Classification for Fusion Magnet Systems

7. Analyses of the consequences of possible CTR magnet accidents have not yet been made, nor the safety.

performance goals been specified. Upper limits to accident consequences can be approximated on the basis of fission reactor safety analyses and experience, however, and these limits prove helpful in estimating performance goals, until detailed analyses are made on the consequences of CTR magnet accidents. It should be emphasized that accident consequences in CTR reactors can be made much less than those of fission reactors, since with suitable design, the radioactive inventory in fusion reactors can be many orders of magnitude less than that in fission reactors.

8. For purposes of this study, four categories of magnet accidents have been assumed for CTR reactors. Category 1 corresponds to sufficient damage to the magnet system that replacement is necessary, but no radioactivity is released. Category 2 corresponds to a magnet disruption with radioactivity release but without breach of the radiological containment structure. This would be analogous to a core melt accident in an LWR without breach of containment. Categories 3 and 4 correspond to magnet disruption and breach of containment, with two different levels of release to the environment, and correspond to analogous types of release for fission reactors. In terms of plant reliability

and economic losses, an upper bound to the allowable probability for a category 1 accident would appear to be on the order of 1 to 3×10^{-2} per reactor year of operation. An upper bound for a category 2 accident would appear to be on the order of 10^{-4} per reactor year of operation, which is similar to the estimated probability for a LWR core melt without release of radioactivity to the environment.

9. The assumed lower limit to the average time interval between category 2 accidents is three orders of magnitude larger than the total accumulated operating time on large superconducting magnet systems. Even the average time interval between more acceptable category 1 accidents has a lower limit approximately an order of magnitude larger than the accumulated operating time. Thus experience on existing large superconducting systems (leaving aside the fact that existing large systems are technically simpler) is insufficient to make meaningful assessments of the safety performance of CTR magnet systems.

I-2.4 Accident Pathways for Fusion Magnet Systems

10. Because of the inherent complexity of CTR magnet systems and the many accident pathways that have to be investigated, event tree and fault analyses tend to be very complicated. In addition, magnet designs are not yet very well

detailed, and the basic analytical methods in many cases (structural, heat transfer, etc.) are not very well developed. As a result, it appears that it will be a number of years before meaningful quantitative estimates of the probability of various types of accidents can be made. It appears likely that techniques similar to those developed for estimation of accident probabilities in fission reactors (e.g., WASH 1400) can be applied to CTR magnet systems. It should be realized, however, that quantitative investigation of CTR magnet systems by these techniques appears to be considerably more difficult and complex than was the case for LWR's, and a correspondingly greater manpower effort will eventually be required. For the next few years, however, it appears desirable, and indeed necessary, to develop qualitative event and fault trees for CTR magnet systems, and not to attempt quantitative investigations. Development of such qualitative trees will assist in overall system design as well as helping to pinpoint some of the problems to be investigated in detail.

1.1. A large number (~25) of potential accident initiators were identified and a list is included in the report. Each accident initiator can in turn result in a large

number of possible accident pathways, depending on the dynamics of the accident, situation, the nature of remedial actions, and the success or failure of various safety systems. Several illustrative simplified event and fault trees were developed in this study, and proved to be very helpful in visualizing accident possibilities. It should be emphasized, however, that a much more extensive effort will be necessary to even qualitatively investigate the complete range of accident possibilities. Based upon present thinking, a rough assessment was made as to the accident initiators that would seem of most concern. These were (no order of importance was attempted): electrical breakdown, conductor breakage, lead failure and coolant channel blockage.

12. In the event of a magnet disruption, the primary containment system of the reactor could be subjected to the impact of portions of the magnet traveling at velocities on the order of 100 meters/second. The disruption of a TF coil could cause disruption of adjacent coils which then trigger more coils. The primary containment for CTRs will probably be similar to the PCRV developed for the HTGR, being reinforced concrete on the order of 10 to 15 feet thick with an internal liner. Detailed analyses have not been carried out

on the structural effect of missile impact on the primary containment. However, it should be possible to design the containment so that penetration by missiles is very unlikely, though it may not retain leak tightness.

I-2.5 Control Strategies for Failure Situations

13. Enclosure of the TF superconducting windings in a thick external coil case appears to be a very interesting design option from the standpoint of safety. The external case could be sized to contain the SC windings even if one assumes the very unlikely event in which the windings completely lose structural integrity but continues to carry full current. [Such an event would correspond to an arc cutting completely across the cross section of the coil, for example.] The cross sectional area of such a case would be comparable to that of the stainless steel reinforcement in the 4°K SC winding, but the case could be at room temperature and constructed of relatively cheap materials (e.g., ordinary steel or aluminum alloy). The cost of such cases would appear to be a small part of the total magnet system cost. There does not seem to be any problem of limitations of space for the coil case with the exception of the portion of the coil on the inside of the torus. Here the case could be designed to be part of the shield. If the case were aluminum, it could function as

a passive ERS to dump part of the stored energy in the coil. Besides reducing the chances of a coil disruption from internal causes, such a case would appear to greatly reduce the chances of accidents caused by events occurring in reactor systems other than the magnet. For example, without a coil case, coolant line ruptures, fires, etc., would appear to have a high probability of causing coil disruption. With a coil case, analyses of accident pathways would be much simpler, and quantitative estimates would be much easier to make. Finally, a thick external coil case would appear to considerably simplify the task of laterally bracing the coil against out of plane loadings. Such loadings will result during normal operation from pulsed transverse fields from OH, divertor and plasma currents, and may also occur if TF coils quench non-uniformly.

14. If an accident situation develops, appropriate remedial action should be undertaken to minimize the consequences of the accident. It appears probable that such actions will have to be pre-planned and automatically triggered by magnet safety control systems, without the possibility of disconnection or override. (i.e., interlocks to the reactor operating systems will be necessary). The complexity of CTR magnet

systems, the massive amounts of monitoring, and the high probability (on the order of 30%) of wrong judgment of human operators in high stress situations indicate that remedial actions should be automatic. Human intervention could probably be allowed in decisions to shutdown magnet systems before automatic shutdown would be triggered. Present magnet systems rely chiefly on human operators, and extensive development of complex automatic safety systems will be required for CTR magnets, as well as detailed accident analyses and simulations to determine optimum response patterns.

15. Accident response patterns have been investigated for some accident situations in which the optimum response appears relatively clear-cut. The examples considered are intended as illustrative and should not be regarded as definitive. Extensive analysis would be required to definitively specify optimum responses. For the case of an arc in a TF coil, one should probably cause electric breakdown at a relatively large number of points in the coil, so that the resultant internal arcs will minimize the energy dump at any one location, and at the same time drive all other coils in the TF assembly normal so as to minimize the chance of cascading disruption. For the case where coolant flow in a lead drops or its temperature is rising, one should

probably switch to a back-up lead and wait to see if the corrective action works. If not, the coils can be shutdown. Detailed analyses (including structural effects) should be carried out comparing the consequences of shutting down one TF coil (and letting others in the assembly continue to carry current) versus shutting all TF coils down if one coil develops trouble. Arguments can be presented for both courses of action and no conclusion as to the optimum course appears possible at this point. Leaving the other coils on while one is shutdown will result in production of large lateral forces on adjacent coils, and a shift from a constant tension loading condition. Extensive lateral supports between TF coils appear necessary. In addition, the changing magnetic fields caused by shutoff of a coil may trigger accident situations in adjacent coils, while at the same time making detection of such situations very difficult or impossible. On the other hand, shutting down all coils by driving the conductors normal may cause magnet damage. For example, this problem of optimum response would arise if detectors indicated that a local region in one TF coil was monotonically increasing in temperature.

16. A temperature equalization system (TES) appears to be the primary line of defense in most accident situations. This would drive all conductors in a coil normal as early as possible in an accident in order to minimize the chances of mechanical damage or electrical breakdown resulting from temperature differences.

17. The best two methods for a TES appear to be either a helium dump, followed by re-pressurization with He gas (which should keep relatively high dielectric strength), or the use of non-inductive heater wires adjacent to the conductors. The inductive reactance of the conductor winding appears too great to permit significant conductor heating by imposed current fluctuations, and an ordinary helium dump without re-pressurization appears to be not certain enough of driving all portions of the coil (and would result in low helium dielectric strength).

I-2.6 Detection of Failure Situations

18. Detection of potential accident situations in CTR magnets appears to require a number of different types of detectors as well as a substantial number of detection points. Further, each detection point will probably have to be served by a number of redundant detectors to ensure that instrumentation failure does not result in a non-detection of a potentially dangerous situation. In addition, to keep false reactor and magnet shutdown signals to an acceptable level, e.g., one per year or less, it may be necessary to require agreement between two of the redundant detectors

at a given location before shutdown. Assuming instrumentation reliability to be somewhat better than that characteristic of the average reliability of fission reactor instrumentation, typically 3 redundant detectors would be required per location if shutdown were triggered by one detector, and 5 redundant detectors if triggered by coincidence of two detectors. The detector system here was assumed to be non-repairable, which will probably be true of the components inside the coils, but may not be the case for the portion of the system outside of the primary containment. Less redundancy will be required for the portions of the detection system that are repairable.

19. For a TF coil system, besides a detection system to locate regions where temperature is increasing, each UWMAK TF coil would appear to require an internal arc detection system (probably by voltage change), a helium pressure monitoring system for the dewar, a liquid helium level or flow monitoring system for the dewar, and a temperature and flow monitoring system for each lead. The resultant system of several hundred detectors (including redundant detectors) for a reactor is large but appears manageable.

20. The most severe detection problem appears to be associated with the detection of localized regions of the coil where conductor temperatures are monotonically increasing because of coolant channel blockage, spacer fracture and/or

movement, etc. If the coil is not shutdown, local conductor failure may occur with subsequent arcing.

Conductor lengths are on the order of 130 km in a UWMAK TF coil. On the order of 70 detectors per UWMAK TF coil (2/disc) appear to be required to ensure adequate detection of localized conductor regions with increasing temperature, if voltage taps along the conductor are used as the method of detection. In an actual TF coil assembly there will be a great deal of background voltage signals from small motions of the conductor and coil (i.e., expansion or tilt), flux jumps, etc., which must be unequivocally and quickly distinguished from a signal indicating a potential dangerous situation. Several thousand independent detecting circuits would then be required for the complete TF coil assembly, since each detection point must have several redundant circuits. Because of the very large number of detectors in this case, it seems highly probable that coincident agreement of two detectors at any detection point will be required to ensure that the false alarm rate is sufficiently low. This very large number of detectors poses problems of reliability, assembly, quality assurance, and cost. Other methods of detection should be investigated - for example, the use of non-inductive separate detector circuits wound together

with the conductor. This problem should be investigated in further detail.

I-2.7 Energy Dispersion and Removal in Fusion Magnet Systems

21. In general, energy dispersion systems, i.e., systems that either disperse by conduction or convection heat that is locally released to other regions in the coil, or cause energy deposition sites to be spread out over the coil, appear to be of limited usefulness in TF coils. The large size of TF coils, together with the need to minimize eddy current heating from pulsed fields, which prevents the use of massive amounts of high thermal conductivity paths, (such as high purity Al), prevents any significant dispersion by thermal conduction. Convective dispersion of heat would appear to be only significant for TF coil constructions that use force cooled conductors, since film boiling in pool cooling constructions greatly restricts convective heat transfer. The energy dispersion system that seems of the most interest is a system to prevent an internal arc from dumping most of the coil's energy at a relatively small region of the coil. If an arc was detected, this system would generate many additional distributed internal arcs inside the coil so that no local region received an unsafe amount of energy. When desired, these multiple internal arcs could be readily produced by a system of high voltage cables.

22. Active energy removal systems appear to be of limited usefulness for present TF coil designs. The allowable voltage across leads during an energy dump appears to be relatively low, at the most about 1000 volts, so that coil discharge times are substantially greater than the time scale on which most accident situations would occur. This condition could be changed by either using a relatively large number of subdivisions in each TF coil (e.g., each disc in a UWMAK-1 coil could be one subdivision with its own pair of leads for energy removal), or by using a much higher conductor current (e.g., 10^5 amp). In either case, energy removal times could be decreased to ~100 seconds for a UWMAK coil. The high conductor current method appears feasible in terms of energy dump and heat leak across leads. Its feasibility in terms of conductor fabrication, magnet assembly, and losses due to pulsed fields would have to be evaluated, however. The many subdivision approaches appears feasible, but problems of dewar constructions, switching, and control should be investigated.

23. A passive energy removal system in which a portion of the TF coil's magnetic energy is externally dumped by inductive coupling to an external room temperature winding appears attractive. The major portion of the TF coil and energy will

still have to be internally dumped in the coil or removed through its leads, however. If a passive external winding is used, it should be capable of carrying the magnetic forces resulting from its induced current. A passive ERS appears to be desirable if combined with an external coil case to contain a TF coil if the primary superconducting winding disrupts. The external coil case could be made of aluminum for example.

I-2.8 Structural Effects in Accident Situations

24. A number of serious structural problems can arise if TF coils partially or completely quench. These problems can be mitigated to some extent by appropriate design, or by taking correct remedial action when a quench situation develops.

25. If a non-uniform temperature distribution develops in the coil, substantial plastic deformation of the colder portions of the coil may result since they will carry an above-average share of the magnetic force loading. This effect has been investigated for a UWMAK -1 TF coil using a 2-D elastic-plastic finite element code. A number of cases were assumed comprising heated zones of varying radial and longitudinal (i.e., along the conductor) extent centered at various positions in the TF coil. For each case the amount and location of plastic deformation around the heated zone was

then determined as a function of the temperature of the heated zone. It was found that substantial deformation would result if the heated zone radially extended across approximately 2/3 of the coil and reached a temperature of $\sim 300^{\circ}\text{K}$. The longitudinal extent and location of the heated zone also affected the results. The substantial plastic deformations found in the analyses, though sufficient to damage the coil to the extent that it would have to be replaced, were not great enough to directly cause coil disruption. A number of secondary processes that might lead to coil disruption can be visualized but these were not analyzed. These secondary processes include: mechanical distortion of conductors sufficient to cause touching and subsequent arcs; plastic yielding or fracture of superconductors (e.g., Nb_3Sn) causing loss of current carrying capacity which then results in conductor heating and possible failure; degradation of electrical insulation leading to breakdown; and blocking of coolant channels.

26. The design of the UWMAK-1 TF coil inherently results in large inhomogenous temperature distributions in the radial direction, since both the amount of copper per unit current and the thermal capacity per unit current are much less at

the outside of the disc than at the inside. Gross plastic deformation of the disc assembly will occur even if all conductors go normal at the same time. The design should be modified so as to have the same copper per unit current and thermal capacity per unit current at all points.

27. The plastic deformations found for the UWMAK-1 TF coil in the non-uniform quench case assumed that all discs had the same temperature distribution in the radial and longitudinal directions. The much more likely case where a few discs are at a substantially higher temperature than the remaining discs was not analyzed in detail because a three-dimensional plastic finite element analysis is required. It is planned to undertake such analyses in the future. Approximate analyses, however, indicate that this case will be considerably worse than the case where all discs are assumed to have the same temperature distribution. The heated discs will try to radially expand more than the unheated discs, but will be restrained by the aluminum bolts tying the assembly together. Substantial bending and failure of the bolts can be expected, even with relatively small temperature differences (e.g. $\sim 100^{\circ}\text{K}$) between discs. Besides deforming the TF coil assembly, such quenches may cause blockage of coolant channels due to spacer movement and fracturing as well as electrical breakdown if the deformed bolts short out conductors.

28. It appears very important to minimize the temperature level for which regions whose temperature is of monotonically increasing can be unequivocally detected, since initial temperature differences between regions will tend to be maintained even when the entire coil is driven normal. This temperature level probably should be no more than $\sim 100^{\circ}\text{K}$, and possibly should be substantially less. Detailed three-dimensional plastic finite element structural analyses, combined with three-dimensional heat transfer analyses, should be carried out to determine this limit.

29. Even if one attempts to avoid large scale inhomogeneous temperature distributions in a UWMAK TF coil during a quench, by making the copper and thermal capacity per unit current everywhere uniform, and by driving all conductors normal if a portion of coil starts to climb in temperature above a few $^{\circ}\text{K}$, there still will be substantial temperature differences between the conductors and the stainless steel disc as a result of the very low thermal conductivity of the epoxy layer around the conductors. Stress analyses indicate that substantial plastic deformation of the conductors may occur at points where the conductors are tightly bound in the disc slots. Cracking of the epoxy layer may also occur.

30. Other types of TF coil construction, e.g., the welded force

cooled conductors (ORNL) or a pancake wrap (ANL) can be expected to undergo comparable plastic deformations if similar inhomogeneous temperature distributions develop. As with the UWMAK coil, such damage is unlikely to directly result in coil disruption, but secondary process [electrical breakdown, coolant channel blockage, conductor breakage (especially brittle superconductors)] might result in disruption.

31. 2-D elastic-plastic, finite element analyses were carried out to determine the consequences of a large crack or other failed region across a UWMAK-1 TF coil. Three cases were analyzed, corresponding to a crack length of either 3, 10, or 30% of the radial extent (93 cm) of the disc. It was found that the coil would not disrupt, even for a 30% radial crack, although substantial plastic deformation of the coil occurred. It was also found that the amount of plastic deformation was less if the portion of the coil around the crack tip were at a somewhat higher temperature than the rest of the coil. In effect, the heated zone sheds some load to remote, colder regions. Although plastic yielding is not sufficient to disrupt the coil, secondary process [electrical breakdown, conductor fracture, etc.] accompanying the plastic deformation may result in disruption.

It should also be noted that the elastic plastic code that was used for the analyses does not incorporate fracture mechanics behavior, so that propagating cracks cannot be followed.

32. Stress distributions in a TF coil with lumped reinforcement (i.e., the conductor winding is inside a thick stainless steel reinforcement structure) were investigated for several non-uniform quench cases using a 2-D elastic-plastic code. No plastic yielding of the stainless reinforcement structure was found for any of the cases. Local yielding of the conductor was found in some cases, however.

33. A 2-D elastic fracture mechanics code (which incorporates a hybrid element at the crack tip with elastic isoparametric elements for the remainder of the structure) was used to investigate whether or not an initial crack in the stainless steel disc of the UWMAX-1 TF coil would propagate under normal operating magnetic loads and temperature (i.e., 4°K). The analysis indicated that relatively small cracks (e.g., ~1cm long) do not appear to propagate, since the fracture toughness is not exceeded. Larger cracks are beyond the range of validity of the code since the disc then cannot be regarded as homogeneous. A more definitive answer to the conditions for crack propagation requires a 3-D inelastic

finite element analysis with the crack not limited to planar homogenous conditions. It is planned to carry out such analyses with codes being developed at BNL.

34. A seismic analysis was carried out using 3-D dynamic elastic codes on a UWMAC-1 TF coil. A standard earthquake input spectrum (1/2 of the El Centro earthquake) was used. No magnetic loadings were included in the analysis, however, and the TF coil structure was homogenized. Analyses were run with two different 3-D codes, and the results agreed relatively closely. Maximum stresses were relatively low, ~4000 psi. Inclusion of magnetic loads and taking coil heterogeneity into account may considerably increase the maximum stress, however. For the next step, it is planned to carry out such an analysis in which inelastic behavior will also be incorporated.

I-2.9 Improvements in Structural Analysis Capability

35. A new BNL 3-D non-linear finite element program (NFEP) incorporating large deformation plastic behavior is presently being tested and verified. Some of the verification cases are described in this report. When it is completed and documented, it will be distributed for CTR magnet design purposes.

Because of limitations on capacity and running time, present elastic and inelastic finite element codes cannot

truly model the complex structure inherent in CTR magnets. Considerable simplification is necessary, involving homogenization of materials with widely differing properties, neglect of interlaminar slip, etc. At best, one must grossly simplify the overall structure to obtain approximate solutions, which then act as boundary conditions for the investigation in finer detail of a portion of the structure. Lacking experiential verification of the validity of these simplifications, it is very difficult to estimate the degree of error introduced by them. This problem has been investigated, and it appears feasible to develop a special isoparametric element for CTR magnets. For TF coils, for example, each element would extend across the full width of the coil, but would be capable of accounting for the behavior of the heterogeneous structure, including inelastic effects, by use of a suitable number of internal integration points in each element. A relatively few number of isoparametric elements could then be used to closely model the entire TF coil structure. Analogous special 3D elements have been developed at BNL, for the very complex structure of PCRVS in HTGR'S. A PCRVS incorporates concrete, rebars, post tensioning members, penetrations, liners, etc., and its complexity is comparable to that in CTR magnets. Such

special elements have proven extremely useful in analyzing the conditions that lead to cracking and failure in PCRVS, and similar techniques should prove very desirable for CTR magnets. Development of such elements will require a substantial period of time and experimental verification, however.

36. The applicability of present fracture mechanics codes to CTR magnet problems is of very limited extent and doubtful validity. Only elastic fracture mechanics codes have been developed to any degree, and then only for very idealized cases involving homogeneous isotropic mediums with planar stress conditions. Extensions of fracture mechanics code capability to problems involving 3-D stress distributions with inelastic effects is being carried out at BNL.

37. Looking beyond conventional structural analysis codes, it appears necessary to develop in the next several years both CTR accident analysis codes and probabilistic failure codes. The former codes would analyze possible accident situations using coupled structural, heat transfer, and electrical analyses in a common finite element grid. The latter codes would predict the probability of component

failure using structural analysis methods combined with appropriate statistical distributions for various material properties. A probabilistic failure code for reactor steam generator tubes has been developed at BNL, for example. Such probabilistic methods appear necessary to determine adequate safety factors for complex structures.

I-2.10 Analysis of Temperature Excursions in Fusion Magnet Systems

38. A number of analyses have been carried out of temperature distributions in a UWMAK-1 TF coil during quench situations.

A 3-D finite difference code, Heating - 3, was used for these analyses. Because of capacity limitations, only 2-D idealized cases were run. In addition, because of the complex and uncertain nature of the convective heat transfer process in a UWMAK TF coil during a quench, a range of cases was investigated with various simplifying assumptions.

39. Substantial inhomogeneous temperature distributions, with temperature differences of several hundred $^{\circ}\text{K}$ radially across the discs, will be generated in the UWMAK-1 (and also UWMAK-2) TF coils, even if all conductors go normal at the same time. This results from the considerable radial variation across the disc in the amount of copper/amp of conductor current and the thermal capacity/amp. Presumably, this undesirable condition can be eliminated by suitable design changes.

40. Even if the above changes are made and the quench starts uniformly everywhere, substantial temperature differences (i.e., on the order of 100-200^oK) will be produced between the conductors and the center of the steel discs they are imbedded in.

41. A number of cases involving non- uniform quenches were studied. Here the quench starts at some point and spreads through the coil. It is assumed that no action is taken to drive the conductors in the TF coil normal. For the condition where conductors are thermally blocked, i.e., they are not cooled by the liquid helium between discs, the quench spreads the most rapidly. The rate of spread is fastest along the conductors, next most fastest radially across the disc, and slowest normal to the disc (because of the micarta spacers). The relative ratio for the various velocities is very roughly 100/10/1. It appears probable that substantial temperature inhomogeneities will be produced in the UWMMAK TF coil unless a temperature equalization system drives all conductors normal at an early state in the quench.

42. With liquid helium cooling, the spread rate of quenches is greatly reduced and they tend to remain localized in the region where the conductors are thermally blocked (i.e., by plugging of coolant channels, spacer fracture or movement,

or vapor lock, etc.). The localized regions monotonically increase in temperature with increasing time, however.

43. Other types of TF coil constructions (e.g., the pancake wrap) will probably exhibit a pattern of quench.

I-2.11 Electrical Breakdown in Fusion Magnets

44. For the type of magnet constructions with helium pool cooling (e.g., the UWMAK disc designs, or the ANL TEPR pancake wrap design) helium appears to be the weak link in electrical insulation, with a voltage withstand capability of only ~1000 volts. Three factors appear to cause this condition: a) helium gas density will be low in the vicinity of heated conductors at atmospheric pressure; b) helium can form Penning mixtures at small concentrations (i.e. a few ppm) of impurities that may either be present in liquid helium, and/or may be released (i.e. radiolytically generated H_2) from solid organic insulators during a quench; and c) background nuclear radiation will generate a relatively high ionization level in helium.

45. The most likely route for electrical breakdown with the UWMAK disc construction is from conductors to the disc through helium at the disc surface. The effective gap length for breakdown via this path is only 0.1 cm. The discs form

equipotential surfaces to which widely separated conductors can arc if sufficient voltage, i.w. ~ 1000 volts, develops. In addition, the aluminum bolts that tie the discs together in a rigid assembly also form equipotential surfaces, so that conductors on widely separated discs can break down via the bolts. The micarta spacers between discs thus do not seem to significantly increase voltage withstand capability. Break-down appears possible, either across leads (when removing energy from the magnet) or internally during a quench in which a small fraction, (e.g., $\sim 10\%$) of the coil is at a slightly different temperature than the rest.

46. The most likely breakdown path with the pancake wrap construction appears to be via helium filled cracks in the insulating spacers between pancakes. Such cracks may result from a variety of causes such as the pulsed force loadings imposed by time varying poloidal fields, when plasma burns start and stop, thermal and mechanical stresses during quenches, or imperfect assembly of pancakes and spacers. Considerable dielectric stress intensification (i.e., a factor of 2-3) will occur in the helium at the edge of conductor turns due to the differences in dielectric constant between spacers and helium.

47. The most likely breakdown path for the force cooled conductor construction proposed for the ORNL-EPR TF coils appears

to be via cracking of the insulating layer between the aluminum jacket and the stainless steel conduit. It is likely during quenches that some of the radiolytic H_2 generated in the insulating layer, (which will be frozen during operation at $4^{\circ}K$), will be released into such cracks. A few days operation has the potential of generating enough H_2 to lower breakdown voltage to ~500 volts. Welding of the stainless steel conduits together, as proposed by ORNL, will make the reinforcement structure an equipotential surface, and widely spaced conductors at different voltages may breakdown to this surface. As with other magnet constructions, cracks in the insulating layer may result from a variety of causes - i.e., pulse force loadings, thermal and mechanical stresses during quenches, and improper assembly. Breakdown may occur either during energy removal or during quenches involving non-uniform temperature distributions.

48. For the disc and pancake type constructions, voltage withstand capability should be considerably improved by pressurizing the helium to several atmospheres during accident situations. For the force cooled conductor construction with welded stainless steel conduits, it does not appear possible to significantly increase voltage capability unless non-organic insulators are used or non-cracking of the insulation can be assured. For all magnet constructions, electrical

problems can be substantially reduced by using much higher conductor currents (e.g., 10^5 amp) or many subdivisions per TF coil (e.g., each UWMAK disc might have its own set of leads).

49. Electrical breakdown may also be initiated by other effects, e.g., (a) mechanical distortion of conductors so that they touch, caused either by imperfect assembly of the magnet or a result of stresses present during normal and/or abnormal operating conditions; (b) bridging of insulation gaps by metallic particles introduced during magnet assembly. The determination of the probability of such occurrences will require extensive operational experience with large magnet systems under the conditions expected in CTR reactors.

50. If the stored magnetic energy released during an arc situation is deposited over the cross section of a TF coil winding in a relatively localized region of the coil, disruption may occur.

51. An arc will tend to rapidly spread transversely across the cross section of the coil. The initial heating rate will be several hundred KW, but will rapidly increase as the arc spreads. Adjacent conductors will probably arc because of thermal heating and/or electrical breakdown, and the equivalent electrical circuit for the coil will then become a set of magnetically coupled inductors with internal resistance connected in parallel through the arcs.

52. The very strong magnetic force on the arc will drive hot helium gas at high velocity to adjacent portions of coils in which interconnected He channels are present (e.g., UWMAK-1, ANL TEPR), causing them to rapidly heat.

53. To avoid the possibility of coil disruption, the best response to an arc situation appears to be to generate additional arcs throughout the coil, which should prevent large local deposition of magnetic energy. This can be done by deliberate electrical breakdown at many points, triggered by pulsed voltages on internal cables.

II GENERAL SYSTEM ASPECTS OF FUSION MAGNET SAFETY AND RELIABILITY

II-1 INTRODUCTION

II-1.1 System Safety and Reliability in General

As yet, fusion reactor designs are of a preliminary exploratory nature. Primary emphasis has been placed on preparing self-consistent conceptual reference designs that serve to highlight the direct engineering problems that may be encountered, as well as to develop solutions to such problems. As these designs evolve, considerations of safety and reliability naturally become part of the overall design goals. It seems best to bring such considerations in at a relatively early stage, however, since they can have an important impact on the kinds of engineering solutions that will be acceptable.

It thus seems appropriate at this time to begin examining in a fairly comprehensive manner questions of safety and reliability for fusion magnet systems. In general, large amounts of magnetic energy are stored in the magnet in such systems and the forces and stresses acting on the structures are large. The systems then to be expensive, and make up a large fraction of the total cost of a fusion reactor. Failure of a magnet system not only could cause large economic losses, but might result in accidents with serious damage to the reactor and possible

release of some of its radioactive inventory. The consequences of the latter event can be minimized by appropriate reactor design and selection of suitable blanket materials, however.

A number of conceptual fusion reactor reference designs have been presented that incorporate magnet systems. In general, these magnet designs are not detailed enough to carry out definitive safety analyses and assessments. Before such analyses and assessments can be made, it will be necessary to have much more detailed designs, including specification of safety systems for detection, protection and containment. In addition, more sophisticated analysis techniques will have to be developed along with more knowledge of basic mechanical and electrical properties. For example, definitive safety analyses will involve in-depth inelastic and fracture mechanics structural analyses, as well as elastic analyses, analyses of the fatigue effects from pulsed loadings on component lifetime; probabilistic failure analyses to estimate component reliability in light of inherent variations in material properties, loadings, flows, etc.; hot channel analyses to account for effects of possible partial or complete blockage of coolant channels and maldistribution of coolant

flow; and finally, detailed fault-tree analyses of the probabilities of potentially important accident sequences. It appears that considerable time and effort will be required before definitive assessments can be made.

However, safety scoping studies can be carried out using as a starting point the existing CTR magnet designs. These scoping studies can identify potential magnet safety problems and investigate possible solutions. The results of these studies can then be used as guide in avoiding design approaches where fundamental safety problems will be encountered, and can also serve to show what types of safety systems are desirable for fusion magnets.

At this point, it is useful to examine the overall system aspects of safety and reliability for CTR magnets, before investigating specific detailed questions. The large body of experience on fission reactor safety and reliability can be very helpful here. It should be emphasized that this comparison should not imply that the consequences of possible accidents in fusion reactors will be as severe as those in fission reactors since in fact, with fusion reactors, accidents can be much less severe. Application of the methodology and standards developed for fission reactors serve as a starting point

and should not be interpreted as final standards for fusion reactors.

Figure II.1 shows an idealized configuration for a Tokamak power reactor. The array of superconducting coils [TF, OH, divertor, and equilibrium (not shown) coils] is required for plasma confinement. The toroidal magnetic field (TF) is constant with time, and accounts for most of the system's stored magnetic energy. The magnetic fields of the other coils are generally pulsed, characteristically rising in a short time, on the order of seconds, and remain constant or slowly varying after that. The total stored magnetic energy in the magnet system will be very large, on the order of 10^5 MJ, for a commercial power reactor. The magnet structures inevitably will be under high stresses, since they must mechanically contain the large magnetic forces. In some ways they are analogous to large pressure vessels with the magnetic field acting like a high pressure gas. If only a small part of this potential energy were converted to kinetic energy, via a coil or dewar rupture, serious damage to the reactor and/or containment structure might occur. For example, 10 MJ corresponds to the kinetic energy of a 2,000 kg mass at a velocity of 100 meters/sec.

Such an accident might be initiated by a fault originating in the magnet system, or might be an amplified response to a smaller accident originating external to the magnet, e.g., the rupture of a coolant line to the blanket.

The consequences of such an accident, if it should occur, depend on many factors which are not well defined at this time. These include: the amount and nature of the radioactive inventory in the blanket, shield and coolant; the structural strength of the blanket, and coolant lines; nature of coolant; amount of energy release in the accident; and degree of containment.

The magnet systems in existing fusion reactor reference designs are only isolated from the blanket, shield and coolant systems by a thin, relatively low strength dewar which could not protect the magnet in the event of an external accident or protect the rest of the reactor in the event of a coil rupture. Effective isolation can be incorporated into the design, however, and it would appear desirable to do so. This could greatly reduce the consequences of a magnet or dewar rupture, and could also protect the magnet system from external accidents. One might place a case around each magnet coil, for example. The case could be thick enough to contain the coil even if a complete coil rupture occurred. The feasibility of such

an isolation system is discussed in more detail in a later section.

II-1.2 Radiological Containment, Accident Situations and Classifications

Two levels of radiological containment are indicated in Figure II.1. (A dual containment approach is used in UWMAK-1, for example.) The inner or primary containment surrounds the reactor and magnet system, and also serves as a biological shield. It would probably be constructed from reinforced concrete with an inner liner, similar to the PCRV for an HTGR, and would be on the order of three meters thick. The outer or secondary containment encloses the inner containment shell, as well as most of auxiliary systems necessary for reactor operation, such as tritium recovery systems, plasma heating systems, etc. The reactor systems which require direct access will be in this region, including the magnet control system. The magnet safety systems that are integral with the various magnet coils, such as detection systems, energy removal systems, etc., will have to be inside the inner containment shell, and access to this region may be difficult and limited. If the blanket and shield are constructed from materials which activate with resultant half lives substantially greater than a few days, the magnet safety systems in this region will have to be remotely maintained.

TABLE II.1

CONTAINMENT LEVELS

<u>MAGNETIC - LEVEL</u>	<u>RADIOLOGICAL - LEVEL</u>
PRIMARY - COIL	NA
SECONDARY- CASE SURROUNDING COIL	NA
TERTIARY ----->	PRIMARY - BIOLOGICAL SHIELD SURROUNDING REACTOR
QUATERNARY ----->	SECONDARY - CONTAINMENT BUILDING

TABLE II. 2

ILLUSTRATIVE CLASSIFICATION SCHEME
FOR MAGNET ACCIDENTS

<u>CLASS</u>	<u>ACCIDENT CONSEQUENCES</u>
1A, 1B, 1C	<ul style="list-style-type: none"> . DAMAGE TO MAGNET COIL(S) AND/OR CRYOSTAT(S) . REPLACEMENT REQUIRED . NO RADIOACTIVITY RELEASE ANYWHERE
2A, 2B, 2C	<ul style="list-style-type: none"> . RUPTURE OF MAGNET COIL(S) [AND CASE(S) IF PRESENT] . BLANKET PENETRATION . HEADER AND COOLANT PIPE RUPTURE - LOSS OF BLANKET COOLANT . RELEASE OF TRITIUM - RELEASE OF RADIOACTIVE COOLANT AND DISPERSION OF RADIOACTIVE STRUCTURE . NO PENETRATION OF OUTER RADIOLOGICAL CONTAINMENT STRUCTURE
3A, 3B, 3C	<ul style="list-style-type: none"> . MAGNET RUPTURE, BLANKET PENETRATION, HEADER AND COOLANT PIPE RUPTURE, LOSS OF BLANKET COOLANT . BREACH OF RADIOLOGICAL CONTAINMENT STRUCTURE . RELEASE OF LARGE FRACTION OF REACTOR TRITIUM INVENTORY TO ENVIRONMENT

TABLE II. 2 (CONTINUED)

<u>CLASS</u>	<u>ACCIDENT CONSEQUENCES</u>
3A, 3B, 3C (CONTINUED)	. CONTAINMENT OF VIRTUALLY ALL OF RADIOACTIVE COOLANT AND STRUCTURE
4A, 4B, 4C	. MAGNET RUPTURE, BLANKET PENETRATION, HEADER AND COOLANT PIPE RUPTURE, LOSS OF BLANKET COOLANT, BREACH OF RADIOLOGICAL CONTAINMENT STRUCTURE . RELEASE OF VIRTUALLY ALL OF REACTOR TRITIUM INVENTORY TO ENVIRONMENT . RELEASE OF LARGE FRACTIONS OF RADIOACTIVE COOLANT AND STRUCTURE INVENTORY TO ENVIRONMENT
SUB CLASS A	NO EXTERNAL COIL CASE -DISTRIBUTED REINFORCEMENT CONSTRUCTION
SUB CLASS B	NO EXTERNAL COIL CASE - LUMPED REINFORCEMENT CONSTRUCTION
SUB CLASS C	EXTERNAL COIL CASE - DISTRIBUTED OR LUMPED REINFORCEMENT CONSTRUCTION

TABLE II.3

POSSIBLE ORDERING AND ALLOWABLE UPPER BOUNDS
FOR MAGNET ACCIDENT PROBABILITIES

<u>ORDERING IN CLASS</u>	<u>ALLOWABLE UPPER BOUND FOR CLASS</u>
$P_{1A} \sim P_{1B} \sim P_{1C}$	$P_1 \leq 1 \text{ to } 3 \times 10^{-2} / \text{REACTOR YEAR}$
$P_{2A} > P_{2B} \gg P_{2C}$	$P_2 \leq 10^{-4} / \text{REACTOR YEAR}$
$P_{3A} > P_{3B} \gg P_{3C}$	$P_3 \leq 10^{-5} / \text{REACTOR YEAR}$
$P_{4A} > P_{4B} \gg P_{4C}$	$P_4 \leq 10^{-6} / \text{REACTOR YEAR}$

Besides serving as radiological containment, these structures can also contain portions of the magnet or dewar in the event of rupture. Table II.1 shows the correspondence between levels of magnet containment and levels of radiological containment. If there is no case around the magnet coils, then the secondary magnetic containment corresponds to the primary radiological containment.

Table II.2 defines four classes of magnet accidents. The first class includes all accidents in which part or all of the magnet system is damaged enough to require replacement, but no radioactivity is released. The second class includes all accidents where the coil or dewar ruptures, with subsequent containment of the released radioactivity by the primary or secondary reactor containment. This would roughly correspond to a partial core melt in a fission reactor with no release to the environment. In the third class, a large fraction of the reactor's tritium inventory is released to the environment, but virtually all of the radioactive coolant and blanket structure is retained. In the fourth class a large fraction of the coolant and structure is released to the environment. The accident subclasses A, B, C relate to the type of magnet construction and whether or not there is an external case for the coil.

Allowable upper bounds for probability of an accident as a function of class of accident are shown in Table II.3. These values are only meant to be illustrative and should not be interpreted as firm standards.

A class 1 accident, even though it poses no safety problems, would have to be a rare event. To remove and replace a significant portion of the magnet system (remote operation would probably be necessary if there were substantial residual radioactivity from the blanket and shield), would require months, and would be very expensive both in terms of cost due to down time and also direct cost of replacement. An average failure rate substantially greater than 3×10^{-2} /reactor year implies that a fusion plant would be shut down several times for magnet replacement during its nominal 30-40 year life. This is probably unacceptable, and the value of 1 to 3×10^{-2} /reactor year probably cannot be exceeded if fusion reactors are to be reliable and economic.

An allowable upper bound of $\sim 10^{-4}$ /reactor year is indicated for a class 2 accident. This corresponds to the estimated probability for a core melt in PWR's⁽¹⁾ without radioactive release to the environment. The effects of this accident would be confined to the reactor. A higher

probability for class 2 accidents might be acceptable for CTR's, depending on accident consequences. However, without further detailed study, at this point it seems best to remain conservative.

An allowable upper bound of 10^{-5} /reactor year is indicated for a class 3 accident, and 10^{-6} /reactor year for a class 4 accident. For comparison, the two worst release categories for a PWR accident⁽¹⁾ are category 1 [steam explosion of reactor vessel with rupture of containment] and category 2 [core melt with containment rupture caused by hydrogen burning and steam overpressure]. Upper bounds to the probability of these accidents are estimated to be 7×10^{-7} /reactor year for category 1 and 5×10^{-6} /reactor year for category 2.

Total operating time for all existing large superconducting magnet systems is on the order of 20,000 hours. This is far too short to form any judgment on the basis of experience as to whether or not large superconducting magnet systems can achieve the level of safety and reliability necessary for CTR magnets, even for class 1 accidents. Further, CTR magnet systems will be very different from the large superconducting bubble chamber magnets. Stored energy will be two or more orders of magnitude

greater, mechanical stresses will be considerably higher and geometry will be more complex. In addition, in the case of Tokamak reactors, conductors will experience rapid changes in magnetic field, magnet structures will be subjected to large pulsed force loadings, and the amount of normal metal stabilizer that is allowable may be quite limited. Finally, it may be necessary to use brittle Nb_3Sn superconductor for plasma confinement, instead of ductile NbTi superconductor.

It thus appears that estimates of CTR magnet reliability and accident probability must be based on theoretical analyses and not on direct experience. This situation will probably continue for many years, since CTR magnet designs will probably not be standardized until a relatively large number of commercial fusion reactors have been built. Since these estimates generally will not be verifiable by experience on operating systems, it seems likely that a conservative design philosophy will be adopted, with substantial safety factors.

The development of the capability to make meaningful estimates of accident probability will require considerable time and effort. Figure II.2 illustrates the types of tasks that probably will be involved. The approach would be similar to that undertaken for fission reactor safety studies and Figure II. 2 is adapted from such work⁽¹⁾.

Knowledge of CTR magnet characteristics has not developed to the point where all these tasks could be undertaken. At present work is only beginning on those tasks within the "A" level boundary, i.e., identification of potentially important accident sequences and investigation of the engineered safeguard features that will be required. It is likely that work will continue at this level for the next few years, and must precede work on the tasks inside the "B" level where, definition of complete magnet designs, including integration of a variety of safety systems, and detailed qualitative examination of all important failure and accident modes will be necessary. Quantitative estimation of failure probabilities and risk assessment is further off. Work on these tasks, shown in the "C" level boundary, will probably not begin in detail for at least another decade. As a point of comparison, the WASH 1400 study on fission reactor safety quantitatively estimated accident probabilities for LWR's only for a relatively limited range of possible accidents. Even so, the study took approximately three years and cost several million dollars. CTR magnet systems will be complex, and the range of possible accident sequences will be substantially greater than those for fission reactors. Analyzing them

will require a correspondingly greater effort than that required for WASH 1400.

In general, each given accident class can have a large number of accident initiators, and there are a large number of possible paths for each initiator. The probability of each accident sequence must be estimated, and all important accident sequences summed to obtain an overall probability for the occurrence of a given class of accident.

II-1.3. Safety Instrumentation and Computer Control

The safety central systems for a CTR magnet assembly will be complex. Figure II.3 illustrates one possible arrangement of safety systems. A large number of various types of sensors will be required to monitor conditions in the magnet coils and to detect events which can lead to magnet damage or failure. These sensors will have to be interlocked so that they cannot be disconnected without shutting off the reactor, and will have to be frequently tested to see if they are functioning properly. In addition, sensors will probably have to be redundant to some degree.

The flow of information will be too great to be processed by reactor operators and will have to be processed by computer. Certain portions of this information would be automatically

displayed to the reactor operators, while other portions can be displayed upon command. Four levels of action are shown in Figure II.3. In level 1, potentially serious conditions in the magnet system would be displayed by the computer to alert the reactor operators. In level 2, conditions would be judged by the computer to be sufficiently serious that the reactor would be shutdown and appropriate action taken (temperature equilization, energy removal) to safeguard the magnet. At level 2 the computer actions could not be overridden by reactor operators. Level 3 would be an auxiliary level of shutdown and protection that could be triggered by reactor operators, if desirable, before automatic actions were taken on level 2. Level 4 would correspond to shutdown and protection actions necessary because of safety system malfunction, such as malfunction of the computer or data system.

Whether or not reactor operators should override computer actions on level 2 is a difficult question. Situations might arise where pre-planned actions are thought inappropriate. However, it is quite possible that in high stress emergency situations, wrong decisions will be made by humans. Studies have shown ⁽¹⁾ that in approximately 20-30% of high stress emergency situations actions by highly trained individuals either make no difference or worsen the situation.

II-2 ENGINEERED SAFETY FEATURES OF FUSION MAGNET SYSTEMS

II-2.1 Integrations of Engineered Safety Features into Magnet Systems

Table II.4 lists various possible engineered safety features for CTR magnet systems. These ESF's are listed by function, and the mechanics of how they could perform their given function are discussed later.

The detection systems (DS) monitor the conditions important to magnet safety. The magnet can be considered to have four regions in which such monitoring can be carried out. The conductor region (including reinforcement and insulator), the lead region, the cryostat region, and the external containment case (if present).

Depending on the region, not all of the various DS's would be needed. In the case of a conductor break, protective action would have to be initiated immediately. For the other detection systems, there may be some delay time allowable between detection of an initial signal and the initiation of protective action. Particular sensors might be involved in more than one DS. For example, monitoring of voltage at a number of points along the conductor might be used to both detect a conductor break and to determine whether

a normal region would continue to increase in temperature. The operating regime of the CTR magnet system will have a strong impact on the design and sensitivity of the various DS's. Detection of abnormal conditions in a DC magnet, e.g., those in mirror reactors, will probably be considerably easier than in pulsed magnets, e.g., those in Tokamak reactors. The pulsed nature of OH coils in Tokamaks is readily apparent, but even the TF coils will be exposed to rapidly varying fields and forces when the plasma is turned on. In addition, there will be some variation of force during the plasma burn, though the rate of variation will be less than during the plasma turn-on.

Some detection functions can be carried out fairly easily, such as loss of flow and/or coolant, while others, such as excessive strain, will be difficult and in some cases not practical.

The temperature equalization systems (TES) would operate if the detection systems indicated that the magnet was starting to quench. Excessive temperature gradients inside the magnet structure could result in mechanical damage to the conductor, reinforcement and/or insulation, which then could lead to magnet failure. The TES would act to minimize temperature differences by driving all of the

TABLE II.4

POSSIBLE ENGINEERED SAFETY FEATURES (ESF)
FOR FUSION MAGNETS

		<u>MAGNET REGION</u>			
1.	<u>DETECTION SYSTEMS (DS)</u>	<u>CONDUCTOR</u>	<u>LEADS</u>	<u>CRYOSTAT</u>	<u>CASE</u>
A.	NORMAL REGION WITH MONOTONICALLY IN- CREASING TEMPERATURE	YES	YES	NO	NO
B.	CONDUCTOR BREAK	YES	YES	NO	NO
C.	LOSS OF FLOW AND/OR COOLANT	YES	YES	YES	NO
D.	EXCESSIVE TEMPERATURE	YES	YES	YES	NO
E.	EXCESSIVE $\dot{B}$	YES	MAYBE	NO	NO
F.	ELECTRICAL SHORT	YES	YES	NO	NO
G.	EXCESSIVE PRESSURE	YES	YES	YES	NO
H.	EXCESSIVE STRAIN	YES	YES	YES	YES
I.	RADIATION LEVEL	YES	YES	YES	NO
2.	<u>TEMPERATURE EQUALIZATION SYSTEMS (TES)</u>				
A.	OVERCURRENT AND/OR HIGH FREQUENCY ON CONDUCTOR				
B.	HEATERS				
C.	COOLANT PRESSURIZATION				
D.	COOLANT DUMP				

TABLE II.4 (CONTINUED)

- 3. ENERGY DISPERSION
SYSTEMS (EDS)
 - A. TRANSFORMER COUPLING
TO INTERNAL PASSIVE
COILS (CRYO OR SUPER-
CONDUCTING)
 - B. SWITCHING OF CONDUCTOR
CURRENT TO INTERNAL
COILS (CRYO OR SUPER-
CONDUCTING)
 - C. HIGH THERMAL CONDUCTION
PATHS
 - D. EMERGENCY COOLING
(FORCED COOLED)
- 4. ENERGY REMOVAL SYSTEMS
(ERS)
 - A. TRANSFORMER COUPLING
TO EXTERNAL PASSIVE
COILS (CRYO OR
NORMAL CONDUCTING)
 - B. ACTIVE EXTERNAL ENERGY
DUMP
 - (1) FAST DUMP
 - (2) EMERGENCY DUMP
- 5. CONTAINMENT SYSTEMS (CS)
 - A. MAGNET COIL CASE
 - B. INNER CONTAINMENT AND
BIOLOGICAL SHIELD FOR
FUSION REACTOR
 - C. OUTER CONTAINMENT BUILDING

conductor into the normal state. If this step were carried out early enough, the rise of conductor temperature and energy deposition would be more or less uniform throughout the magnet.

The energy dispersion systems (EDS) would act to disperse energy released inside the magnet as uniformly as possible and prevent excessive local heating. The TES would act to achieve the same result by initiating superconductor to normal conductor transitions, but the EDS's would function throughout the energy deposition period. For example, current might be reduced in parts of the coil where there was an excessive temperature, or there could be high thermal conductivity paths throughout the coil. In forced cooled systems, coolant flow could be greatly increased and path lengths shortened. The increased pressure drop would be permissible since the object is to shut down the coil in a relatively short time.

The energy removal systems (ERS) would dump as much of the stored magnetic energy as possible outside the magnet. Transformer coupling to external passive coils would be an automatic, virtually foolproof method of dumping some of the energy. Considerable energy would remain in the superconducting coils, however, and this would have

to be dumped by an active system, i.e., the insertion of an external dump resistor into the conductor circuit. Both fast and emergency dump situations could be envisioned. The fast dump considered here is much faster than the routine dump one would use to shut off the magnets for normal maintenance operations on the reactor. A fast dump should not involve a substantial risk of damage to the magnet by the dump process, where an emergency dump could involve the risk of internal electrical breakdown caused by high voltage during the dump. An emergency dump might be necessary in the event of a conductor break, for example.

The containment systems have been briefly discussed in an earlier section.

II-2.2 Detection Systems

For an example of the detection systems likely to be required, consider the TF coil systems for a large reactor like UWMAK-1. Table II.5 gives an illustrative list of the number and types of detection systems for each of the 12 TF coils. Each coil has ~130 km of conductor, with a total volume (coil plus conductor) of ~100 m³. The systems listed seem likely to be the minimum required - additional numbers of detection points and/or other types may be necessary for a real system.

TABLE II.5

NUMBER & TYPES OF DETECTION SYSTEMS FOR
UWMAK - TF COIL

<u>DECTION FUNCTION</u>	<u>NUMBER & LOCATION(S)</u> [REDUNDANT DETECTORS ARE USED AT EACH LO- CATION]	<u>TYPE OF DETECTOR</u>
1. INTERNAL ARCS	(1) ACROSS COIL LEADS	VOLTAGE
2. He PRESSURE IN DEWAR	(2) TOP & BOTTOM OF DEWAR	PRESSURE TRANSDUCER
OR		
He PRESSURE IN FORCE COOLED CONDUCTOR	(N) - 1 DETECTOR LO- CATION FOR EACH PARALLEL He FLOW PATH IN COIL	PRESSURE TRANSDUCER
3. He COOLANT LE- VEL IN DEWAR	(1) TOP OF DEWAR	LIQUID LEVEL INDICA- TOR
OR		
He FLOW IN FORCE COOLED CONDUCTOR	(N) - 1 FLOW DETEC- TOR FOR EACH PAR- ALLEL He FLOW PATH IN COIL	FLOWMETER (TRANSIENT HEAT PULSE?)
4. TEMPERATURE OF LEAD	(2) - 1 FOR EACH LEAD	VOLTAGE
5. He COOLANT FLOW IN LEAD	(2) - 1 FOR EACH LEAD	FLOWMETER
6. HOT SPOTS IN SC WINDING [DUE TO COOLANT CHAN- NEL BLOCKAGE, ETC.]	(60) - 1 FOR EACH SIDE OF DISC [~2Km OF CONDUCTOR PER SIDE]	BIFILAR SC LEAD WOUND WITH 10 KA SUPERCON- DUCTOR

Most detection functions should require relatively few stations per TF coil. In Table II.5, one detection station is assigned to each of the following: internal arcs, excessive He pressure in the dewar (or force cooled conductor), and loss of coolant in dewar (or flow in force cooled conductor). Two detection stations, one for each lead, would monitor the temperatures of the coil leads. Another two stations would monitor the coolant flow in the coil leads. Internal arcs could be detected fairly easily by monitoring voltage across the coil leads. The arc voltage, ~30 volts, is relatively large and would appear as a sudden change in the voltage signal. Pressure and temperature and liquid level monitoring could be carried out by standard techniques. Flow monitoring might be carried out using transient heat pulses to measure flow velocity.

At this stage it is not possible to project what will be the frequency of initiating events that will require detection and remedial action to prevent magnet damage. It should be noted that some initiating events, e.g., internal arcs, cannot be prevented from damaging the magnet, though their consequences can be greatly reduced by appropriate action. It is also not possible

at this stage to project what will be the frequency of false alarms that trigger remedial actions and shutdowns that are not required. We can specify allowable upper bounds to their frequency, however, and see what this implies in terms of detection system reliability and redundancy. A reactor shutdown of once every year due either to an actual or false alarm in the TF magnet system will be of serious concern to the reactor operations staff. A considerable fraction of the TF energy will be dumped internally, and substantial testing will be required to determine if damage was done and to what degree. Magnet cool-down and removal of the dumped energy will also take considerable time. A down time of a week or more seems likely. In addition, internal damage to the magnet may result from heating, electrical breakdown, etc., when the magnet shuts down.

As an illustration, assume that initiating events can occur that could lead to potentially serious consequences (e.g., a class 2 accident with release of radioactivity to the containment vessel), if not detected and appropriate remedial action taken. We further assume that such initiating events occur several times during the 30 year life of the reactor plant. We also assume that the failure rate of a detector

is $\sim 10^{-6}$ /hour ($\sim 10^{-2}$ /year), comparable to that observed for instrumentation in nuclear reactors⁽¹⁾, and that detectors are non-repairable. In practice, parts of the detector will be repairable (e.g., amplifiers, power supplies) while others will not, e.g., the transducers or wires inside the TF coils. Suppose that each detection point has n redundant detectors monitoring the same quantity, e.g., temperature in a lead. The average probability of detectors failing to detect a dangerous initiating event is

$$\bar{P} = \frac{\lambda^n t_o^n}{t_o} \text{ per reactor year}$$

where t_o = lifetime of reactor (30 years)

λ = failure rate of detector (0.8×10^{-2} /year)

In order for P to be $\sim 10^{-4}$ /reactor year, 4 redundant detectors must be used at each detection station. To avoid common mode failures, each detector should be independent of other detectors, with its own amplifier, transducers, wiring, etc.

The level of redundancy required is strongly affected by the reliability of the detectors. Indeed, it appears that if the reliability of the non-repairable components of the

detectors is only a factor of two worse than the assumed value (e.g., 2×10^{-6} failures/hour instead of 10^{-6} /hour) 8 redundant detectors would be required to achieve $P \sim 10^{-4}$ /reactor year. At this point it is not known what level of reliability can be achieved for the detectors. A considerable development effort for each of the detection types will undoubtedly be required, with extensive testing under actual operating conditions. If reliability values of 5×10^{-7} /hour could be achieved, then 3 detectors would be sufficient; if 2×10^{-7} /hour could be achieved, then 2 detectors would be sufficient. An economic balance will probably decide what level of reliability will be achieved,-- more detectors at lower cost/detector vs. fewer detectors at higher cost/detector. For purposes of the analysis it is assumed that a reliability value of 5×10^{-7} failures per hour can be achieved, and that 3 detectors per station is adequate.

In order to keep spurious signals from detectors from causing too many unnecessary shutdowns (on the order of one false shutdown/year should be acceptable) it may be necessary to have two or more detectors at a given station agree before shutting down the reactor. With

3 detectors per station and 84 stations per reactor (7 stations per TF coil; 12 TF coils) there would be one false shutdown/year if shutdown were triggered by one detector and the spurious signal rate were 5×10^{-7} /hour. If shutdown is triggered by agreement of two detectors, the level of redundancy must be increased to maintain $\bar{P}$ at $\sim 10^{-4}$ /reactor year. Five redundant detectors, with agreement of any two necessary for shutdown, would meet the desired reliability rate, i.e., $< 10^{-4}$ failures/reactor year, at a failure rate of 5×10^{-7} /hour for individual detectors. The shutdown rate due to spurious signals be negligible, i.e., less than 10^{-2} /year. At this point, it seems marginal as to whether or not detector agreement will be necessary for reactor shutdown.

The above discussion only pertains to shutdowns due to detector malfunction. The problem of judging whether or not a given signal is unequivocal enough to shutdown a reactor is much more complex and uncertain. In the case of an initiation of an arc, the detection signal should be very clear and unmistakable. In the more likely situation of attempting to determine whether or not there is a region of the TF coil with monotonically increasing temperature, the signal may be of the same

order as (but greater than) signals generated during non-dangerous situations involving, for example, coil movement, flux jumps, etc. Furthermore, the frequency of these non-dangerous situations may be relatively high. In this case, very good discrimination ability will be required to be able to simultaneously keep the false alarm rate low, while not missing situations where a serious accident could occur unless remedial action were undertaken. As an example, suppose that the frequency of potentially serious conditions (unless remedial action is taken) is 10^{-2} /reactor year, while the frequency of non-dangerous situations activating detectors is 10^2 /reactor year. In order to keep reactor shutdowns at one per year 99 out of 100 signals have to be disregarded while not missing more than 1 out of 100 dangerous situations.

The problem of detecting regions in the TF coil where runaway temperature conditions are occurring, before it is too late to take remedial action, is analyzed more fully in a later section of this report. Briefly, each UWMAC TF coil has ~130 km of conductor. On the order of 70 voltage detectors per TF coil (2/disc) would probably be required to adequately detect a potentially dangerous

normal region in the ~2 km length of conductor between detectors. Spurious signals arising from small changes in conductance due to coil shape changes, disc tilt, etc., as well as non-dangerous normal regions will have to be distinguished from significant signals. If 5 redundant detectors were used at each detection point, the reactor would require 4,200 detecting circuits. With this number of circuits, even if agreement of two detectors at each detection point were used, there would be several false shutdowns per year for a detector reliability of 5×10^{-7} per hour.

This very large detector net is unappealing and would certainly pose very difficult problems of monitoring, repair, etc., not to mention cost. From a detection point of view, the problem of determining when runaway hot spots occur in a TF coil appears to be the most difficult of all the detection problems, because of the very long length of conductor to be monitored, the relatively localized behavior of trouble spots, and the presence of considerable background noise. Operating experience with large magnets is not very meaningful in assessing how severe the problem will be. Not only

is the total operating time too short by orders of magnitude to yield any idea of the probability of occurrence of such runaway hot spots, but TF coils will undergo much more demanding service than has been experienced by large DC bubble chamber magnets. The CTR magnets will probably be under considerable higher structural stress, will be subjected to many cycles of large pulsed magnetic fields and forces, and may have to operate with relatively low stabilizer/SC ratios to achieve acceptable eddy current and pulsed field losses.

It would be very desirable to develop a detection method that could detect runaway temperature forces in a TF coil before the temperature exceeded $50\text{--}100^{\circ}\text{K}$, using ten or less detection points per coil. Perhaps a separate detection wire, wound bifilarly with the conductor to minimize inductive effects, might be a practical method.

The abnormal conductor detection problem will be common to all the methods of TF coil construction, whether force cooled conductor, conductors in discs, or pancake wrapped coils. Detection of abnormalities in flow and/or helium temperature in a force cooled conductor would appear to have similar problems of large

detection nets as conductor temperature, since there will be a large number of parallel flow circuits, and flow velocity in each circuit will be very low, i.e., < 1 meter/second.

Higher conductor currents would greatly ease this detection problem. If 10^5 amp conductors were used instead of the 10^4 amps used in the reference designs, the number of detection locations could be reduced by an order of magnitude.

II-2.3 Temperature Equalization Systems

For many accident situations, a Temperature Equalization System will probably be the primary line of defense against serious magnet damage, since it does not appear feasible in most cases to dump more than a small fraction of the magnetic energy outside the magnet. The TES should be capable of rapidly driving the remaining superconductor in all portions of the magnet into the normal state past the recovery point when detectors indicate a potentially accident situation. This would prevent the dumping of the magnetic energy in a limited region of the magnet which might lead to conductor and/or reinforcement damage or failure, and would minimize the chances of electrical breakdown during a quench.

If the TES could bring all portions of the conductor to the same temperature, one would have an ideal quench situation where the temperature would rise uniformly through the magnet and potential differences between different points in the magnet were minimal. This condition would essentially be fulfilled, for example, in a situation where one drove the magnet normal when loss of coolant flow was detected in a lead. For other situations, the ideal quench situation cannot be fulfilled. For example, consider a TF coil in which a zone of monotonically increasing temperature is detected, and the TES is triggered when the temperature of the zone reaches $\sim 100^{\circ}\text{K}$. The temperature of the entire coil will then begin to rise, but a temperature difference on the order of 100°K will persist between the initial zone and the rest of the coil.

There are several methods that might be used in a TES:

1. Rapid dump of He coolant.
2. Rapid dump of He coolant and re-pressurization.
3. Presence of an alternating current component in the conductor.
4. Heating of the conductor by passage of current through auxiliary heating wires.

Rapid dump of helium coolant does not appear satisfactory for large TF coils, particularly those with pool cooling type of construction or long lengths of force cooled conductor. It is difficult to ensure that pockets of coolant do not remain in some regions of the coil, which would keep these regions in the superconducting state. For example, it is possible that vapor locks in the helium coolant channels could result in some portions of the coil retaining liquid helium, which has a very low density, for a considerable time after helium has been vented from the rest of the coil. The regions with good coolant drainage will then rise in temperature while those with poor drainage will remain at $\sim 4^{\circ}\text{K}$.

Another disadvantage of method #1, dumping helium coolant, is that the consequent low helium pressure will result in a low dielectric strength for breakdown paths through helium gas. This is discussed more fully in a later section, but He at atmospheric pressure at temperatures above $\sim 100^{\circ}\text{K}$ will probably have a breakdown strength of only 500-1000 volts.

Method #3 does not appear feasible for TF coils, at least for the conductor currents and coil inductances characteristic of present reference designs. The UWMAK-1

TF coil inductance is 500 henries and the corresponding inductive reactance is 3000Ω at 1 H_z . Given the ~ 1000 volts maximum likely for TF coils, only a very small current component could be introduced by an external voltage across the coil leads. The AC heating would appear to be insufficient to drive the conductor normal. In principal, one could use a set of auxiliary leads connected so that each disc was in parallel to the imposed external AC voltage. The AC heating would still probably be insufficient, however, and the parallel connections might result in unbalanced DC current distributions between the discs.

Method #4 appears to be feasible if the heating wires are bifilarly wound with the conductors to minimize inductive voltages. For the UWMAK-I TF coils one could wind in heating wires with the conductor on each side of the 34 discs, connected in parallel at the outside of the discs. These heating wires would be electrically insulated from the main conductor, but in relatively good thermal contact. They might be anodized aluminum strip, for example. A heating rate of $\sim 1 \text{ watt/cm}$ of conductor (400 KW per

disc) for several seconds would probably be sufficient to ensure that the conductors could not recover the superconducting state. The applied voltage per heating wire circuit would probably be on the order of a few hundred volts.

Method #2, rapid dump and re-pressurization with warm helium, appears to be the best method for a TES. It avoids the complication of extra wires and additional possible breakdown paths, it should be quite reliable in reaching all portions of the coil, and ensures that the breakdown strength of helium is high during a quench. As much as possible of the initial liquid He inventory would be rapidly dumped while the magnet was still relatively cold. Helium gas at some temperature above 4°K would then be used to repressurize the helium in the dewar. Even if pockets of He gas at 4°K cannot be eliminated, repressurization to 10 atm, will raise the conductor temperature from 4°K to $\sim 10^{\circ}\text{K}$. The principal requirement is that the dewar be capable of holding helium at elevated pressures. This approach would be particularly suitable if an external coil case were part of the TF coil system. This coil case could then readily withstand relatively high internal helium pressures.

II-2.4 Energy Dispersion Systems

Energy dispersion systems appear to be of relatively limited usefulness for CTR magnets, and primarily applicable to DC magnets that are either not exposed to pulsed fields or are only infrequently exposed. One possible system would be a composite conductor integrating the SC, stabilizer and reinforcement. Fiber reinforced high purity aluminum [Al_2O_3 , graphite, and boron have been used to make very high strength composites with ultimate tensile strengths in the order of 100 ksi] seems very attractive for such purposes. The thermal diffusivity of high purity aluminum is very large, on the order of $10^5 \text{ cm}^2/\text{sec}$. Even with conductor currents of 10^5 amp , however, the conductor length in CTR magnets is far too long for energy released at one point in a coil to disperse significantly to all parts of the coil. Significant local dispersion would occur over lengths of several tens of meters, however, which would mitigate local hot spots to some degree.

Another possible EDS is to use high purity aluminum spacers between coil segments. Local I^2R heating in conductors could then be dispersed relatively widely throughout the coil. The spacers could be anodized to prevent short circuits, and could take the place of insulating spacers

between conductor pancakes in a TF coil, for example. The eddy current losses due to pulsed fields in such a TF coil appear to be prohibitive, however, unless very long plasma burns, e.g., an hour or more, are possible.

Since such EDS's are passive, they will automatically function during any accident situation. There does not appear to be any objection to this, provided that it does not reduce structural safety or increase the chances of electrical breakdown. The latter might be the case with high purity aluminum spacers between conductor pancakes, for example.

An interesting possible EDS is the deliberate creation of multiple internal arcs if an internal arc is detected inside the coil. If an arc occurs, the coil is damaged enough that it cannot be used again, and the principal concern is that an arc not dump enough energy in its vicinity to cause a significant chance of coil disruption. Creation of multiple internal arcs at dispersed locations in the coil would appear to greatly reduce the amount of energy dumped at the location of the original arc, and the chances of coil disruption. For example, if an arc is created at some point in each disc of a UWMAK-1 TF coil (the arc points could be spaced

around the perimeter of a TF coil so they would not overlap), the maximum amount of energy that could be dumped at each arc point would be $\sim 5 \times 10^8$ J, which could heat a zone of 0.1 m^3 volume to a maximum temperature of $\sim 1000^\circ\text{C}$. This volume could be further reduced by starting additional internal arcs. Without such an EDS the potential affected volume around the original arc point would be several m^3 . Analyses of arc situations are discussed in more detail in a later section.

It should be simple to cause helium breakdown which will initiate conductor arcs at a large number of selected internal points by means of pulsed high voltage on a set of parallel circuits to the selected points. The high voltage cable can be of relatively small diameter and readily placed in the coil.

II-2.5 Energy Removal Systems

Active energy removal systems would connect external resistors to the magnet leads if a potential accident situation were detected. For the TF coils in present Tokamak reference designs, voltage restrictions sufficiently restrict the rate of energy removal that active ERS's cannot remove any significant amount of energy during an accident.

Subdivision of TF coils or larger conductor current could change this condition, however. For example, if separate leads are used for each disc in a UWMAK-I TF coil, the coil's magnetic energy could be dumped in ~100 seconds with a voltage limitation of 1000 volts across leads. This would permit most of the magnetic energy to be dumped externally in most accident situations.

This proliferation of leads (~400 pairs per set of reactor TF coils) increases the complexity of the magnet system. It is probably not desirable to have all those leads connected to separate external power supplies during normal operation, since in this case, the total heat leak would be large, and each pair of leads would require a number of monitoring circuits. Instead, each TF coil could have a single set of leads during normal operation, with the additional leads at 4°K and not connected to external resistors. The discs or pancakes in the TF coil would then be connected in series. When a rapid dump was desired, the additional leads to each disc or pancake could be connected to a separate external dump resistor (leaving the series connections in place) and the pancakes would then discharge in parallel. The dump leads would then probably not require

individual monitoring or cooling, since they would be in standby service for almost all of the time, and would not contribute any heat leak to the refrigeration load. Retaining the normal operating series connections between discs (or pancakes) would permit an individual disc (or pancake) to discharge to adjacent dump resistors if for some reason its pair of dump leads fails to connect. The series connection between discs (or pancakes) would be difficult to break anyway, and might possibly initiate arcs. In general, there will be some energy transfer (both inductive and through the series connections) between discs or pancakes during a dump, since their resistive impedances will not be identical.

Larger conductor currents also improve the ability of ERS's to remove significant amounts of energy during accident situations. With a conductor current of 10^5 amp, it appears feasible to remove most of the energy of a UWMAK-1 TF coil in ~100 seconds with only two subdivisions per coil.

The relative merits of many coil subdivisions vs. large conductor currents are not clear enough at this point to be able to choose the best approach. Large current conductors favor simpler coils, lower assembly

costs, and fewer safety circuits while many subdivisions favor reduced eddy current and lead heat leaks and a conductor nearer to the state of the art.

Problems of power supplies and handling of dumped energy also must be considered in evaluating alternative ERS's. With the many subdivision approach, solid state power supplies are practical for the relatively small, $\sim 10^4$ amp conductor current. Dump resistors can be simple water cooled stainless resistance elements. The resistors themselves can be located within the primary containment region, and the water coolant circulated between some outside cooling pond and the resistors. Redundancy on pumps and flow paths should be built in so that the chance of an individual resistor not being adequately cooled is small; however, this does not appear to be a significant problem. Removal of $\sim 2 \times 10^{11}$ J in ~ 100 seconds (values for the TF coil set in UWMMAK-1) is a major engineering project, with the heat dump comparable to that in a 1000 MW(e) LWR. It may not be necessary to dump all of the energy in a given accident situation, but it seems likely that in many situations one would want to remove as much as possible of the magnet systems energy. The heat removal system will thus have to be sized assuming

the maximum possible energy dump. During a complete dump, on the order of two thousand m^3 of water with an average temperature rise of $\sim 50^\circ\text{C}$ would be circulated.

Conventional power supplies for conductor currents of 10^5 amp appear feasible by paralleling a number of solid state rectifiers. Similarly, energy dump in water cooled metal resistors appears feasible for very large conductor currents. An interesting alternative for large current conductors is the use of liquid metal MHD generators for power supplies. Such generators can produce very large currents with good efficiency and low voltage drops, and can be compact and cheap. When a rapid energy dump is desired, the mechanical pump maintaining the MHD channel in the generating condition could be simply switched off. The MHD channel would then function as a liquid metal pump. The developed pressure would then be irreversibly dissipated as heat in the fluid or recovered by a mechanical turbine. This does not rule out dumping some of the energy in liquid metal or water cooled resistors, of course, and this would probably be desirable. The turbine recovery alternative does not seem cost effective since the value of recovered energy would be

only about a thousand dollars, and the dump process should take place infrequently, i.e., once per year or less. However, energy recovery via mechanical work might be desirable to reduce the amount of thermal energy to be dumped. Alternatively, since liquid metal heat transfer surface is relatively expensive, it might be desirable to thermally store the dissipated magnetic energy in a pot of heated liquid metal (a storage volume of several hundred m^3 would be sufficient). The thermal energy could then be removed during a time period that was much longer than the ~ 100 seconds required to dump the coil.

The volume of the MHD channel will strongly depend on what fraction of the magnet energy of the coil is dissipated as pressure head in the pump circuit. If most of the coil's energy appears as I^2R heat in a resistor, the channel, which will primarily be a generator of current for normal operation, can be very compact, i.e., on the order of 10^4 cc for each TF coil. If a major fraction of the coil's magnetic energy is dissipated as pressure head in the MHD pump, the volume will have to be larger, i.e., $\sim 10^5$ cc, for each TF coil, but this is still very compact, however.

Passive ERS's can also be incorporated into a CTR magnet system. For example, if an external room temperature winding is part of the confinement for a TF coil, significant amounts of magnetic energy will appear as I^2R heat in the external winding during a quench. Because of the large size of TF coils, inductive coupling between the SC TF coil and the external r.t. winding will be quite good. Transfer of a large part of the TF coil current to the external winding will result in strong magnet forces on the external winding. For this reason, such a passive ERS should be combined with an external coil case designed to take the magnetic forces of the TF coil. If the coil case were a thick aluminum shell around the SC winding, its resistance would be comparable to that of the copper stabilizer in the SC TF winding during a quench, so that a large part of the TF coil energy would automatically be dissipated in the coil case. An automatic, passive ERS is very appealing since it does not rely on detectors and other active safety components. However, it can only supplement and not supplant other safety systems. The use of passive ERS's will probably depend on whether or not it is decided to use an external coil case. Some cooling of the external passive ERS might be

required (the temperature rise without cooling would probably be on the order of 100-200°C), but this does not appear to be a problem.

The principal line of defense against magnet damage will probably be the TES, however, since one may not be able to remove most of the magnet energy with ERS's, either because of design restrictions (low conductor current, relatively few subdivision), or because of possible non-functioning of the ERS upon demand. A combination of a TES, active ERS, and passive ERS with external coil case would appear to both minimize the chances of severe accidents (class 2, 3 and 4) resulting from magnet disruption, as well as accidents (class 1) involving no magnet disruption.

II-2.6 Containment Systems

The principal goals of the containment systems should be, first, to reduce the chances of coil disruption, and second, if coil disruption occurs, to reduce the chances of release of radioactive inventory from the reactor to the environment.

The use of an external coil case for TF superconducting windings, as shown in Fig. II.4 (C and D) should greatly reduce the chances of coil disruption in the event of failure of the coil winding.

The type A construction shows a conventional coil winding, with the conductor interlayered more or less uniformly with the coil structural reinforcement. If an arc develops in the winding, it appears possible, though very unlikely, that sufficient energy could be locally deposited to cause the winding to pull apart. The type B construction shows an alternate coil construction in which the conductor is wound inside a steel ring. This construction was proposed by ORNL in an early reactor study. This lumped reinforcement would appear to be much less vulnerable to arc accidents, since the arc heat source would be limited to the surface of the reinforcement, rather than distributed through it. The type B construction is still relatively unconfined in the lateral direction, however, since the lateral restraint will be relatively weak, and the cryostat has very limited strength.

It should be emphasized that magnet accidents could conceivably arise not only from internal initiations (e.g., electrical breakdown, coolant channel blockage, conductor rupture, etc.) but also from external events, (e.g., coolant line rupture, coolant fire, etc.). Without isolation of the magnet windings, external initiating

events could cause magnet disruption even if the probability of internal initiating events were zero. A thin cryostat around TF coils would not afford any significant degree of protection against external initiators.

They type C construction shows a lumped reinforcement winding with a thick external coil case. The external case only carries a magnetic force loading in the event of failure of the SC winding, or in the event of a quench of the SC winding [only if the case is aluminum]. Such a coil case could also be used with a distributed SC winding.

In the type D construction, the external coil case carries a significant portion of the magnetic force loading during normal operation. The case is at room temperature and the 4°K coil is separated by a barrier of low thermal conductivity (e.g., epoxy-fiberglass). Such types of construction have been investigated in a study on warm reinforcement magnet structures for CTR reactors. (2) The heat leakage through the thermal barrier appears to be acceptable. Such a construction would appear to offer some economic advantage over type C construction, but would create some technical problems. Type C construction with distributed reinforcement would appear to

be the easiest mode of construction if an external coil case is to be used.

In the very unlikely event of a TF coil disruption, a portion of its stored magnetic energy would be converted to kinetic energy of the coil and its adjacent structure. Hypothetical accidents have been discussed⁽³⁾ elsewhere in which the stored magnetic energy is converted to kinetic energy of the coil structure, and a value of 500m/sec has been computed for acceleration of the complete coil and 10km/sec for 1/100th of the coil. Such accidents do not appear to be physically possible, however. If a complete breakage of the winding did occur, the two ends of the coil would be accelerated outwards by the magnetic forces on them. As the coil ends separated, the arc between them would lengthen (it might also jump to other portions of the magnet structure to complete the circuit). Detailed analyses of various postulated accidents would be required to establish how far the ends could move before the arc was extinguished. However, it is likely that the outwards magnetic force on the arc would rapidly lengthen the arc (at a velocity approaching the sonic velocity in the gas) which would cause voltage drop to be very high. This effect is often used as a means of rapidly extinguishing

arcs in opening circuit breakers. It seems possible that the arc might blow out and restrike a number of times before finally extinguishing, as the ends of the coil moved apart.

The outwards acceleration of the ends of a disrupted UWMAK TF coil would appear to be on the order of 3000m/sec^2 , if the full coil current was being carried at the moment of break, and external bracing structure did not slow their movement. If it is assumed that the arc extinguishes after the coil ends have moved ~ 2 meters (the separation between the ends would then be several meters), the extinguishing time would be ~ 35 millisecond and the corresponding velocity of the ends of the magnet would be ~ 100 meter/second. The average arc voltage necessary to dissipate the magnetic energy during this interval would be on the order of 10^4 volts, which seems plausible. It thus seems reasonable to assume that the coil velocity cannot exceed ~ 100 meters/second.

The above hypothetical illustration should be viewed only as an attempt to establish a reasonable upper limit for the coil velocity during a disruption, and not as a prediction of its actual behavior. The only objective of the exercise is to show that accidents in which a portion

of the coil could reach or exceed a velocity 500 meters/second are not physically possible. It is important to consider this point, since the primary containment system should probably withstand impact of missiles at velocities of ~100 meters/second (which is the worst case that can be visualized) without major breaching (though leak tightness may be lost), but it may not be possible to design the primary containment to contain large missiles at velocities of 500 meters/second.

In fact, it should be possible to design bracing structures for coils such that large scale movement of the coil will not occur even if the winding (and case if present) fails.

The principal concern with a TF coil accident appears to be its secondary effects, i.e., the rupture of coolant headers which might result in coolant fires, and/or pressurization of the primary containment. The containment should be designed to contain such releases anyway, since accident pathways other than those from magnets (i.e., blanket accidents) could potentially cause similar types of releases. It thus appears that hypothetical magnet accidents do not require any more extensive containment than would be the case without considering such accidents.

II-3 POTENTIAL ACCIDENT SEQUENCES FOR FUSION MAGNET SYSTEMS

II-3.1 Potential Accident Initiators

Table II-6 lists the principal potential accident initiators. Intuitively, some seem lower in probability than others, and a ranking comparison can be made for initial scoping studies. For example, missiles external to the reactor seem much less important than electrical breakdown initiators.

Some initiators can be eliminated by appropriate design choices; for example, using solid leads cooled by conduction rather than by flowing helium gas will eliminate the initiator due to inadequate lead cooling. This increases the lead heat leak, but the greater reliability may make this acceptable.

The probability of certain initiator occurrences may be greatly reduced (but not eliminated) by selection of appropriate materials and design/operational parameters. For example, decreasing the structural stress in the magnet reduces the chances of conductor and reinforcement fracture, as well as the chances of excessive conductor movement and insulation cracks. However, for technical and economic reasons there are limits to the degree such

parameters can be manipulated. As an example, the cost of the TF coils in a Tokamak reactor might be unacceptable for structural stresses much less than ~ 30 Ksi.

Other initiators are related to quality control of fabricated materials, i.e., flaws in the stabilizer of the conductor, cracks in the reinforcement, metal inclusions in insulators, etc. The large amount of materials in a large fusion magnet system (e.g., there is 130 km of conductor in each UWMAC-1 TF coil), as well as the problems of testing under operating conditions will make adequate quality control difficult. This is particularly difficult since one undetected bad spot might make the whole system fail.

Still other initiators could be caused by mistakes during magnet assembly. This category would include metal particles causing electrical shorts, inadequate gluing of conductor, misalignment of assembled components leading to excessive stress, etc. Detection of some of these initiators may be possible by extensive pre-testing the magnet before completion of the fusion reactor. The defective portion of the magnet system could then be replaced more easily than when construction is complete and the reactor structure has become activated. Testing methods might include rapid energy removal from a single

coil during an artificially induced quench, and application of relatively high voltage from an external source.

Finally, the complete magnet assembly could be proof tested to ensure that the safety systems would respond correctly in an accident situation.

No quantitative conclusions can be presently made about the various possible accident initiators. At this time the following initiators appear to be the most qualitatively significant (not necessarily in order of importance):

1. Electrical shorts through helium, either caused by voltages present during quenches or discharge of the coil, or by shorts through metallic particles.
2. Conductor break as a result of local heating due to coolant channel blockage or loss of flow, or excessive conductor movement.
3. Inadequate quality control during fabrication and/or installation of conductor, resulting in low dielectric strength or insufficient stabilization.
4. The mechanical fracture of conductors and/or reinforcement under normal pulsed loadings.
5. Accidents resulting from damage to conductor, reinforcement, and/or insulation, with such damage a result of previous operation in which substantial temperature inhomogeneities were produced in the magnet.

TABLE II.6

SUMMARY OF POTENTIAL ACCIDENT INITIATORS

MECHANICAL INITIATORS ASSOCIATED WITH OPERATION OR ASSEMBLY OF MAGNET

- . FRACTURE OF CONDUCTOR
- . FRACTURE OF REINFORCEMENT
- . CRACKING OR PLASTIC DEFORMATION OF STABILIZER RESULTING IN HIGH RESISTIVITY
- . FRACTURE OF SUPERCONDUCTOR FILAMENTS (NON-CRYOGENICALLY-STABLE CONDUCTOR)
- . EXCESSIVE CONDUCTOR MOVEMENT (GLUE FRACTURES)
- . CRACK IN INSULATION
- . RUPTURE OF CRYOSTAT

ELECTRICAL INSULATION INITIATORS [NON-MECHANICALLY CAUSED]

- . METAL INCLUSION IN INSULATOR (AS FABRICATED OR DURING ASSEMBLY)
- . METAL PARTICLE CAUSES BRIDGING BETWEEN CONDUCTOR TURNS
- . EXCESSIVE VOLTAGE DURING COIL DUMP OR QUENCH
 - . LOW DIELECTRIC STRENGTH OF HEATED HELIUM
 - . RADIATION INDUCED IONIZATION IN HELIUM
 - . DEGRADATION OF INSULATION BY RADIATION
- . IMPROPER INSTALLATION OF INSULATION - MISSING OR TOO THIN

COOLANT INITIATORS

- . BREAK OF COOLANT LINE(S) FROM REFRIGERATOR
- . COOLANT CHANNEL BLOCKAGE BY BROKEN SPACER

TABLE II.6 (CONTINUED)

SUMMARY OF POTENTIAL ACCIDENT INITIATORS

- . COOLANT CHANNEL BLOCKAGE BY CONDUCTOR OR COIL MOVEMENT.
- . COOLANT CHANNEL BLOCKAGE BY COLLECTION OF IMPURITIES FROM COOLANT (FROM REFRIGERATOR OR OTHER PARTS OF MAGNET)

CONDUCTOR INITIATORS [NON-MECHANICALLY CAUSED]

- . RADIATION DAMAGE TO STABILIZER CAUSING HIGH RESISTIVITY
- . RADIATION DAMAGE TO SUPERCONDUCTOR (NON-CRYOGENICALLY STABLE CONDUCTOR)
- . IMPURITIES OR INCLUSIONS IN STABILIZER FROM INGOT OR CONDUCTOR FABRICATION
- . EXCESSIVE $\partial B / \partial t$ (NON-CRYOGENICALLY STABLE CONDUCTOR)
 - . SUDDEN LOSS OF PLASMA CURRENT
 - . MAGNET CHARGE OR DISCHARGE TOO RAPID
 - . COILS PULSED TOO FAST

LEAD INITIATORS

- . FRACTURE OF LEAD
- . INADEQUATE COOLING OF HEAT RESULTING IN MELTING

INITIATORS EXTERNAL TO MAGNET SYSTEM

- . MISSILES FROM REACTOR PLANT (TURBINE, COOLANT LINE RUPTURE) FLYWHEEL OF ENERGY STORAGE SYSTEM, ETC.)
- . MISSILES EXTERNAL TO REACTOR PLANT (AIRPLANES, ETC.)
- . EARTHQUAKES
- . SABOTAGE
- . HUMAN ERROR

TABLE II.6 (CONTINUED)

SUMMARY OF POTENTIAL ACCIDENT INITIATORS

CONTROL SYSTEM MALFUNCTION INITIATORS

- . LOSS OF POWER TO CONTROL SYSTEM
- . FAILURE OF SENSORS
- . FAILURE OF INTERLOCKS
- . COMPUTER MALFUNCTION
- . FAILURE OF ENERGY DUMP SWITCHING SYSTEMS

II-3.2 Accident Event Trees

Accident event trees are a convenient way of visualizing the various possible consequences of a given accident initiator. They are much easier to construct and interpret than fault trees, which start with a given final state, e.g., a coil rupture, and work backwards through the possible paths that could lead to the given final state. Event trees have been used in fission reactor studies and have proven very useful. For this CTR magnet study, event trees were used to examine the consequences of a given accident initiator, and fault trees to determine the routes by which a given accident initiator could occur.

Figures II.5 through II.9 show simplified event trees for accident sequences following a given accident initiator. At each of the decision points, a favorable or unfavorable path is possible. The actual decision as to which path is taken will be considerably more complex than shown in these event trees, since the decision itself will depend on a number of physical processes and various safety systems. Thus the total number of accident sequences will be much larger than the 20 or so shown in the figures. Estimating the

probability of a favorable or unfavorable response at any decision point will be a complex and difficult problem. Roughly about half of the possible accident sequences will lead to a coil rupture. To obtain an overall probability of rupture for a given accident initiator, the probability must be estimated for each sequence leading to rupture and then a sum made of all such probabilities. Note that some sequences in an event tree may then lead to an initiator in another event tree. For example, some of the sequences in Figures II 5, II.6 and II.8 lead to the conductor break event tree.

Even if a coil ruptures, it may not result in release of radioactivity to the environment. Figure II 10 shows a containment tree in the event of coil rupture. Again, the tree is considerably simplified, and many of the decision points will depend on a number of physical processes and safety systems, as well as the details of reactor plant design.

If external cases are used for all the magnet coils, the possible accident sequences become greatly reduced and simplified. The worst case one can visualize is failure of all the coil conductors and reinforcement with

full current continuing to flow in arcs between the broken edges of the conductors, and no energy removal, either from the failed coil or from adjacent coils. In this event, the external case for the coil will have to take the full magnet forces and will be heated from internal arcs in the damaged coil. Estimation of failure probability under these conditions will be difficult, but still far easier than trying to examine and predict and sum failure probabilities for hundreds of accident sequences. If the failure probability is low enough, then the problem of ensuring adequate magnet safety becomes much simpler. The coil case would then be sufficient protection against all accidents except those of class 1, which involve only magnet damage and economic loss due to plant shutdown. The allowable probability of failure for class 1 accidents is much greater than for class 2, 3, or 4 accidents. The coil case approach could have benefits in terms of better coil performance (higher allowable structural stress, less stabilization with consequent reduced heating due to pulsed fields, less stringent quality control standards, higher allowable voltage etc., and the cost savings of these benefits would tend to offset the additional cost of a coil case. In fact, it might well turn out the magnet system would be cheaper

with a coil case than without. The case could be constructed of relatively cheap materials, since it would be at room temperature. Further, since it would only have to carry load for a short time during emergency conditions, and could undergo a considerable amount of deformation provided it did not rupture, its operating stress level could be high. These factors would tend to keep its cost relatively low. It would seem that a cost comparison study between the two design approaches, i.e., with and without coil case, would be helpful in assessing this question.

II-3.3 Accident Fault Trees

Fault trees are convenient in defining pathways by which accident initiators can occur. Figure II.11 shows a simplified fault tree for a conductor break. Not all pathways leading to a conductor break have been included, but the 29 pathways that are shown are representative. Most of the pathways would be more complex than shown in figure II.11, since several intermediate steps may be involved at each of the branch points.

The quantification of failure probabilities for the various possible pathways will be very difficult since most of the pathways in figure II.11 involve initiators that

are difficult to quantify. For example, what is the probability of a metallic inclusion added during magnet assembly or the probability of a spacer breaking and plugging a coolant channel during normal operation? These cannot be assumed to be of zero probability, but meaningful quantification can only be obtained from long term operation of a large number of CTR magnet assemblies. Such direct experience will not be available for decades.

Lacking quantitative values for failure probabilities, one will be forced to rely on individual qualitative judgements, which will be strongly subjective. Disagreements between individuals then are not readily resolved, and conflicting assessments on magnet safety and reliability between constructors and regulators are possible. Such disagreements could considerably lengthen the time required to license CTR's.

Figure II.12 shows the fault tree for a TF coil rupture if an external coil case is used. Not only is the number of possible accident pathways reduced by a very large factor, but the accident pathways are much easier to quantify. If the coil case is designed to carry the full magnetic force load assuming the SC winding completely

fails, then one does not need to quantify the various accident pathways leading to winding failure. Instead, one needs only to quantify probabilities for pathways like a flaw in the case sufficient to cause rupture, or a coolant fire damaging the case. It seems likely that agreement on the probability of such accident pathways would be much easier to obtain than for accidents where a coil case is not used.

Figure II.1. Idealized tokamak reactor configuration.

A LEVEL - QUALITATIVE IDENTIFICATION OF IMPORTANT POTENTIAL ACCIDENT SEQUENCES AND ENGINEER SAFETY FEATURES

B LEVEL - QUALITATIVE DEFINITION OF INTEGRATED SAFETY SYSTEMS FOR MAGNETS

C LEVEL - QUANTITATIVE ESTIMATE OF RELIABILITY OF INTEGRATED MAGNET SAFETY SYSTEMS AND RISK ASSESS

Figure II.2. General tasks involved in assessing magnet safety systems for fusion reactors.

A. DISTRIBUTED REINFORCEMENT -
NO EXTERNAL CASE FOR COIL

B. LUMPED REINFORCEMENT -
NO EXTERNAL CASE FOR COIL

C. LUMPED OR DISTRIBUTED REINFORCEMENT -
EXTERNAL CASE FOR COIL CARRIES NO MAG-
NETIC FORCE LOAD DURING NORMAL OPERATION

D. LUMPED REINFORCEMENT -
EXTERNAL CASE FOR COIL CARRIES
MAGNETIC FORCE LOAD DURING NORMAL
OPERATION

Figure II.4. Types of coil reinforcement and coil cases.

S2 - SEE NORMAL REGION EVENT TREE

S3 - SEE CONDUCTOR BREAK EVENT TREE

Figure II.5. Simplified event tree - initiator is propagating crack in reinforcement (no external case for coil).

S1, S12 - SEE NORMAL REGION EVENT TREE

S10 - SEE CONDUCTOR BREAK EVENT TREE

Figure II.6. Simplified event tree - initiator is loss of coolant (no external case for coil).

Figure II.7. Simplified event tree - initiator is lead break (no external case for coil).

S2, S4, S6, S8, S9, S11, S12, S13 - FOR SUBSEQUENT ACCIDENT SEQUENCES - SEE EVENT TREE FOR CONDUCTOR BREAK

Figure II.8. Simplified event tree - initiator is monotonically increasing temperature of normal region (no external case for coil).

Figure II.9. Simplified event tree - initiator is conductor break (no external case for coil).

Figure II.10. Simplified containment fault tree-coil ruptures.

Figure II.11. Simplified fault tree for conductor break (no external case for coil).

Figure II.12. Fault tree for coil case rupture.

III CTR MAGNET STRUCTURAL ANALYSIS PRESENT CAPABILITIES AND FUTURE REQUIREMENTS

III-1 INTRODUCTION

In general, CTR superconducting magnet structures will be large, with characteristic dimensions on the order of tens of meters for proposed Tokamaks and mirror machines. These structures (Figs. III.1 through III.4 depict some of the proposed designs) will be made up of complex composites involving superconductors, insulators, stabilizers, structural reinforcements, glues, etc., all of which are interspaced with many types of cooling channels containing liquid or gaseous helium. Not only are these structures more physically complex than those usually encountered in engineering analysis but the possibility also exists of sudden local or general heating that could take place rapidly due to transitions of the superconductors into its normal state. Local movements caused by existing stresses and strains, fracture of the glue or the other magnet components, and fatigue failure of the various materials can all be responsible for the above mentioned transitions.

In addition to the causes mentioned above, changing magnetic fields may themselves trigger such transitions or may shift the stress-strain configuration enough to cause local movements which then result in transitions. Furthermore, other

events such as coolant channel blockages and externally imposed impulse loads, e.g., earthquakes, missiles, dewar rupture, etc., can also lead to such transitions.

It should be realized that detailed analyses of such magnet structures are beyond the present state of the art unless considerable simplifying assumptions are made. For example, only after a very substantial effort in the fission reactor program, is it now possible to carry out an in-depth cyclic inelastic analysis with a fairly accurate inelastic constitutive equation relationship for such commonly used simple isotropic materials as 304 stainless steel. Even for this material, however, it is still questionable whether long term creep-fatigue interaction for such reactor components as the high temperature piping system can be accurately treated, given limitations on analysis costs. Obviously, the superconducting magnet with its various composite and isotropic/orthotropic materials presents a great challenge, especially when inelastic behavior has to be accounted for. A preliminary projection of the improvements in structural analysis techniques that will be needed for CTR magnet systems comprises the balance of this section.

It should be emphasized that structural analysis by itself, no matter how detailed, will be insufficient if it is desired to fully examine all aspects of magnet safety. While structural analysis

methods can be developed to predict various material failures that occur during the operational life of the magnet, other techniques must be used to predict the consequences of such failures. It is foreseen that magnet accident analysis codes will have to be developed in which integrated structural, electrical and heat transfer calculations are carried out. Results from such codes can then be used to determine the conditions leading to drastic magnet failures. A discussion of the above mentioned requirements needed for codes is given in a later section.

In addition, conventional design ignores the fact that material properties, dimensions of a component, and even the applied loads are statistical and not deterministic or single valued quantities. Thus conventional solutions to design equations result in uncertainties. Consequently, structures may fail even though the analysis indicates that they should not. In the traditional procedure, the designer hides these uncertainties under the blanket called "safety factors". In situations where accidents may result in serious consequences, e.g., fission reactors or aircraft, and where a number of variables is very large, it is very desirable to extend the structural analysis by including meaningful probability of failure or minimum allowable reliability predictions carried out by statistical techniques. It seems likely that these methods will have to be

applied for the CTR magnets since safety considerations here are analogous to those for fission reactors or aircraft. An outline of the developments of these methods is presented in a later section.

It is a known fact that complex analytical methods must be verified with respect to valid closed form analytical solutions and/or experimental results. It appears very likely for the codes to be developed and used for the CTR magnet studies that very few closed form analytical solutions exist, although some degree of verification may be gained from comparison with simplified closed form solutions. Complete verification however, will require an experimental setup which may have to be fairly complex. These experiments do not necessarily have to be performed on magnet prototypes, but in the experimental validation of codes it is necessary that all pertinent physical properties and processes inherent in the magnets be simulated. The requirements for code verification are discussed more completely in a later section.

III - 2 STRUCTURAL ANALYSIS CODES

Structural analysis of CTR systems can be divided into two classes; 1) analysis for normal operating conditions, and 2) analysis for abnormal and/or accident conditions. To date, all of the published CTR magnet structural analyses have essentially been of the first class. Even here, the analyses have been generally confined to approximations involving a good deal of material homogenization, restrictions to isotropic behavior, and neglect of interlaminar slip

that could occur between material layers. The second class of analyses for abnormal and/or accident conditions will in most instances really determine actual CTR magnet system designs.

In order to more readily understand the types of structural analysis codes that will be necessary, it is worthwhile to consider a representative design problem, that of the toroidal field coil proposed for UWMAK-1. More complex design problems could of course be envisioned; for illustrative purposes, however, this design is sufficient.

Figure III.2 depicts a cross section of the UWMAK-I stainless steel disc with its glued-in copper-NbTi conductors, together with one of the Micarta spacers which separate the discs. As shown in figure III.1, there are 34 discs per coil and 60 conductors per disc. If one attempts to incorporate the actual heterogeneity of disc, spacers, conductors and glue into a correct physical model, using the typical finite-element grid with a maximum aspect ratio of about 4 to 1, which approaches the possible limit, it seems impossible from both computer memory requirements and time limitations to carry out such an analysis. As a consequence, it has been necessary to homogenize the structure to a considerable degree. There are a number of approaches to homogenization, e.g., a psuedo-material made up from volume averaged moduli, etc. At this point, it is not clear which homogenization approach is the most realistic to use for the CTR magnet structural evaluation.

Some degree of detailed understanding of the stresses and strains acting in the actual heterogeneous structure can be obtained by an analysis of a small portion of the magnet coil using as input boundary conditions the strains or forces obtained from a homogenized analysis of the entire magnet structure. This result, however, is subject to the same uncertainties mentioned above, since the applied boundary conditions arise from the homogenization approximation. In addition, even for the small portion analysis, a correct three-dimensional evaluation of the actual heterogeneous structure will probably have to incorporate details regarding the inelastic behavior of the copper in the composite, the sliding and fractional effects of the spacers between the discs, the fractural behavior of the plastic spacers, the inelastic, and possibly even (because of the epoxied fiberglass) the orthotropic and fracture mechanics behavior of the glue. It is to be noted that fracture mechanics effects are usually evaluated from one-dimensional tests. For the three-dimensional cases, involved in the fusion magnet, relating the one-dimensional measurements to the actual design evaluation can be a very difficult problem. Furthermore, since there will be a large cyclic stress variation in the Tokamak system due to pulsing, with the total number of cycles reaching into

the many millions, it seems inevitable that fatigue (perhaps even creep-fatigue), must be analyzed. At this stage, it seems that such detailed analyses would be very difficult to carry out. Because of the desire for a very high degree of safety, reliability, and economy for commercial reactor systems, it seems likely such analysis capability will have to eventually be developed for such types of magnet structures. In fact, it does not appear to be too difficult to develop this type of capability in the next few years. If this is done, such analyses could be used in designing magnet systems for experimental power reactors.

Returning to the second class of analyses involving non-normal or accident conditions, where melting, large deformations and all types of material failure may occur under three-dimensional conditions, it can be seen that more complex and sophisticated structural analysis methods must be used. Some of the techniques for these analyses already exist in various forms, others will have to be developed and verified by experimental means. It will be more difficult to fully develop this analysis capability, and more time and manpower will be required.

III-2.1 Elastic Analysis

Over the last few years, by far the most versatile method applied for complex structural analysis work has been the finite-element method, and there are a number of excellent references

describing this method in detail. The principle advantages of this method for analyzing CTR magnet structures are: 1) its ease of application to complex shapes with arbitrary loading and boundary conditions, and 2) ready accommodation of highly heterogeneous structures, with regard to both material properties and composites. As an example, a typical PCRV (Prestressed Concrete Reactor Vessel) composed of concrete, rebars, pre and post tensioning tendons, and a gas tight liner, all of which interact together under various types of loads (pressure, temperature, etc.) can readily be analyzed with finite-element methods. Because of the interaction effects, it would be extremely difficult to solve this type of problem by other methods, even if the shape were readily simple, which the PCRV is not. 3) Relatively rapid computer solution times even for complex heterogeneous structures.

The principle limitation of the finite element method for analysis of CTR magnet structures is that three-dimensional CTR magnets with complex loading and a considerable degree of heterogeneity (such as composites incorporating conductor/reinforcement/glue/insulator/coolant passages that are characteristic of most CTR magnets) require a very fine grid especially where relatively steep stress and strain gradients are expected. For the CDC 7600 computer which for the moment is one of the fastest and the most advanced systems available, core storage is still not sufficient to model the actual structure without some approximation to keep the total number of elements

within a manageable level relative to the available computer storage capacity.

In connection with the problem of computer capacity, the evolution of newer more sophisticated finite-elements has contributed substantially to analysis capability, given the limitations of present day computers. Not so long ago, the most advanced three-dimensional method available made use of tetrahedral elements having linear poly-nominal displacement functions typical of the type shown in figure III.5. A further advance was the introduction of a family of hexahedron elements which proved to be superior to the tetrahedron elements. Most recently the introduction over the last few years of isoparametric elements (shown in figures III.6 and 7), which make use of various shape functions that allow element distortion in the XYZ coordinate system, has substantially improved analysis capabilities for large complex structures.

These isoparametric elements require far less grid or mesh refinements than the lower order elements mentioned previously. In spite of this, however, it seems that for the present, CTR magnet structures will still require some degree of homogenization in order to obtain detailed solutions, even when only elastic response is desired.

As an example of the complexity involved, even for elastic behavior, the element in figure III.6 would become overly stiff

and give wrong results if applied to thin shells, for example, the glued regions. Also, for highly distorted elements it is known that artificial stresses could result. Thus a mixture of element types, (i.e., like those shown in figure III.8) have to be applied with inter-element deformation compatibility. Furthermore, it is also possible that a better, more sophisticated element could be developed in which the homogenization process fully describes the behavior of the real magnet structure. Some investigation into this possibility is contemplated. Finally, it should also be realized that both orthotropic and interlaminar slip type behavior prediction needs to be included as part of the elastic analysis package.

A number of elastic codes are available for static as well as dynamic analysis and a listing of some of these is given in Table III.1. The Mark, MESAP, ELAS, and Ansys codes are proprietary and thus the fine details of how the calculations and computer algorithms are performed and constructed are not that well known to us. It appears desirable that if these codes are to be used for CTR magnet structure analysis, that some degree of verification versus known solutions for CTR magnet structures be carried out. This is especially important for the inelastic behavior which will be discussed later.

Of the codes documented and available to the public, SAP IV appears to be quite versatile for elastic solutions.

TABLE III.1

AVAILABLE ELASTIC STATIC & DYNAMIC COMPUTER
CODES

NESAP ^{*2}	ELAS ^{*2}	NFEP (BNL) ^{1,2}	ANSYS ^{*2}	MARK ^{*2}	NASTRAM	SAPIV
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* PROPRIETARY

1 BNL (UNDER DEVELOPMENT)

2 THESE CODES ALSO HAVE INELASTIC CAPABILITY

There are, however, situations in which the results are not a true representation of the actual stresses acting in the structure. This is due to limitations as to the type of element available in the code. As an example, the SAP IV code limits the three-dimensional element to a maximum of 12 nodes. For certain structures with complex loading conditions, the results could be too stiff.

The above discussion is also true for the earlier three-dimensional computer codes that for instance, used tetrahedral elements which in most instances require many hundreds of elements plus a substantial amount of modeling experience to obtain a correct solution. Extrapolation from simple to unverified complex structure by use of these methods could result in error. Specifically, the above discussion can be translated to CTR magnet designs where the above methods are used to evaluate the elastic response of TF coils in Tokamaks undergoing static and dynamic twisting due to out-of-plane loads caused by poloidal fields, quenches in adjacent coils or seismic events.

The proposed TF coil designs for EPR's and commercial reactors are generally D shaped constant tension designs, where analysis assumptions are that the material is

homogeneous, isotropic and that the only loads acting on the structure are in-plane. In reality, however, the out-of-plane forces may be acting in a direction where the structure is substantially less stiff either because of inadequate lateral support, or because the magnet coil material has a very low modulus in the transverse direction. It is not entirely certain at this point if the structural response for certain proposed support designs are being correctly evaluated. It is possible that the elements used in some of the computer codes applied for these evaluations need adjustment in terms of nodal points, integration points and for material anisotropic behavior. Just because a method gives a correct solution for a relatively simple case, such as a plane or axisymmetrically loaded relatively thick structure, does not mean that it gives correct results for a relatively slim and flimsy structure undergoing complex out-of-plane three-dimensional static and dynamic loadings. While some limited check runs have been carried out thus far at BNL, it is our opinion that further investigations should be undertaken to determine the limits of validity of the various elements currently being used to evaluate structural response of all possible conditions to be encountered in the CTR magnet structures. Instabilities due to invalid analysis that produce erroneous strain and stress distributions could result in safety related problems.

Before turning to the inelastic methods, it is worthwhile to finish with a sample solution of a simple thick walled cylinder that demonstrates some of the points raised regarding possible modeling errors when using isoparametric elements to evaluate elastic structural response in CTR magnets.

Figure III.8 depicts the grid used to investigate the stress effects caused by a crack located at the uppermost outer-surface of the cylinder at θ equal to zero. As can be seen from the elastic displacement curve plotted for the uncracked (i.e., solid) cylinder, the radial displacement pattern around the circumference is egg-shaped instead of circular, which is of course incorrect. Furthermore, the displacement curve turns out to be circular when the grid is changed to that of symmetric pattern shown in figure 9. Nevertheless, the nodal displacements are still too stiff and do not match the results given in the literature⁽¹⁾ for this problem. While it is relatively simple to correct this model with the symmetric pattern to yield results compatible with closed form analytic solutions, it is more difficult to correct the grid for the case involving the crack, where the stress and strain gradients around the crack necessitate a non-symmetric pattern. The above is especially true for three-dimensional cases where core memory requirements are difficult to meet in most instances.

As mentioned, some work on checking out the limits of validity of elements for CTR magnet analysis is currently being pursued by the magnet safety group. The examples above were only prescribed to illustrate some of the problems. Results of a more detailed study will eventually be issued under a separate report. Obviously, it is extremely important to find out what pitfalls to avoid in using finite element codes generally available to the public. As mentioned, in some cases it may be worthwhile to carry out actual complex model test for verification purposes.

III-2.2 Inelastic Analysis

In general, with the exception of fracture mechanics and fatigue (which will be discussed later) and elastic instability (ie. buckling) problems, elastically loaded structures should not experience safety problems, provided that they always remain fully elastic. In order to examine consequences of accidents that could lead to magnet failure or even disruption, it is necessary to analyze in detail the inelastic response of the magnet structure. This type of analysis is much more complex and expensive to carry out than the elastic methods.

While there are several different procedures available for non-linear structural analysis, most finite element techniques tend to be iterative in nature. Essentially

the structure is analyzed many times using either a decreasing stiffness matrix and/or load vector, until a solution that satisfies both the equilibrium and the material stress-strain relationships is obtained.

One of the most accurate solution techniques is the so-called Multiple Steps Variable Stiffness Method. As indicated by its name, the method involves multiple load steps with variable or modified tangent rigidity and stiffness evaluated for each step. Additionally, each of the steps can encompass many cycles which in turn also involve tangent rigidity and stiffness modifications for each cycle. This method is quite expensive and time consuming, however, and several variations of this method are widely applied. These include the; (1) modified variable stiffness, (2) constant stiffness single load step, and (3) variable stiffness single load step methods. In (1) the tangent rigidity and stiffness is modified at the beginning of each load step but remains constant for all cycles within the step. In (2) the original linear elastic rigidity and linear elastic constant stiffness is used in conjunction with a multiple cycle method (shown in figure III.10). Similarly, method (3) also involves a single load step, however, for this method (shown in figure III.11) the tangent rigidity and stiffness are modified at each cycle. It should be noted that methods

(2) and (3) require the calculation of the unbalanced load vector for each cycle. The major difference between the two techniques lies in the fact that (3) requires additional updating of the stiffness matrix at each iteration cycle.

Generally speaking for structures that are slightly non-linear and where the non-linearities are confined to very local regions, the single load step variable stiffness and the multiple steps modified variable stiffness method give fairly good results while even for this case the constant stiffness results are too stiff. This method is not recommended even for simple structures where no failure situation exists and there is only relatively small amounts of plasticity. For cases involving gross non-linear behavior where the entire structure becomes plastic, it becomes necessary to use the multiple step variable stiffness method in order to obtain correct solutions.

In terms of evaluating the safety of CTR magnet structure where gross plasticity can take place throughout the structure it seems prudent to use the more accurate multiple step variable stiffness method especially for cases involving failure prediction. From an economic standpoint, it would seem advisable that the simpler and less time consuming modified

variable stiffness method (1) be used for scoping studies and for cases involving small amounts of plasticity. The relative accuracy and computational time saved depend on the individual problem and thus vary from problem to problem. From our own experience, covering a limited number of cases involving the extremes, we found that about half the time could be saved with the simpler methods and with fairly good accuracy. We have not as yet attempted these comparisons with large deformation problems and results for these cases may or may not be as favorable. We plan to carry out further work and comparisons in this area.

It should be noted that for the existing standard isoparametric finite element formulation which use Gaussian quadrature numerical integration, the state of stress used to determine whether the element becomes plastic or fails (for a fracture criteria) is the stress at the Gaussian integration point. This means that for a two point Gaussian, the location of the stress under consideration is substantially below the surface of the element. Instead of the outer-most fiber becoming plastic first in an element undergoing bending, current methods only indicate plasticity when a point substantially removed from the outer fiber becomes plastic. Thus the code

can substantially underestimate the amount of plasticity occurring in the structure. This is especially true when, for instance, a single thickness element is used to evaluate the plastic bending response of a structure. Current work at BNL will eliminate this problem without resorting to a more costly, finer grid approach. We mention this only to point out the care that must be exercised in applying and in interpreting results obtained from existing available methods.

A further problem with plastic deformation analysis is that once the deformation exceeds 1 or 2 percent, the infinitesimal strain theory yields wrong results. For this type of problem the finite strain theory must be used. Figure III.12 illustrates this effect for a simple aluminum tube subjected to an internal pressure. Note that the decreasing pressure with the finite strain theory indicated tube failure at a pressure close to that of the experimentally observed case, while the infinitesimal theory shows no failure and in fact, indicates increasing pressure carrying capability. The material was found to be somewhat orthotropic which accounts for the difference between the experiment and the analysis.

As far as we know, existing (July 1975) 3-D public domain in elastic codes do not at the moment have this capability. BNL is at present testing such a finite strain theory element and this will be incorporated in the BNL code package for CTR magnets.

A three-dimensional large displacement plastic deformation computer analysis of a thick walled cylinder under internal pressure has been carried out and the results compared with those given by MacGregor et al. As far as we know, this is the only large displacement closed form solution available for checking purposes. With more complicated boundary condition, as for instance would be the case for CTR magnets with a large local quench and subsequent complex local plastic deformation, the problem becomes impossible to solve analytically. As can be seen from figure III.13, the computer results are indeed quite close to those obtained by MacGregor, et al.⁽²⁾

At the present state of development, the large displacement formulations mentioned above can only be applied to problems in which there are relatively few elements, e.g., the three-dimensional case shown in figure III.13. Work is in progress to extend these techniques so that they can be applied to three-dimensional problems that require more elements. This will be especially useful for CTR problems where we envision the need for hundreds of elements encompassing thousands of nodes. This is particularly needed when investigating plastic failure around cracked areas. Once this is accomplished, it is planned to increase the program efficiency so that the computational time can be decreased substantially. It can be expected that the extended techniques mentioned above would still

require several hours for a program involving hundreds of elements. With the increased efficiency routines it should be possible to reduce the running by a factor of two or more.

The plastic-large deformation capabilities described in the last few paragraphs are part of a non-linear finite element program (NFEP) which is being assembled by BNL. When development, testing, and varification are completed, we plan to release NFEP and make it generally available. It should be noted that the plastic analysis results presented elsewhere in this report were carried out by an earlier elastic-plastic code developed by BNL. These elements are not of the isoparametric type. For some analysis, these elements are useful since one can use a large grid with relatively short computer running times. They are, however, less versatile and are restricted to axisymmetric or plane stress or strain problems. The NFEP package will include both small and large deformation elastic-plastic solution techniques with two and three dimension isoparametric elements. The older more simpler elastic-plastic codes will also be available in the NFEP program package.

While the improvements discussed previously will yield better results than those obtained from current methods, the problem of composite homogenization remains.

It will still not be possible to model the exact structure of CTR magnets in every detail because of computer core and cost limitations inherent for three-dimensional analysis. We thus advocate the development of a special family of isoparametric elements for CTR magnets. For example, in the case of a Tokamak TF coil, the method will allow the detailed analysis of the entire cross section with one element. Thus the entire structure can be covered with relatively few elements, without resorting to homogenization. The element will be so constructed that all possible constitutive relationships of the cross section are accounted for including: elastic, inelastic, small and large deformation, interlaminar slip, etc. Obviously, such a development will require a great deal of analytical work in conjunction with physical test and model studies. The computer algorithm development for those elements will be such that changes encompassing cross sectional material compositions properties, etc., can be accounted for without undue difficulty, when analysis of a range of different designs is required. Similar types of development have been carried for special types of complex structures and have indeed proven to be extremely useful in yielding accurate in-depth analysis results.

It appears that such a development will have to be carried out for definitive safety analyses. It should be started relatively soon to ensure that it will be available in the next few years for the EPR safety analyses.

III-2.3 Fracture Mechanics and Fatigue

To date, most of the fracture mechanics work has been restricted to brittle fracture which is defined as failure at stress levels below the yield strength. Even here the fundamental concepts apply only to thin shell type structure experiencing plane strain or plane stress conditions. The effects of complex loading causing combined Mode I and Mode II behavior are only now being investigated and are not so well understood. Furthermore even for Mode I fracture, the current theory neglects thermal-mechanical coupling, dynamic effects, and as mentioned, only considers elastic behavior. For three-dimensional elastic analysis, the postulated relationships between K_I , K_{II} , K_{III} , and the energy release rate are no longer valid. Thus for CTR magnets where non-zero stress intensity factor (K_I , K_{II} , K_{III}) could be expected due to the complex loading situations, it would be unrealistic to evaluate magnet safety on methods that are based in a K_I fracture criterion alone.

There are many three-dimensional stress situations where 3-D fracture mechanics methods should be used to predict the operational safety of flaws not detected during inspection process.

For accident conditions, it can be expected that parts of the structure can undergo gross plasticity. This occurs during certain quench or arcing situations, or possibly during seismic events. In order to predict failure under various temperature conditions where the material contains flaws, it will be necessary to develop methods that can be applied for such loadings. As an interim technique, we can apply the large displacement plasticity techniques outlined previously in Section III.2.2.

In addition to its application for brittle fracture the concept of stress intensity can also be used for prediction of the safety of components undergoing fatigue and stress corrosion. Here the stress intensity factor K_{IC} is used to predict slow crack growth, commonly referred to as "sub-critical crack growth". Although a flaw may be too small to cause brittle fracture, it can nevertheless be above the threshold for fatigue crack growth. The above possibility can indeed exist in a magnet structure where small amplitude stress caused by force variations can act on existing small cracks, eventually resulting in crack extension and consequent quench effects. For one-dimensional conditions or simple planer shapes, the current methods should suffice.

There are, however, a class of cyclic behavior problems that could cause out-of-phase biaxial or even triaxial cyclic stresses and strains. During the plasma operating cycle the TF coils will be subjected to time varying out-of-plane loadings

resulting from transverse fields generated by OH diverter and equilibrium coils. In general the field and hence, the forces will not be in-phase and will vary in a complex manner in both time and space around the perimeter of the TF coil.

Most work to date has been carried out with one-dimensional specimens and the applications of these results to problems involving biaxial fatigue are questionable for both in-phase and out-of-phase cyclic conditions. Thus, the validity of using these methods for magnet safety prediction is completely unknown. Work in this area is currently in the planning stage.

III-3 PROBABILISTIC DESIGN

As mentioned, all material properties, dimensions, loads, fatigue, fracture, etc., are not single valued properties. For critical designs such as the large size superconducting magnets proposed for CTR's comprising systems costing hundreds of millions of dollars, it seems prudent to treat the engineering variables statistically. Essentially, each of the variables can be modeled to reflect the spectrum of possible values. These can then be adopted and checked for adequacy of fit for various classical distribution techniques such as Weibull, log normal, gamma, extreme value, etc., in order to yield probabilities of failure for the magnet structures. It needs to be pointed out that the reliability of the assumptions and estimates obtained from the above methods will strongly depend on the availability of data, which is quite scarce for some of the

components comprising the CTR magnets. Thus, in order to obtain meaningful statistical safety predictions, it will be necessary to carry out a substantial number of physical tests.

As an example of the application of statistical techniques to predict the probabilities of component failure, BNL is currently involved in estimating probabilistic failure occurrence for LWR and HTGR steam generator tubing systems. In this area we are combining three-dimensional inelastic analysis with extreme value and Weibull distribution techniques.

III-4 ACCIDENT ANALYSIS CODES

As pointed out earlier, the evaluation of magnet safety and reliability requires considerably more than structural analysis. We foresee the need for accident analysis computer codes in which the coupled structural thermal, hydraulic and electrical analyses are incorporated in such a manner that the interactions between them are fully taken into account. At the present time, safety analysis is carried out in a step wise manner where results of individual analyses are fed into the individual computer code for the next step. In most cases this is very cumbersome, and in many instances requires simplifications since the elements of the different codes may not match. This type of accident analysis code will require a finite element grid compatible with all the analysis types mentioned above. It should be realized that this type of code will require large capacity and long computer running time

since it involves many sequential analysis steps. This will certainly be a long term development process and will not be available for quite a few years. Simpler versions (i.e., coupled thermal-structural analysis) will probably be available within a year with isoparametric elements.

III-5 BENCHMARK PROBLEM

As was shown previously, even for long developed elastic and inelastic techniques, the individual response of various types of elements can yield erroneous results. For the more sophisticated specialized isoparametric elements that will allow analysis without homogenization, the possibility of erroneous results also exists. We foresee the need to develop benchmark solutions to check out the various codes and elements currently being applied as well as those that will be developed for CTR magnet safety analysis.

Figure III.1. UWMAK - 1 TF coil.

Figure III.2. Cross-section of UWMAX
- 1 magnet disc.

Figure III.3. Argonne TFTR magnet.

Figure III.4. Cross section of Argonne TEPR magnet.

Figure III.5. 3-D quadrahedral element.

Figure III.6. 3-D isoparametric element.

Figure III.7. 3-D isoparametric membrane shell element.

Figure III.8. 2-D tube model.

Figure III.9. 2-D tube model.

Figure III.10. Constant stiffness, single load step method.

Figure III.11. Variable stiffness, single load step method.

Figure III.12. Pressure expansion curves
 for aluminum tube $\frac{R_1}{R_0} = 2.0$

Figure III.13. Pressure expansion curve for aluminum tube (17SO aluminum, $R_1/R_0=20$).

IV. ANALYSES OF SELECTED ASPECTS OF FUSION MAGNET SAFETY AND RELIABILITY

IV-1 STRUCTURAL SAFETY ANALYSES

IV.1.1 Introduction

During the past year our structural safety analysis for superconducting fusion magnets has been concentrated on the safety aspects of the toroidal field coils found in reactors of the Tokamak type. Specifically, the toroidal coils proposed for the University of Wisconsin UWMAK-I design were investigated in great detail. The UWMAK design was the first to be analyzed because at the time this investigation was initiated, UWMAK-I was the only design which had been developed enough by its originators and for which sufficient engineering details were available to make an analysis meaningful. As other reactor designs are being proposed and developed, they also are being investigated by BNL. As far as the present report is concerned, while specific inputs for the analysis were taken from the UWMAK-1 system, many of the results are not tied to the exact internal coil geometry of UWMAK-1 and may help in pointing out problems in the design of other magnet systems.

The coils are described in detail in the Wisconsin report⁽¹⁾ but a brief summary of their design is given here for completeness.

Depicted in Fig. IV.1.1 is a typical profile of the "D" shaped toroidal field magnet. Because of symmetry, only one-half of

the magnet is shown. In the proposed design, the magnet consists of a stack of identical thin discs separated by micarta spacers which also serve as insulators.

Each disc is comprised of Nb-22% Ti and Nb-48% Ti as the superconducting materials, copper as the stabilizing metal and stainless steel as the structure material. A typical cross section of the disc is shown in Fig. IV.1.2.

Each magnet coil is comprised of 34 discs fastened together by 1,670 aluminum alloy bolts one inch in diameter. Each disc has a total of 60 conductor turns, i.e., 30 turns on each disc face. The design calls for a field of 3.83 Tesla at the plasma center which is at a radius of 13 meters from the toroid center. The conductor current specified has a value of 10,212 amperes.

The coils described have been analyzed using a variety of large capacity finite element computer codes that allow for the evaluation of stresses in elastic or elastic-plastic zones and around singularities in the magnet structure. Most of the codes also have the ability to include thermal stresses due to temperature inhomogeneities. The elastic-plastic stress analysis and fracture mechanics codes used for a large portion of this work are limited to two-dimensional elements, so that in most cases under discussion here, a plane state

of stress has been investigated not a three-dimensional one. Particular codes are discussed further in subsequent sections of this report where they have played a role in the analysis under discussion.

A look at the magnet cross section depicted in Fig. IV.1.2 shows the complexity of the internal structure of each disc. The many different layers of superconductor, copper stabilizer, glue, steel reinforcement and micarta spacers make the computer code modeling for the coil properties a difficult task. (It should be noted that while Fig. IV.1.2 shows the UWMAK-1 coil cross section, the many different layers of materials in the coil interior are typical of all the major designs proposed.) The number of finite elements needed to model each region of material separately would far exceed the capabilities of any computer codes presently available. The only alternative is to use larger elements which have "averaged" properties assigned to them. In other words the whole coil or portions thereof, are considered to be made up not of separate layers but of a single material whose properties are a homogenization arrived at from the properties of the various layers. For the purpose of a stress analysis, different approaches can be taken toward this homogenization ranging from neglecting the strength of all the coil materials except the structural

steel, to a detailed calculation of appropriate constants for laminated composites.

For the purposes of the initial analysis discussed here the properties used in the computer codes were obtained by averaging the properties of the individual material components according to the percentage of the total cross section they comprise. Due to the small fraction of superconductor and glue compared to the amount of copper and steel, the properties of the former materials were not included in the averaging processes. Thus the values used were between those of copper and steel and interpolated according to the fraction of the total made up by each of these two materials.

Property constants chosen in this manner will lead to correct calculations of gross displacements but the stress values obtained from the code will be those of the fictitious "average" material. Under certain assumptions, as to the state of stress in the "averaged" material, stress values in the actual copper and steel can be found. Details of these calculations are discussed in the next section.

The problem of choosing appropriate material constants is an important one which needs more investigation. Some recent results indicate that the shear modulus may have to be chosen, independently from the other constants.⁽²⁾ Future work at BNL will

compare material properties found from composite laminate theories with those of the simpler averaging methods used to date.

In subsequent sections of this report the following structural safety problems arising from possible accident conditions are discussed:

1) Overall stress distributions due to normal operating loads in a coil disc. Stress values around the circumference and through the radial width of a D shaped disc are determined due to the design toroidal magnetic field values.

2) Stresses due to inhomogenous temperature distributions in magnets where quenches have been initiated. Various shapes of "quenched" regions in a portion of the coil have been assumed and the stresses due to several temperature profiles in these regions have been calculated.

3) Stress distributions and yield areas due to cracks and failed regions in the coil. Stress concentrations due to long narrow crack-like gaps orientated in a radial direction in the disc are determined. Some fracture mechanics analysis related to crack propagation has also been initiated.

4) Magnet response to seismic excitation. Using a response spectra approach the stresses due to an assumed earthquake input spectrum were determined.

The investigations discussed under points 1) and 4) above involve modeling a complete coil disc on the computer.

(Actually only a symmetric half is modeled.) But the areas mentioned under 2) and 3) involve modeling a portion of the disc in greater detail. The following method was used to achieve this detailed modeling without exceeding the capacity of the available computer programs. Once overall stresses and displacements in the whole coil disc due to the toroidal field had been established, a section of the coil was isolated and modeled in greater detail, i.e. with a finer element network. Appropriate boundary conditions for this detailed section were determined according to the results of the whole coil displacement picture. When it was verified that the stress and displacement values of the detailed section matched those found at the appropriate location in the whole disc for normal operating conditions, the effect on the stress and strain state in the detailed portion due to temperature rises and failed regions was determined.

Except for the seismic analysis only steady state type loadings have been considered. Cyclic effects arising from pulsing magnetic loads for instance have not been considered in the present work. Moreover no prestressing was taken into account, i.e. all materials were assumed to be unstressed at 4°K under no load. It was felt that the difference in initial stress states due to cool down, etc. would not have significant influence on the large scale effects under investigation here.

IV-1.2 Overall Stresses in a Coil Disc

During the operating conditions, the field magnets are subjected to large radial forces due to the interactions between the field magnets and current carrying conductors embedded in the coils. These forces are generally nonuniform along the circumferential direction of each magnet and produce high stresses in the structure.

One of the symmetric halves of a toroidal field coil of the pure tension D shape type called for in the UWMAK-I design was analyzed using both elastic and elastic-plastic BNL in-house codes. The coil shape was that supplied by Warren Young⁽³⁾ of the University of Wisconsin. 936 triangular elements were used to model the disc for the elastic runs and 1092 elements were used in the elastic-plastic code. A typical element grid for these computer runs is shown in Figure IV.1.3.

The average force on a unit length of coil can be calculated as follows: The force on a conductor per unit length due to a magnetic field is given by $F = B \times I$ where B is the field varying according to $B = B_0 R_0 / R$. For UWMAK-1 B_0 is specified as 3.82 Tesla at $R_0 = 13\text{m}$. I is the current in the conductor and R the distance from the conductor to the center of the field. Assuming the force on a coil disc varies linearly from a maximum on the inside to zero on the outside the average force per unit coil length is given by $F_{\text{avg.}} = (B \times I)/2$. For UWMAK-1 this force

is equal to $\frac{1.675 \times 10^6}{R}$ lbs. per inch length per inch depth.

The direction of the force is always normal to the coil circumference and outward.

Forces are applied at the nodal points of the finite element mesh and vary linearly across the 8 radial nodes from a maximum at the inside node to zero at the outside node. The total of the nodal forces across a particular circumferential location adds up to the average load on an element arc length of coil. Other investigators have treated the forces as magnetic "pressures" applied at the inside of the coil. We have compared computer results for axisymmetric coils of radius 25 and 50 inches of 7 inch width loaded with a force varying radially as described above and loaded with an equivalent "pressure" on the inside surface. The hoop stresses of the pressure loading were within 10% of the varying force loading in both cases but radial stresses are much less in the varying force case than in the pressure case. Since hoop stresses are an order of magnitude greater than the radial stresses, the "pressure" type calculations are adequate for the gross stress picture but results will obviously not be as accurate as those obtained with the radially varying force distribution used for the results reported here.

Element stiffnesses and yield points were taken as lying between those of the two dominant materials of the coil, i.e. copper and steel, according to the volume fraction of these

materials in the disc cross-section. With an elastic modulus of 30×10^6 psi for steel and 17×10^6 psi for copper the "homogenized" materials modulus was taken as 24.6×10^6 psi (1.7×10^{11} N/m²) for a 42% copper, 58% steel material. A yield point of 61 ksi (4.2×10^8 N/m²) for the averaged material was determined in a similar way from a σ_0 of 97 ksi for steel and of 12 ksi for copper.

While overall displacement results obtained from use of these averaged elements are close to the actual gross copper and steel displacements, the stress values resulting from the averaging method are not meaningful in themselves and must be converted back to stress values in the copper and steel. Under the assumption of pure tensile stress, this conversion is a simple one. (In the actual coil the stress is not pure tension but tensile effects dominate.) With the parameters used, it is easy to show that for an average stress $\sigma_{avg.} < 17.4$ ksi the copper and steel will both be in the elastic range and the stress in the steel σ_s and the copper stress σ_c are related to the average stress as follows:

$$\sigma_s = 1.22 \sigma_{avg.} \quad \sigma_c = 0.69 \sigma_{avg.}$$

For $\sigma_{avg.} > 17.4$ ksi the copper has yielded and only the steel remains elastic. In this range:

$$\sigma_s = 1.725 \sigma_{avg.} - 8.7 \text{ ksi} \quad \sigma_c = 12 \text{ ksi}$$

The above conversion ignores any work hardening in the copper although the averaged material in the code does work harden. If $\sigma_{avg.} > 61$ ksi both copper and steel have yielded.

The stress values in the disc calculated by our elastic-plastic code agreed roughly with levels expected in Wisconsin's UWMAK report. ⁽¹⁾ Peak stresses near the central support point of the D were found to be 68.5 ksi compared to Wisconsin's estimated maximum stress of 60 ksi. The copper of course was in a yielded state throughout the coil disc. As expected tensile stresses predominated, with principal stresses orientated along the coil contour.

However, the stress pattern we found did differ in some important respects from that anticipated by the Wisconsin designers. We found stresses varying radially across a coil section indicating the presence of significant bending stresses. This was not unexpected and other investigators have also predicted this variation in the constant tension D shaped coil. ⁽⁴⁾ However, we also found that maximum stress values in the supporting steel at a radial section were not always on the inside radius, but instead varied from inside to outside and back to the inside as one moved along the disc circumferentially. On the portion of the D toward the center of the torus the highest stresses are at the inside radius, but toward the top of the D a transition has occurred with the peak stresses now at the outside radius. Along the outside vertical portion, another transition putting the highest stresses back on the inside has occurred. The results are depicted in Table IV.1.1 which shows stresses at various locations

along the circumference of the coil. These locations are also indicated on the disc grid of Fig. IV.1.3.

It was also found that if the center support of the coil was increased along its circumference, the overall stress pattern with the transitions described above remained the same but shifted along the circumference of the disc toward the outside of the torus. A slight reduction in all stress magnitudes occurred as the support at the center was increased along the D.

An attempt was also made at a more accurate homogenization of the materials by varying the properties of each of the 14 elements across the radial dimension in accordance with the volume fraction of copper and steel in each corresponding radial segment of the actual design. (Since the width and height of the conductor and stabilizer in the UWMAK-1 design vary from inside to outside of the disc there is proportionally more steel to copper near the outside surface than the inside.) This resulted in a disc with a slightly stiffer material on the outside than on the inside. As could be anticipated stress levels, when compared to the previous case, were now less on portions of the disc with peak stresses on the inside, but stresses were raised at the top of the D where high stresses are near the outside edge. A comparison of this more complex homogenization with the simpler one used during most of the present work is shown in Table IV.1.2

For further comparison, a D shaped coil of the same dimensions and copper to steel ratio as the UWMAK-I design, but with the entire conductor region located along the inside of the disc was also examined. The same loading magnitudes were used. Stresses in the steel reinforcement now exclusively making up the outside of the coil are shown in Table IV.1.3 at the same locations as the UWMAK-1 design values in Table IV.1.1. Because of the simplified geometry, no homogenizations or averaging was necessary for the computer inputs for this case. Comparison shows that stress levels in Table IV.1.3 are actually somewhat lower, indicating that the conductor on the inside design may have structural advantages, although one must be careful in interpreting the differences in stress values: For the UWMAK-1 design stress values in the steel were calculated by converting from the stress in the averaged material under the assumptions mentioned previously, while stresses were found directly for the case with the conductor on the inside.

The next step in this overall coil stress analysis is a three dimensional code capability. While some 3 dimensional elements have already been used to find coil stresses both here at DNL and elsewhere⁽⁴⁾, these were rather crude models with an entire coil cross section represented by one element. A three dimensional analysis with a fine enough element mesh to account for some of the subtleties of the internal coil

geometry is one of the main goals for future work on this problem at BNL.

IV-1.3 Stresses Due to Quenched Regions in the Magnet

An abnormal situation defined as a quenched state of the conductor may arise in the magnet, in which the superconductor goes through a phase change and recovers its electrical resistance. When such a state occurs, the conductor may rapidly heat up, inducing large local temperature gradients which can result in severe thermal stresses. Consequently, this may lead to localized failure or even melting of the coil assembly as a result of uncontrolled temperature rise.

The codes available for an initial analysis of such temperature effects on the stresses in the magnet were two-dimensional ones. To compensate for the three-dimensional effects of a temperature inhomogeneity in the coil disc, two different approaches using the two-dimensional codes were applied:

In one set of analyses the coil cross section was modeled in detail, (i.e. a section perpendicular to the circumferential coil direction) and the coil itself was assumed to be axisymmetric rather than of the constant tension D shape. Conductors and reinforcement could now be modeled separately and the effects of temperature inhomogeneities through the depth of the disc could be calculated. Of course temperature increases were the same all around the circumference of the disc due to the

axisymmetric assumptions. Only an elastic analysis was done for this geometry. In the second type of analysis a section from the actual D shape of unit depth was chosen for a more detailed finite element grid to investigate stresses arising from temperature variations along the circumferential directions of the coil. An elastic-plastic analysis was done for this case. Now the temperature was assumed constant through the disc thickness. Since the second type of analysis depended on the results of the overall coil stress distributions presented in the previous section, it will be discussed here first:

A section at the top of the D shape was chosen for detailing. This location was selected because it had relatively high stress levels during standard operating conditions and yet was far enough removed from the central support so that the end effects due to the support would not dominate the results. Actually, two sections were modeled, both centered at the top of the D. The smaller one using 1120 elements and 609 nodes represented a 62 in (158 cm) long circumferential segment of the coil of full width (36.6 in or 93 cm). The temperature excursion was assumed to occur in a circular zone $2/3$ of the disc width in diameter. The temperature was assumed to decrease parabolically from a maximum at the center of the region to the regular operating temperature at the edge of the circle. The longer region was made up of 1200 elements and 663 nodes and encompassed 175 in. (444.5 cm) of circumferential coil length. For this

section the temperature rise occurred in an elliptical zone whose minor axis was $5/6$ of the radial disk dimension, and whose major axis was 4 times the minor one. Again, a temperature decreasing parabolically from the center to the edge of the region was assumed.

For both sections, appropriate boundary conditions from the whole disc results were used along the radial edges and nodal forces due to the magnetic loadings were calculated. Forces and temperatures were applied consecutively using at least 20 steps to reach full load and temperature values. Temperatures were not elevated until the full loads for normal operating conditions had been applied. Once these loads and their corresponding stress levels had been reached, the maximum temperature at the center of each region was increased in 25°K increments to a maximum value of 400°K . Temperatures in the remainder of the region followed in the parabolic manner previously mentioned. The results of these computer runs modeling the temperature excursions showed that significant regions of yielded material in the steel supporting structure of the disc could be produced by thermal stresses due to moderate temperature rises during a quench. Results also showed that the radial location of the temperature inhomogeneity played a significant role. This is illustrated in Figures IV.1.4, IV.1.5, IV.1.6 in which identical circular temperature excursions are considered, but located

toward the inside, middle, and outside of the disc respectively. The most benign case occurred if the temperature rise was in the middle of the section. Figure IV.1.5 shows very little yielded material and this appears only at 400°K. The most severe situation occurred for a temperature rise near the outer edge, shown in Figure IV.1.6 where yielding started at 275°K and the number of elements yielded at 400° was quite significant. The case of a temperature inhomogeneity near the inner edge of Figure IV.1.4 was worse than that near the middle but better than that of the outer edge. The situation of Figure IV.1.5 was probably least severe because in this case there is more surrounding material to shed the stress from the heated region onto than in the other two cases. The outer edge at this location had the highest stresses in the normal operating case, so it is not surprising that the worst situation occurs there. However, results also indicate that the amount of bending stress at a particular location has a large influence on the severity of the temperature effects.

For an elongated quenched zone with a major axis four times its minor axis it was found that significant yielding did not occur until the width of the zone was 5/6 of the radial disc dimension. In this sense, the elongated quenched zone seemed more benign than the circular zone, probably because of the proportionately greater amount of surrounding unheated area available to take up the load in the elongated case.

Now the worst radial position for the quenched zone was near the inside coil edge with the next most severe at the outer edge and the least worrisome again at the center as shown in Figs. IV.1.7, IV.1.8 and IV.1.9. Apparently the heated zone size is coupled to the radial position in determining when the most yielding takes place. Stress contours for the case of Fig. IV.1.7 are shown in Fig. IV.1.10.

Another case with a different temperature profile was analyzed. The results are shown in Figure IV.1.11, which shows the yielded elements due to a uniform temperature rise in a square region $2/3$ of the radial width of the disk on a side, located at the inside edge of the disc. The temperature in the entire region was increased in 25° increments up to a value of 400°K . As was to be expected, more yielding took place here than in the parabolic tapering case.

The results of these "quench" analysis show that temperature distribution, zone size and location all play significant roles in determining the stress picture in the magnet, and in determining whether permanent damage will be done by a particular temperature excursion.

As a comparison to the above quenches occurring in a coil made up of alternating layers of conductors and structural steel, a quench in a coil with all the conductors on the inside and all the structural steel on the outside, as mentioned in IV.1.2, was also analyzed. The quench here was of the same length as the

elliptically elongated quench discussed above, but confined radially to the width of the conductor. Results showed that extensive stress redistributions took place in the copper conductor, but that none of the structural steel yielded even at quench temperatures of 350°K , the highest temperatures for which this case was investigated.

It should be mentioned that the above analysis did not include temperature effects on material properties such as elastic modulus and yield strength. Since in general the yield point is lowered with increasing temperature, yielding may occur sooner and be more wide-spread than the above analysis indicate.

The variation of thermal expansion with temperature has been accounted for, however. A piecewise linear function approximating the thermal expansion versus temperature curve was included in the computer program. Using this function the average thermal expansion coefficient for each temperature increment was calculated at each temperature step increase in the program.

The axisymmetric analysis of stresses due to temperature variations in the axial depth of the disc was accomplished in the following manner.

At the inner and outer edges of the 93 cm. coil width conductor blocks, steel reinforcement and micarta spaces were

modeled separately and in detail since stress extremes can be expected to occur at the disc edges. The central part of the disc was modeled as a smeared averaged material. The finite element grid at the disc edges is shown in IV.1.12 to IV.1.18. All conductors are assumed to rise to the same temperature (100° or 300° K) while the steel reinforcement disc in which the conductors are imbedded remains at 4° K. Figs. IV.1.12 - IV.1.14 show results for a 100° K conductor temperature and IV.1.15 - IV.1.18 for a 300° K conductor temperature. Both maximum stresses (in the plane of the figures) and theta stresses (coil hoop stresses along the conductor), resulting from the conductor temperature rise are shown. The theta stresses arising from the magnetic body forces must be added to the theta stresses from thermal forces to find the total stress acting on each part of the coil. The magnetic theta stresses are +40 to +68 Ksi, depending on position in the coil as mentioned in the previous discussion of overall disc stresses. A positive number indicates a tensile stress while a negative sign indicates compression.

Stresses are calculated both with the actual coolant gap in the micarta spacer between magnet discs and without a gap, so as to determine the effect of the necessary coolant channels on stress distribution. Two types of boundary conditions are assumed: Fixed-fixed and fixed-free. The

magnet disc is symmetric about its midplane, so only half of it is used in the analyses. The right boundary of the discs shown in Figures IV.1.12 - IV.1.18 is the disc midplane while the left boundary is the midplane of the micarta spacers. Nodes on the right boundary are always constrained against movement perpendicular to the boundary. Though the average boundary is fixed, nodes on the left boundary are either constrained against movement perpendicular to the boundary (fixed) or are allowed to locally move (free), to test the effect of boundary conditions. The fixed boundary corresponds to a coil assembly that was clamped very tightly, and the free boundary to one that was clamped less tightly.

The results indicate that a 100°K temperature rise in the conductor approaches a dangerous condition in the UWMAK-I magnet. With the extra thermal stresses the steel reinforcement is close to yielding. A yielding would not rupture the magnet however, since it would relieve the thermal stress. It would plastically deform the magnet, but a number of cycles would be required to rupture the magnet. The in-plane stresses in the micarta spacer are ~ 5000 psi without a gap. It is likely that the micarta spacer will crack, and may block the coolant channel. Equally serious is the probable cracking of the epoxy bond between steel and copper, which can cause electrical shorting during subsequent

charge and discharge of the coil.

A 300°K rise in conductor temperature will cause stresses far beyond the yield point. The steel reinforcement will yield until its strain approaches the temperature induced strain in the copper (i.e., a few tenths of a per cent); with peak stresses in the micarta spacers in the order of 30,000 psi, the spacers will crack over a large area, and many coolant channels could be effectively blocked. As the copper conductor is re-cooled, the epoxy bond will completely fail, since the steel has yielded in many places, opening up a gap between the copper conductor and steel. Conductors will then be essentially free in the slots in the steel disc, and can rattle around when magnetic loads are reimposed. Such motion can result in local normal regions leading to conductor melting.

Continuing operation after a temperature of 300°K had been reached in the conductor would probably result in magnet rupture.

A uniform quench would be the least dangerous type of quench from a safety point of view, since available energy would limit the conductor temperature rise to about 150°K and damage would be minimized. A more dangerous but probably more likely event is a propagating quench spreading from some initiating point. The quench can spread both along the conduc-

tor from the initiation point as well as laterally to other conductors which communicate via coolant channels. In this case the conductor temperature rise is virtually unlimited (until melting) since the total energy of the coil can be deposited in a small region.

The actual temperature distribution will not have the conductor at one uniform temperature and the reinforcement at 4°K . Transient heat transfer analyses are being carried out to determine actual temperature distributions, which will then be used as input to the quench and failure effect analyses. Fig. IV.1.19 shows an analysis of the outer (low field region) 3 conductors in the UWMAK-1 coil. A 3-dimensional finite difference transient code (Heating-3) is used. The conductors and reinforcement are divided into a large number (~ 30) of regions for the analysis. Starting temperature is taken as 76°K for both the conductor and reinforcement since the very small capacities below 76°K cause the calculation time to be impractically long (the time step interval for a stable numerical solution decreases as heat capacities decrease). Heating-3 is deficient for these types of analyses in that the time step cannot be varied during the analysis to reflect changing material constants (which results in very long solu-

tion time if one starts from 4°K), and that the heating terms (I^2R heating in the conductors) must be explicitly given as a function of time, not temperature as actually is the case (the latter restriction means that one must iteratively converge on the true solution, assuming an apriori heating rate change with time, and then compute a new one based on Heating-3 results). It may be possible to modify the code to remove these restrictions, and this will be investigated in the next time period.

From the results in Fig. IV.1.19, it is clear that in about 40 seconds the conductors reach $\sim 300^{\circ}\text{K}$, assuming only self I^2R heating from the currents they carry, and that there is an approximately 100°K temperature difference between conductor and steel reinforcement, even when heat flows from the conductor into the reinforcement, and the large thermal resistance of the epoxy bond is neglected.

The previous analyses assumed that all conductors start quenching at the same time. The much more likely case is that of a non-uniform quench. Here the temperature difference between conductors and their adjacent reinforcement will be small compared to the temperature difference between the quenched region and more distant parts of the magnet, which remain at 4°K . Therefore, a calculation was done with the inner 10% of the coil (copper and steel) going normal while

the rest of the cross section remains at 4°K . It was found that stresses in the heated regions could be as high as 100 ksi but in the rest of the cross section only about 5 ksi stress increases due to temperature were experienced. Therefore in the heated region plastic deformation of the magnet is very likely. It must also be pointed out that the above analysis assumed direct contact between the copper stabilizer and the steel reinforcement and therefore is completely valid only for sections where the conductors fit very tightly into the slots. In the average fit situation, the epoxy-resin layer between the conductor and steel is able to absorb some of the copper thermal expansion and therefore should reduce the stresses somewhat. However the epoxy thermal resistance may also cause additional increases in the temperature rise in the conductor.

A quench in one or a number of disks in a magnet coil can cause another threat to the structural coil integrity. The heated discs will expand relative to the unheated ones. This causes a shear stress in the aluminum bolts holding the 34 disks in a coil assembly together. While the exact bolt pattern (or alloy) is not specified in the UWMAC-1 design, it would seem that the 1670 aluminum alloy bolts on 12 inch center would be adequate to withstand large shear stresses. Indeed, preliminary cal-

culations, assuming that the bolts carry the shear loads equally) indicate that for temperature rises in several disks as high as 300° , shear stresses as well as principal stresses in the bolts remain at acceptable levels. However the assumption of equal load sharing by the bolts is a fallacious one. Instead the problem here is similar to the classical ones of thermal stresses in riveted elements such as discussed by Gatewood⁽⁵⁾ for instance. What is likely to happen is that almost the entire shear load will be carried by the extreme radial bolts and if these yield the load will redistribute itself to the adjoining row of bolts and may "zipper" through all the bolts in this manner. To confirm this supposition we modeled a 34 disc coil cross section for an axisymmetric coil with finite elements and assumed temperature rises in a number of discs. Computer results showed as expected that the shear stress between heated and unheated disks were very high at the inside and outside radial edges but negligible in between. It was found that even if only one disk quenched to a temperature of 100°K the resulting shear force at the edge, when distributed over the cross sectional area of the outer row of bolts, would cause shear stresses of over 70,000 psi in these aluminum alloy bolts. Obviously such stress levels at these relatively low temperatures are not acceptable, and the

bolt shearing due to thermal expansion is a serious enough problem to require a redesign of the mechanism holding the disks together.

While the two dimensional approach of finding the stress pattern in perpendicular cross sections of the coil disc has given insights into the thermal stress problems experienced under quench conditions, it also indicates the need for a three dimensional analysis in which the state of stress in the actual coil shape can be found due to a non-uniform quench spreading both along the coil circumference and across the radial and axial directions of the coil. Such an approach with an elastic-plastic capability will be undertaken by BNL in the near future.

IV-1.4 Stresses Due to Failed Regions

The purpose of this part of the analysis was to obtain stress distributions arising from small failed regions in a coil, particularly from long narrow, crack-like gaps orientated in a radial direction. The thrust of the method was not in the fracture mechanics direction (although some preliminary fracture mechanics results will also be discussed here) involving fracture toughness or crack propagation but instead was aimed at obtaining stress displacement values under the condition of an existing gap or crack of a particular length.

Such coil cracks would of course be accompanied by conductor failures, arcing, and high heat generation.

As in the first part of the temperature analysis discussed in the previous section, a small section, again centered at the top of the D shaped coil was selected for the more detailed analysis. This section was 2m (79 in.) long and full 93 cm (36.6 in.) radial thickness in width. A variable finite-element grid was used with the smallest mesh in the vicinity of the crack, as shown in Figs. IV.1.20 through IV.1.25. Appropriate boundary conditions were again taken from the overall coil results and the nodal forces corresponding to the magnetic loads calculated.

Cracks extending from the coil's outer edge toward the coil center were investigated. At this particular circumferential location of the D shape the highest tensile stresses are at the outer edge making this the worst location for a crack. Stress distributions for cracks with lengths of 3%, 10% and 30% of the coils radial width were obtained. These lengths correspond to the failure of one, three, and ten conductors and adjacent reinforcing material.

Initial analyses of these cracks were made with a strictly elastic code. Resulting stress levels in the steel reinforcement in the crack vicinity for the three cracks using an

elastic analysis are shown in Figs. IV.1.20 through IV.1.22. The normal steel reinforcement stress levels in an uncracked coil under full load at this location range from 32 ksi on the inside to 47.3 ksi on the outside.

As can be seen from the figures the elastic analysis predicts extremely high stress values (well above yield even for the 3% crack) at the crack tip which drop quickly within a short distance from the tip.

An elastic-plastic analysis, the results of which are indicated in Fig. IV.1.23, showed a small yield zone surrounding the crack tip with stresses again dropping quickly away from the tip and corresponding closely to the stress values of the elastic analysis except very near the tip itself.

Since, as mentioned above, a coil crack would be accompanied by heat generation, an attempt was made to include temperature effects in the analysis of the 10% crack. A circular temperature rise centered at the crack tip, with maximum temperature at the center dropping parabolically to 4°K at a radius of 13 cm (5 in), lowered all stresses in the reinforcement below yield for a maximum center temperature of 250°K . If the temperature rise dropped off more quickly in a distance of 6 cm (2.5 in.) some yielding still took place, but stress levels were significantly reduced.

An initial attempt at a fracture mechanics approach to

cracks in the coil disc was made using a fracture mechanics code on a 3/8 inch deep crack in the outer surface of the disc. This crack went completely through the circumferential thickness of the coil, and extended radially inwards. The results of several elastic runs of the whole coil and the deflections of the boundaries of a selected section, where stresses were maximized, were used as input data to the fracture code. The hybrid element grid with the special crack tip element is shown in Fig. IV.1.24

The results of the fracture mechanics runs indicated that maximum stresses in the vicinity of the crack tip should be between 140 and 160 ksi, rapidly falling off away from the crack as in a classic double-lobed crack-blunting model.^(6,7) The 3/8 depth is wholly within the rim of the coil. The stress intensity factor ranged from 40 to 50 ksi, which is below the published values for this type of material,⁽⁸⁾ which is usually listed as over 200 ksi. The above data indicates a satisfactory design, but there are several points that must be raised. The most significant is the method of stress distribution within the intricately laminated coil. The method of crack propagation in such a part is also questionable.

The material properties used in the fracture code was a homogenized compromise, and the relative thinness of the coil

indicates that for a material with the toughness of stainless steel, the validity of the theory used in the code is marginal.

It is in the fracture mechanics area, even more than elsewhere, that the difficulties of adequately modeling a complicated layered structure such as the magnet coils becomes apparent. State of the art fracture mechanics theory is wrestling with the problems of a two-dimensional state of stress. Therefore the difficulties of analyzing a crack propagating through regions under complicated states of stress in all three principal directions are large. Add now the additional complications of the layered inhomogeneous material and the application of presently available methods to this problem leave much to be desired. Obviously the exact details of the internal coil geometry and material layers are even more important in assessing crack growth and propagation than for thermal loads or other more macroscopic types of loadings.

After considering the above comments, as well as the apparent factor of safety indicated by the generally low values of the stress intensity factors, it seems that according to the present limited results crack propagation is not likely to occur in this design.

Crack stability under fatigue conditions is of course a

separate question needing a separate analysis.

IV-1.5 Earthquake Response Analysis

For the earthquake response analysis of the toroidal magnet, several assumptions relative to the characteristics of the structure and typical earthquake were made. The toroidal magnet was assumed to be linearly elastic with homogeneous material properties and elastic constants defined as before. The unit weight of the structure was taken as 370lb/ft^3 . Response spectra method was used with 50% of the El Centro 1940 earthquake as the input response spectrum.^(9, 10) The vertical displacement was assumed to be $2/3$ of the horizontal components. The two ends of the finite-element model were restrained from lateral movements as well as rotations.

For comparison of results, two finite-element models were used: one consists of three-dimensional beam elements capable of deforming in space and the other one is a three-dimensional solid structure consisting of 39 brick elements (8 nodes per element). Table IV.1.4 shows the comparison of the natural frequencies for the beam and brick models and good agreement was obtained for the first five frequencies. Examination of the modal participation factors revealed that the dynamic response of the structure was predominated by the first two vibrational modes. Higher modes had little significance

on the structural response.

The normal stresses were calculated for both the brick and beam elements, and the stress distributions (vs. angular positions) are plotted in Fig. IV.1.25 for comparison. It is seen that the difference between the two analysis models is insignificant.

The computer program for the 3-D brick element gives stresses at the center sections of the element, at which the stress may not assume its maximum value. In fact, the maximum stress occurs near one of the corners of the element due to the combined bending actions. The beam model does give such maximum values as shown in Fig. IV.1.26. From the figure, high bending stresses occurred near the fixed ends. Nevertheless, the stress condition in the magnet caused by earthquake alone is rather low and thus does not seem to cause any concern for the proposed design at present. Moreover, the coil support in the actual design is substantially greater than that in the model assumed for the seismic analyses, making seismic damage in the design even more unlikely.

The effect of earthquakes on the structure may attain greater importance if the out of plane forces on the toroidal coils due to the other magnetic fields, i.e. equilibrium and ohmic heating coils, are taken into account. Present indi-

cations are that even slight displacements from the vertical equilibrium position of any one coil can lead to serious structural consequences throughout the torus. Therefore while the structure may be braced for the expected out of plane forces due to these fields, even a slight disturbance to the equilibrium positions of the coils from an unexpected source such as an earthquake may result in serious damage. In the near future earthquake response analysis and its effect on equilibrium of the coils will be undertaken at BNL.

TABLE IV.1.1

<u>Location</u>	<u>Distance From Torus Center</u>	<u>Distance Above Torus Midplane</u>	<u>Stresses in UWMAK Coil</u>		
			<u>Inside</u>	<u>Middle</u>	<u>Outside</u>
1	190 in.	201 in.	64.9	54.6	44.4 ksi
	483 cm	510 cm	4.48	3.77	$3.06 \times 10^8 \text{ N/m}^2$
2	219	323 in.	68.5	49.9	33.9 ksi
	556	820 cm	4.73	3.44	$2.34 \times 10^8 \text{ N/m}^2$
3	286	406 in.	53.0	40.1	30.3 ksi
	726	1031 cm	3.66	2.77	$2.09 \times 10^8 \text{ N/m}^2$
4	373	451 in.	37.0	39.7	43.2 ksi
	947	1145 cm	2.55	2.74	$2.98 \times 10^8 \text{ N/m}^2$
5	466	460 in.	30.4	40.1	48.5 ksi
	1184	1168 cm	2.10	2.77	$3.35 \times 10^8 \text{ N/m}^2$
6	555	442 in.	29.2	40.1	49.6 ksi
	1409	1123 cm	2.01	2.77	$3.42 \times 10^8 \text{ N/m}^2$
7	697	355 in.	37.8	39.7	41.1 ksi
	1765	902 cm	2.61	2.74	$2.84 \times 10^8 \text{ N/m}^2$
8	786	239 in.	46.8	39.6	32.8 ksi
	1996	607 cm	3.23	2.73	$2.26 \times 10^8 \text{ N/m}^2$
9	832	119 in.	50.1	39.4	29.7 ksi
	2113	302 cm	3.46	2.72	$2.05 \times 10^8 \text{ N/m}^2$
10	846	0 in.	50.3	39.4	28.6 ksi
	2149	0 cm	3.47	2.72	$1.97 \times 10^8 \text{ N/m}^2$

TABLE IV.1.2
Stresses in UWMAK-1 Coil with Vary-
ing Material Properties Across Section

<u>Location</u>	<u>Inside</u>	<u>Middle</u>	<u>Outside</u>
1	55.6 3.84	55.6 3.84	51.1 ksi $3.52 \times 10^8 \text{ N/m}^2$
2	54.2 3.74	50.8 3.51	39.6 ksi $2.73 \times 10^8 \text{ N/m}^2$
3	45.3 3.13	40.8 2.81	35.5 ksi $2.45 \times 10^8 \text{ N/m}^2$
4	30.6 2.11	40.5 2.80	49.5 ksi $3.42 \times 10^8 \text{ N/m}^2$
5	24.7 1.70	40.0 2.76	55.3 ksi $3.82 \times 10^8 \text{ N/m}^2$
6	23.5 1.62	39.9 2.75	55.6 ksi $3.84 \times 10^8 \text{ N/m}^2$
7	31.4 2.17	40.1 2.77	47.1 ksi $3.25 \times 10^8 \text{ N/m}^2$
8	39.6 2.73	40.4 2.79	38.1 ksi $2.63 \times 10^8 \text{ N/m}^2$
9	42.5 2.93	40.5 2.80	34.8 ksi $2.40 \times 10^8 \text{ N/m}^2$
10	42.7 2.95	40.5 2.80	34.7 ksi $2.40 \times 10^8 \text{ N/m}^2$

TABLE IV.1.3

Stresses of Coil with Conductor on Inside

<u>Inside</u>	<u>Middle</u>	<u>Outside</u>
61.4	55.6	50.5 ksi
4.24	3.84	3.48×10^8 N/m ²
51.7	44.0	37.0 ksi
3.98	3.04	2.55×10^8 N/m ²
38.8	34.9	31.8 ksi
2.68	2.41	2.19×10^8 N/m ²
42.0	39.8	38.5 ksi
2.90	2.75	2.66×10^8 N/m ²
37.5	41.0	44.1 ksi
2.59	2.83	3.04×10^8 N/m ²
32.9	40.4	47.3 ksi
2.27	2.79	3.26×10^8 N/m ²
32.7	38.0	43.0 ksi
2.26	2.62	2.97×10^8 N/m ²
38.8	37.2	35.8 ksi
2.68	2.57	2.47×10^8 N/m ²
47.9	38.2	29.3 ksi
3.25	2.64	2.02×10^8 N/m ²
49.8	38.6	28.0 ksi
3.44	2.66	1.93×10^8 N/m ²

TABLE IV.1.4 - Comparison of Natural Frequencies

Mode	1	2	3	4	5	6
3-D Brick	71.8	88.7	189.9	193.2	353.9	361.3
3-D Beam	70.0	88.4	191.4	196.1	352.6	404.9

Figure IV.1.1. Stainless steel structural disc for "D" shaped toroidal field magnet.

Figure IV.1.2. Cross-section of disc of UWMak - 1 TF coil.

Figure IV.1.3. Element grid and stress locations referred to in Tables IV.1.1 and IV.1.2.

Figure IV.1.4. Yielding resulting from circular quench near inside edge of UWMak - 1 TF coil.

Figure IV.1.5. Yielding resulting from circular quench in middle of UWMak - 1 TF coil.

Figure IV.1.6. Yielding resulting from circular quench near outside edge of UWMAK - 1 TF coil.

Figure IV.1.7. Yield zone at $T_0 = 400^\circ\text{K}$ for elongated quench near inside edge of UWMAK - 1 TF coil.

Figure IV.1.8. Yield zone at $T_o = 400^\circ \text{K}$, elongated quench in middle of UWMAK - 1 TF coil.

Figure IV.1.9. Yield zone at $T_o = 400^\circ \text{K}$, elongated quench near outside edge of UWMAK - 1 TF coil.

Figure IV.1.10. Stress contours (ksi) in steel of coil for the case of Figure IV.1.7.

Figure IV.1.11. Structural effects of quench with uniform temperature zone in UWMAK - 1 TF coil.

Figure IV.1.12. Maximum stress (in plane), fixed free, no gap, for conductor temperature of 100°K in UWMAK - 1 disc.

Figure IV.1.13. THETA stress (hoop direction), fixed free, no gap, for conductor temperature of 100°K in UWMAK - 1 disc.

Figure IV.1.15. Maximum stress, fixed-fixed, with gap (in plane) for conductor temperature of 300°K in UWMK - 1 disc.

Figure IV.1.16. Maximum stress (in plane), fixed-free, no gap, for conductor temperature of 300°K in UWMAK - 1 disc.

Figure IV.1.17. THETA stress (hoop direction), fixed-fixed, with gap, for conductor temperature of 300°K in UWMAK - 1 disc.

Figure IV.1.18. THETA stress (hoop direction), fixed free, no gap, for conductor temperature of 300°K in UWMAK - 1 disc.

Figure IV.1.19. Transient temperature profile of UWMAK magnet disc.

Figure IV.1.20. Stress distribution in UWMAX
- 1 disc with 3% radial crack.

Figure IV.1.21. Stress distribution in UWMak
- 1 disc with 10% radial crack.

Figure IV.1.22. Stress distribution in UWMAX
 - 1 disc with 30% radial crack.

Figure IV.1.23. Stress distribution in UWMAX 1 dioc with 10% radial crack, elastic-plastic analysis.

Figure IV.1.24. Hybrid element grid with cracktip element.

Figure IV.1.25. Normal stresses for seismic response of UWMAK - 1 TF magnet.

Figure IV.1.26. Maximum stresses (at corner of finite element) for seismic response of UWMAK - 1 TF magnet.

IV-2 TEMPERATURE DISTRIBUTION DURING QUENCH SITUATIONS

IV-2.1 Introduction

In order to determine whether or not there is a significant chance of magnet damage (either for mechanical or electrical reasons) during a quench, it is necessary to predict the time-dependent temperature distributions in CTR magnets undergoing quenches. The temperature distributions that are obtained will strongly depend on the assumptions of how the quench started, as well as the magnet design. Further, in many cases the behavior of the helium coolant during a quench cannot be accurately predicted, and it is necessary to examine the range of possibilities by analyzing and comparing number of idealized models for its behavior. Finally, limitations on analysis (number of regions, time, etc.) will generally prevent detailed analysis of the complete CTR magnet so that certain smaller portions must be analyzed. The behavior of the rest of the magnet must then be assumed, so that the resultant analysis is considerably idealized.

In this study, temperature distributions in the UWMAK-1 TF coil were predicted for a variety of conditions. The principal goal of this study was to see if the disc approach had any fundamental problems from the standpoint

of temperature distribution during a quench. The problem of temperature distributions during an arc situation was not examined. It is planned to study this problem in FY 1976.

Temperature dependent values for electrical resistance, thermal conductivity, and heat capacity for copper and stainless steel were obtained from the BNL Bubble Chamber Data Book⁽¹⁾.

A three-dimensional finite difference transient code called Heating-3⁽²⁾ was used. Only two-dimensional problems were analyzed in this study, however, because of limitations on time and code capacity. The conductors and reinforcement are divided into a large number of regions and lattice points for the analysis. There are several deficiencies of Heating-3 for these analyses.

1. The explicit method used in Heating-3 for transient calculations requires very small time increments for a stable numerical solution and this results in very long solution times. Therefore, a starting temperature of 70°K for both the conductor and reinforcement was initially assumed at the beginning of this study. During the course of the study, we shifted to a modified explicit method to reduce the computer time. This allowed us to use a starting

temperature of 4°K , and the results shown were obtained with the latter method.

2. The limitations on region capacity (100), lattice lines along each axis, ⁽²⁾ and numbers of heat generation functions (20) made a complete detailed analysis of a single disc impractical. As a result, detailed analyses are restricted to relatively small portions of a disc or discs.

3. In the code the heat generation function is programmed to be time-dependent rather than temperature-dependent, whereas the actual case in the magnet is the latter. This restriction was overcome by iterative analysis. An a-priori time dependent heating rate was initially assumed. This was then adjusted, based on Heating-3 results, to obtain a more accurate time dependent heating rate.

A number of cases were selected involving uniform quench and non-uniform quenches. Selected areas of the UWMAK-1 coil were analyzed in detail to determine the actual temperature rises in the conductor, as well as the effects of quenching to the neighboring conductors, reinforcements, discs and insulators, i.e., micarta spacers of the coil under various assumed conditions of heat transfer, with or without

helium coolant. The results for the cases studied are summarized in the rest of this section.

IV-2.2 Time Dependent Temperature Distribution

Figure IV.2.1 shows the time dependent temperature distribution for the outermost portion (3 conductors in radial extent) of the UWMAK-1 disc, assuming that all conductors in the TF coil are driven normal at $t=0$, e.g., by a TES upon detection of a potential accident situation. The rate of rise of conductor temperature is quite fast for the outermost conductor turns since they have (1) less copper per amp than the turns of the inner edge of the magnet disc, and (2) lower thermal capacity [i.e., the volume of copper and steel per amp is also less on the outer part of the disc]. As a result, there will be a considerable temperature inhomogeneity in the UWMAK-1 coil, even assuming the most favorable condition, i.e., all portions of the conductor go normal at the same time. The conductors at the inner edge of the disc will only be at $\sim 70^{\circ}\text{K}$ when the outer conductors have reached 300°K . This large temperature inhomogeneity can result in plastic deformation of the stainless steel disc, as discussed in section IV-1.

This situation can be corrected by using the same volume of copper and thermal capacity per amp in all portions

of the disc. However, there will still be substantial temperature gradients between the conductor (due to the high conductivity of copper the conductor will be essentially at constant temperature) and the inner parts of the stainless steel disc. In Figure VI.2.1 there is a temperature difference of $\sim 100^{\circ}\text{K}$ between the conductor and the center line of the disc. More copper in the outer turns would help this situation and temperature differences could be reduced. Thermal stresses in the steel and copper caused by such a temperature difference would probably be tolerable.

However, because of insufficient code capacity, the analysis shown in Figure IV-2.1 did not include the thermal resistance of the epoxy layer between the conductor and stainless disc. Temperature differences on the order of $100\text{--}200^{\circ}\text{K}$ will probably appear across this layer (more if cracking occurs) when conductor temperatures approach 300°K . This will considerably increase the rate of temperature rise of the copper conductors, since the stainless steel disc will now be much less effective as a sink for the I^2R heat, and the disc will remain much closer to 4°K than the analysis shows.

This large temperature difference will expand the copper much more than the surrounding steel disc. Cracking of the epoxy layer is likely to occur, and the conductor

may plastically deform. After subsequent cooldown and contraction of the conductor, the conductor may not be firmly bound in the disc, and motion may be possible. Such motion may initiate quenches during subsequent operation and the damaged epoxy layer will have a reduced breakdown voltage.

Figure IV.2.2. analyzes the case where a quench is initiated in a conductor on one side of the disc (25 cm from the outer edge of the disc) and the progress of the quench is followed in time. The disc is assumed to be thermally insulated on both sides. This maximizes the rate at which the quench spreads, since heat transfer to the helium coolant will slowdown the rate (and may stop the quench for certain conditions). The NbTi in the conductors in the path of the quench is assumed to go normal when the conductor temperature reaches 7°K , and the conductor then generates I^2R heat appropriate to the temperature dependent electrical resistance of copper. The quench is assumed to extend indefinitely along the conductors. This results in a two-dimensional heat transfer problem defined by the cross section of the disc. Note that the temperature values plotted in Figure IV.2.2 refer to a distance of 1.7 cm from the disc center. (The disc surface is 2.5 cm from the center.) This line cuts

through the conductors as well as the steel between conductors.

The thermal resistance of the epoxy layer between conductor and disc is not incorporated in the analysis. Inclusion of this resistance will slowdown the rate of quench spread and increase its peak temperature at any given time. In effect, the quench wavefront will be considerably steeper than is shown in Figure IV.2.2. Note that after 50 seconds, the peak temperature in the quench shifts towards the outer edge of the disc. This is a result of the UWMAK-1 design in which the amount of copper per unit current monotonically decreases from the inside to the outside of the disc. The rate of temperature rise at the outer edge is less in Figure IV.2.2 than in Figure IV.2.1 because the quench start involved only one half of the disc. The half of the disc on the other side of the center plane then acts as a heat sink to retard the spread of the quench, since it only begins to generate I^2R heat after some time delay.

IV-2.3 Effect of Cooling

Figure IV.2.2 and IV.2.4 indicate how the quench is affected by cooling due to the liquid helium bath. In this analysis the convective flow of liquid helium is assumed to be sufficiently good that the helium temperature

is always at 4°K outside the blocked region. In Figure IV.2.3, three conductors are assumed to be thermally blocked (e.g., their coolant channels do not function) indefinitely along their length, while all other conductors can transfer heat to the helium bath with a heat transfer coefficient of $0.5 \text{ watt/cm}^2\text{K}$, characteristic of nucleate boiling. When adjacent conductors reach 7°K , the NbTi is assumed to go normal.

With helium cooling, the quench is confined to the conductors that are thermally blocked for the time period studied (60 seconds). If the three conductors are blocked on only one side of the disc, the temperature rise is very slow, since the opposite side of the disc acts as a heat sink. The thermal resistance of the epoxy layer should considerably increase the rate of temperature rise, since less heat will flow to the center stainless steel disc as well as the unblocked conductors. The quench will remain localized if the effect of the epoxy thermal resistance is taken into account.

IV-2.4 Effect of Cooling Channel Blockage

With blockage of three conductors on both sides of the disc, the rate of rise of conductor temperature is considerably greater, since the other side now does not act as a heat sink. The quench is localized, but there is an indication

that the temperature of the adjacent unblocked conductors is rising, and these will probably go normal after 100-200 seconds.

Figure IV.2.4 shows qualitatively similar results for 5 blocked conductors. The principal difference is that 5 blocked conductors result in a somewhat faster rate of rise of conductor temperature than 3 blocked conductors.

IV-2.5 Effect of Adjacent Discs

Figure IV.2.5 shows the effect of adjacent discs, mica spacers, and heat transfer to the helium bath for a quench involving the outer 4 conductors in a disc. The conductors on both sides of the disc are assumed to go normal at $t=0$. The helium convective flow is assumed blocked in the region of the quench. It thus functions as a heat sink only to the extent that the mass of helium between discs remains constant in the course of the quench, but its temperature rises with time. The effective thermal conductivity of helium is assumed to be high, i.e., $1 \text{ watt/cm}^{\circ}\text{K}$, because of convective mixing in the trapped helium. The thermal conductivity of mica is assumed to be $10^{-3} \text{ watt/cm}^{\circ}\text{K}$, and the surface temperature of the adjacent discs is determined by heat transfer from the trapped helium between discs (i.e., the coolant channels

on opposite sides of the micarta spacers are assumed to readily communicate through holes in the disc).

Comparing Figures IV.2.5 and IV.2.1, it is evident that the helium heat sink, even if it is stagnant, considerably reduces the rate of rise of conductor temperature. It should be noted, however, that this result strongly depends on the degree of mixing in the trapped helium. If in the actual situation heat transfer to the helium were essentially conductive with a K approximately equal to that of liquid helium ($\sim 0.25 \times 10^{-3}$ watt/cm²K), the results would then be very similar to those in Figure IV.2.1. Note the large temperature difference between the center of the micarta spacer and the conductor, because of the very low conductivity of micarta. During a quench, its center will remain near 4°K, while the surface may rise several hundred °K. The resultant large thermal stresses may crack the spacer.

IV-2.6 Quench Propagation

Figure IV.2.6 shows the speed of propagation of a quench in the direction normal to the plane of the discs. It is assumed that all conductors in one disc go normal at $t=0$, and the quench then propagates through the set of neighboring discs. The location investigated in Figure IV.2.6 is at a radial position 2/3 out from the inner edge

of the disc. The temperature in the radial as well as the longitudinal (i.e., along the conductors) direction is assumed to be uniform and only varies in the direction normal to the discs. Heat transfer through the micarta spacer-liquid He region is assumed to be conduction controlled. The effect of the equivalent thermal conductivity for this region is investigated: a high value, 10^{-1} watt/cm⁰K, for thermal conductivity corresponds to considerable convective mixing of helium in this region (we assume that there is no convective flow of He from or to the region investigated in the analysis), while a low value, 10^{-3} watt/cm⁰K, corresponds to essentially zero convective mixing.

The rate of spread of the quench is very low if $K=10^{-3}$ watt/cm⁰K. After 300 seconds the quench has only propagated to the sixth disc beyond the point where the quench started. The rate of spread is approximately 3 times faster with $K=10^{-1}$ watt/cm⁰K. In an actual situation, the rate of spread will be considerably slower than the values shown here, because of the thermal resistance of the epoxy layer around the conductors, and the cooling by convective heat transport of helium. In fact, it seems

quite likely that any quench situation will be confined to a few discs. This is undesirable since the resultant inhomogeneous temperature distribution can cause serious stress problems, particularly in the aluminum bolts holding the disc assembly together. The high temperature discs will attempt to expand radially outwards, but will be restrained by the bolts tying them to the adjacent low temperature discs.

Figures IV.2.7 and IV.2.8 examine how rapidly quenches can propagate in the plane of the disc. To achieve a two-dimensional problem, it is necessary to make the disc surface extend indefinitely in a plane that is normal to the plane of the disc. [The temperature dependent heat generation rate per unit volume in the copper that is used in this idealized model is taken equal to that in the actual finite thickness disc.] Because of code capacity limitations, only a portion of the disc surface was examined. This portion was 20 conductors wide and 200 cm long. Since the problem is symmetrical about a line across the conductors, this 200 cm long section (with a $q=0$ boundary at the initial quench location) is equivalent to a length of 400 cm centered at the initial quench location which is $2/3$ out from the inner edge of

the disc. Three conductors are assumed thermally blocked for a length of 20 cm (10 cm on each side of the center line). Figure IV.2.7 shows the rate of quench spread assuming no heat transfer to the helium on the disc surface [i.e., heat transfer is solely by conduction through the disc]. Note that the quench will spread almost an order of magnitude faster along the conductors than across them. This difference would be substantially larger if the thermal resistance of the epoxy layer around conductors had been included, since this slows up conductive heat transfer across the conductors, but not along them.

Figure IV.2.8 shows the effect of helium cooling on the rate of quench propagation for the same problem. The helium bath temperature is assumed to be constant at 4°K , and the heat transfer coefficient also constant at $0.03 \text{ wall/cm}^2 \text{ }^{\circ}\text{K}$. The nucleate boiling range is not included in the analysis. The conductor is assumed to be fully superconducting till 7°K , at which point it goes fully normal. Neglecting the nucleate boiling portion of the heat transfer curve will tend to overestimate the rate of spread of the quench. Subject to this limitation, Figure IV.2.8 indicates that the quench spreads only slightly slower when the effect of the helium bath is included.

IV-2.7 Summary

The results of this study on UWMAK-1 TF coils can be summarized as follows:

1. If blockages of coolant channels (or other similar events) occur, local regions of the disc (or discs) can monotonically increase in temperature. The quench appears to propagate relatively slowly through the TF coil, even assuming conditions (i.e., no thermal resistance across the epoxy layer around conductors and no cooling by the helium bath) that maximize the rate of propagation. In the time scale of a typical accident situation, e.g., ~100 seconds, large temperature inhomogeneities will be produced if a TES is not used to drive all portions of the coil normal when a quench starts.

2. The quench spreads fastest along the conductor, next most rapidly across the conductors in a disc, and slowest in the direction normal to the plane of the discs. The relative ratios of rate of spread in the three directions are very roughly (100/10/1).

3. Even if all conductors go normal at the same time (i.e., an ideal TES), there will be large temperature differences between the inner and outer edge of the UWMAK-I disc due to the smaller volume of copper/unit conductor current at the outer edge of the disc. This condition can

be corrected, however, by making the copper/amp ratio everywhere the same [the thermal capacity per amp should also be the same everywhere].

4. During a quench, large temperature differences will be generated between the inside and outside of the micarta spacers. The resultant thermal stresses may crack the spacers.

5. The large temperature differences across the epoxy layer between the conductors and the stainless disc will result in the conductor expanding much more than the disc. This may crack the epoxy glue, and the copper may plastically deform. As a result, the conductor may not be firmly locked in place after a quench, and relatively large conductor movements may be possible during magnet operation.

It appears likely that other types of TF magnet constructions, e.g., the ORNL force cooled conductor and the ANL TEPR pancake wrap, will behave in a similar manner during quench situations. The conductor lengths are very long and there are many discrete conductor regions that are not thermally coupled except through insulating and reinforcement layers that have low thermal conductivity.

The UWMAK-1 disc construction would appear to produce somewhat faster rates of quench spread radially across the discs than the ANL TEPR construction. In the latter construction the conductor and reinforcement strip in each turn is insulated from adjacent turns by low conductivity electrical insulation, whereas in the disc approach, the thermal conductivity of the stainless disc is somewhat higher than that of the electrical insulation. The rates of spread in a direction normal to the discs or pancakes should be comparable for the two types of construction, as should the rate of spread along the conductors (assuming equal copper volume per unit amount of current and comparable cooling conditions).

In the ORNL force cooled conductor construction with welding of the stainless steel conduits into a monolithic structure, quenches will propagate in the plane normal to the conductor direction at a relatively slow rate which should be comparable to the rate across the UWMAK-1 disc with no helium cooling. Since the stainless conduits are welded into a thermally continuous structure in the plane, however, one should not see the very slow rate characteristic of spread in the direction normal to the UWMAK-1 discs, since there will not be the large thermal resistance of

the insulating spacers. Propagation rates along the conductor in the downstream helium flow direction will probably be faster than for the other two types of construction, and will probably be comparable in the upstream direction.

It is clear that more accurate, less idealized, heat transfer analyses should be carried out for a variety of magnet constructions and quench situations. Much detail is lost by making two dimensional analyses, and three-dimensional analyses should be performed. The capability of using temperature dependent heating sources instead of time dependent heating should be added, since the latter restriction makes analysis very cumbersome and time consuming. The problem of determining what the convective heat transport behavior of helium will be, particularly in the pool type dewars used in the UWMAK-1 and the ANL TEPR designs is very difficult, since realistically one cannot assume either fixed helium temperature or specified flow velocity distributions. Local helium temperatures and velocities will depend in a complex manner on the conditions of the quench. In general, these analyses have used assumptions that will underestimate temperature distribution problems. More accurate analyses are necessary to gain a better understanding of how severe such problems may be.

Figure IV.2.1. Transient temperature profile of disc in UWMAK - 1 TF coil if all conductors go normal at $t = 0$.

Figure IV.2.2. Transient temperature profile in disc of UWMAK - 1 TF coil if one conductor goes normal at $t = 0$ and all conductors are insulated from He.

Figure IV.2.3. Transient temperature profile of disc in UWMAK - 1 TF coil if three conductors are blocked from He cooling.

(CALCULATION BASED ON ONE DISC WITH 24 CONDUCTORS ON EACH SIDE)

Figure IV.2.4. Transient temperature profile of disc in UWMAK - 1 TF coil if five conductors are blocked from He cooling.

Figure IV.2.5. Transient temperature profile of disc in UWMAK - 1 TF coil assuming cooling by adjacent discs.

(CALCULATION BASED ON 12 DISCS WITH HELIUM COOLANT AND MICARTA INSULATION BETWEEN THE DISCS)

Figure IV.2.6. Transient temperature profile of discs in UWMAX - 1 TF coil for quench spread in direction normal to plane of disc.

(CALCULATION BASED ON ONE DISC WITH 20 CONDUCTORS, EACH OF 200 cm LENGTH, AND WITHOUT COOLING)

Figure IV.2.7. Transient temperature profile of disc in UWMak - 1 TF coil for quench spread in plane of disc, without He cooling.

Figure IV.2.8. Transient temperature profile of disc in UWMAK - 1 TF coil for quench spread in plane of disc, with He cooling.

IV-3 STABILITY AND AVOIDANCE OF QUENCHES

IV-3.1 Introduction

A major problem in the early development of superconducting magnets was the degradation in performance of superconducting wires when they were wound into a coil. A reduction in the apparent current carrying capacity of the wires was usually observed which leads to unpredictable magnet performance and premature quenches wherein the energy stored in the field of the superconducting magnet is dissipated quite rapidly in the magnet and magnet dewar as the superconducting material made the abrupt transition from the superconducting to the normal or resistive state.

Magnet quenches can be initiated, for example, by conductor motion and by magnetic field variations at the conductors. Because the magnetic flux can diffuse faster through bulk superconductor than can heat generated by moving flux, it is difficult to maintain isothermal conditions in the superconductor under conditions of changing field. If a sudden field change or conductor movement occurs, the superconductor is heated. Recalling that the critical current density of a high field superconductor decreases with increasing temperature, the field penetration depth in the conductor, which is inversely proportional to the critical current density, increases. A temperature rise thus

leads to a further field penetration, which again leads to a heating effect and further field penetration. This so-called thermomagnetic feedback can lead to a thermal instability or a flux jump which may destroy superconductivity and create a normal or resistive region that may propagate rapidly.

A number of different techniques were developed to stabilize the performance of superconducting magnets. A technique used successfully for large present day magnets is to bond the superconducting wire or filaments in good thermo-electrical contact with a normal metal of high electrical conductivity such as copper. When current is passed through such a composite conductor, no voltage appears as the current is increased until the critical current of the superconductor is reached. Any current in excess of the critical value flows in the copper giving rise to a voltage. The composite conductor is said to be cryogenically stable up to a given current if there is sufficient normal metal and cooling such that when the conductor is carrying its rated current and is driven completely normal by a disturbance, all the current then essentially appearing in the normal metal shunt for an arbitrary long time period, the temperature excursion is limited and the conductor recovers its superconducting properties.

One of the earliest forms of stabilization observed was that

achieved by application of an external magnetic field. For current to flow in the superconductor magnetic flux must move into the material. When the critical current density is high, however, the flux flow can become unstable and produce heating. The application of a background field which reduces the critical current density improves stability. This method of increasing stabilization has limited application.

The heating effect of unstable flux motion leads to the formation of normal regions in the magnet windings. Stabilization may also be achieved by balancing the energy dissipation against the enthalpy of the conductor. By adding a normal metal of high specific heat to the superconductor, or by utilizing helium in close proximity to the superconducting wires, the thermal diffusion time may be made comparable to or less than the magnetic diffusion time. This type of stabilization is termed adiabatic or enthalpy stabilization.

A related technique which has seen some application involves the use of a means, such as inter-layer foils of high conductivity copper, to damp the flux motion inside magnet windings. The aim of this magnetic damping is to increase the magnetic diffusion time such that it is greater than the thermal diffusion time of a disturbance.

Following the predictions of Hancox, P.F. Smith and others, it has also been demonstrated that flux jumps in superconducting wires can be eliminated when the diameter of the wires is so small that the energy associated with magnetization currents in the wires induced by an external changing field is insufficient to cause an appreciable increase in temperature when this energy is released suddenly as the magnetization currents decay. Superconducting wires which satisfy this size criterion are said to be intrinsically stable. Composite conductors incorporating fine superconducting filaments were developed with a major incentive for this development coming from an anticipated need for AC magnets. If the conductor length is greater than a critical value, however, magnetization currents flowing through the superconducting filaments in opposite directions induce a voltage between these filaments. This voltage drives eddy currents through the normal metal and generates ac losses. To minimize these losses, the superconducting filaments were twisted or transposed such that the twist pitch was smaller than the critical length for the conductors.

Based on the understanding of the behavior of high-field, high-current superconducting materials, these stabilization techniques were developed to improve the predictability and reliability of performance of superconducting magnets.

The avoidance of superconducting magnet quenches is, however, a very basic and subtle matter affecting the choice of fusion magnet design concepts. The detection of incipient quenches or other potential accident situations as they develop and the initiation of appropriate action to avoid them if possible, or at least to contain them safely is a very complex problem.

The great sizes and enormous stored energies, and the intricate structures and forces involved in the magnets of a CTR power reactor represent a large extrapolation from present experience. In addition, the superposition of the rapidly pulsed fields required in some reactors imposes further complexities and constraints on design. Because of the unique potential for self-destruction inherent in superconductors, the designers of fusion devices will have to judge carefully the state-of-the-art as it applies to the superconducting magnets being proposed for these devices. A foundation of operational safety for reactors will be the soundness of the ultimate future design choices.

IV-3.2 Stabilization and Cooling of Superconductors

The efficient removal of heat resulting from imposed field changes or mechanical motion is a critical aspect effecting

superconducting magnet performance. The largest DC magnets to date have used large cross section composite conductors whose current density is relatively low. These magnets are essentially cryogenically operated copper coils with superconducting "shunts". The surface area of copper exposed to the helium coolant is designed so that even if all the current temporarily flows in the copper, the temperature of the conductor will remain low enough that the current could return to the superconductors which make up only a small part of the cross-sectional area of the composite conductor.

In the relatively simple geometries used so far for these magnets, they have, overall, performed very well although troubles have occurred because of joint failures, shorts, lead problems etc, in some cases. There is the caution, however, that even for DC fusion reactor magnets, much more stringent safety criteria must be satisfied than in these quite successful laboratory type magnets. Also, the problems are considerably more difficult because of the greater complexity of magnet shapes, orientation, coupling between magnets, etc. as was briefly mentioned above.

The toroidal field magnets of the Tokamak design UWMAK I⁽¹⁾ and II⁽²⁾ and ANL⁽³⁾ are based on experimentally established DC design techniques. If plasma turn on times of the order of seconds

are necessary for fusion reactors, such coils have difficulties. Eddy current heating in the metal conductor matrix and in the coil and dewar structures becomes very large. In addition, the auxiliary pulsed magnet coils, such as the divertor or transformer coils, are themselves very large stored energy magnets by present standards.

Adiabatic and dynamical stabilization concepts have been used in pulsed superconducting magnets. These stabilizing techniques, however, are not as well established experimentally as is the technique of cryogenic stability. Adiabatic stability in practice can be attained by approximating a finely subdivided transposed conductor (Litz wire) so that the impressed changing magnetic field causes acceptably small heating. A magnet wound with such conductor can be operated with minimal cooling. Thus even a coil potted with epoxy resin can withstand rapid pulsing. However, in high field magnets very small motion of conductors under the large forces involved can generate intense local heating. If the magnet structure and assembly are not adequate to prevent such motion, this stabilizing technique is ineffective. It should be pointed out that, so far, CTR magnet designs have assumed high operating stresses in the magnet structure to achieve economically attractive designs. Such stress levels would not appear to be

compatible with potted magnets. Dynamic stability refers to the eddy current damping of flux movement and results in a slowing down of the thermal impulse to a degree such that the existing heat transfer may be adequate.

It should be noted that cryogenic stability can be achieved, at least in principle, even with higher current density conductors which do not require large normal metal shunting. Since the ratio of surface area to cross-sectional area increases as wires become smaller, as r^{-1} , a conductor of many small cabled wires can expose sufficient surface area to the coolant to satisfy the conditions for full cryogenic stability. However, the enthalpy available during thermal transients is also considerably smaller for this type of conductor, so that the amount and duration of heating that can be tolerated is considerably less than with conventional cryogenically stable designs.

There have been interesting experiments conducted at the National Magnet Laboratory evaluating the effectiveness of forced flow cooling with this type of conductor. In practice, a large current conductor of this type in which there is full mechanical support for the wires may be difficult to construct without compromising cooling.

Larger scale demonstration conductors using this promising

technique will determine whether or not they have the performance reliability of the large cross-section, rigid, composite conductors of superconducting wires bonded in a matrix of copper, or "laminated" copper using CuNi barriers to reduce eddy current.

The edges or faces of composite conductors or a combination of both may be used in the pool boiling mode of cooling. Increasing the surface area exposed to helium for greater reserve stabilization represents a compromise with the area of mechanical contact and, therefore, with mechanical support. In practice, for the low overall current densities being considered for the Tokamak TF coils, sufficient surface cooling area can be provided to satisfy the criterion for cryogenic stability. Since the recovery from film to nucleate boiling in liquid helium occurs at surface heat fluxes in the range 0.1 to 0.8 watts/cm², depending on the configuration of the conductor-helium interface, there must be sufficient normal metal and provisions for cooling to keep the dissipation below this level when the superconductor is in the normal state and essentially all the transport current is flowing in the normal metal.

Several cautions should be mentioned:

1. The effect of required dynamic field properties on the parameters of the composite conductor and the coil support structure may lead to deterioration of both longitudinal and

and transverse electrical and thermal conductivities.

2. The steady state heat transfer coefficient for nucleate boiling is not the only factor. Quenching is a transient phenomenon and transient heat exchange is very relevant. As will be discussed later in reviewing quench detection, very minute motions can rapidly liberate mechanical heat energy in magnitude equal to the energy transport current in the normal metal.

3. The determination of the degree of stabilization required depends on estimates of the probability that unusual events may adversely effect the theoretical or calculated heat transfer. Examples of such unusual events are partial or complete blockage of cooling channels by spacer movements and deposition of materials creating surface coatings which inhibit heat transfer. In detailed designs, criteria can be estimated for the length of conductor which can be cooled only by longitudinal conduction and which will still recover from a transient disturbance driving it normal because excess cooling capacity is available.

It should be noted that existing TF coil designs using cryogenic stabilization with pool cooling have generally determined copper/SC ratios and cooling areas on a maximum heat flux criterion of $\leq 0.4 \text{ w/cm}^2$, and such a criterion has proven satisfactory in existing large magnet systems. For reactor magnet systems, how-

ever, substantially larger safety factors may have to be employed. Reliability in such systems must be extremely high, and the physical scale will be much larger, so that small probability events become of greater concern. In addition, operating life will be much longer and operating conditions (street level, pulsed forces, etc.) will be more severe. If safety factors of 3 or 4 on burn-out heat flux prove to be necessary, design will be affected to a considerable degree.

It is beyond the scope of the present discussion to describe all the advantages and disadvantages cooling by pool boiling compared to single phase forced flow. Indeed, present knowledge does not permit an unequivocal choice.

Pool boiling does provide excellent heat exchange transverse to cooling channels. With proper design, this feature can provide excellent heat transfer with the fluid that is in contact with the heated conductor but also with neighboring turns. This means of energy sharing can inhibit quenching and may also provide a mechanism to help propagate the quench transversely through the coil once a quench is initiated.

Forced flow cooling may permit the use of hollow conductors and "dry" coil designs similar to water cooled copper coils with some attractive mechanical features. Pressurized, subcooled fluid

can be pumped through, providing excellent heat transfer and cooling capacity in the longitudinal direction. This method has some attractive features for dissipating moderate amounts of heat, or, if a quench begins, of propagating the quench. However, if substantial heating occurs causing the temperature and pressure to rise significantly, the question as to whether or not the heat source becomes the dominant "pump" and adversely effects the cooling should be evaluated.

IV-3.3 Possible Future Concepts

The possible requirements for good dynamic field properties and higher peak magnetic fields which press the limits of performance for present NbTi conductors may lead to lower temperature operation or the use of different conductors. Both possibilities have a basic impact on safety related properties as well as on overall design. These requirements have given impetus to Nb₃Sn development and progress has been very promising.

The use of high purity aluminum as a normal metal stabilizing material, giving an order of magnitude or greater increase in electrical and thermal conductivities, can also have a major impact since these enhanced properties may permit compromises on other aspects of conductor stabilization. Normal conductor

resistance may be decreased and eddy current time constants increased to decrease heating rates. Smaller cross-sectional areas of stabilizing material may be used than with copper, again reducing eddy current effects and allowing more space for high strength, higher resistance structural material. Metallurgical problems have impeded the development of high purity aluminum conductors in the past, but progress has been made with this conductor material which appears very promising. Another approach, initiated by some of the authors of this report, is to form a macroscopic bond between an "off-the-shelf" high current density Cu-stabilized superconductor and high purity aluminum. The Cu-stabilized composite is more tolerant of the point-by-point contact resistance to the shunting high purity aluminum. Because the Cu-superconductor composite conductor current density is high compared to the overall current density, the cross-sectional area of Cu which is 1 or 2 times the cross-sectional area of the superconductor adds little to the overall size. The macroscopic bond technique not only makes easier the problem of obtaining contact uniformity with the aluminum, but, if brittle superconductors such as Nb_3Sn are used, also provides low resistance shunting paths which could greatly increase the tolerance to small breaks in the superconductor. This technique should be explored to complement

the microscopic or direct metallurgical bond approaches.

A very attractive possibility is to combine the conductor, stabilizer and structural reinforcement into one composite. High strength composites (eg. tensile strengths of 10^5 psi) of aluminum with reinforcing fibers (eg., Al_2O_3 , B, C) have become available in the last few years. With relatively pure aluminum as the matrix material, one would achieve very low heat surface heat fluxes if the conductor went normal. Such a composite appears achievable, and its use would appear to greatly simplify the construction of the magnet and increase its reliability. Energy dispersion would also greatly be enhanced.

IV-3.4 Present Experience Related to Quench Avoidance

Present experience has also pointed out aspects of superconducting magnet systems which merit careful consideration. The composition of conductors to achieve the required degree of stabilization must be understood with regard to superconducting filament size, heat transfer characteristics, and the changing properties, e.g., electrical resistivity, of the normal matrix material in the composite as a function of stress and magnetic field intensity.

Problems have been experienced with superconducting magnets, some of which have arisen because of basic properties of super-

conductors. This experience bears on the safety and reliability of fusion magnets.

Existing superconducting magnets, when reviewed in the perspective of proposed superconducting fusion magnet systems, have comparatively low stored energies, low mechanical stresses, and have usually been constructed to meet the unique requirements of a laboratory or development device. The largest of these magnets with stored energies in the range of hundreds of megajoules are DC. The AC superconducting magnets which have been constructed have stored energies generally smaller than a megajoule, and many of these magnets have encountered substantial difficulties.

Proposed Tokamak fusion magnet systems with stored energies many times that of the largest existing DC superconducting magnets will be subjected to strong pulsed field effects, and therefore must be regarded, in part, as very large AC magnet systems. These magnets may also be operated in radiation intensities which have not heretofore been experienced.

Because of the increasing number of applications of superconducting magnets in many different areas, questions of magnet safety has recently grown more intense and more experimental work related to safety is being done. Questions relevant to the safety of superconducting magnets have, however, been studied

particularly in the area of high energy physics, and detailed discussions of these questions and of the existing large superconducting magnet systems are well documented.

There exists considerable information on the adverse effects of conductor movement on the performance of superconducting magnets. The mechanical reinforcement and support of conductors, coil, and magnet structure should limit conductor motion and prevent coil deformation and conductor breakage. For large magnets, the conductor, and mechanical reinforcements, and the heat transfer and cooling capabilities must be evaluated. The effect of one component on another, for example, that of the mechanical reinforcement on the conductor and heat transfer should be reviewed.

Provision must also be made in the coil and magnet structure for adequate heat transfer and cooling with account being taken of the heat capacities and thermal conductivities of the materials used. The effect of electrical insulation in the magnet on heat transfer and cooling should be anticipated.

For the large, complex and sophisticated systems being contemplated, errors that can be introduced through poor quality control or during assembly may initiate quenches. The performance of large quantities of conductor will have to be known, which may require an analysis for broken superconducting filaments, filament

size, voids in conductor matrix, insulation, etc. The conductor, coil and support structures will have to be examined for micro-cracks, inhomogeneities, dimensional tolerances, quality of welding, etc.

The concerns during assembly will be many, ranging from proper fitting of major components to careful control of electrical joints between conductors to cleanliness of magnet coil and cooling channels, where, for example, metal chips and unpolished burrs could cause shorts and voltage breakdowns or blockage of cooling passages in the magnet coils or power leads.

Concern about the adequacy of radiation shield design is relevant to superconducting fusion magnets. The potential for a magnet quench due to excessive localized heating from neutrons, the possibility that neutron radiation may cause deterioration of the superconducting material, shortening the lifetime of the magnet or degrading its performance, and the effects of such radiation on probes and sensors must be evaluated.

Criteria will have to be established for instrumentation and for magnet power supplies, covering, for example, the acceptable level of AC ripple. The cryogenic systems will also have to be carefully reviewed.

The above discussion touches only briefly on aspects of superconducting magnet systems which can be identified based on

our present experience as potential sources of magnet quenches. It should be pointed out that this experience is limited and that there may be other types of initiators which are not yet identified. An example of a possible failure mode about which little information is presently available is the degradation of magnet performance with long term operation. For pulsed operation it is very important to get fatigue life information on materials which will be utilized in a reactor.

In future fusion power reactors employing superconducting containment magnets, the great sizes and stored energies, and the structures and forces involved will be a large extrapolation from present experience. As mentioned before, the superposition of rapidly pulsed fields required in some reactors adds further constraints on design and greater complexity.

For Tokamak reactors the toroidal field (TF) coils are already more complex than existing large DC magnets. The additional requirements imposed on the coils and structural design by the AC and transverse force components may further complicate both magnet design and magnet safety. It should be pointed out that even the rapidly pulsed auxiliary coils are very large magnet systems with very large energy transfers by present standards.

IV-4 FAULT DETECTION

The purpose of fault detection studies as related to the safety aspects of future large superconducting magnets for CTR will first be considered. Conventional water-cooled or gas-cooled electromagnetic devices with large stored energy, such as large magnets or generators, have evolved to a degree where they are operated with high reliability. Nevertheless, these devices are subject to failure and must be constantly monitored and protected. Mechanical failures, for example, can lead to fracture and arcing; electrical shorts can produce excessive eddy current heating and subsequent failure; excessive heating at junctions can cause melting, cooling flow can fail, etc.

Projecting into the foreseeable future and even taking into account the likelihood of further technical refinements, there are, however, beyond such failure modes, additional modes which would appear to be associated uniquely with superconducting fusion power reactors. (a) By their very nature, superconductors can rapidly switch into a resistive state. (b) The known superconductors are superconducting only at very low temperatures. At these temperatures, other material properties are also rapidly varying functions of temperature. Heat inputs, therefore, which are very small relative to

the total stored energies involved, can lead to large property changes. (c) The essentially non-dissipative performance possible with superconductors as well as the nature of the fusion application leads to designs for devices with high magnetic fields, large forces, and/or very large stored energies. The potential for failures and their consequences is therefore increased with scale. (d) The generally greater sophistication of high technology devices adds constraints. There must be, for example, an awareness of the possibilities of fusing gas-cooled power leads, of the impact of radiation on the electrical resistivity of the normal metal matrix of composite conductors, etc. (e) The first generation of electric power apparatus brought immediate large gains. The apparatus evolved in size and performance with long empirical experience. High technology replacement must "leap-frog" to big systems which generate power economically enough to compete with existing systems. The need to be competitive together with the greater complexity of the fusion systems implies that fault detection and related safety systems must be incorporated in design considerations from the very beginning and must be carried forward with the project.

The measurement of electrical resistance as obtained from observations of current and voltage has been a powerful diagnostic tool. Cryogenic stabilization has been extensively studied and understood for composite conductors with superconducting and normal metal components surface-cooled by liquid or gaseous helium. From a knowledge of heat transfer, surface areas, and normal metal dissipation as a function of current, stability characteristics including, for example, that for recovery to the superconducting state can be predicted. Knowledge of the electrical and thermal properties of the conductor, make it possible to predict, in addition, the velocity of propagation of the normal region in those cases where a runaway situation occurs. (1.2.3.)

The heat capacities for the materials used in a superconducting magnet are generally very small, except for helium. For example, a NbTi superconducting composite conductor at 50 percent of short sample at 4.5 K and 40 kG has a reserve enthalpy of $\sim 0.3 \times 10^{-3}$ joules/gram. If the conductor is surface cooled by pool boiling liquid helium, vaporization of liquid volume of 1×10^{-3} of the conductor volume provides the same enthalpy increase that would warm the conductor to a temperature at which it would be at 100 percent of short sample. This example serves to illustrate why efficient heat transfer is very important for large magnets.

Voltage detection also can be used to study such phenomena and verify computer simulations as well as to monitor magnets.

Transverse propagation of a quench through actual magnet coils has in some cases been both measured and calculated. The velocity of transverse propagation is typically one to two orders of magnitude slower than the velocity of propagation along the conductor. Transient heat transfer to helium in confined cooling channel geometries is more difficult to evaluate analytically than for other situations where it plays a lesser role such as with epoxy impregnated coils. Both flux jumps and the large thermal transients caused by mechanical forces and motion or "training" in coils are quite rapid phenomena. Thus the thermal and electrical conductivities, the thermal and magnetic diffusivities, and the heat capacities of the coil are relevant. The normal metal can have a damping effect on the dynamic magnetic field transients. Voltage detection, again, at various locations throughout a coil provides a powerful means of studying such effects.

High current density magnets for high energy physics have attained lengths of several meters. Some of these

magnets have had difficulties when a portion of the magnets quenched and became quite hot while the remainder of the magnet remained cold. Studies of these magnets are useful when the construction of large magnets which can safely absorb energy is considered. In the next year of the Magnet Safety Studies Program, data from these magnets which is being rapidly accrued in several laboratories at present, will be given major attention. The analysis of these data will provide considerable information relevant to the effectiveness of different techniques for detection as well as magnet performance.

It seems likely that future superconducting fusion magnet systems, operating with very large stored energies, high mechanical stresses, and power plant type reliability will require extensive development of much more sophisticated protection and monitoring systems. However, even very sophisticated systems have limits, and at least some types of accidents which have occurred with existing magnets may still occur in future fusion magnets.

The area of instrumentation for large superconducting magnet systems is also extensive and complex and includes analyses of fail-safe and non fail-safe components, instrument

malfunction conditions, fail-safe logic routes, and redundancy of instrumentation and controls. The sensitivity and reliability of measuring devices, and durability of probes and instrumentation inside magnet dewars which may be subjected to turbulent conditions, must be assessed.

Quenches of superconducting magnets have been detected by monitoring the voltages across sections of the magnet coil or the entire coil^(4, 5), center-tapping the magnet to form a bridge circuit with external high impedance resistors^(6, 7), placing small secondary coils as pickup or B coils inside the magnet to sense the collapsing field due to a quench,^(8, 9) and measuring the voltage across a pair of filaments in a multifilamentary superconducting conductor to sense a normal region⁽¹⁰⁾.

There has also been a recent attempt⁽¹¹⁾ to evaluate the feasibility of using microphones in the superconducting magnet dewar to detect the noise, after filtering, due to a quench. Strain gauges have also been employed to detect "training" quenches and inelastic motion.

It appears that a large number of monitoring points will be necessary in fusion magnet systems, with information from these monitors stored, logged, and routinely processed by

computer for significance. Certain critical elements can activate a hierarchy of responses ranging from visual inspection through operator action to automatic scram.

To illustrate the scope of the problem, consider as an example the application of 1000 voltage taps as monitoring points to the 24 TF coils of the proposed UWMAK-2 Reactor design⁽¹²⁾. There are, then, approximately 40 taps per magnet coil, or about 2 taps per magnet disc. This distribution would allow the installation of voltage sensors at both ends and at the center of each disc. Note, however, that even this large total number of taps does not provide for monitoring directly across conductor joints if that were desirable. The voltage taps can be electronically monitored in a comparator matrix and analyzed for maximum detection redundancy. Again, while 1000 taps seems like a large number, for a full scale reactor with the magnet operating at 10^4 amperes, the length of the conductor between taps is about 2000 meters. The inductance per tap, L , is about 5.4 henries.

To give some perspective to the problem of detecting a normal region assume 1.0 m of conductor is in the resistive state but still at about operating temperature. Assume also an electrical resistivity 50 times less than at room temperature

at the operating magnetic field or $R \sim 10^{-6}$ ohms. With a current of 10^4 amperes, the normal region voltage is:

$$V_R = 10^{-6} \Omega \times 10^4 \text{ A} = 10 \text{ mV}$$

The problem is to identify the 10 mV in the presence of background noise in a time short enough to prevent damage to the coil. Three examples are given:

a. Current Stability

$$\text{here } L \dot{I} = 10 \text{ mV, } \dot{I} = \frac{10^{-2} \text{ V}}{5.4 \text{ henries}} \sim 2 \times 10^{-3} \text{ amp/sec.}$$

$$\text{or } \dot{I}/I = \frac{2 \times 10^{-3} \text{ amp/sec}}{10^4 \text{ amp}} = 2 \times 10^{-7} / \text{sec}$$

Although current ripple and I due to charging of the magnet, etc., can be compensated for and subtracted out to a high degree of accuracy, the presence of coil capacitance and other sources of error may well unbalance the I compensation, making it difficult to detect current variation and therefore, resistance in the coil, to the level indicated above.

It might also be noted that the heat dissipated in the 1.0 m of cold normal coil conductor is 100 watts which may be detected by other means.

b. Coil Motion

Electrical noise due to coil motion also confuses detection. Consider coil motion, i.e., inductance change, sufficient to

give a signal of 10 mV. Then for a current of 10^4 amperes,

$$I \frac{\Delta L}{\Delta t} = 10^{-2} \text{ V, and } \frac{\Delta L}{\Delta t} = 10^{-6}$$

Assume, arbitrarily, a sudden small motion in a Δt of 1 msec duration. Then $\Delta L \sim 10^{-9}$ henries. This change in inductance compares with the inductance of 5.4 henries between taps.

Thus, $\Delta L/L \sim 2 \times 10^{-10}$ for motion of all the turns (30) between taps, and $\Delta L/L \sim 6 \times 10^{-9}$ for motion of a single turn.

These numbers, even for motion of only a single turn, are very small. The effective diameter of the magnet coil, D , is: $D \sim 18\text{m} \sim 2 \times 10^7$ microns.

For a one micron increase in coil turn diameter,

$$\Delta L/L \approx 2\Delta D/D = \frac{2}{2 \times 10^7} = 1 \times 10^{-7}$$

A single turn increasing in diameter by $\Delta D = 0.06$ microns in 1 msec would, therefore, produce a 10 mV signal.

For motion of 10 msec duration, an increase in diameter of $\Delta D = 0.6$ microns would generate 10 mV.

These rough estimates are made for scaling purposes.

However, with the very large forces on the magnet conductors, such small displacements, damped by frictional forces, may occur very rapidly. A more realistic case would involve a

large number N , of coil turns taking a new set position. The voltage signal would then be generated by an N times smaller increase in the diameter of these turns.

For example, consider the case where the 30 turns between voltage taps, comprising ~ 2 km of conductor, undergo a series of small outwards movements (with any given movement involving only a short length of the 2 km long conductor) at random times during a 10 second interval. Assume that the time and spatially average outwards movement of the conductor is $\sim 20 \mu$ during this interval, and that fluctuations about the average rate of movement are relatively small. The inductive voltage caused by this motion will then be ~ 10 mV over the 10 second interval. It would appear difficult to distinguish this inductive voltage from a comparable resistive voltage. The increase in conductor strain during this motion is only $\sim 10^{-4}\%$, which is negligible compared to the existing mechanical strain.

Another type of motion that could induce spurious signals would be the progressive tilting of a disc or pancake relative to the other discs or pancakes in a TF coil. The discs or pancakes are subjected to large overturning moments due to poloidal fields from the plasma current, OH, and divertor coils, and are only restrained from tilting by

external structure. The insulating spacers between discs or pancakes could yield, causing a given disc to tilt slightly.

c. Background Normal Regions

A third source of background noise for voltage detectors is the presence of normal regions along the conductor produced by sequences of conductor motions. A sudden small local conductor movement, e.g., on the order of several 10's of microns, can cause enough heating to drive the conductor normal. The recovery time will depend on the amount of movement, cooling capability, copper to SC ratio, etc., but will probably be on the order of a second or so. It seems possible that a succession of such movements could occur during a given time interval producing a voltage signal that would be difficult to distinguish from a signal indicating a non-recovering conductor. In the former case where conductors were automatically recovered, no shutdown by the safety circuits is needed. In the latter case, a shutdown must be carried out fairly soon, (e.g., in a few tens of seconds), if conductor failure, or a dangerous temperature inhomogeneity, is to be avoided. One can readily visualize, for example, a succession of 20 or 30 movements, each involving a meter or so of conductor, occurring over a few tens of seconds in the 2 km of conductor between voltage taps.

If the pattern of the conductor movements was such that the resultant voltage signal approximated that of a steady resistive region, then it would be very difficult to distinguish the two cases.

It can be seen from this illustration that, even with many voltage taps, in such a large magnet system the background voltage "noise" due to motion will be quite large. Distributed spatial logic patterns in addition to frequency spectral analysis can be used to discriminate between signal and noise. If each coil is powered separately, the sum of all inductive voltage impulses across all taps of the coil should add up to zero for internal movement. Only detailed development will determine the limits of voltage detection and whether or not such detection can be used to monitor for subtle effects in the magnet. Voltage detection will certainly be used as a reliable indicator of major difficulties, such as arcs, and can be supplemented by other techniques, some of which are mentioned next.

If the superconducting fusion magnet systems are cooled by helium pool boiling, microphones and other pressure sensitive transducers may be able to detect local boiling due to hot spots in the magnet coils. If forced flow cooling

internal to the conductor is used, pressure and temperature changes might be detected which were sensitive to slight changes in local heat input. These methods, as with the voltage probes, will have transient background noise and heating due to minute coil movements, B eddy currents, etc. If the detection of difficulties which produce continuous heat or voltage for a time duration on the scale of one reactor cycle is evaluated as being adequately fast, spectral analysis may permit the rejection of transient noise to a high level of accuracy.

Other detection methods exist and are being developed. The superconducting probe method ⁽¹⁰⁾ observes directly the "hot spot" in the conductor by monitoring superconducting strands wound non-inductively with the conductor. This method may detect a region above the transition temperature with less electrical noise.

This discussion is only intended to indicate some of the problems in detection; much more analytical and experimental work is required. The sample calculations above are illustrative only, to set the scale. For example, the detection of a 10 mV voltage due to one meter of normal conductor is not a

comment on the probability of this event. However, the example illustrates the difficulty of observing, in the presence of background noise.

In summation, both detection and protection are very important subjects. On the one hand, one wants the necessary sensitivity to avoid serious damage and the ability to take the maximum protective action. On the other hand, one must balance and minimize the probability that these functions themselves become a source of serious difficulties.

Experimental data of direct relevance to the study of quench initiation and propagation as well as the capacity of magnets to absorb radiation heating in the superconducting state are also being obtained from studies of an operating 8° superconducting beam transport magnet system used on-line in a parallel high energy physics program. There has been demonstrated, for the type of cooling and construction used in this beam transport system, a capacity to absorb without quenching, very short thermal pulses from radiation which — deposits energy in amounts on the order of magnitude greater than the enthalpy increase required to raise the conductor above its critical temperature.

IV-5 ENERGY REMOVAL

A safety problem central to superconducting fusion magnets is that concerning the removal of the very large stored energy in the magnet system when such removal is required by impending difficulties or a malfunction which has already occurred. This problem is complicated by the fact that the pulsing rate and other fundamental parameters affecting basic magnet design are not as yet clearly defined. The study of the subject of magnet protection must therefore be pursued for magnets whose pulsing rates cover a wide range. Computations are necessary on various schemes for energy sharing and removal to prevent damage to coils.

As part of the Magnet Safety Studies Program, various potential accident detection and protection systems including methods for rapid removal of stored magnetic energy for superconducting fusion magnets are being examined using the collected experience with present systems as well as untried concepts.

A number of different systems have been devised to protect superconducting magnets in the event of a magnet quench. A magnet is said to be self-protecting if the normal region initiating the quench spreads throughout the magnet without creating excessive temperature gradients.

Magnets have been protected by connecting a shunt resistor across the coil or several shunt resistors across sections of

the coil internal to the magnet dewar. The stored energy of the magnet is then dissipated inside the magnet dewar.

Shunt resistors are also used in parallel and in series with appropriate switching external to the magnet dewar to dissipate a substantial part of the stored energy outside the magnet cryostat.

A capacitor and diode have also been used in place of an external series shunt resistor for small magnet systems.

Closely coupled secondary windings have been installed to protect superconducting magnets during a quench. The stored energy of the magnet is transferred inductively to the secondary coil, and is then dissipated internally in the secondary or discharged in resistors connected to the secondary outside the magnet cryostat.

Suggestions have also been made to transfer the electromagnetic stored energy to a DC power line, a DC motor, or a DC to AC inverter and biased transformer.

The magnitude of the problem for such large inductors can be seen again referring to magnets as large as the 24 TF coils making up the UWMAK-2 Conceptual Design. Assume that for some reason an arc has developed in one magnet coil which is independently powered at a current of 10^4 amperes. If the arc voltage is 50 volts, the time constant τ of the arc would

be initially 44,000 sec. The power consumed in the arc is at first 0.5 MW, which will melt ~ 2.5 lbs. of copper per second. This is clearly an unstable situation. Assume that each magnet coil has a resistor, $R_s = 0.22 \Omega$, in series with it and its power supply. Under normal operating conditions, R_s is shorted out. If unshorted in an emergency, the initial voltage across the resistor and also the magnet coil terminals is 2200 V. The time constant, τ_{R_s} , equals 1000 sec, which can only be shortened by increasing R_s and the initial magnet coil terminal voltage. In practice, the power dissipated in the arc would heat the magnet and be a major cryogenic disturbance, vaporizing ~ 175 liters per sec of LHe. It is instructive to consider several other time constants: (1) $\tau(\text{cold}) = 3300$ sec for the entire magnet coil normal at $\sim 10^\circ\text{K}$, (2) $\tau(80^\circ\text{K}) = 800$ sec for the entire magnet coil at 80°K , and (3) $\tau(300^\circ\text{K}) = 66$ sec for the entire magnet coil at 300°K . Note that for one turn at 300°K and the rest superconducting, $\tau(1 \text{ turn}) = 73000$ sec.

It should be strongly emphasized that preemptive action prior to an arc developing would be utilized to attempt to make such an incident nearly impossible. Nevertheless, the very large time constants involved, even with series resistive dumping are instructive. Any event which is judged to require discharge faster than on the order of 1000 sec will require additional action. For example, in an extreme case the removal or pressur-

ization of the cryogen and active triggering of heaters throughout the coil can drive the entire coil normal. The coil would have to reach almost 80°K before its resistance equalled R_s , and thus halved the time constant of discharge. Other possibilities for more elaborate internal or external energy dumps and inductively coupled circuits will no doubt be taken into consideration for rapid transfer or dissipation. This involves subtle trade-offs of advantages versus complexity which can only evolve with detailed considerations of designs.

It would be very advantageous if large fusion magnets such as the Tokamak TF coils could be actively discharged in emergencies in relatively short times of the order of 100 sec with the confidence that no ill effects would result from the act of discharge. However, even a very heavily normal metal stabilized conductor with reserve cryogenic stability and mechanical rigidity must rely on heat transfer to the cryogen for safe operation. Any accident to the coil or the cryogenic system, however improbable, which interferes with the cooling and heat transfer can create a hazardous situation. It is unlikely that one can assume that such accidents or those which result in anomalously high heat input are "impossible" even with the most conservative designs.

As an example, consider the discharge of the full-scale UWMAK-II conceptual design magnets individually into external

resistors with a 100 sec time constant. To discharge these magnets with this time constant would require a terminal voltage of 22 KV. Even the individual magnets of the smaller Argonne EPR design would require a terminal voltage of 2 KV to be discharged with a 100 sec time constant. As mentioned earlier, these voltages are disturbingly high.

The magnitudes of these time constants and voltage are incentives to consider dividing the coils further, and/or since for τ constant $V \propto I^{-1}$, powering the magnets with higher currents.

Alternatively, energy must be taken out by other means, or distributed throughout the magnet by generating uniformly, if possible, a high internal resistance.

To avoid the propagation of disturbances from magnet to magnet, the TF coils should be designed to withstand successfully electromagnetic and cryogenic coupling effects initiated by failures of adjacent magnets as well as the normal or any abnormal pulsing behavior of auxiliary transformer coils, etc. Even the normal cyclical behavior of the transformer coils may impact heavily on magnet design and thus on quench behavior and energy removal.

Energy removal from the auxiliary coil systems which may have required pulse rise rates of up to 5T per second will need careful consideration. Fast pulse times make even more rapid energy removal difficult, so coil designs which inhibit quenching

but can propagate quenches, if they are initiated, rapidly enough to be self protecting would be attractive. If the coils are not self protecting then voltage detection or other detection techniques will have to be employed to cause the quench to be distributed by active measures such as using heaters, or pressurizing the cryogen, etc. Since these pulsed coils of necessity will use higher current density conductors, this task will be easier than for the TF coils.

In addition to the possibilities of protecting coils by actively initiating and/or propagating quenches, there are other means by which the coil energy may be internally dissipated. One common method is to connect internal resistors in parallel with all or portions of the coil. This method, however, has limited application. In the BNL 8⁰ superconducting bending magnet system mentioned in the summary of operating magnets, each of the 2 m magnets, which are operated in series, has a 0.25 Ω shunt connected internally across it. The only purpose of the shunts is to protect the magnet from arcing if a gas cooled lead or the internal circuitry were to open accidentally and break the circuit. Since the operating current of the magnet system is 1000 amperes, an instantaneous voltage of 250 volts would appear across the magnet. Since 6 V is typically used for charging the magnets, the initial dissipation in each of the shunt resistors is 36 watts. This heating effect dictates against making the

shunt resistors much smaller. The time constant of each magnet with its protective shunt resistor is about 1 sec. The enthalpy of the resistor could absorb the total magnet energy. However, it is emphasized that no switching is used so that the time constant of the low resistance parallel path through the power supply with the magnet is much longer. Thus, during a quench only a small part of the energy is dissipated in the shunt resistors while the major part is dissipated in the magnets.

The scaling of such internal shunting resistors to a very large stored energy, high inductance, TF coil is formidable. If the capability of terminating each magnet section so that the full 10^4 amperes can flow through the resistors is to be kept, the resistance must be lowered to about 0.025Ω to avoid high voltages. (If the current is entirely shunted, $I^2R = 2.5 \text{ MJ}$). Because the inductance of the coil is very large, the time constant will be thousands of seconds, and the time constant for charging will be much longer. An even more serious objection is that the enormous stored energy of the magnetic field would then potentially have to be dissipated in the parallel resistors inside the dewar system. For example, assume that each of the 19 discs in the magnets of the UWMAK-2 design had separate parallel resistors. This assumption could reflect on the design in a major way and the design would have to be carefully analyzed to evaluate whether or not situations could arise which could lead to severely

unbalanced forces etc. If each disc were to be discharged solely through its own parallel resistor, the discharge time constant would be, $\tau = L/R = \frac{11.5 \text{ henries}}{0.025\Omega} = 460 \text{ sec.}$ During this discharge time, each resistor could absorb about 500 MJ. The power dissipation could be, then several megawatts per resistor.

In summary, the use of internal parallel resistors to remove energy from the magnets appears to have many problems except if the resistors could be somehow incorporated into a system for widely distributing quenching so that the entire magnet system could be safely utilized as an energy sink.

There are several possibilities involving systems using active switching arrangements. Internal switching, for example, to a superconducting persistent mode may have potential problems which must be compared with the complexity of the additional power lead penetrations required for external energy dumps. External energy removal has the advantage of taking out the large amount of stored energy from the magnet and dewar system and dissipating this energy over large resistor surfaces which could be water-cooled.

With sufficient segmentation of the magnet coils to avoid the generation of high voltages external energy removal can be done relatively quickly if there are the required number of power lead penetrations. The reliability of being able to switch many magnet segments, perhaps simultaneously, must also be evaluated.

The magnet power circuit must be interrupted somewhere with a switch to force the current through the resistors. In the series arrangement, the external resistors are in series with the magnet and power supply and are normally shorted out. Where the external resistors are connected in parallel with the magnet and power supply, the connection to the power supply is interrupted by a switch and the magnet current then discharges through the resistors. In this arrangement, little or no current flows through the external dump resistors under normal conditions. In principle, many situations can be anticipated and appropriate corrective action taken using switch gear, but obviously with some risk. In the next Fiscal Year, the Magnet Safety Studies Program will investigate in detail the performance capability and reliability of switchgear used at present in the electrical utilities.

Transformer action can also be utilized to remove energy from magnets. However, it should be pointed out that through this action the energy is transformed to circuits with long time constants. This feature should be evaluated when considering the application of this technique to remove energy from magnet systems which, even when in difficulty, have very long time constants. There is also the more subtle question as to whether or not a very closely coupled superconducting magnet, i.e., two coils essentially wound "two-in-hand", is really more reliable

than one substantial coil. High conductivity normal metal laminated for eddy currents, can provide not only dynamic stability but also may aid in current and energy sharing. The dewar structure also may be able to absorb some energy.

Finally, methods of propagating normal regions throughout a magnet in order to absorb safely the stored magnetic field energy in the internal enthalpy of the system should be considered. One technique involves the use of high purity anodized aluminum. The BNL 8⁰ superconducting beam transport magnets were constructed with aluminum sheets, corrugated to provide cooling channels and anodized for electrical insulation, interleaved with the conductor layers. A heat exchanger geometry is thus formed where the thermal conductivity and diffusivity are so high that a local hot spot heat exchanges via the helium to the aluminum and then to the helium again over the entire large aluminum surface area. In practice, very intense local heating can be sustained without quenching by this "sharing". When a conductor layer goes normal, the entire cross-section of the coil rapidly makes the transition to the normal state, thus propagating the quench and distributing the energy safely. This technique may be applicable in part to CTR magnets.

In the next Fiscal year, energy removal methods for the several proposed magnet systems will be examined in detail. It is clear that this problem is central to the successful and safe operation of CTR magnets.

IV-6 MAGNET LEADS

For the superconducting magnets being contemplated for future CTR devices, power leads carrying high currents will have to be introduced into the cryogenic region from the outside environment. The design of such power leads must reflect not only the heat load imposed on the refrigeration system during various modes of operation, but also the potential safety problems which could be caused by malfunction or failure. During the development of large superconducting magnets for various experimental programs, many studies⁽¹⁻¹⁰⁾ have been carried out to optimize power leads, especially helium gas-cooled leads for high direct currents. Most of this design and study effort has been aimed at optimizing the leads to achieve minimum heat leak at the operating current level. Less effort has been directed toward analyzing the consequences of malfunctions and failures and assuring safety and reliability.

Typically, if the cooling paths in such leads are partially plugged or if the gas flow is otherwise reduced or diverted, the leads can heat quickly and be damaged or destroyed in less than a minute. In many cases, the tolerance of the leads to perturbations of the gas cooling flow and current is very small. Lead failure has the

potential for initiating electrical breakdown and arcs inside the magnet.

The time constant for lead damage is very short, and severe overheating can occur rapidly. Even if a lead does not fail, it is sufficiently degraded to prevent further safe use. In superconducting magnet applications to the present time, leads that have malfunctioned have usually been replaced. In fusion power plant type of operation it probably will not be possible to replace damaged leads without a long shutdown and serious economic penalty. A number of lead initiated accidents and near accidents have occurred in existing superconducting magnet systems and some of these are described in Section V of this report.

Anticipating the requirement for reactor reliability, it is instructive to go through a simple exercise to show how this requirement reflects on power lead reliability. Assuming that the life-time of a fusion power reactor is 30 years, one ideally would want the leads to function without failure for that period. This condition implies that the probability of failure of one of a set of 20 leads connected to the magnet system of a power plant should

satisfy the criterion that:

$$P \leq \frac{1}{20} \times \frac{1}{30 \times 10^4} \sim 1.7 \times 10^{-7} / \text{lead-hours}$$

This requirement assumes that a lead failure only results in a class 1 accident. If it results in a class 2 accident with radioactive release, the lead will have to be orders of magnitude more reliable than the above value.

It should be pointed out that in reviewing the accumulated experience with leads to date, the probability of failure is significantly, by orders of magnitude, greater than 1.7×10^{-7} failures over lead hour of operation. However, this statement should be viewed in the perspective that the consequences of lead failures in existing magnets have less serious implications than would lead failure in a power reactor, and that lead failure rate could probably be substantially reduced with more development. If the leads could be designed with a parallel mechanical back-up system to provide a secondary current path in case of a primary lead malfunction or failure, the probability of failure of the combined lead system could be reduced even further.

From experience in present nuclear power plants, the probability of failure of conventional mechanical closure switch type devices is on the order of 10^{-3} failures/actuation. The probability of failure of a lead in the magnet system would then be, assuming a backup lead for each primary lead;

$$P \sim 10^{-5} \times 10^{-3} \sim 10^{-8}/\text{lead hours}$$

assuming that the primary lead failure rate is 10^{-5} /hour. [This would appear to be achievable with present gas cooled lead constructions].

The power lead back-up system would be designed to be open normally to reduce heat flow to the cryogenic region and would be actuated to close rapidly when a primary lead malfunctioned. Clearly, were such a system implemented, careful consideration would have to be given to the means of detecting malfunctions and failures.

An alternative to gas cooled leads with backup mechanical closure leads would be the use of simple non-cooled copper bus for the loads. The heat leak for this type of construction would be considerably greater than for gas cooled leads, but the simpler construction should have a much lower failure rate since it is not subject to

failures resulting from loss of flow, plugging, etc. It is assumed that the failure rate of a simple conduction cooled bus would be sufficiently low that a backup system would not be required.

An important consideration related to this alternative is heat load. To provide a perspective, a comparison is made between a solid metal lead and a gas-cooled lead. According to McFee's⁽¹⁾ analysis, an optimized 100 cm copper bus carrying 1000 A from room to liquid helium temperature will introduce 42 watts per lead to the helium system. Using McFee's linear heat-current relationship, a 10^5 A lead would introduce 4200 watts into the helium system, and a set of 20 leads of the order of 10^5 watts.

A 1000 A helium gas-cooled lead of the type manufactured, for example, by AMI⁽¹¹⁾ will introduce approximately 1 watt⁽²⁾ to the cold region. Again, the heat-current relationship is linear. A 10^5 A lead will therefore introduce 100 watts into the cold region, and a set of 20 leads 2×10^3 watts.

It thus appears possible to use solid non-cooled leads at the 10^5 amp level, if desired. The heat leak is large but would probably be tolerable for a power reactor.

Such a system would appear to give high lead reliability while at the same time permitting one to use a very high conductor current for rapid energy removal.

As pointed out by Henning⁽³⁾, for all types of gas-cooled leads the actual observed heat load is always higher than the suggested design value, probably, as has been demonstrated by experiment, because the heat exchange to the effluent gas is less than anticipated. Henning and others⁽⁴⁻⁶⁾ have also suggested means of further reducing the heat load to the cold region to minimize energy consumption. One should be reminded, however, that the gas-cooled lead, being more complex, is also more failure prone than the simpler solid metal bar.

At this point it is not necessary to which of the various alternative approaches is optimum for CTR magnet leads. The important conclusion is that it appears possible to obtain sufficiently high reliability for CTR magnet leads either by using very conservative design approaches, or by redundant backup systems.

IV-7 ELECTRICAL INSULATION

IV-7.1 Introduction

During operational periods with essentially zero voltage across the leads of the superconducting magnets, electrical shorts may be present inside the coils, but should constitute no safety problem, since potential differences are not present in the coil. The current prefers to follow the superconductor since it has zero resistance compared to any shorting path, even for example, if a metal particle were to bridge the insulation between turns.

If potential differences do appear in the coils, however, current then may flow in existing electrical shorts and may result in arcs between turns (or pancakes), or arcs between the conductors and the case surrounding the coils. Such arcing has been observed in a number of magnet failures. The potential differences can result from:

1. Normal charge or discharge of the magnet through leads.
2. Rapid discharge of the magnet through leads during potential accident situations.
3. Transition of some or all of the conductor to the normal state, causing IR drops.

4. Flux changes caused by current changes in adjacent coils.

5. Flux changes caused by short circuits in coil.

The last route assumes some initial arc situation in the coil that causes a relatively rapid flux change through the rest of the turns in the coil. The resultant voltage gradients may cause secondary arcs, leading to a cascading, dispersed set of arcs.

In addition to electrical shorts caused by mechanical touching of conductors, or the presence of extraneous metal particles that bridge between conductors, shorts may also be caused by breakdown of helium and/or solid dielectrics, which then result in arcs. For the rest of this section, we concentrate on the possibility of electrical breakdown through He or solid dielectrics, since more analysis is possible on this topic. It should be emphasized that the breakdown through metallic shorts may well be the more serious problem, and we do not consider it further only because it is not readily amenable to analysis. One can readily visualize plausible situations in which workmen assembling TF coils, accidentally incorporate small metallic pieces (screws, metal shavings, etc.) which may not be detectable by testing, but which could migrate at some future time and cause breakdown during a quench situation. The likelihood of such accident initiators will probably have to be

estimated from actual experience with large superconducting coils, subjected to quenches.

Figures IV.7.1 through IV.7.3 illustrate the possible electrical breakdown paths for three types of coil construction of Tokamak TF coils.

IV-7.2 Insulation for Force Cooled Conductors

Consider the force cooled conductor construction shown at the top of Figure IV.7.1. The S.S. reinforcement conduits around each conductor jacket are welded together to form a solid monolithic structure, as proposed by ORNL.⁽¹⁾ This structure will be at a uniform potential, while the conductors inside will be at varying potentials, depending on position, the distribution of internal resistance, the rate of flux change through the TF coil, and the voltage across the coil leads.

The insulation thickness is not specified in the ORNL design but would probably be on the order of 0.1 cm. The insulation material is not specified either but could be either (1) a wrapped tape, impregnated with some liquid or grease-like dielectric [either of which would freeze on cooldown], (2) some type of castable dielectric-like epoxy, or (3) a thick anodized layer on the aluminum jacket.

If there were no voids, cracks, or inclusions in the insulator, the insulation could stand off a voltage on the

order of tens of kv. However, there are a number of structural stress effects that will probably generate cracks in the insulation at a number of locations. These effects include thermal stresses caused by cooldown, mechanical stresses due to toroidal magnetic force loading on conductors, fatigue resulting from stress cycling caused by variations in force loadings when OH and equilibrium field coils are switched on or off, and thermal stresses resulting from local temperature increases during partial or complete SC to normal transitions. If the Al jacket is leaktight, He from the forced convection cooled conductor will not be able to permeate the cracks.

At the normal operating temperature of $\sim 4^{\circ}\text{K}$, a high vacuum will exist in the crack and the dielectric strength of the insulation will remain high. In the event of a transition of the SC to the normal state and subsequent heating to temperatures above $\sim 10^{\circ}\text{K}$, however, some of the H_2 that is produced by radiation effects in the organic insulators' will be released.

A conservative G value for gas formation (predominantly H_2) in organics is on the order of 0.1 molecule/100 ev absorbed. The G_{H_2} value for mylar is on the order of 3, for example (2). The peak radiation heating in the ORNL TF coil is $5 \times 10^{-5} \text{ w/cm}^3$ [$0.17 \text{ MW (th)}/\text{m}^2$ neutron loading], which corresponds to a gas generation rate of approximately 3×10^{16} molecules/day per cc of insulation. A few days operation

will generate enough H_2 gas to fill cracks with a gas density on the order of $10^{17}/\text{cm}^3$, even if only a very small fraction [e.g., 1%] is released into the crack [cracks volume will probably be small, on the order of 1%].

Figure IV.7.4 shows the breakdown strength [derived from Ref. (3)] of H_2 gas in the crack as a function of gas density in the crack, assuming an insulation thickness of 0.1 cm with the crack directed radially through the insulation. The effect of magnetic field strength on breakdown is not taken into account, but any effect will not significantly alter the conclusion that a very small release of gas is sufficient to lower the insulation breakdown strength to approximately 300 volts. This takes no account of the reduction in dielectric strength of the H_2 gas due to ionizing radiation, an effect which will be discussed later. It also does not allow for possible surface roughness or stress intensification in the gas due to the presence of the solid dielectric ($K_{\text{solid}} \sim 2-3$, $K_{H_2} \approx 1.0$), both of which can lower the dielectric strength of gases.

It should be pointed out that the breakdown strength of the H_2 filled gap will be relatively independent of gap length if the pd value is on the asymptotic part of the Paschen curve. This means that a thicker insulation will not result in a higher breakdown voltage, until much higher pressures are reached. It should be noted that during a quench, one will always pass through the

minimum on a Paschen curve with this type of construction

If the TF coil remained superconducting almost everywhere, the maximum voltage one could then safely apply across the leads to externally dump the magnets energy would be on the order of 400 volts if the SS monolithic structure was floating in potential and approximately 200 volts if the SS structure were grounded to one lead. Breakdown could then occur at localized regions in the coil where temperature was above 10°K .

If part of the conductor goes normal, the IR voltage drops can cause relatively large potentials across the electrical insulation. For example, a 1000 meter section of conductor at 80°K will have a voltage drop of ~ 250 volts. Such a temperature could be reached in a relatively short time, i.e., a few seconds after the conductor passed the point of recovery. Each TF coil in the ORNL design has 11,500 meters of conductor so there appears to be the possibility of large IR drops causing arcing during a quench.

With 400 volts maximum voltage across the leads several hundred seconds would be required to discharge the coil. The coils may be subdivided for more rapid energy removal, of course. It appears that if the postulated mechanism of gas release in the insulation is valid, several subdivisions of each coil would be required, each with their own leads, to ensure sufficiently fast removal that a significant portion of

the coils magnet energy could be dumped externally during a quench. Since TF coils for commercial reactors will store an order of magnitude more magnetic energy, the degree of subdivision of each coil would have to be correspondingly greater.

The alternate type of construction shown in Figure IV.7.3 would not be as sensitive to the gas release mechanism, since the S.S. reinforcement conduits no longer form an equipotential structure. If pancake wound, a value of 400 volts between pancakes would allow a lead voltage of ~8000 volts. The structure would not be as rigid as a welded monolith, however, and questions of mechanical stiffness as well as the integrity of the glued bond would have to be investigated.

Returning to the first type of construction for the force cooled conductor, is there an alternative kind of electrical insulation could offer a higher dielectric strength than a solid layer between the Al jacket and the S.S. conduit? One could for example, use a tape wrap of porous solid dielectric, (e.g., Nomex) around the aluminum jacket. Holes in the aluminum jacket would then permit pores and joints to be filled with helium fluid, which would prevent the presence of low pressure H_2 in cracks. However, fabrication of such a conductor might be difficult, and its mechanical rigidity would be poor.

In addition, the dielectric strength of helium gas is not very high if the conductor temperature rises during a quench. Figure IV.7.5 shows the ionization coefficient, η , for electrons in pure gas He as a function of E/P_0 . η is related to the more usual form of Townsend's first ionization coefficient, α by

$$\eta = \left(\frac{\alpha}{P_0} \right) \left(\frac{E}{P_0} \right)^{-1} = \alpha E^{-1},$$
 and is expressed in ion pairs per volt. The reduced pressure in torrs is given by

$$P_0 = P \left(\frac{273}{T} \right)$$
 where P is the actual pressure of gas and T is the temperature in $^{\circ}\text{K}$.

Figure IV.7.6 shows the estimated breakdown voltage [derived from values of α from Chanin (4) and Dutton (5)] of a He gap between the aluminum jacket and the S. S. reinforcement conduit as a function of He temperature in the tape wrap for $P(\text{He}) = 1500$ torr. It is assumed that breakdown corresponds to ηV equal to 15 [this substantially overestimates breakdown voltage, since breakdown typically takes place for $\eta V < 10$] i.e., an initial current i_0 in the gap is multiplied by $\exp(15)$. Gaps of 0.1, 0.2 and 0.4 cm are considered at a temperature of 100°K . For a gap of 0.2 cm, for example, the breakdown strength would be 5,000 volts. This estimate, of course, does not include the effects of non-uniformity of electrode surface and

local stress intensification in helium due to the presence of solid dielectric. Both effects will reduce the breakdown strength of He in the gap.

In addition, there are three important effects which can greatly reduce the He breakdown strength. The first is the Penning effect. In the case of rare gases the energy lost by electron-atom elastic collisions is very small, on the order of $(M_e)/(M_A)^{-1}$ times the energy difference between electrons and the atoms. As a result, the average energy of the electrons is much greater than the energy of the gas atoms and the predominant loss mechanism is by excitation of electronic states in the atoms. Many of these states are metastable with collisional deactivation cross sections that are orders of magnitude less than the gas-kinetic cross sections. If the rare gas contains an impurity with an ionization potential below the metastable energy level, then the metastable atom can ionize the impurity. This cross section for this ionization process is on the order of the gas-kinetic cross section. Thus in a Penning mixture, in effect almost every significant electron collision (in terms of energy loss) can be an ionization, proceeding by the route (He* is the metastable atom)

The metastable energy level in He is sufficiently high ($>20\text{eV}$) that almost any impurity can be effective in this process. Such impurities (air, argon, neon, etc.) may be present in the cold He in the condensed state, with subsequent vaporization during a quench situation as part or all of the magnet coil heats up, or can come from radiolytic effects on the insulation, i.e., H_2 . During normal operation the radiolytic H_2 would be frozen in the insulation. Release of a small fraction of this H_2 to the helium coolant during a quench would result in a He- H_2 Penning mixture. Not only do gas phase ionizations by He metastables lower breakdown voltage, but in addition, metastables can increase the second Townsend coefficient by collisions with the cathode (increasing the probability of secondary electrons emission) and this also reduces breakdown voltage.

Figure IV.7.7 shows the effect of very small concentrations of argon in a classic neon-argon Penning mixture⁽⁶⁾. The ionization coefficient, η remains at 0.01 even for very low $\bar{E}/P_0$ (i.e., $\sim 1 \text{ volt/cm torr}$), at argon concentrations of a few ppm. At lower $\bar{E}/P_0$, η decreases since the average electron energy gain between elastic collisions with neon atoms becomes comparable with the average electron energy loss per collision and the production of metastables decreases.

At very low argon concentrations (below a few ppm) the neon metastables will primarily deactivate by collisions with ground state neon atoms, which then reduces the value of η .

Figure IV.7.8 shows the variation of η vs E/P_0 for He - H₂ Penning mixtures ⁽⁷⁾. There is a very rapid drop of η for $E/P_0 < 3$ volt/cm torr. However, η is relatively high, i.e., in the range of 0.01 to 0.03, for $E/P_0 > 3$ η is a function of E/P_0 only, and no pressure effect is observed ⁽⁸⁾. The lowest concentration of H₂ investigated in Chanin's experiments was 100 ppm. Measurements of de-excitation cross sections for He metastables and the cross sections for collisional ionization of H₂ ⁽⁹⁾ indicate that η should remain about the same even for H₂ concentrations of only 10 ppm.

The effect of small admixtures of H₂ as breakdown voltage is shown in Figure IV. 7.9 for the same set of conditions [($\eta V = 15$) $P = 1500$ torr, gaps of 0.1, 0.2, and 0.4 cm] assumed in estimating the breakdown strength of pure He [Figure IV.7.7]. Note the large reductions in breakdown strength that are possible for the Penning Mixture, as compared to pure helium. The breakdown strength is reduced by the largest amount at the higher temperatures, i.e., 200 to 300 °K, and less at lower temperatures, i.e., 50 to 100 °K. Breakdown strengths are reduced to 1000 volts or less for gaps of 0.2 cm or less. Because

of the rapid variation of η with E/P_0 , the criterion of $\eta V = 15$ does not affect the results very much. The breakdown strength is essentially controlled by the condition that E/P_0 in the gap is in the range of 3.0 to 4.0 volt/cm torr, since η varies by almost an order of magnitude in this range.

The assumption that $P_{He} = 1500$ Torr may be somewhat questionable. For a TF coil with He pool cooling, the helium pressure will be near atmospheric pressure (760 Torr), both to keep conductor temperature as low as possible and to make the nucleate boiling heat flux as high as possible. If the He pressure is atmospheric, the breakdown voltages for He-H₂ Penning mixtures will be approximately one-half of the values shown in Figure IV. 7.9 for temperatures above 200°K, and considerably less than one-half of the Figure IV.7.9 values for temperatures less than 200°K.

One of the potential accident situations is dewar rupture, in which case helium pressure would equal ambient regardless of what the dewar design pressure was.

A rapid depressurization and dump of helium has been suggested as a possible response in the event of a quench, so as to spread the magnetic energy uniformly through the TF coil. From a breakdown point of view this does not appear

desirable. A rapid pressurization in the event of a quench seems much more desirable since the dielectric strength rapidly increases with increasing He density. A rapid pressurization can also serve to drive conductors normal and equalize temperatures in the TF coil.

It should be noted that the estimates of breakdown voltage in Figure IV.7.9 assume plane parallel electrodes. If there is local stress intensification (particularly at cathodes) due to electrode geometry on surface irregularities, or the presence of nearby solid dielectrics, breakdown strengths may be considerably lower in the low temperature region, i.e., 50-100°K, since η varies so rapidly with E/P_0 .

The second effect which acts to reduce breakdown voltage is the ionization of helium by nuclear radiation, i.e., neutrons and gammas. Even if the reactor is turned off when a magnet starts to quench, gammas from radioactive species in the blanket and shield will continue to produce ions pairs in the helium. A rough idea of the relative importance of this effect can be gained as follows. Consider a gap of X_0 length, a volume ionization rate of S ion pairs/cm³ sec. and a first Townsend coefficient α . The current flowing across the gap (both electrons and positive ions) in the cold helium gas is then,

$$I = \frac{2Se}{\alpha} [\exp (\alpha X_0) - 1] = \frac{2SX_0 e}{\eta V_0} [\exp (\eta V_0) - 1]$$

assuming positive ions cause no ionization (too slow) and recombination is negligible (ion and electron density too low). If we take a nuclear heating rate of 10^{-6} w/cc in helium gas [EPR type reactor, with effective He density $\sim 1\%$ of metal density], which will be an upper bound for CTRs, at 40 ev/ion pair, S then is 1.5×10^{11} ion pairs/cm³ sec. If we adopt as a criterion for breakdown a current of $100 \mu\text{A}/\text{cm}^2$, which should be an upper bound, a one centimeter gap would breakdown at a value of ηV_0 of 10 as compared to the earlier upper bound criterion of 15 for a non-irradiated gap. This assumes that η is essentially constant over the range of V being condensed. If E/P_c is in the range where η is rapidly varying, then breakdown will not be substantially affected by whatever ηV is chosen, and instead the breakdown voltage will be that which gives the appropriate value of E/P_0 . Thus it appears that irradiation of helium, although significant, will not drastically change breakdown voltage, reducing it by at most a factor of ~ 1.5 for a one centimeter gap. Shorter electrode gaps will produce a relatively smaller effect. For cases where η is rapidly varying with voltage across the gap, irradiation will essentially not affect breakdown voltage.

The third effect which will tend to lower the He breakdown voltage in CTR magnets is the effect of nuclear radiation on

electrode surfaces. A flux of high energy electrons will be ejected from metal surfaces due to Compton scattering of gamma photons and neutrons. These electrons will produce secondary ionization and excitation in the helium adjacent to metal surfaces. This effect can be important for metal surfaces acting as cathodes. In effect, nuclear radiation, besides producing ion pairs directly in the helium, will cause more pairs through secondary electrons. The high density of metals makes the rate of electron production high; on the other hand, the Compton electrons have fairly short range in metals so that it is difficult to quantitatively predict how important this effect will be in comparison to the direct volume ionization in helium gas. To be as significant as the direct ionization, on the order of 1% energy deposited in the metal conductors would have to appear as energy of electrons ejected from a cathode surface.

If we adopt a value of $\eta=0.01$ for He (which is somewhat conservative, as higher values are observed) as representative of He with impurities, then it appears that a tape wrapped He impregnated insulation should be capable of withstanding ~1KV under the conditions in a fusion magnet. This is somewhat above the capability of a solid organic insulation but is still relatively low. However, it is not certain that such an

insulation would be rigid enough to be practical, since small conductor movements could cause normalities. Also installation of such an insulation between the force cooled conductor and the stainless steel conduit might be difficult. Thus the tape wrap type of insulation seems to offer no significant advantage over the solid layer insulation.

In summary, more detailed examination of the electrical insulation capability is needed of the force cooled conductor approach, whatever method of construction is adopted. Particular attention should be paid to the effects of pulsed force loading on the integrity of the insulation and the possible production of cracks. The effects of nuclear radiation, particularly regarding H_2 evolution by radiolytic decomposition, should be examined. Coil constructions where a uniform ground plane is placed next to all portions of the conductor should be avoided if mechanisms exist that will seriously degrade the electrical insulation.

IV-7.3 Insulation for Conductors in Discs

Figure IV.7.2 illustrates the construction proposed in UWMMAK-1 and 2 designs ^(10,11), with the possible breakdown paths. Here each disc forms a ground plane. Two or more conductors that are imbedded in it may arc to this ground plane

either through cracks in the epoxy glue that holds the conductor in place, or more likely, through the helium gap between a conductor and the disc at the disc face (path 2 in Figure IV.7.2). Two arcs would be necessary to complete the circuit if the disc is floating.

Since the disc assembly will be subjected to large out of plane pulsed force loadings at the beginning and end of plasma burns, there seems to be a reasonable chance that cracks will appear in the epoxy glue at various locations. In addition, stresses and increments induced by normal scheduled magnet current cut off (e.g., during blanket replacement) of thermal cycling (e.g., during anneals of the stabilizer) or temperature gradients (e.g., during unscheduled partial or complete quenches) may induce cracking in the epoxy glue. The breakdown strength in these gaps will be controlled by the properties of the helium. If a portion of the magnet goes normal and reaches temperatures substantially above liquid helium temperature, the helium dielectric strength will be relatively low. As discussed earlier, H_2 produced by radiolytic decomposition of the epoxy may be present in the helium producing a Penning mixture subject to irradiation by activation products in the blanket and shield.

Under these conditions, a crack in the epoxy glue-insulation would reduce breakdown voltage to ~1000 volts if

the conductor undergoes a modest temperature rise, e.g., $\sim 100^{\circ}\text{K}$.

A similar modest temperature rise at the conductor face will reduce He gas density at the boundary between the conductor and the equipotential disc, again leading to a breakdown strength of ~ 1000 volts.

Normally, the disc should float in potential approximately midway between the two extremes of potential for the conductors imbedded in it. If a local brush discharge occurs from a conductor, however, the disc potential may be quite close in potential to one of the conductor potential extremes. To be conservative, one should assume the worst case, i.e., that the disc is at either one of the two extremes in conductor potential. The maximum voltage between the conductor and disc is then equal to the maximum voltage between conductors, and the breakdown voltage will be on the order of that for a single gap, i.e., ~ 1000 volts.

Thus, as in the force cooled conductor construction, voltage limitations prevent rapid removal of magnet energy, so that it has to be dumped internally. However, this produces a temperature rise and the associated resistive voltage may cause electrical breakdown and arcs.

So far we have only considered the electrical problems of a single disc. Breakdown could occur between discs through

channels or cracks in the micarta spacers. A more serious problem, however, is breakdown along the aluminum bolts that hold the assembly of 30 discs together. These pass transversely through holes in the discs and are tightened so that the discs form a rigid assembly that resists transverse loadings and slippage. These bolts are equipotential surfaces, and breakdown can occur from a conductor on the disc at one end of the assembly to the conductor on the disc at the opposite end of the assembly via the bolt. Insulating washers, either organic, i.e., epoxy-fiberglass (except that its modulus of compression may be too low for good clamping) or inorganic, i.e., ceramic (which may fracture) will have to be used at each end of the bolt. Also, insulating washers will be necessary where the bolts pass through holes in the 30 discs. Even so, probably only ~1000 volts could appear between the conductors at opposite ends of the disc assembly without breakdown occurring through helium pathways at the ends of the bolts. This greatly reduces the allowable average temperature rise in the conductor before breakdown would occur. In fact, a condition where all the conductor was normal at 4°K carrying 10^4 amp would result in a voltage of ~1000 volts between conductors at opposite ends of the disc assembly, and a temperature of 100°K would generate on the order of 2×10^4 volts. Any quench in a UWMAK-I magnet

generates enough voltage that breakdown can occur unless the quench is very well controlled.

For an ideal quench situation in which the IR voltage gradient along the conductor were everywhere the same in the TF coil, and where there was no external voltage (either imposed, or through an external resistor) across the coil leads, the presence of conducting discs and bolts would not matter, since the IR voltage would everywhere be cancelled by the LI back voltage. This ideal case would require uniform temperature everywhere in the coil at all stages of the quench, as well as a uniform cross sectional area of low resistance stabilizer in the conductor. In a real quench situation, there will probably be substantial temperature differences between various points in the coil which will result in relatively large potential differences between conductors and nearby discs (and bolts) in some regions.

In order to visualize the magnitude of this problem, consider an idealized case where N discs of the UWMAK I TF coil are at a temperature T and the rest of the coil is superconducting. Figure IV.7.10 shows the maximum voltage between conductors for $N = 1$ and $N = 3$. In the $N = 1$ case, breakdown would be from conductors through He to the disc, for the $N = 3$ case, through He to the bolts. The approximate breakdown voltage is shown as 1000 volts and is taken as independent of temperature. This seems

reasonable for temperatures above 100°K , because of the relative consistency of η with E/P_0 , but underestimates breakdown voltage for temperatures substantially below 100°K .

Relatively modest temperature rises, ~ 100 to 130°K for a single disc and $< 100^{\circ}\text{K}$ for 3 discs, are probably sufficient to cause electrical breakdown. The temperature distribution assumed for this case is relatively unlikely, of course. A more likely case is that where the TF coil has a relatively uniform temperature except in the vicinity where a quench starts. Upon detection of a local rising temperature, the Temperature Equalization System would drive the entire magnet normal, so as to minimize temperature differences.

If all the magnetic energy of the UWMAK-1 coils is dumped internally and uniformly through the coils, the coil temperature will be $\sim 150^{\circ}\text{K}$ after the conductor current has dropped to zero. Consider an idealized case where a fraction of the coil reaches T_1 , the balance of the coil is at T_0 , and conductor current is non-zero. The corresponding maximum voltage ($LI-IR$) between conductors in the TF coil is shown in Figure IV.7.11 as a function of T_1 and T_0 for cases where 10% and 30% of the coil are at T_1 . [In the actual UWMAK-1 design, the copper cross sectional area of the conductor varies greatly ($\sim 5/1$) along the conductor, so that large ($LI-IR$) voltages would be present even if the coil

were at a uniform temperature. For the calculation in Figure IV.7.11, the copper cross sectional area is assumed constant at 3 cm^2].

Relatively modest temperature differences between the coil sections [e.g., less than 40°K for the 10% case, and less than 20°K for the 30% case] produce sufficiently large voltage differences that breakdown appears possible. It thus appears that temperature differences during quenches will have to be quite small in the coil, which will require early detection of quench initiating situations and early remedial action to equalize temperatures in the coil. The low breakdown strength of helium prevents any significant amount of energy removal from the coil during the quench. It would require on the order of 1000 seconds to remove the TF coil energy with 1000 volts across an external dump resistor, while the heating during a quench could be complete in a much shorter time, on the order of 100 seconds. This situation will also apply to UWMAK-2.

If the UWMAK-1 and 2 TF coils were arranged so that each disc had its own set of leads and external resistors, the situation would be considerably improved, since the energy could be removed much more rapidly and the most voltage one would have to contend with would be the voltage across a single disc. This would require a set of ~400 leads and

resistors for the complete set of TF coils.

Another alternative would appear to be the use of much higher conductor current. With 10^5 amp conductors, energy removal times and resistive voltages could be reduced by an order of magnitude from the values for 10^4 amp conductor. Some subdivision of individual TF coils would probably be required but less than with smaller currents. As an example, consider a UWMAK type reactor with 24 TF coils, 2 subdivisions per coil, and 10^5 amp conductor. The stored magnetic energy could be removed in ~100 seconds with 500 volts across the leads of each subdivision. If the energy is not removed, an average temperature rise of $\sim 100^\circ\text{K}$ in subdivisions of a TF coil would result a total IR voltage of ~300 volts.

Most of this would be locally compensated by the LI voltage. Thus there seems to be no breakdown problem with 10^5 amp conductor, except that electrical shorts due to contact between conductors and the disc or metal particles that bridged insulating gaps might be capable of initiating arcs. Of course, questions of fabrication of such large current conductors and eddy current losses must be investigated. The heat leakage due to leads would be large (~50 kw), but still tolerable for a reactor.

In summary, the rigid metal disc approach proposed for the UWMAK-1 and UWMAK-2 magnet systems appears to be limited in

voltage capability to about 1000 volts, since the discs and metal bolts that tie the discs together provide equipotential surfaces to which breakdown may occur. This results in very long times, on the order of 1000 seconds, for discharge of the magnet energy. Effectively, all magnetic field energy must be dumped internally, since the L/R time constant of the magnet is considerable shorter than this discharge time. However, dumping of the energy internally will cause large IR voltages along the conductor, and it appears quite possible that breakdown will occur. The disc approach only seems viable if considerably larger conductor currents and more coil subdivisions are used, or if one can always ensure that temperatures are uniform throughout the coil during a quench.

IV-7.4 Insulation for Conductors in Wrap Type Coils

Figure IV.7.3 illustrates the insulation construction in a pancake wrap coil of the type proposed for the ANL TEPR⁽²⁾. This is similar to the type of construction used in most bubble chamber magnets. In contrast to the construction discussed for the force cooled conductor and conductors in discs, there are no equipotential ground planes in this wrap type coil. It should be noted that the ANL design indicates an insulating layer between the conductor and the stainless reinforcing strip. For inductive voltages this construction keeps the potential at

any given point along the conductor the same as that on the adjacent S.S. strip. However, if a portion of the conductor in a TF coil goes normal, the S.S. strip will then act as a equipotential ground plane with respect to IR voltage along the conductor, and there can be substantial potential differences between the conductor and its adjacent S.S. strip. If the insulating layer between the conductor and the S.S. strip is removed, this possibility will be eliminated. The insulating layer between turns of conductor/S.S. strip is then the only electrical barrier but should be adequate. As with the disc construction, the most likely breakdown for discharge in a single pancake appears to be through the helium gaps at the edge of the conductor to adjacent turns. The turn to turn voltage will be considerably less [in the ANL TEPR design, the turn to turn voltage is ~15 volts at a conductor temperature of 300°K] than the 500-1000 volts breakdown voltage, and it seems unlikely that breakdown could occur via this path. However, larger voltages (i.e., ~500 volts at 300°K) can be present between the conductors on the inside and the outside of a single pancake.

Non-uniform brush charges between adjacent turns could possibly result in local voltage gradients considerably in excess of the average turn to turn to voltage gradient. This effect should be investigated in more detail before ruling out breakdown between turns as a possibility.

Instead of a single pancake, now consider an assembly of pancakes forming one TF coil. The more likely breakdown route would seem to be between pancakes, where voltages on the order of 500-1000 volts may appear across the insulating spacer. Normally, the dielectric strength of the relatively thick spacer (0.4 cm in the ANL TEPR design) is far greater than the maximum electric field between turns. However, the insulating spacer may crack either as a result of thermal stress due to a non-uniform temperature distribution (perhaps from a previous quench) or from the pulsed transverse force loadings on the TF coil when the plasma burn is started or stopped. The pulsed force loadings will produce relatively large compressive loadings in the insulating spacers. The maximum compressive loads on the insulating spacers will depend on the details of the coil support structure, the transverse fields, and the TF coil current. In the ANL TEPR design, for example, compressive loadings on the order of 1000 psi seem possible at the top and

bottom of the TF coils where the transverse fields are the largest. Local compressive loads will be substantially higher than this, however, since the insulating spacers will be grooved and alignment of the conductor turns and spacers will not be perfect. Fracture of the spacers could occur as a result of the combination of high local loadings and repeated cycling ($\sim 10^5$ cycles/year).

In addition, during quenches where the non-uniform temperature distributions result from I^2R heating in the conductor, large local stresses may be produced in the insulating spacer. These will be exacerbated by misalignment of pancakes and spacers. For example, the pancakes of conductor and reinforcing strip should be free to expand in the radial direction if their temperature rises, which means that the edges of the turns of conductor/reinforcing strip must slide along the grooved insulating spacers. If this motion is hindered due to local misalignment and binding of the pancake/spacer, very high stresses may result. Design requires that the assembly of pancakes and spacers should be quite tight during normal operation to prevent conductor motion from initiating quenches, and conductor sliding already will be hindered to some degree. If local heating and expansion of the conductor then occurs due to initiation

of a quench, it seems quite possible that portions of a pancake coil can be strongly hindered from sliding with resultant high local stresses. In addition, there will be large temperature gradients through insulating spacers in a quench due to their poor thermal conductivity. These also may crack the spacer.

If the spacer cracks, its insulating strength can be greatly reduced, since breakdown can occur through a helium filled channel, and the helium density will be low if local heating occurs. Furthermore, the spacer has a relatively high dielectric constant ($K \sim 2$ to 3) and it will be in series (with respect to the electric displacement vector) with the helium dielectric ($K=1$) at the edge of the pancakes. This will produce considerable stress intensification in the helium, and the local electric field in helium conductor at the conductor edges will be 2-3 times higher than the average field calculated by $E_{AV} = \Delta V / \Delta X$, where ΔV is the voltage between pancakes and ΔX is the thickness of the insulating spaces. Thus local electric fields in helium of several thousand volts/cm appear possible with voltages between pancakes, on the order of 500-1000 volts. Streamers initiated at conductor edges may then propagate through cracks

in the spacer. For large TF coils, voltages of this order may occur during quenches. More detailed stress analysis is needed to determine what conditions can cause cracking and whether they can be avoided.

In the summary, the pancake type of TF coil construction appears to have no serious electrical insulation problems unless cracking of the insulating spacers occurs. Such cracking may occur if excessive thermal and/or mechanical stresses are generated in the spacer during pulsed force loadings and/or conductor heating. If cracks form, breakdown may take place even with relatively low voltages., i.e., ~500-1000 volts, between pancakes.

IV-7.5 Effects of Radiation on Dielectric Strength

As mentioned before, H_2 is generated from decomposition of solid dielectrics by nuclear radiation, and this will reduce the dielectric strength of helium through the formation of He- H_2 Penning mixtures if it is released into helium. Also nuclear radiation from the plasma and activated blanket/shield structure, will reduce the dielectric strength of both helium and solid dielectrics by producing a relatively large volume source of electrons and ions. A more speculative effect is the possible buildup of free radicals in solid dielectrics. When nuclear radiation interacts with molecular organic

materials, the free radicals that are produced, e.g., H atoms, recombine as fast as they are generated. However, free radicals at liquid helium temperatures recombine very slowly (in effect they are frozen). A relatively small increase in temperature will cause rapid recombination of the frozen radicals and it is conceivable that momentary large concentrations of free charge could result by chemionization processes which might significantly lower the dielectric strength of solid dielectrics. Exposure times on the order of a week could store several joules of chemical energy in the frozen radicals. Further investigation should be undertaken to see if this is a potential problem for CTR magnets. If long term exposures should store troublesome amounts of free radical energy, it should be feasible to periodically release this energy in a manner similar to the Wigner releases in graphite moderated fission reactors.

IV-7.6 Conclusions

Reasonably plausible routes for electrical breakdown are found for all the various proposed methods of TF coil construction, even during quench situations in which the energy is dumped internally and not removed from the magnet. The IR voltages in quenches are potentially large enough to cause breakdown in certain situation.

Helium seems to be the weakest link in the electrical insulation. It will experience a large density decrease as portions of the magnet heat up, and it can form Penning mixtures of relatively low dielectric strength, either with impurities (N_e , A, H_2) that may be present in liquid helium, or with gas that may be released from radiative decomposition of organic solids (e.g., H_2) used for insulation and/or glues. In addition, it has a dielectric constant essentially equal to one, and small helium gaps are subject to stress intensification if insulating solids with higher dielectric constants are present. Furthermore, nuclear radiation, either from the plasma and/or activated blanket and shield, creates a relatively large volume source of ions and electrons, which will further reduce the dielectric strength of helium. The dielectric strength of helium can be kept relatively high if it is pressurized.

If solid insulators could be relied on to not crack, the electrical insulation capability of certain types of magnet construction would be quite high. However, in the absence of extensive experience with large magnet systems subjected to relatively high cyclical mechanical and thermal stresses in solid insulators, it seems prudent to assume that such cracking may occur, which will result in a relatively low insulation capability.

With the very large coil units and relatively small conductor currents that are postulated in various CTR magnet designs, it appears that the chances of electrical breakdown during a coil quench are substantial enough to be of concern, and that further investigation of this problem is warranted. Design approaches that minimize the chances of electrical breakdown are: large conductor current, a relatively fine degree of coil subdivision, avoidance of equipotential conducting surfaces in close proximity to a large number of turns of conductor, shutdown measures that minimize temperature differences inside the TF coil, and pressurization of helium during quench situations.

It should be noted that electrical breakdown through helium dielectric has occurred in existing superconducting magnet systems at voltages well below 1000 volts, so that the estimates of voltage withstand capability developed in this section must be regarded as conservative upper limits to breakdown. A more detailed experimental study will be necessary to determine actual safe voltage limits for helium in CTR magnets.

- ① ALUMINUM JACKET TO REINFORCEMENT THROUGH INSULATOR
- ② ALUMINUM JACKET TO REINFORCEMENT THROUGH GAS FILLED CRACK IN INSULATOR.

ALTERNATE CONSTRUCTION

Figure IV.7.1. Potential electrical breakdown paths for TF coils using force cooled conductor.

Figure IV.7.2. Potential electrical breakdown paths in disc type TF coil construction.

Figure IV.7.3. Potential electrical breakdown paths for pancake type TF coil construction.

Figure IV.7.4. Spark breakdown voltage in hydrogen.

Figure IV.7.5. η vs E/P_0 for pure He.

Figure IV.7.6. Predicted breakdown voltage for pure He gas (plane parallel gaps, $\eta V = 15$) α/P_0 data from Chanin (4) & Dutton (5).

Figure IV.7.7. η vs E/P for Ne - A mixtures.

Figure IV.7.8. η vs $(E/P_0 \times \text{Torr})$ for He - H₂ mixtures.

Figure IV.7-9. Breakdown of plane parallel gaps in He - H₂ penning mixtures as a function of He temperature and gap lengths.

Figure IV.7.10. Maximum IR voltage between conductors in UWMK-1 TF coil.

Figure IV.7.11. Effect of temperature inhomogeneities in UWMAK-1 TF coil on internal voltage during quenches.

IV-8 CONSEQUENCES OF INTERNAL ARCS

As discussed earlier in this report, there are a number of initiating events that may result in internal arcs in CTR magnets. In this section we examine whether such arcs can cause the magnet to break apart. Since the magnet will be irreversibly damaged by any arc, it will have to be replaced, so we are not concerned with the extent of damage during the arcing process, other than to avoid magnet disruption.

Three questions can be asked about arc situations in large superconducting magnets.

(1). Is there enough magnetic energy in the magnet to cause magnet disruption (by heating and melting of part of the structure) if this energy is localized?

(2). If the answer to (1) is yes, will the arcing process lead to sufficient localization to cause coil disruption?

(3). If the answer to (2) is yes, are there safety measures that can be taken to prevent disruption?

Considering the first question in relation to Tokamak TF coils, the average magnetic energy per coil stored in

an assembly of N TF coils is

$$W_1 = W_m / N$$

Not all of this energy will be available for heating the region around the arc point. First, the coil current and magnetic field will decrease as magnetic energy is degraded to heat. Some energy must remain in the magnetic field, however, otherwise there will be no magnetic forces tending to disrupt the coil. Second, some of the magnetic energy may be dumped outside the TF coil system, or may be transferred to adjacent coils by induction.

Consider first the situation where an arc is detected and the response is to dump all TF coils as rapidly as possible by driving them normal. (Because of voltage breakdown limitations it is not feasible to dump a significant fraction of the coils energy externally unless much higher conductor currents or many more coil subdivisions are used). It is assumed that all coils wind down at the same rate, including the arcing one, so as to minimize out of plane loadings. A single expression then relates the amount of energy deposited in each coil to the remaining magnetic force on each winding. If a fraction, F_A , of the original magnetic energy, has been deposited as heat, the remaining

force loading is $(1-F_A) F_O$, where F_O is the original force loading.

Figure IV-8.1 shows the length L_A , of the UWMAK-I TF coil reading a temperature T_A , as a function of T_A , assuming that 50% of the Wm/N energy has been deposited in L_A . The remaining force loading is then 50% of the original. This approximates the worst condition during the accident. After full release of the stored energy the force loading is zero, while for full force loading no energy is yet deposited. Note that relatively long lengths, i.e., three meters, can reach temperatures of $\sim 700^\circ\text{C}$. This temperature is regarded as an upper limit to the safe temperature.

Now consider the other extreme where the rest of the TF coil set is maintained in the superconducting state. As the current in the arcing coil decreases, assuming no voltage across leads of the remaining coils, their current will increase so as to maintain constant flux through each coil. The TF coils immediately adjacent to the arcing coil will experience the greatest current increase. A large fraction of the energy that would be dumped in the arcing

coil if all coils were driven normal is transferred to the magnetic energy of the adjacent coils. The fraction transferred will depend on the geometry and number of the TF coils. For the UWMAK-2 TF coil set with 24 coils, the fraction will be greater than for UWMAK-1. Taking UWMAK-2 as a lower limit, the fraction dumped in the arcing coil would then be $\sim 0.3 \text{ Wm/N}$ for the condition that the magnetic force loading on the arcing coil has dropped to 50% of the normal loading (Figure IV-8.1). This can be compared to an energy deposition of 0.5 Wm/N for the case where all TF coils lose energy at the same rate.

An actual arc situation will probably be somewhere between these two extremes. On the basis of energy balance, it is evident that ~ 2 meters of coil could be heated to $\sim 700^\circ \text{C}$ while still retaining a large magnetic force loading (i.e., $\sim 50\%$ of the original loading). It should be emphasized that the actual arc situation may be considerably worse than this average approximation would indicate, since the local energy deposition may be confined to a few discs. If part of the coil disrupts (e.g., several of the UWMAK discs) the results could be almost as bad as if the whole coil disrupts. If one examines energy concentration in a smaller

part of the coil, then it becomes even easier to postulate sufficient concentration for coil disruption.

Thus the answer to the first question appears to be that there is sufficient energy to cause coil disruption, if properly distributed in the coil. The second question can now be examined by examining the course an arc might take.

The course it actually takes will strongly depend on the coil construction and the location of the initial arc. For a UWMAK-1 TF coil, the arc could occur by electrical breakdown between conductors on a single disc (with the circuit completed via the equipotential surface of the disc) or between conductors on separated discs (with the circuit completed via one of the aluminum bolts holding the disc together). [An arc would also occur by mechanical failure or melting of a thermally blocked conductor in which event only one arc point would initially be present]. In the first case, each of the two arc points will rapidly heat their surroundings. At a heat rate of several hundred kw per arc, local melting and penetration of the disc would occur in a few seconds at each arc point. The conductor winding

on the opposite side of the disc will then be part of the arc circuit. Before the disc is penetrated, the current flow would divide between the short circuit path in the disc and the SC winding between the two arc points. After penetration, current will flow in short circuit paths across the disc at the two arc points. The equivalent electrical circuit of the TF coil can then be viewed as a large inductor (the remaining 33 discs) with some internal resistance, in parallel with 3 smaller inductors on the shorted discs, each with their own internal resistance and magnetically well coupled to the larger inductor, with the various inductors connected through the low resistances of the arcs. Beyond this point, the progress of the arc becomes very difficult to predict. There will be rapid heating of the disc in the vicinity of the arcs, large convective transport of heat by hot helium (the very strong $I \times B$ force in the plasma of the arc will pump the helium) radially outwards across the faces of the disc, and rapid degradation of the insulating spacer between discs. It seems quite possible that the arc can rapidly spread to adjacent discs, and the heated discs can lose mechanical integrity and fail. Detailed accident analyses would have

to be carried out involving coupled heat transfer, structural, and electrical computer codes to be able to even approximately predict what will happen, and whether or not the TF coil would disrupt. The important point to make here is that one must regard the arc as a spreading process involving ever larger regions of the coil through additional breakdowns and convective heating, rather than unrealistically assuming that it can be analyzed by postulating that the arc will remain localized, heating its surroundings by thermal conduction. This latter type of analysis would probably yield the answer that the coil would not fail, but this might not be the correct answer.

The second type of arc formation would occur by breakdown between conductors in different discs via one of the bolts holding the disc assembly together. This would appear to be more likely than the first type involving conductors on a single disc, because of the greater voltages likely to be available. The bolt would be melted at the arc point in a second or so, and would reach the melting point along its entire length in ~20 seconds. Secondary arcs will then probably be generated to the discs between the original discs involved in the breakdown. The subsequent course of

the arc would be extremely difficult to predict.

Thus in answer to the second question, "can the arcing process lead to sufficient localization to cause coil disruption," there is not enough information at this point to attempt an answer, let alone estimate a probability for disruption.. By carrying out a number of in-depth analyses involving heat transfer, structural, and electrical aspects, however, it should be possible to obtain a rough answer to this question. Realistically, such analyses would require several man years of effort, and one would still only have rough approximation for an answer.

Assuming for the moment that the answer to the second question is "yes", can safety measures be taken that will prevent coil disruption? The answers to this question appears to be yes.

The formation of an arc inside a coil will irreversibly damage it, so that it must be replaced. One is thus not trying to save the coil for future use, but rather to prevent disruption. It should be quite easy to cause a large number of secondary arcs at scattered points around the coil that would prevent most of the coils energy being deposited in the vicinity of the original arc. Such secondary

arcs could be initiated by electrical breakdown triggered by a network of high voltage cables in the TF coil. Such cables could be easily positioned at the outside of the discs or pancakes windings, and would not interfere with the cooling channels or assembly of the coil. The detection of an arc situation appears to be unequivocal enough that one does not have to worry about ruining a coil on a false alarm signal. If this procedure were adopted, it then would not appear necessary to carry out detailed analyses of arc accident disruption consequences for each magnet design, though other consequences (e.g., He pressurization of primary containment, possible dewar rupture, etc.) would have to be examined.

Figure IV.8.1. Length of UWMAK - 1 TF coil heated to temperature T_a as a function of average temperature of arc zone.

V. COLLECTION OF SAFETY DATA ON EXISTING MAGNET SYSTEMS

V-1 SIGNIFICANCE OF PAST AND PRESENT SUPERCONDUCTING MAGNET EXPERIENCE TO FUTURE FUSION MAGNETS

Although the discovery of superconductivity occurred in the beginning of the century, practical high-field superconducting conductors and sophisticated superconducting devices were not developed until the last decade. Of the various areas ⁽¹⁾ in which superconducting magnets have been applied, it has been in the area of high energy physics that large-volume high-field magnets have seen most extensive use. Notable examples of magnets for high energy physics are the big bubble chamber magnets and the accelerator magnets in this country and around the world. A great number of high-field solenoids have also been built for various research purposes, many of which were and are commercially available.

A superconducting material has the unique property that it can undergo an abrupt phase transition from the superconducting to the normal or resistive state. For magnets wound with superconducting materials, the possibility exists that such a rapid transition may occur during which the energy stored in the magnetic field is rapidly converted to heat. The sudden conversion of energy may lead to damage or destruction of the magnet. Consideration must therefore be given to the detection of normal regions and the proper removal of energy from the magnet in

order to insure reliable and safe operation of the magnet system. The techniques for detection of normal regions and for energy removal are, in principle, the same for both large and small superconducting magnets. In practice, the protection systems differ because of the specific design of a magnet, the magnitude of the stored energy, the time constant of the magnet system (L/R), the type of electrical or mechanical noise, the amplitude of the noise, etc. A method of quench detection and energy removal applicable to one type of magnet may not be able to be applied directly to another. Nevertheless, all protection schemes are closely related because of the nature of the superconductor. This study, therefore, will provide essential background information for future superconducting fusion magnet systems and other superconducting devices. It includes:

- A) A survey of superconducting magnet safety design parameters.
- B) A survey of protection methods used on superconducting magnets.
- C) A survey of operating experiences with superconducting magnets.

It is hoped that from this study, the following may be learned:

A) The best approaches to safe designs for fusion magnets to minimize the possibility of failures.

B) Promising protection methods for fusion magnets.

C) The identification of potential difficulties and dangers from a listing of the mistakes and failures.

V-2 OBJECTIVES AND METHODS

The objectives of this part of the Magnet Safety Studies program can be stated as follows:

A) To provide safety data compiled from superconducting magnet systems.

B) To evaluate the safety data and recommend the best approaches for safe design and operation of fusion magnets.

C) To coordinate the safety study for all types of superconducting fusion magnet systems.

The methods used to achieve the objectives of this study are summarized as follows:

A) Developing contacts and communications with expert and knowledgeable people around the world in the field of applied superconductivity, especially those working on large superconducting magnet systems, and collecting data on existing systems.

B) Collecting and studying available data published on the safety aspects of the superconducting magnets.

C) Participating in experiments related to superconducting

magnet safety studies at other laboratories where such participation is feasible.

D) Participating in, or obtaining the results of plasma experiments, which use conventional magnet systems at various fusion laboratories to learn of requirements relevant to future superconducting magnet systems, provided that cooperation of this kind can be arranged.

E) Conducting small-scale magnet protection experiments in the future, based on the information and data obtained in this program. These experiments are aimed at evaluating the reliability and practicability of different protection schemes. As a result of these experiments, improvements to proposed protection systems will be suggested.

Based upon the above approach, a number of laboratories have been visited and many scientists and engineers were consulted. The following is a list of names of persons and laboratories from which information was obtained.

<u>Laboratory</u>	<u>Persons Visited or Contacted for Safety Data</u>
Argonne National Laboratory, (ANL), Illinois	G.L. Chapman, J. Purcell
Fermi National Accelerator Laboratory, (FNAL), Illinois	H. Kautzky, J. Kilmer, D.F. Sutter
General Atomic Company, (GA) California	G. Hopkins and his group

<u>Laboratory</u>	<u>Persons Visited or Contacted for Safety Data</u>
Garrett Airesearch Manu- facturing Company, (GARRETT) California	M. Calderon
General Electric Company, (GE) New York	M. Jefferies, P.A. Rios
Intermagnetic General Corp., (IGC), New York	C. Henning
Lawrence Berkeley Laboratory, (LBL), California	W. Gilbert, R. Meuser, F. Voelker
Lawrence Livermore Laboratory, (LLL), California	D. Cornish, R. Nelson, S. Sackett, C. Taylor, A. Waugh
McGill University, Canada	R. Stevenson
Magnetic Engineering Associates, (MEA), Inc., Massachusetts	R.D. Hay, P.G. Marston
Massachusetts Institute of Technology, (MIT), Massachusetts	M.O. Hoenig, B. Montgomery, J.L. Smith, Jr.
National Aeronautics and Space Administration, (NASA), Ohio	W.D. Coles, L. Nagy, J. Reinmann, J.R. Roth, W.M. Krawczonek
National Bureau of Standards, (NBS), Colorado	V. Arp, A. Clark, J. Ekin, F. Fickett, J. Hust, H. Ledbetter, N. Olien, R. Reed, R. Schramm, K. Sparks, R. Tobler
Naval Ship Research and Development Center, (NSRDC), Maryland	H.O. Stevens, M. Superczynski
Office of Naval Research, (ONR), Virginia	T. Berlingcourt
Princeton University New Jersey	J. File, J. Kaugerts
Stanford Linear Accelerator Center, (SLAC), California	S. St. Lorant

<u>Laboratory</u>	<u>Persons Visited or Contacted for Safety Data</u>
University of California at Los Angeles, (UCLA), California	T. Botts, F. Chen, T. Frederking, D. Okrent
Westinghouse, Pennsylvania	J. H. Murphy, M. Walker

The data received from the persons visited varies greatly depending upon the degree of involvement in superconductivity, and such factors as the proprietary nature of the data from private industry. It is hoped that this type of data will be more readily available in the future to make coordination of the safety study much easier and more effective.

V-3 PRINCIPLE OF PROTECTION - A LITERATURE SURVEY

Questions relevant to the safety of superconducting magnets have been studied in the past, and detailed descriptions of protection systems for large superconducting magnets are well documented. (2-6) Because of the large-scale application of superconducting magnets in many different areas, (1) the study of magnet safety has recently grown more intensive, and much work is being done related to magnet safety. (7,8,9) A significant part of this effort is in the field of high energy physics areas. The summary of different quench detection and magnet protection methods is given below with references to developers and users of particular methods.

V-3.1 Quench Detection Method

- a) Pick-up coil (or $\dot{B}$ coil) method (6,10,11) (Fig. V. 1a).

A small secondary coil is placed inside the magnet to sense the collapsing field (B) due to a quench and the signal from this secondary coil is usually amplified and bucked against a signal from the magnet coil to cancel the induced voltage (LI). Sutter⁽¹⁰⁾ of Fermi Laboratory, uses this method successfully to trigger the energy removal mechanism and protect the FNAL energy doubler prototype (Fig. V. 2). Mulhall et al.⁽¹¹⁾ used this method to perform quench studies (Fig. V. 3).

b) Bridge method^(12,13) (Fig. V. 1b).

The magnet coil is center tapped and forms a bridge circuit with external high impedance resistors. Any portion of the magnet becoming normal (provided that the normal region is not evenly distributed on both sides of the center tap) will throw the bridge out of balance and thus provide a quench signal. The 12-foot Bubble Chamber uses this method to give an alarm⁽¹²⁾ (Fig. 4). Culham's Levitron in England also uses the same method to detect a quench.⁽¹³⁾

c) Voltage taps and terminal voltage method^(14,15) (Fig. V. 1c).

Voltage drops across many sections of the coil or the total magnet coil are monitored. The sudden surge of voltage from its reference indicates a magnet quench. Usually, this type of method is used on DC magnets. Brookhaven National Laboratory's (BNL) 8° beam transport magnet⁽¹⁴⁾ (Fig. V. 5), SLAC's Large Aperture Solenoid Spectrometer (LASS)⁽¹⁵⁾ (Fig. V. 6) use this method for detecting the normal region.

d) Superconducting probe method.⁽¹⁶⁾

Conventional superconducting wire or cable is made of hundreds or thousands of twisted filaments in a copper matrix. A pair of twisted filaments in this cable is chosen to be the probe. The function is the same as liquid level probe⁽¹⁷⁾ in that current is passed through this pair of filaments and voltage across the ends will be monitored. Any normal region along the superconducting wire will immediately create heat to make that portion of the probe resistive and this resistance shows at the end of the wire as a voltage surge. This type of quench detection method is under development by IGC.⁽¹⁶⁾

e) Microphone method.⁽¹⁸⁾

This method which was recently developed, is still in the stage of testing and evaluating its feasibility in quench detection. Several microphones are installed in the magnet dewar, and the helium bubbling noise due to quench or the charging and discharging of the magnet, and the noise due to other kinds of mechanical movement including the cracking sound of epoxy and wire movement can be heard.⁽¹⁹⁾ If the noise of a quench can be positively distinguished from the other background noise, one probably could use such a device as the quench detector by filtering the background noise out electronically. One development of this technique has been carried out by Sutter of FNAL.⁽¹⁸⁾ Karlsruhe⁽²⁰⁾ is also looking into this possibility.

V-3.2 Magnet Protection Method

a) External parallel resistance shunt method ⁽²⁻⁵⁾ (Fig. V. 7a).

A shunt is connected across the magnet terminals outside of the dewar. When the magnet quenches, a signal from the quench detector will activate a switch mechanism to isolate and turn off the power supply forcing the decaying current to go through the shunt and dissipate the stored energy externally in the shunt. The percentage of stored energy removed this way depends upon the shunt resistance as compared to the resistance of the magnet coil. Obviously, a higher value of shunt resistance will remove more of the stored energy externally, but unfortunately, this will also create a higher voltage drop across the magnet coil and might cause a voltage breakdown depending upon the strength of the insulation. This method is used with many magnets to dump the energy externally either automatically or manually. Both the ANL's 12-foot Bubble Chamber magnet ⁽¹²⁾ (Fig. V. 8) and the FNAL's 15-foot Bubble Chamber magnet ⁽²¹⁾ (Fig. V. 9) adopted this method, but the energy dump is done manually. SLAC's LASS magnet ⁽²²⁾ and Rapid Cycling Bubble Chamber (RCBC) magnet have both auto and manual dump control (Fig. V. 10 & 11), while the FNAL's energy doubler magnet ⁽²³⁾ is dumped automatically (Fig. V. 12)

b) Internal parallel resistance shunt method (Fig. V. 7b).

The resistive shunt is connected across the magnet coil inside the magnet dewar and usually no switch is used. The total stored

energy will, therefore, be dissipated internally during a quench. The 8° beam transport magnet ⁽²⁴⁾ of BNL (Fig. V.13) and Bumpy Torus ⁽²⁵⁾ of NASA (Fig. V.14) employ this method to protect the magnet.

c) Internal subdivided resistance shunt method (Fig. V.7c).

To apply this method, the magnet coil is either built in sections or tapped in sections, and each section is connected to a shunt resistor where all the parallel shunt resistors are inside the magnet dewar to avoid penetration and extra heat loss problems. Again, the stored energy will be dumped internally. This method is used by many magnet designers especially the commercial companies. ⁽²⁶⁾ The Nb₃Sn insert in the NASA Superconducting Magnetic Mirror Apparatus (SUMMA) ⁽²⁷⁾ magnet (built by AVCO); the NSRDC ship propulsion system's motor and generator ^(28,29) (Fig. V.15) are examples of the protection system employing this method.

d) External series resistance shunt method (Fig. V.7d).

A shunt is connected in series with the magnet coil and the power supply but it is normally shorted by a switch. When the magnet quenches, a signal from the quench detector activates the switch mechanism to open it and at the same time turn off the power supply. The decaying current dissipates the stored energy through this external series shunt. The power supply will be by-passed through an array of free-wheeling diodes. The 7-foot

Bubble Chamber magnet of BNL adopted this method (Fig. V.16).

e) Coupled secondary winding method^(2,3,30) (Fig. V.7e).

This method uses the same principle as a transformer. The stored energy is transferred inductively to a closely coupled secondary coil during quench. The energy can be either discharged in the resistors connected with the secondary coil outside the magnet dewar, or dissipated internally in the coil winding of the secondary. But the efficiency of the method depends upon the coupling constant and the ratio of the time constant of both coils.

f) Capacitance method⁽³⁾ (Fig. V.7f).

This method used a capacitor and a diode in place of the series shunt resistor as described in method (d). In the normal operating condition the capacitor and the diode are shorted by a switch. The switch opens during a quench, and the stored energy is used to charge up the capacitor. The diode prevents the capacitor discharging back through the magnet coil. This method, however, is not suitable for large magnets since the required capacity of the capacitor bank must be very large.

g) Self-protecting method.⁽³¹⁾

A magnet is self-protecting if the windings are made with superconducting wire with a copper to superconductor ratio such that the normal region will spread fast enough to warm up the entire magnet uniformly, i.e., the stored energy is dissipated in the winding. A magnet which is using the self-protecting method may be epoxy impregnated. Several commercial companies favor this method.^(26,32)

h) Other methods.

A suggestion ⁽⁵⁾ has been made to remove the magnet stored energy to a DC power line, A DC motor, or a DC to AC inverter and a biased transformer, but no detailed information or data has yet been developed on these methods of energy removal.

V-4 DATA FOR EXISTING SUPERCONDUCTING MAGNET SYSTEMS

V-4.1 Magnet Design Parameters, Quench Detection and Magnet Protection Method

The information described in this section was obtained either through discussion or through correspondence. The techniques used by each individual may be changed or improved in the future and therefore the data presented here will be updated from time to time. The superconducting magnets under this survey are roughly classified according to their stored energy.

Large Magnets - above 10 Mj

Intermediate Magnets - 100 kj up to 10 Mj

Small Magnets - less than 100 kj

Most of these magnets are laboratory devices used for research and some of them were built by commercial companies.

Large Magnets

The magnets fitting into this category include many bubble chamber magnets around the world. At the present time six magnets in this country have been studied.

a) 7-foot Bubble Chamber magnet, (BNL).

This magnet is a 30 kG solenoid containing about 61 Mj of stored energy.

b) 12-foot Bubble Chamber Magnet, (ANL). This magnet is an 18 kG solenoid containing about 65 Mj of stored energy.

c) 15-foot Bubble Chamber Magnet, (FNAL). This magnet is a 30 kG solenoid containing about 396 Mj of stored energy.

d) The LASS Magnet, (SLAC). This 25 kG system consists of four solenoids (OD=2.93M) connected in series with about 35 Mj of stored energy.

e) The SUMMA Magnet, (NASA). This 88 kG system consists of four magnets (Solenoid OD. = 0.9M) containing about 18 Mj of stored energy.

f) The Baseball Magnet, ⁽³³⁾ (LLL). This system is a 20 kG baseball seam shaped magnet containing (mean dia. = 1.2M) about 12 Mj of stored energy.

Table V.1 lists some of the basic magnet parameters and the major feature of each protection system.

Intermediate Magnets

The magnets in this category include many types of magnets for different applications.

a) 8⁰ Beam Transport Magnet System, (BNL). This 40 kG superconducting bending magnet system for high energy particle beams consists of two 1.8 meter long dipole coils connected in series. The stored energy is about 0.3 Mj.

b) 10-foot Energy Doubler ⁽³⁴⁾ (FNAL). This magnet is a prototype of a 40 kG cosine theta dipole magnet designed for high energy particle accelerators. The stored energy is about 0.1 - 0.15 MJ.

c) Motor and Generator ⁽³⁵⁾ (Garrett). Both the motor and generator are 50 kG (in the superconducting coil) solenoids. (OD = 0.4M) designed for ship propulsion systems. The stored energy is 0.33 to 0.5 MJ.

d) Generator ⁽³⁶⁾ (GE). This magnet is a 0.56 M long, 72 kG test racetrack coil designed for electric power generation. The stored energy is about 0.17 MJ.

e) High-field solenoid, McGill University, Canada. This system consists of a 75 kG, 41 cm bore solenoid and a 19 cm bore Nb₃Sn coil insert to create a field of 150 kG. The total stored energy is about 4 MJ.

f) Bumpy Torus (NASA). This 30 kG system consists of 12 solenoids (OD = 0.31 M) connected in series, designed for fusion research. The stored energy is about 0.6 MJ.

g) Motor and Generator (NSRDC). Both magnets are high-field (~60 kG) solenoids (OD = 0.2 M), designed for ship propulsion systems. The stored energy of each system is about 0.12 MJ.

h) The RCBC Magnet ⁽³⁷⁾ (SLAC). This 20 kG bubble chamber magnet (OD = 0.88 M) has about 1.2 MJ of stored energy.

i) Beam Transport Magnet System ⁽³⁸⁾ (LBL). The system

consists of one 42 kG, one meter long cosine theta type dipole magnet containing 0.7 Mj of stored energy, and two 6 kG/in., 0.73 meter long quadrupole magnets, each containing 0.15 Mj of stored energy.

j) High-field Hybrid Magnet ⁽⁴¹⁾ (MIT). The 225 kG system consists of a 60 kG superconducting coil (OD = 0.67 M) and a water-cooled coil insert. The superconducting coil contains about 2 Mj of stored energy.

k) 60-inch Levitated Ring ⁽⁴²⁾ (Princeton University). This ring generates 28 kG at the conductor and has about 0.3 Mj of stored energy. This ring is designed for plasma research.

l) IGC Test Solenoid (Princeton University). This 55 kG magnet has a 16.5 cm bore with 0.1 Mj of stored energy. It is used as conductor testing facility.

The magnet parameters and protection methods of each magnet system are listed in Table V.2.

Small Magnets

Most of these magnets are models, prototypes or test solenoids. The magnets reviewed are listed below:

a) Alternator ⁽⁴³⁾ (MIT). This magnet is a 25 kG (inside winding) one meter long saddle coil pair designed to demonstrate the practicability of large superconducting AC machines. The stored energy is 90 kj.

b) AC3 ⁽⁴⁴⁾ (Rutherford Laboratory, U.K.). This 40 kG prototype synchrotron magnet is designed for high energy

particle accelerators. The stored energy is 60 kj.

- c) Small cosine theta type dipole model (LBL)
- d) Small solenoid and racetrack (LBL).
- e) Cosine theta energy doubler model (FNAL).
- f) Solenoid research magnet(NASA).

The major magnet parameters and their respective protection systems of the first two magnets are listed in Table V.3. The magnets listed from c to f will not be tabulated since a dozen of small magnets are included in each category, and these magnets are usually designed to test the conductors, new design ideas, or as small solenoids for research. Normally a shunt resistor across the magnet is provided for protection whenever a protection system is incorporated with the magnet. Because of the small stored energy, most of these magnets do not have any quench detection devices.

V-4.2 Magnet System Operating Experience

In this section, superconducting magnet failures are discussed and the major cause of each failure presented based on information from the appropriate laboratory. The remedial action taken to avoid failure in each case is also tabulated. It is believed that a compilation of magnet failures and the corrective actions are very valuable for the future superconducting devices, especially the fusion magnet system, in providing a broad background of experience, designs and direct special attention to

critical system components in order to minimize magnet system accidents.

Most reported accidents involve a burned magnet coil since the magnet dewars are usually designed adequately to cope with the rapid increasing vapor pressure arising from evaporation of liquid helium. However, there are a few cases where helium dewars have been damaged. These will be described below.

a) A quench initiated by a local hot spot due to wire movement, ⁽⁴⁵⁾ vapor locks, etc. Inadequate heat transfer in the magnet is another cause of the formation of a localized hot spot. The hot spots may cause deterioration of the electrical insulation and create turn to turn or layer to layer shorts ⁽⁴⁶⁾ which produce arcing. Accidents of this type occurred in some prototype magnets of FNAL and LBL, (Fig. V.46 & V. 47). Fig. 36 shows melted indium and burned insulation on FNAL's 2.5 foot model energy doubler due to a local hot spot. Fortunately, this magnet is intact.

b) A quench initiated by a local hot spot due to improper lead wire installation and support. This type of accident may cause melting of the wire and produce an open circuit which in turn can result in severe arcing. An example is an alternator of MIT ⁽⁴⁷⁾ (Fig. V. 37).

c) A quench initiated by a local hot spot due to cooling channel blockage. ⁽⁴⁸⁾ In this case also, illustrated by SIAC's

RCBC magnet (Fig. V.38), the conductor was melted which produced an open circuit and arcing.

d) Excessive heat can be generated along the power lead when the lead design and instrumentation are inadequate, giving rise to possible lead or magnet damage. Four examples are given below.

i) In the case of the BNL 7-foot Bubble Chamber ⁽⁴⁹⁾ instrumentation did not detect a temperature rise due to insufficient liquid hydrogen cooling. The lead was overheated, and solder at a joint melted (Fig. V.39), causing hydrogen leaks into the vacuum space. The magnet is intact and did not quench.

ii) In the case of the NASA SUMMA magnet, ⁽⁵⁰⁾ 32 power leads are continuously monitored. The overheated lead was not detected in time because of the slow instrumentation scanning rate. The lead was damaged (Fig. V.40), the magnet quenched and the coil section connected to this lead was damaged. The details of the damage is not known, except that this section of the magnet cannot be charged to full current.

iii) In the case of the BNL 8⁰ transport magnet, the overheated lead melted the conductor wire (Fig. V.41) and the magnet arced to ground (Fig. V.42). The lead was also damaged.

iv) In the case of AC3 cosine theta type magnet of Rutherford Laboratory, ⁽⁵¹⁾ the current lead broke off at full current causing arcing under the liquid helium and vaporization of a few cc of copper (Fig. V.43). The magnet is intact.

e) Damage due to inadequate support structure. There are two examples:

In the case of NASA SUMMA magnet built by AVCO, (52) end-turn packing strips and turn to turn insulation were blown out of the NbTi magnet section causing arcing at many places. (53) In the Nb₃Sn magnet section, Nb₃Sn ribbon conductors slipped under the pancake separator strips. (53) Many shorts were created in the magnet which required repair.

The high field solenoid at McGill University was deformed by diamagnetic forces (54) which exceeded the mechanical strength of the magnet confinement structure (Fig. V.44 and V.45).

f) Magnet performance degradation due to conductor movement. The performance of the levitated Nb₃Sn ring at Princeton University was degraded because of the conductor movement (Fig. V.48 & V.49). Additional conductor confinement and mechanical support were required.

g) Dewar damage.

The lead on MIT's alternator magnet (47) melted creating an open circuit and causing arcing at many places. Portions of the field winding and the outer stainless steel cover were damaged. (55) The proximity of the dewar wall to the magnet coil winding, coupled with the decreasing dielectric strength of helium as the temperature increases, contributed to the involvement of the dewar in this accident. Another magnet

dewar was damaged at NASA Lewis Research Center because of induced eddy currents. A copper getterpan was soldered to the bottom of a stainless steel dewar in the vacuum space to hold charcoal powder. The force between the getterpan and the magnet deformed the dewar. (53)

The information on magnet system accidents is tabulated in Table 4.

V-5 DISCUSSION AND CONCLUSION

The material covered in this report includes many magnet systems built and operated in this country and the AC3 magnet of Rutherford Laboratory in England. There are still many magnet systems which have not yet been reviewed and the magnet systems which are still in the design stage or in construction. At present, detailed information on many magnet systems abroad in European countries, Russia and Japan, has not yet been obtained. In fact, full contact and coordination have not been established with groups in these countries. It is hoped that data on these additional systems can be made available in the future reports of this program. It is also possible that some of the data in this report may be out of date or incorrect. Errors will be corrected and information updated in future reports so that current status of the various magnet systems will be presented. The discussion presented in this report describes magnet difficulties already experienced, and failures which may be predicted

from common experience. Areas in which there is a lack of safety information are of major concern. The following are the types of errors which could lead to possible magnet failure and damage during both normal operation or emergency conditions.

V-5.1 Design Errors

a) Inadequate conductor design.

A conductor design may be inadequate in relation to its current carrying capacity in high magnetic field, thermal and magnetic stabilization, heat transfer properties, mechanical properties, electrical insulation, etc. Known changes in the resistivity of copper occur due to high stress (>17000 psi) and magnetic field intensities (magnetoresistivity). High mechanical stress arising either thermally or magnetically can also induce changes in superconducting properties such as critical temperature. A magnet, designed with inadequate consideration being given to the conductor design, may not be able to achieve its design specifications and failure may result.

b) Inadequate mechanical reinforcement.

Mechanical reinforcement includes conductor reinforcements, coil and magnet structure reinforcement, etc. Inadequate design could lead to conductor movement, conductor breakage, coil deformation or even magnet destruction.

c) Inadequate heat transfer design.

Heat transfer design includes considerations of total

exposed cooling areas, choice of materials of certain thermal conductivity, heat transfer rate, etc. Improper design could lead to local hot regions which could, in turn, result in excessive thermal stresses and voltage breakdowns.

d) Inadequate cooling design.

The method of cooling, cooling channel design, capacity of refrigeration etc. are included in this category. Improper design could lead to vapor locks and localized hot spots which could result in the type of difficulties described in (c).

e) Inadequate electrical insulation design.

Electrical insulation design refers to insulations applied to the conductor such as formvar, epoxy, fiberglass, mylar, oxidized layers, etc. the spacer and space between coil layers, between coils, and between coil and its leads to ground. Improper design could lead to voltage breakdown or even severe arcing during voltage excursions and could cause serious coil or dewar damage. It is known that the dielectric strength of helium deteriorates with increasing temperature. Arcing due to a voltage breakdown through hot helium gas is not uncommon. The most important concern is that whenever an arc is established, it is very difficult to stop until the stored energy is dissipated.

f) Inadequate power lead and other penetration design.

This design category includes penetrations for power leads, instrumentation, energy removal leads, refrigeration, and other

items which need to be introduced into the low temperature region from room temperature. The joint and support between power leads and the magnet, and between the leads and the external cable warrant careful consideration also. Improper design could lead to excessive heat loss and temperature excursions along the power lead. During emergency conditions, inadequate design of the power lead could result in burn out and arcing which again could cause severe damage.

g) Inadequate control and safety monitoring instrumentation

The area of instrumentation is extensive and complex and includes non-fail-safe components, instrument malfunction conditions, fail-safe logic route design, redundancies, sensitivity and reliability of the measuring devices, durability of the probes and instrumentation components inside the magnet dewar during turbulent conditions created by quenches or other causes of violent nature. Inadequate design could lead to unrecorded or unnoticed difficulties which could cause magnet failure.

h) Inadequate radiation shield design.

Reference here is to the neutron radiation shield. Improper design could lead to unexpected magnet quench due to excessive localized heating by neutron beams, and deterioration of superconductor which either shortens the lifetime of the coil or causes degraded magnet performance. Radiation can also cause probe or sensor malfunction or variation in performance.

V-5.2 Assembly Errors

Even if the magnet system is designed with every possible safety consideration, errors can still be introduced during assembly. These would be caused by human mistake, negligence, or inadequate quality control of magnet components which will be mentioned in the next section. An improperly assembled magnet system could either force operation of the system below its specifications or cause magnet failure. Care during assembly must be given to proper mechanical support assembly, electrical joints between conductors, pancakes, coils, power leads and external cables; installation of insulating spacers; cleanliness of coil and cooling channels, etc. For example, metal chips and unpolished burrs or protruding metal objects could cause voltage breakdowns and shorts; foreign objects could cause cooling channel blockage or plug the power lead cooling path. Improper vacuum seals, welding and soldering could cause leaks which could lead to undesirable shutdowns or quenches. Improper installation of sensors and probes could create instrumentation difficulties and failures during turbulent conditions caused by quench or other sources. The sources of assembly errors are very numerous, and it may, therefore, be desirable to create a well prepared checklist long before assembly.

V-5.3 Quality Control Errors

a) Conductor - The quality control should include an

evaluation for undetected broken superconducting strands, voids in conductor matrix, current carrying capacity, thermal properties, electrical insulation, size of the filaments and conductor, etc.

b) Mechanical support structure - Internal and external microcracks, inhomogeneities, quality of welding, physical dimensions, etc. should be evaluated.

c) Instrumentation - The sensitivity, reliability and durability of the components, probability of malfunction and failure should be evaluated.

d) Electrical system - The review should include an evaluation of the AC ripple and the reliability and durability of the magnet powering system. Unreliable power supplies and high amplitude, high frequency components could trigger a quench.

e) Cryogenic system - Included in this area would be an examination for heat leaks, air leaks, improper seals and welding which could lead to undesirable shutdowns or quenches.

V-5.4 Human Operating Errors

Improperly handled controls and monitoring devices, erroneous readings and wrong decisions, carelessness and negligence, etc. are included.

V-5.5 Instrument Operating Errors

Included in this category are accidents due to instrumentation malfunctions. To minimize these incidents the system

should be designed with sufficient logic routes and redundancies to cover possible instrumentation errors. Careful quality control should reduce the probability of component failures.

V-5.6 Natural Events

These include earthquake, lightning, hurricane and tornados, sand, snow and rain storms and flooding, airplane crashes, explosions due to external causes not related to human controlled sources, etc.

V-5.7 Sabotage

Included in this category would be any intentionally created damage of human origin.

V-5.8 Others

Any accident or damage not accounted for in the above areas.

In the fourteen cases of magnet failure analysis presented in Table 4, ten cases can be classified as due to design error, two cases due to instrumentation error and two cases due to assembly error. Actually, the failures may involve several types of errors, but they are classified according to the most immediate cause of the failure. These preliminary statistics show that most magnet system failures studied so far are due to design, instrument and assembly errors. Failures caused by human and quality control errors do not contribute to any appreciable amount at this time. No magnet failures thus far can be attributed to natural events and sabotage. There are apparently no fundamental

difficulties in the various magnet systems. However, the following two points must be emphasized.

a) The magnets studied are mostly used as research devices. The sizes and the number of the magnets are small, and most magnets are not operated on a day to day, year-round basis.

b) These identifiable sources of magnet system failures have been deduced from actual operating experiences or are predictable failures which could happen to superconducting magnets for power plants.

There may be other types of failures not yet identified. In particular, there may be failures associated with long term operation. For example, performance of the magnet may degrade with time.

At the present time, the magnet which has been operated for the longest period of time, 15,000 hours, is the ANL 12-foot Bubble Chamber magnet. Safety information concerning the continuous operation of a superconducting device for 25 to 30 years is not available. Yet, this type of information is vitally important for any pioneer engineering installation to demonstrate commercial feasibility. It is probably desirable to test run a superconducting magnet to accumulate long term operating experience under conditions similar to those for fusion power plants. In conclusion, the following statement can be made concerning future fusion magnet systems.

a) The initial difficulties and failures which happened to most superconducting devices can be identified and used to prevent the occurrence of similar mistakes in future fusion magnet systems.

b) At present, evidence of long term reliability and data concerning long term operating difficulties for any superconducting device is not yet available.

c) The operating experience at most laboratories indicates that each superconducting magnet system usually has its own characteristic problems. This implies that successful operation of one EPR device should not be considered as a guarantee of safety for other devices of different design, or emphasized as a reason for standardizing designs for future power reactors.

d) Comparatively speaking, the safety study is a rather small effort in the entire fusion reactor development project. However, every safety measure should be considered in each stage of development and construction to safeguard the huge investment and the interest in new energy sources.

Acknowledgements

We wish to thank those people who have supplied us with their magnet data and valuable advice, and made this portion of the magnet safety report possible. Most people's names and the institutions cooperating with us in this study are listed on page 346-348.

TABLE 1 - SOME PARAMETERS

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>
Magnet Classification and Total Hours of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (Amps)	Design Heat Transfer Rate (W/cm^2)	Cooling Method	Type of Electrical Insulation
<u>L1</u> 7 ft. Bubble Chamber (BNL) T $\approx$ 2000 hrs.	16 circular double pan-cakes with ID=2.40M OD=2.75M	Mechanical support with distributed stainless-steel reinforcement. (Fig. 17)	Untwisted NbTi filaments in OFHC copper matrix. (Fig. 18)	5500 DC _{max.} 5000 DC _{oper.}	0.4	Pool cooling by liquid helium.	Turn-to-turn: 0.127 mm thick Mylar bonded to S.S. strip (Fig. 17) Between pan-cakes: 3.2 mm thick phenolic spacer Between double pancakes: 15.9 mm thick phenolic spacer (Fig. 19)
<u>L2</u> 12 ft. Bubble Chamber (ANL) T $\approx$ 15000 hrs.	30 circular pancakes with ID=4.81M OD=5.28M	Mechanical support. (Fig. 20,21)	Untwisted NbTi filaments in OFHC copper matrix. (Fig. 22)	1800 DC _{max.} 1800 DC _{oper.}	0.1	Pool cooling by liquid helium.	Turn-to-turn: 0.25 mm thick glass fiber-filled Teflon Pancake-to-pancake: Al spacer coated by 0.38 mm thick epoxy and 0.127 mm thick polyimide film.
<u>L3</u> 15 ft. Bubble Chamber (FNAL) T $\approx$ 5000 hrs.	40 circular pancakes with ID=4.27M OD=5.08M	Mechanical support with distributed stainless steel reinforcement. (Fig. 23)	Twisted NbTi filaments in OFHC copper matrix. (Fig. 23,24)	5000 DC _{max.} 5000 DC _{oper.}	0.18	Pool cooling by liquid helium.	Turn-to-turn: 0.254 mm thick Mylar. Pancake-to-pancake: 6.35 mm thick Micarta spacer.

*Figure n & Table n = Figure V.n & Table V.n referred in text

OF LARGE MAGNETS*

<u>IX</u>	<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u>	<u>XIV</u>	<u>XV</u>
Maximum Stored Energy & Inductance	Quench Detection System	Magnet Protection Mechanism	Other Protection and Major Instrumentation	Maximum Voltage Across Magnet During Most Rapid Discharge Mode	Breakdown Voltage 1) Turn-to-turn 2) Pancake-to- Pancake 3) Magnet-to-Ground	Quench Status & Quench Time Const. τ
61×10^6 j 4h	Bridge method (it has been removed and is not in use).	External series shunt resistor method (Fig. 16) manual dump. Auto dump triggered by power lead voltage, temp. and liquid hydrogen level (for cooling the lead only) normal energy removal time const. = 6.6 min.	1) Power lead resist- ance, temperature alarm and interlock. 2) Power lead liquid hydrogen level alarm and interlock. (Both 1 and 2 will trigger the energy dump switch automatically). 3) Helium dewar liq- uid level alarm 4) Helium dewar pressure alarm. 5) Three 6" rupture disks for dumping the helium into air.	55V	Not known	Never quenched. τ not known
65×10^6 j 40h	Bridge method (Fig. 1b and 4) will give alarm but is not used for triggering the energy dumping mechanism	External parallel shunt resistor (Fig. 8) manual dump only. Normal energy removal time const. = 6.6 min.	1) Helium dewar liq- uid level alarm. 2) Helium dewar pressure alarm. 3) Voltage taps across pancakes (not used). 4) Two rupture disks for dumping the he- lium into storage tank.	180V	Not known but 500V leakage measure- ment was performed between the stacked coils and every spacer bar.	Top two pan- cakes quenched twice. τ not known
396×10^6 j 31.7h	none at present	External parallel shunt resistor (Fig. 9) manual dump only. Normal energy removal time const. $\approx$ 50 min.	1) Helium dewar liq- uid level alarm 2) Helium dewar pressure alarm. 3) Power lead cooling gas flow alarm and voltage alarm. 4) Voltage taps across pancakes available but not used. 5) Rupture disk for dumping helium into air.	180V	Not known	Never quenched. τ not known

TABLE 1 - SOME PARAMETERS

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>	<u>IX</u>
Magnet Classification and Total Hours of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (Amps)	Design Heat Transfer Rate $\frac{W}{cm^2}$	Cooling Method	Type of Electrical Insulation	Maximum Stored Energy & Inductance
<u>L4</u> LASS (SLAC) T $\approx$ 20 hrs.	4 solenoids in series ID $\approx$ 2.03M OD $\approx$ 2.92M L $\approx$ 0.51M(3x) $\approx$ 1.03M(1x)	Mechanical support with distributed stainless steel reinforcement. (Fig. 25)	Twisted NbTi filaments in copper matrix. (Fig. 26)	1780 DC max. 1650 DC oper.	0.485	Pool cooling by liquid helium.	Turn-to-turn: .254 mm thick Mylar bonded to both sides of stainless steel reinforcement. Layer-to-layer: $\approx$ 2 mm thick phenolic spacer strips. Coil-to-ground: $\approx$ 0.51 mm thick Dacron-Mylar-Dacron cloth overlaid with 3.18 mm thick G10 strips.	34.8×10^6 J 22 h
<u>L5</u> SUMMA (NASA) T $\approx$ 1000 hrs.	4 solenoids 0.31M long ea. consists of 3 sections. <u>Inner Coil</u> 20 circular pancakes ID = .51M OD = .56M <u>Middle Coil</u> ID = .57M OD = .78M <u>Outer Coil</u> ID = .78M OD = .90M (Fig. 27)	<u>Inner Coil</u> Mechanical support with distributed stainless steel substrate reinforcement. <u>Middle and Outer Coil</u> Mechanical support.	<u>Inner Coil</u> Nb ₃ Sn ribbon (Fig. 28) <u>Middle and Outer Coil</u> 2.18 mm sq. conductor (Fig. 28) with $\approx$ 14 untwisted NbTi filaments Cu: SC = 3.07:1 Note: the fourth magnet has twisted filament.	Max. Current: <u>Inner Coil</u> 300DC <u>Middle Coil</u> 300DC <u>Outer Coil</u> 425DC Oper. Curr. (40) <u>Inner Coil</u> 100DC <u>Outer Coil</u> 2 inbore coil 400DC 2 outbore coil 100DC <u>Middle Coil</u> 300DC	<u>Inner Coil</u> .288 <u>Middle Coil</u> .262 <u>Outer Coil</u> .382	Pool cooling by liquid helium.	<u>Inner Coil</u> Turn-to-turn: Intermittent Mylar tapes (Fig. 29). Pancake-to-Pancake: G10 spacer (Fig. 29). <u>Middle and Outer Coil</u> Turn-to-turn: Intermittent Mylar tapes. Layer-to-layer: G10 strips (Fig. 29).	<u>Inner Coil</u> 3.04×10^5 J 1.1 h <u>Middle Coil</u> 8.3×10^5 J 8.98h <u>Outer Coil</u> 1.44×10^6 J 12.7h Note: stored energy of the system = 18×10^6 J because of the mutual inductance.
<u>L6</u> Baseball Magnet (ILL) T $\approx$ 9000 hrs.	Baseball seam shaped coil with mean diameter of 1.2 meter (Fig. 55)	Mechanical support	24 untwisted NbTi filament each 0.64 mm in diameter in copper matrix with a square cross-section of (6.35 mm) ² Cu:SC = 4.8:1	1950 DC max. 1600 DC oper.	0.4	Pool cooling by liquid helium.	Turn-to-turn: G10 dot (0.81 mm thick, 4.76 mm in diameter) epoxied onto the conductor layer-to-layer: 1.6 mm thick perforated G10 sheet (Fig. 56)	11.4×10^6 J 6h

LARGE MAGNETS (CONT'D)*

<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u>	<u>XIV</u>	<u>XV</u>
<u>Quench Detection System</u>	<u>Magnet Protection Mechanism</u>	<u>Other Protection and Major Instrumentation</u>	<u>Maximum Voltage Across Magnet During Most Rapid Discharge Mode</u>	<u>Breakdown Voltage-</u> 1) Turn-to-turn 2) Pancake-to-Pancake 3) Magnet-to-Ground	<u>Quench Status & Quench Time Const. τ</u>
Terminal voltage method, signal is used to trigger energy removal mechanism. (Fig. 6).	External parallel shunt resistor (Fig. 10) normal energy removal time const. ≈ 17 min. (CB2 closed). Fast energy removal time const. = 220 sec. (CB2 open). Energy removal is done automatically but manual control is provided.	<u>3 Stage Alarm (39)</u> 1) Liquid He level. 2) Major vac. failure. 3) Normal vac. failure. <u>2 Stage Alarm (39)</u> 1) Dewar pressure. 2) Liquid N ₂ shield. 3) Power lead. The above instrumentation will automatically dump the magnet at the last stage alarm but manual control is also provided.	320V	(1), (2) not measured but estimated at ~ 30 V. (3) Estimated at ~ 350 V but breakdown occurred at both higher and lower voltages.	Never quenched τ not known.
<u>Inner Coil</u> none <u>Middle and Outer Coil</u> Voltage taps for recording the quenches only. Not used for activating the energy dumping mechanism.	<u>Inner Coil</u> internal parallel shunt resistor. <u>Middle and Outer Coil</u> external parallel shunt resistor, manual dump only. (Fig. 30)	1) Helium dewar liquid level alarm. 2) Magnet dewar helium pressure alarm. 3) Power lead over-heat alarm. 4) Automatically adjusted power lead flow with manual emergency cooling control. 5) Power supply current regulation alarm. 6) Magnet dewar insulating vacuum failure alarm.	<u>Inner Coil</u> ~ 100 V/pair <u>Middle Coil</u> 300V/pair <u>Outer Coil</u> 400V/pair	Not known Except layer to layer voltage breakdown measurement performed on a similar coil package 1) liquid He 4500 - 7500V 2) Rm temp. air 2000V 3) 5°K He vapor 1400V	Quenched ~ 25 times τ (based on 3 magnets system) <u>Inner Coil</u> 3 sec. <u>Middle Coil</u> 8-18 sec. <u>Outer Coil</u> 12-24 sec.
(1) Helium dewar pressure and liquid helium level. These two alarms have to agree to initiate the 20 sec. delayed automatic dump. (2) Two-stage terminal voltage alarm. The second alarm will automatically dump the magnet. Note: Manual override is provided.	External Parallel shunt resistor method. (Fig. 57)	Two-stage power lead alarm and interlock. The second alarm will dump the magnet immediately.	160V	Not known	Quenched ~ 10 times $\tau \approx 6$ sec.

TABLE 2 - SOME PARAMETERS

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>
Magnet Classification and Total Hours of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (Amps)	Design Heat Transfer Rate ² (W/cm ²)	Cooling Method	Type of Electrical Insulation
<u>I1</u> 8°-Beam transport magnet (BNL) T ≈ 2500 hrs.	2 window frame racetrack dipoles connected in series Each module is ~1.8M long and ~(.01M) ² in cross section	Mechanical support in radial direction only, free to expand or contract axially	361 NbTi filaments 76μ in diameter, twisted 1/in. in copper matrix with rectangular cross section 1.37mm x 2.87mm Cu:SC=1.25:1	1350 DC max. 970 DC oper.	0.35	Pool cooling by liquid helium.	Conductor is coated with formvar Turn-to-turn: ~90 μ of formvar layer-to-layer: ~102 μ of formvar plus anodized pure aluminum sheet with built-in cooling grooves
<u>I2</u> Energy doubler prototype C2-10 C10-3 (FNAL) T C2-10 ≈ 100 hrs. T C10-3 ≈ 10 hrs.	4 shell pairs (Fig. 31) to simulate the Cosine θ current distribution. The coil is 3.05M long ~0.15M in diameter.	Conductor is spiral wrapped with B-stage glass tape and cured in heated molds and the coils are confined by intermittent sand-blasted and epoxied stainless steel banding	Graded Wires 1) 7 Strands Rutherford cable with cross section 1.91 mm x 3.81mm Cu:SC=1:1 2) 11 Strands Rutherford cable with cross section 1.27mm x 3.81mm Cu:SC=1:1	<u>C2-10</u> 1700AC (max. 6oper.) <u>C10-3</u> 2030AC (max. 6oper.)	Not Known	Pool cooling by liquid helium.	Turn-to-turn: D stage impregnated tape, bar-bar pole insulation, 75% coverage. layer-to-layer: 102μ epoxy Fusaflex sheet
<u>I3</u> Ship propulsion system magnet (Garrett) T=0 under construction by ANI	Solenoid generator 2 coils ID=0.3M OD=0.4M L=0.13M Motor 1 coil ID=0.34M OD=0.4M L=0.44M	Epoxy Impregnation	NbTi filaments twisted one/two inches in copper matrix with cross section 0.66 mm x 0.95mm Cu:SC=3:1	<u>Generator</u> 139.5 Amp. (design operating current) <u>Motor</u> 138.2 Amp. (design operating current)	Not Available	Force cooling by liquid helium	Turn-to-turn: 76μ Formvar layer-to-layer: 102μ epoxy glass
<u>I4</u> Test racetrack GE T≈150 hrs.	Racetrack W=0.25M H=3.8M L=0.56M	Epoxy Impregnation with distributed fiberglass reinforcement.	1400 Strands NbTi filament in copper matrix with cross section 1.27mm x 2.54 mm Cu:SC=1.8:1 varnish insulated	940DC max.	Not Available	Pool cooling by liquid helium	Not Available
<u>I5</u> High field solenoid (Magnet Lab. McGill Univ.) T≈20 hrs.	Solenoid <u>Inner Coil</u> ID=0.19M OD=0.4M <u>Outer Coil</u> (See Table 2a)	Mechanical Support	<u>Inner Coil</u> 1mm thick vapor deposited Nb ₃ Sn on metal alloy substrate with cross section 0.5mm x 12.7mm <u>Outer Coil</u> (See Table 2a)	<u>Inner Coil*</u> 850DC <u>Outer Coil*</u> 940DC *Max. Curr. during separate run	0.2	Pool cooling by liquid helium	<u>Inner Coil</u> Turn-to-turn: .13mm to .2mm epoxy glass dot (Fig. 32) Pancake-to-pancake: 1.59mm epoxy" disk <u>Outer Coil</u> Turn-to-turn 0.43 ± 0.07mm thick G-10 epoxy filled fiberglass strips (Fig. 32) Pancake-to-pancake (same)

OF INTERMEDIATE SIZE MAGNETS*

<u>IX</u>	<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u>	<u>XIV</u>	<u>XV</u>
Max. Stored Energy & Inductance	Quench Detection System	Magnet Protection Mechanism	Other Protection and Major Instrumentation	Maximum Voltage Across Magnet During Most Rapid Discharge Mode	Breakdown Voltage 1) Turn-to-Turn 2) Layer-to-Layer 3) Magnet-to- Ground	Quench Status and Quench Time Const. τ
$3.65 \times 10^5 \text{ J}$ 0.4h	Terminal voltage method (Fig. 1c & 5) and dewar pressure. Both signals will activate auto dump.	Internal par- allel shunt resistor (Fig. 7b & 13) and internal dissipation	1) Power lead over heat alarm and interlock. 2) Helium dewar liquid level alarm and inter- lock. 3) Helium dewar pressure alarm and interlock. 4) Insulation vacuum alarm and interlock. 5) Quench interlock. The above interlocks will automatically turn off the power supply.	~8v	Not Known	Quenched ~50 times. $\tau \approx 1 \text{ sec.}$
<u>C2-10</u> $\sim 1.06 \times 10^5 \text{ J}$ $73 \times 10^{-3} \text{ h}$ <u>C10-3</u> $1.5 \times 10^5 \text{ J}$ $73 \times 10^{-3} \text{ h}$	Pick up coil method. (Fig. 1a & 2)	External par- allel shunt resistor (Fig. 7a & 12)	None	~200V	Measured in liquid helium. 1) ~1000V 2) & 3) not known but is estimated to be greater than 1000V	Quenched ~14 times for both τ not known
<u>Generator</u> $3.33 \times 10^5 \text{ J}$ 34.25h <u>Motor</u> $4.56 \times 10^5 \text{ J}$ 47.8h	none	Self-protec- tion method.	Not available	Not available	Not available	Not available
$1.66 \times 10^5 \text{ J}$ and 0.375h	Bridge cir- cuit method (not used for this magnet.	Self-protec- tion method.	None	Not available	Not available	Quenched ~20 times τ not known
<u>Inner Coil</u> $1.2 \times 10^6 \text{ J}$ 3.3h <u>Outer Coil</u> $3.1 \times 10^6 \text{ J}$ and 6.9h	Terminal voltage measurement method for record only.	<u>Inner Coil</u> External par- allel shunt resistor. <u>Outer Coil</u> External par- allel shunt resistor.	None	~5V	not known	Quenched ~10 times. $\tau_{in} = 15 \text{ sec.}$ $\tau_{out} = 30 \text{ sec.}$

TABLE 2 - SOME PARAMETERS OF

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>
Magnet Classification and Total Hours of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (Amps)	Design Heat Transfer Rate $\frac{2}{(W/cm^2)}$	Cooling Method	Type of Electrical Insulation
<u>I6</u> Bumpy Torus (NASA) T $\approx$ 2000 hrs.	12 solenoids each with ID=0.22 M OD=0.31 M L=0.12 M (Fig. 14)	Mechanical Support Only (Fig. 14)	Type A) 7 magnets were wound with 2.03mm. sq. conductor with ~14 untwisted NbTi filaments Cu:SC=3.07:1 Type B) 5 magnets were wound with 2.16mm. diameter round wire with 133 twisted filaments Cu: SC=2.5:1	670DC max. 500DC oper.	~ 0.3	Pool cooling by liquid helium	Turn-to-turn: Type A: Intermittent tapes (Fig. 14) Type B: The wires were coated with oxide and there is no other insulation Layer-to-layer: Both type A&B were insulated by G10 strips (Fig. 14)
<u>I7</u> Ship Propulsion system magnets (NSRDC) T _G $\approx$ 10 hrs. T _M $\approx$ 250 hrs.	Solenoid generator 2 coils ea. ID=0.19 M OD=0.24 M L=0.1 M Motor 1 coil ID=0.14 M OD=0.2 M L=0.3 M	Generator epoxy impregnation with distributed fiberglass reinforcement Motor Made by IGC (26) The coil is potted by grease	Generator 180 NbTi filaments twisted 2/in. with cross section .56 mm x .7mm Cu: SC=1.8:1 Motor 0.76mm diameter round wire with 361 NbTi filaments twisted 2/in. Cu:SC=1.25:1	Generator 145DC max. 100DC oper. Motor 135DC max. 112DC oper.	Not Available	Pool cooling by liquid helium	Generator Turn-to-turn: Formvar Layer-to-layer: Formvar plus 102 μ thick epoxy fiber-glass Motor Formvar coated wire
<u>I8</u> RCBC Field Coil (SLAC) T $\approx$ 10000 hrs.	Split Helmholtz pair ID=.8M OD=.88M Coil length = .16M ea. distance between coils = .22M	Mechanical Support only	Twisted (3/in.) NbTi filaments (53.3 μ in diameter) in copper matrix (wire diam. = 1.27mm) with formvar coating. Type A: 180 filaments with Cu:SC=3.15:1 Type B: 114 filaments with Cu:SC=2.95:1	275 DC max. 180 to 244 DC oper.	Not Available	Pool cooling by liquid helium	Turn-to turn and layer-to-layer: Formvar coil to ground; 2.03 mm thick G10 insulation
<u>I9</u> DC Beam transport magnet system (LBL) T $\approx$ 500 hrs.	1 Dipole ID=0.29M OD=0.38M L=1.0JM 2 Quadrupoles ID=0.25M OD=0.32M L=0.73M	Mechanical Support only	NbTi filaments (62 to 246 strands) twisted 1 to 2 per inch with cross section 1.14mm 2.29mm. Cu:SC=2:1 or=3.3:1	600 DC max. 1 oper. Not Available (each magnet is powered by independent power supply)	Not Available	Pool cooling by liquid helium	Turn-to turn: Spiral Wrap, 50% coverage, .127mm x 3.18mm epoxy impregnated woven fiber glass tape (not bonded to conductor) Layer-to-layer: 1.25mm diam. helically wrapped over each layer 2.03mm pitch

INTERMEDIATE SIZE MAGNETS (CONT'D)*

<u>IX</u>	<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u>	<u>XIV</u>
Maximum Stored Energy & Inductance	Quench Detection System	Magnet Protection Mechanism	Other Protection and Major Instrumentation	Maximum Voltage Across Magnet During Most Rapid Discharge Mode	Breakdown Voltage 1) Turn-to Turn 2) Layer-to-Layer 3) Magnet-to- Ground Quench Status and Quench Time Const. τ
6.06x10 ⁵ J 2.7h	Terminal voltage measure- ment for record only	Internal parallel shunt resistor. (Fig. 14) Magnet is manually dumped.	1) Power lead over temperature monitor. 2) 48 probes for low liquid helium level alarms. 3) 2 pressure relief valves.	~5V	Not known Quenched ~170 times $\tau \approx 30$ sec.
<u>Generator</u> 1.25x10 ⁵ J 11.9h <u>Motor</u> 1.1x10 ⁵ J 12.1h	Terminal voltage comparing method. Com- mercial de- vice made by IGC (26)	Internal sub- divided par- allel shunt resistors (Fig. 15)	1) Power lead over heat interlock protection. 2) Liquid level inter- lock.	~15V	1) 1600V 2) 5600V 3) 800V (Calculated Value) Quenched ~10 times $\tau_G \approx 1$ sec. $\tau_m \approx 1$ sec.
1.19x10 ⁶ J 31.4h	Terminal voltage method same as LASS system	External par- allel shunt resistor. (Fig. 11)	Same as LASS magnet system except the major vacuum pro- tection system is not used here.	~300V	~5000V Estimated from formvar coating Quenched ~100 times τ not known
<u>Dipole</u> 6.6x10 ⁵ J 3.5h <u>Quadrupole</u> 1.5x10 ⁵ J per mag. 0.5h per mag.	Pick up coil (B) Method for record only	Self-protection and internal dissipation	None	Not Avail- able	Not known except Layer-to-layer insulation was tested with 300V impulse Quenched ~5 times $\tau \approx 0.5$ sec.

TABLE 3 - SOME PARAMETERS

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>
Magnet Classification and Total Hours of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (Amps)	Design Heat Transfer Rate (W/cm^2)	Cooling Method	Type of Electrical Insulation
<u>I10</u> 60" Levitated Ring (Princeton) T $\approx$ 2740 hrs.	Layer wound circular coil major diameter = 1.52M minor diameter = 0.18M (Fig. 51)	The SC tape is held down by spot vacuum grease in each layer and confined by coil forms. (Fig. 48) The completed winding is reinforced by cloth tape and Hastelloy ribbon.	Nb ₃ Sn tape RCA-SR2100 R-60252 R-60284	90DC max. 58DC oper.	Not known	Pool cooling by liquid helium with no attached refrigeration	Turn-to-turn: ~.25mm space Layer-to-layer: .04mm copper sheet with .006mm Mylar on both sides of the copper sheet. Hastelloy shorting strips are placed between these copper sheets.
<u>I11</u> IGC Test Solenoid (Princeton) T $\approx$ 25 hrs.	Solenoid ID~0.17M OD~0.21M L ~0.31M	Vacuum epoxy impregnation	NbTi multi-filamentary composite conductor with 1.02mm wire diameter Cu:SC=1.8:1	256.5DC max. 240DC oper.	Not known	Pool cooling by liquid helium	Not known
<u>I12</u> Hybrid Magnet (MIT) T $\approx$ 12 hrs.	24 stacked double pancakes ID=0.41M OD=0.67M L =0.58M (Fig. 53,54)	Mechanical Support only	60 untwisted NbTi strands each 0.25mm in diameter, in copper matrix with cross-section 10mmx2mm Cu:SC=8:1	1440DC max. (920-1440) DC oper.	0.25	Pool cooling by liquid helium	Turn-to-turn: 0.38mm thick intermittent Conolite spacer 25% coverage. Layer-to-layer: 1.58mm thick Micarta spacer. Coil-to-ground: 1.58mm thick G10 spacer (Fig. 54)

OF SMALL SIZE MAGNETS*

<u>IX</u>	<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u>	<u>XIV</u>	<u>XV</u>
Maximum Stored Energy & Inductance	Quench Detection System	Magnet Protection Mechanism	Other Protection and Major Instrumentation	Maximum Voltage Across Magnet During Most Rapid Discharge Mode	Breakdown Voltage 1) Turn-to-Turn 2) Layer-to-Layer 3) Magnet-to- Ground	Quench Status and Quench Time Const. τ
$\sim 3 \times 10^5 \text{ J}$ 74h	1) None dur- ing operation. 2) Voltage taps to record the normalcies during testing stage.	Internal dissipation An outer copper band is also provided for inductive energy dissipation and coil protection.	None	Not known	Not known	Quenched ~ 200 times $\tau \approx 1 \text{ sec.}$
$1.15 \times 10^5 \text{ J}$ 3.5h	Pick up coil method. Sig- nals are used to trigger automatic dump. (Fig. 50)	Both external and internal par- allel shunt resis- tors are provided when it is used with Princeton 3-section magnet (data for this magnet will not be reported) as shown in Fig. 50 and 52	1) Liquid helium level monitor. 2) Pressure relief 3) Power lead flow monitor. This instrumentation has to be manually monitored and adjusted.	Not known	Not known	Quenched ~ 10 times $\tau \approx 110 \text{ sec.}$
$1.8 \times 10^6 \text{ J}$ 1.7h	1) Voltage taps method 2) Helium pressure transducer The above signals are for indica- tions only, and will not activate any protection mechanism.	0.1 Ω parallel shunt resistor with manual power supply disconnect.	Liquid level detector.	$\sim 150 \text{ V}$	Not known	Quenched ~ 6 times $\tau \approx 10 \text{ sec.}$

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TABLE V.2a
Conductor and Coil Dimensions of
McGill University's High Field Solenoid

Section	A	B	C	D	E	F
ID, cm	41.10	44.10	46.93	49.86	54.60	59.54
OD, cm	44.00	46.60	49.60	54.34	59.31	64.29
Length, cm	56.26	57.00	56.34	56.59	56.59	56.59
Conductor width, mm	9.37	8.86	8.03	7.01	7.01	7.01
Conductor thickness, mm	1.17	1.12	1.14	1.24	1.24	1.24
Cu: NbTi ratio	4.5	5.8	6.8	6.6	6.6	6.6

Each conductor contains 36 untwisted NbTi filaments ranging in diameter from 0.27mm to 0.20 mm.

TABLE 3 - SOME PARAMETERS

<u>I</u>	<u>II</u>	<u>III</u>	<u>IV</u>	<u>V</u>	<u>VI</u>	<u>VII</u>	<u>VIII</u>
Magnet Classification and Total Pairs of Operation T	Coil Geometry	Magnet Construction	Conductor	Magnet Current (AMPS)	Design Heat Transfer Rate W/cm ²	Cooling Method	Type of Electrical Insulation
<u>S1</u> Alternator (MIT) T ≈ 15 hrs.	6 layers of Saddle coil pair ID=0.15M OD=0.2M L = 0.97M	The B-stage epoxy glass tape between turns provides turn- to-turn and layer-to-layer bonding and each layer is confined by a series of continuous filament glass roving and epoxy band. (Fig. 33)	24 0.254mm NbTi fila- ments twisted 1/inch in copper matrix with dimension 1.27mm x 3.18mm and Cu:SC= 2.6:1	I _{max} = not known estimated to be 1000 DC I _{oper} = 770 DC	Not known	Forced flow cool- ing by liquid helium.	Wire is coated by copperoxide. Turn-to-turn: 0.16-0.38mm glass tape woven between turns layer-to-layer: 1.52-1.78mm thick fiberglass epoxy band. (Fig. 33)
<u>S2</u> Prototype synchrotron magnet-AC3 (Rutherford Lab) T ≈ 20 hrs	6 double layer pan- cakes ID = 0.1M OD = 0.2M L = 0.4M (Fig. 34)	Epoxy impregnation, Separate pancakes resin rich areas filled with granules of sintered aluminum oxide. (Fig. 34)	See Table 3a.	5100 AC max. 4500 AC oper.	~0.02	Pool cool- ing by liquid helium.	Turn-to-turn: Terylene braid, woven around the cable layer-to- layer: Sheet of parallel insula- ted (enamel and epoxy) copper wires.

OF SMALL SIZE MAGNETS *

<u>IX</u>	<u>X</u>	<u>XI</u>	<u>XII</u>	<u>XIII</u> Maximum Voltage Across Magnet During Most Rapid Discharge Mode	<u>XIV</u> Breakdown Voltage 1) Turn-to-Turn 2) Layer-to-Layer 3) Magnet-to- Ground	<u>XV</u> Quench Status and Quench Time Const. τ
Maximum Stored Energy & Inductance	Quench Detection System	Magnet Protection Mechanism	Other Protection and Major Instrumentation			
$90 \times 10^3 \text{ J}$ 0.18 h	Pick up coil method but is not used be- cause of false triggers due to the armature noise.	1) Current In- verter 2) Voltage limiter 3) Thyrite resistor 4) Spark gap	1) Power lead over- heat alarm 2) Helium vapor flow alarm (both main flow and power lead flow)	$\sim 40 \text{ V}$	Not known	20 times. τ not known
$60 \times 10^3 \text{ J}$ $4.8 \times 10^{-3} \text{ h}$	Balanced poten- tiometer type. Three indepen- dent units con- nected to differ- ent center tap points in the magnet so as to give some indica- tion of where the quench started. (Fig. 35)	External cir- cuit breaker and a $33 \text{ m } \Omega$ dump resistor	Voltage trips on vapor cooled current leads.	$\sim 165 \text{ V}$	1) 500V 2) 500V 3) $> 5 \text{ kV}$ (based on estimate)	Quenched ~ 30 times $\tau \approx 0.15 \text{ sec.}$

TABLE V.3a
RUTHERFORD CONDUCTOR

Cable Size	5mm square
Number of strands	90
Twist pitch at (3) stage	12 mm
Twist pitch at (5x3) stage	20 mm
Twist pitch at (6x6x3) stage	63 mm
Filling factor (total metal, including fillers)	60%
Filling factor (composite only)	46%
Composite wire diameter	0.4 mm
Number of filaments	1045
Filament diameter	8
Twist pitch of wire	3.6 mm
Constituents of composite:	
niobium-titanium	40%
copper	53%
cupro nickel	7%

TABLE 4 *

MAGNET SYSTEM FAILURES

<u>Magnet Classification</u>	<u>Failure Description</u>	<u>Cause of Failure</u>	<u>Protection System at Time of Failure</u>	<u>Note</u>
<u>S1</u> Alternator (MIT)	Sections of lead wire conductor evaporated (Fig. 37) and magnet arced to ground causing some damage to the outer helium dewar wall.	1) Lead conductor does not have adequate copper to keep the temperature low during quench condition. 2) Insufficient lead cooling. Both of these cause a thermal runaway phenomenon.	Same as indicated in columns XI and XII in Table 3.	Six turns of damaged conductors were stripped and the magnet is back in operation with an asymmetrical coil.
<u>I8</u> RCBC Field Coil (SLAC)	Lead wire melted open and magnet arced to ground. (Fig. 38).	Insufficient cooling of the lead conductor due to the plug of cooling channel.	Same as indicated in columns X, XI and XII in Table 2.	Magnet was repaired and is operational.
<u>L1</u> 7-foot bubble chamber magnet (BNL)	Power lead overheated causing hydrogen leaks into the helium dewar vacuum space. (Fig. 39).	Insufficient lead cooling flow arising from inadequate instrumentation.	Same as in Table 1, except that Items 1 and 2 in column XII and the flow monitoring system were absent. The energy dump is also accomplished by manual control only.	Magnet intact and power lead was repaired. System is operational.
<u>L5</u> SUMMA Magnet System (NASA) (A) test (B) operation	(A) 1) The outer NbTi coil of the 4th magnet: Insulation and end packing strips were blown out and magnet arced at many places. 2) The Nb ₃ Sn inner coil: Shorts among pancakes at the ends due to slipping of Nb ₃ Sn ribbon under pancake separator strips. (B) One of the 32 power leads connected to outbore outer coil pair overheated and was damaged during operation. (Fig. 40)	(A) 1) Insufficient mechanical support and electrical insulation. 2) Insufficient mechanical support. (B) Insufficient cooling and instrumentation.	(A) 1) Both quench detection and auto/manual energy removal systems were provided. 2) Internal shunt resistor across pancakes. (B) Power lead temp. monitor and manual energy removal.	(A) 1) The magnet was repaired and is operational. 2) Coil was repaired by MCA and is operational. (B) Lead was replaced, the magnet (outbore outer coil) is operational but its performance was degraded from 400 Amps to 260 Amps due to this accident.

TABLE 4 (Cont'd)*

MAGNET SYSTEM FAILURES

Magnet Classification	Failure Description	Cause of Failure	Protection System at Time of Failure	Note
<u>I1</u> 8° Beam transport magnet (BNL)	One of the power leads overheated and the conductor wire connected to this lead melted. (Fig. 41) The magnet arced to ground. (Fig. 42)	1) faulty power lead 2) insufficient instrumentation 3) improper lead wire installation	None except visual voltage monitor across each lead and the liquid level probe.	1) New power leads were tested before installation. 2) The damaged coils (4 layers) were replaced. 3) The lead wire installation error was corrected. The magnet is operational and its performance stays the same. The accident occurred during testing in the test area where the designed instrumentation was not provided.
<u>S2</u> Prototype synchrotron magnet-AC3 (Rutherford Lab.)	Arcing between current feed under liquid helium. (Fig. 43)	One of the current feeds broke off at full current.	External circuit breaker and a 33m Ω dump resistor.	Magnet intact and a new current feed was installed. The system is back in operation.
<u>I5</u> High field solenoid (Magnet Lab. McGill University)	1) Mechanical deformation of the Nb ₃ Sn pancakes (Fig. 44), the NbTi Coil and the 1.27cm thick stainless steel end plate. (Fig. 45) 2) Minor electrical damage due to arcing after the mechanical failure of the NbTi Coil.	1) Diamagnetic force. 2) Insufficient mechanical support.	External parallel shunt resistors were provided for both inner and outer coils.	Both coils have been rewound and potted with epoxy resin. To date it is operational but not fully tested for its expected performance.
<u>I2</u> Energy doubler prototype C10-3 (FNAL)	Arcing produced between coil shells and windings were burned open. (Fig. 46)	1) The energy removal system was short-circuited due to the failure of the powering circuit causing the dissipation of stored energy internally. 2) Dielectric breakdown between coil shells when helium in this region converted to gas of temperature greater than 400K.	Same as described in Table 2 columns X and XI.	All but one of the half shell windings were burned beyond repair and the remaining one was damaged.
<u>I9</u> Beam transport magnet system (LBL)	Burnout occurred between two corresponding coils at the same radius, one on each pole. (Fig. 47)	Believed to be due to a short between windings.	None	Failure occurred during testing with only part of the winding completed. Additional insulation (Mylar film) was added and the magnet was repaired. It is now on loan to FNAL.

TABLE 4 (CONT'D)*

MAGNET SYSTEM FAILURES

<u>Magnet Classification</u>	<u>Failure Description</u>	<u>Cause of Failure</u>	<u>Protection System at Time of Failure</u>	<u>Note</u>
<u>I10</u> 60" Levitated Ring (Princeton)	Coil degradation due to conductor movement (Fig. 48 & 49)	Insufficient Mechanical support.	None	A spare ring was installed to continue the operation, while the degraded coil was under repair and test.
<u>I11</u> IGC Test Solenoid (Princeton)	The lead wire, connected to the built-in Nichrome ribbon shunt, melted. The magnet quenched, but the coil was not damaged. (Fig. 50)	Design Error	Same as described in Table 2	
<u>I12</u> Hybrid magnet (MIT)	Degradation, i.e., each time the magnet is operated, it quenched at a lower current than previous times.	Not clear, but probably due to the quality of the conductor itself.	Same as described in Table 2	Coil will be rewound with modern twisted multi-filamentary NbTi composite conductor.
<u>I13</u> 3-Section Solenoid (Princeton)	(A) Lead failure (Fig. 50) One of the power leads connected to the center section burned. (B) Coil damage (Fig. 50) The center section was damaged in a subsequent quench following the lead accident. The degree of damage was not known except that the total room temperature resistance is lower than before.	(A) Lead failure 1) Insufficient instrumentation. 2) Insufficient lead cooling flow. 3) Possibly the lead itself is defective. (B) Coil damage The coil was not disassembled and the cause of this damage was not known, possibly, the damage was due to arcing during quench.	As shown in Fig. 50.	The magnet parameter of this solenoid will not be reported. At present, it is not in use since the IGC test solenoid, designed as an insert for this magnet, can create high enough field (55kG) for conductor testing purposes at Princeton.

(a)

(b)

(c)

Figure V.1. Schematic diagram of several normal region detection methods.

Figure V.2. Block diagram of the energy doubler's quench detection circuit (Ref. 10).

Figure V.3. Schematic diagram of the quench detection circuit used in quench studies by Mulhall et al. (Ref. 11).

Figure V.4. Schematic diagram of the quench detection circuit for ANL's 12 foot bubble chamber magnet (Ref. 12):

Figure V.5. Block diagram of the quench detection circuit for BNL's 8° magnet system.

Figure V.6. Block diagram of SLAC's quench detection circuit for LASS and RCBC magnet (courtesy of S.J. St. Lorant, SLAC).

Figure V.7. Schematic diagram of several magnet protection methods.

Figure V.8. Schematic diagram of ANL's 12 foot bubble chamber magnet protection method (Ref. 12).

Figure V.9. Schematic diagram of FNAL's 15 foot bubble chamber magnet protection method (courtesy of H. Kautzky, FNAL).

Figure V.10. Schematic diagram of SLAC's Lass magnet protection method (courtesy of S.J. St. Lorant).

Figure V.11. Schematic diagram of ELAC's RCBC magnet protection method (courtesy of S.J. St. Laurant).

Figure V.12. Schematic diagram of quench detection and energy removal method for FNAL's energy doubler magnet (Ref. 10).

Figure V.13. Schematic diagram of BNL's 8° magnet protection method.

Figure V.14. Internal shunt protection method used by NASA's bumpy torus magnet system (Ref. 25).

MOTOR

GENERATOR

Figure V.15. Schematic diagram of magnet protection method used for Navy's ship propulsion system (courtesy of H.O. Stevens, NSRDC).

Figure V.16. Schematic diagram of BNL's 7 foot bubble chamber magnet protection method.

Figure V.17. The coil construction configuration of BNL's 7 foot bubble chamber magnet.

Figure V.18. The conductor used for BNL's
7 foot bubble chamber magnet windings.

Figure V.19. The pancake construction configuration of BNL's 7 foot bubble chamber magnet.

Figure V.20. The coil construction configuration of ANL's 12 foot bubble chamber magnet (Ref. 12).

Figure V.21. A photograph of ANL's 12 foot bubble chamber magnet (Ref. 12).

Figure V.22. The conductor used for ANL's 12 foot bubble chamber magnet windings (Ref. 12).

Figure V.23. The conductor and its reinforcement used for FNAL's 15 foot bubble chamber magnet (15 Foot Bubble Chamber Safety Report Vol. 2).

Figure V.24. The conductor used for FNAL's
15 foot bubble chamber magnet (Ref. 21).

Figure V.25. The coil construction configuration of SLAC's LASS magnet, (A) clamp for stainless steel strip (tension to ~600 lb.), (B) splice between adjacent pancakes and support, (C) phenolic spacers, (D) ground insulation (dacron-mylar-dacron) and G-10 strips, (E) temporary clamp, (F) temporary conductor retainer, (G) inner conductor cross-over, (H) voltage tap, (J) nylon line used during construction to hold spacers in position (courtesy of S.J. St. Laurent, SLAC).

Figure V.26. The conductor used for SLAC's
Lass magnet windings (Ref. 22).

Figure V.27. Schematic diagram of NASA's
summa magnet system.

NbTi MULTICORE CONDUCTOR CONFIGURATION

Nb₃Sn CONDUCTOR

Figure V.28. The conductor used for NASA's summa magnet (Ref. 27).

NbTi WINDING CONFIGURATION

Nb₃Sn WINDING CONFIGURATION

Figure V.29. Inter-turn and inter-layer insulation configurations of NASA's summa magnet (Ref. 27).

Figure V.30. Schematic diagram of NASA's summa magnet protection method. Figure shows two typical protection circuits for all coils (courtesy of L. Nagy, NASA).

Figure V.31. Coil configuration of FNAL's energy doubler magnet (Ref. 34).

INNER COIL CONDUCTOR AND INSULATION

OUTER COIL CONDUCTOR AND INSULATION

Figure V.32. Inter-turn insulation method used by magnet laboratory of McGill University, Canada.

Figure V.33. Schematic diagram of the field winding configuration of MIT's alternator (Ref. 43).

Figure V.34. Schematic diagram of the dipole coil configuration of Rutherford Laboratory's AC3 (Ref. 44).

Figure V.35. Schematic diagram of Rutherford Laboratory's quench detection and magnet protection method (courtesy of M.N. Wilson, Rutherford Laboratory, U.K.).

(A)

(B)

Figure V.36. FNAL's 2.5 foot energy doubler prototype. Photos show the result due to local hot spot, (A) melted indium, (B) burned insulation (courtesy of D.F. Sutter, FNAL).

Figure V.37. Details of the lead failure of MIT's superconducting alternator (Ref. 47).

Figure V.38. RCBC magnet failure. About 6" of doubled conductor vaporized due to arcing (courtesy of S.J. St. Lorant, SLAC).

Figure V.39. BNL 7-foot bubble chamber power lead failure. Photo shows the burned fiber-glass lead support clamp, due to excessive heat generated by the lead.

NASA
C-74-3160

Figure V.40. NASA's summa magnet power lead failure. Photo shows the overheated section of the G-10 sleeve (courtesy of J. Reinmann and L. Nagy, NASA).

Figure V.41. BNL 8° transport magnet failure.
Photo shows melted conductor due to excessive
heat generated by a faulty power lead.

Figure V.42. BNL 8° transport magnet failure.
Photo shows magnet arced to ground due to
melted conductor shown in Fig. V.41.

Figure V.43. Rutherford Laboratory's AC3 dipole magnet current feed failure (courtesy of M.N. Wilson, Rutherford Laboratory, U.K.).

Figure V.44. McGill University's high field solenoid magnet failure. Photo shows the deformed coil due to the diamagnetic force (courtesy of R. Stevenson, McGill University, Canada).

Figure V.45. McGill University's high field solenoid magnet failure. Diagram shows the deformed upper stainless steel flange and the displaced NbTi coil package (Ref. 54).

Figure V.46. FNAL's 10-foot energy doubler failure. Photo shows the burned section caused by localized heating during an internal energy dump (courtesy of D.F. Sutter, FNAL).

Figure V.47. LBL's beam transport dipole magnet failure. Photo shows the coil damage caused by localized heating during quench (courtesy of R.B. Meuser and W.S. Gilbert, LBL).

Figure V.48. Photo shows the sixth layer of the Princeton 60" levitated ring. Unlike layers one through five, no evidence of conductor movement can be detected (courtesy of J. File and J. Kaugerts, Princeton U.).

Figure V.49. Degradation of the Princeton 60" levitated ring due to conductor movement. Photo shows the first layer (courtesy of J. File and J. Kaugerts, Princeton U.).

Figure V.50. Schematic diagram of Princeton superconducting magnet system (courtesy of J. Kaugerts, Princeton U.).

Figure V.51. Coil geometry of Princeton 60" levitated ring (Ref. 42).

Figure V.52. Schematic diagram of Princeton IGC test solenoid protection method (courtesy of J. Kaugerts, Princeton U.).

Figure V.53. Photograph shows MIT's superconducting solenoid as the outer part of a high field hybrid magnet system (Ref. 56).

Figure V.54. Photograph shows the pancakes and the layer to layer insulation of MIT's superconducting solenoid (courtesy of B. Montgomery, MIT).

Figure V.55. Coil geometry of Livermore's baseball II magnet (Ref. 33).

Figure V.56. Turn to turn and layer to layer insulation method of Livermore's baseball II magnet (Ref. 33).

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Section V

1. In high energy laboratories, superconducting magnets are used in bubble chambers, such as, 7-foot bubble chamber of BNL, 12-foot bubble chamber of ANL and 15-foot bubble chamber of FNAL; in accelerators such as energy doubler development of FNAL, ISABELL development of BNL and ESCAR of LBL; in Beam transport magnet such as 8⁰ beam transport magnet system of BNL, and Bevatron beam transport system of LBL. In transportation, superconducting magnets are planned to be used for high speed levitated trains. In Airforce and Navy laboratories, superconducting magnets are in the process to be used in power generation and ship propulsion systems. In power transmissions, superconductor will be experimented for transmitting large amounts of power. Superconducting magnet is also going to be used as energy storage. These are a few examples of the major applications of superconductors in this country beside the application to the present and future fusion devices.

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2 stage alarm: Same as above except the second alarm means automatic dump.

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40. The four magnets are lined up in a row (See Fig. 27), and are separately powered. The two magnets in the middle are called inbore magnets. The two magnets at each end are called outbore magnets.
 - (1) The four inner Nb_3Sn coils are independently powered by individual power supplies.
 - (2) Each pair of inbore middle coil and outer coil are independently powered by individual power supplies.
 - (3) Each pair of outbore middle coil and outer coil are independently powered by individual power supplies.
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