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April 2, 1986

Mr. Harold R. Denton, Director
Office of Nuclear Reactor Regulation
U. S. Nuclear Regulatory Commission
Washington, D. C. 20555

Attention: Mr. B. J. Youngblood, Project Director
PWR Project Directorate No. 4

Re: Catawba Nuclear Station
Docket Nos. 50-413 and 50-414
McGuire Nuclear Station
Docket Nos. 50-369 and 50-370

Dear Sir:

By letter dated March 25, 1986, Duke Power Company provided responses to the questions transmitted by Mr. B. J. Youngblood's letter of December 17, 1985. As a result of further discussions with the Staff, the previous responses have been revised.

Attached herewith are twenty (20) copies of Revision 14 to Duke Power Company's report, "An Analysis of Hydrogen Control Measures at McGuire Nuclear Station". As noted in Revision 9, this report is applicable to the Catawba Nuclear Station. This revision replaces Revision 13 in its entirety.

Very truly yours,

Hal B. Tucker

Hal B. Tucker

ROS:slb

Attachment

xc: Dr. J. Nelson Grace, Regional Administrator
U. S. Nuclear Regulatory Commission
Region II
101 Marietta Street, NW, Suite 2900
Atlanta, Georgia 30323

NRC Resident Inspector
Catawba Nuclear Station

A001
1/20

Response to questions transmitted by letter from NRC (B. J. Youngblood) to Duke Power Company (H. B. Tucker) dated December 17, 1985

1. Provide details of the fan response calculation to support the statement in Duke's March 29, 1985 letter that burns occurring at hydrogen concentrations of 6.5% or less do not create sufficient pressure differential across the fans to speed them up to synchronous speed. Include in your response a description of the hydrogen combustion assumptions (e.g., flame speed, burn completion, compartment venting, containment spray, heat removal), and the fan and electrical system models and assumptions.

Response: To determine a best estimate time-differential pressure loading curve for the air return fans, a CLASIX run was made using restart information from the containment analysis reported in Reference 1. Starting from containment conditions which are typical of those found by analysis during the time that hydrogen is being released and burned (containment pressure approximately 22 psia), an upper compartment global burn was forced to occur by lowering the ignition limits in the upper compartment until ignition occurred. The time history of the pressure difference between the upper compartment and the deadended compartment, which represents the fan flowpath differential pressure was noted. The following are the specific characteristics of this analysis:

a. Hydrogen ignition occurred when the hydrogen concentration was slightly above 6.5% by volume in the upper compartment.

b. Ice condenser top deck doors were greater than 20% open throughout the transient; ice condenser immediate deck doors were closed due to the reverse differential pressure. Venting from the upper plenum into the ice bed region occurred through the 20 square foot bypass opening around the intermediate deck. No venting was allowed to occur from the upper compartment to the lower compartment through the bypass area.

c. The heat and mass removal mechanisms operating in the upper compartment - the containment spray, air return fans, and upper compartment heat sinks - were operable.

d. The burn time for the upper compartment was 9 seconds, corresponding to a flame speed of approximately 12 feet/second.

Under these conditions, a differential pressure transient with a peak of 8.1 psid and a duration of 12 seconds was imposed on the air return fans. A time plot of this transient is shown in Figure 1.

There is significant conservatism built into this analysis of an upper compartment burn transient. As has been shown in the NTS test series, hydrogen burning in a turbulent atmosphere created by spray can take place at concentrations as low as 5% by volume.

The NTS test series also showed that hydrogen burning in a transient injection mode, such as at the exit of the ice condenser, is likely to be by diffusion flames if the exit velocity is low; velocities which created diffusion flames in NTS are comparable to those at the exit of the ice condenser. Therefore, even if one accepts the premise that some type of inhibiting mechanism will move the burning regime from the lower compartment to the upper plenum and upper compartment, it is very likely that hydrogen burning will be by diffusion flames at the exit of the ice bed and propagating into the region immediately above the top deck doors, and global accumulations with accompanying whole compartment deflagrations will not occur.

Using the fan blade geometry, this differential pressure was converted into an imposed torque on the air return fan blades. This torque curve has the same shape as Figure 1 and has a peak of 3493 ft-lbf.

To determine the effect of this torque on the air return fan speed, the fan and motor were modeled using the information in Reference 2. The general differential equation which describes the transient rotation of the induction machine is

$$J\frac{d\omega^r}{dt} + D\omega^r + \nu T_{e\phi} = T^r \qquad (1)$$

where
ω^r = operating angular speed
J = rotating moment of inertia
D = total windage coefficient (losses)
T^r = shaft mechanical torque
ν = number of phases
$T_{e\phi}$ = per phase torque of electrical origin

Slip (S) is defined as

$$S = \frac{\omega_{syn} - \omega^r}{\omega_{syn}} \qquad (2)$$

where ω_{syn} = synchronous speed of the machine

For small values of slip in the operating range of steady state, the per phase torque of electrical origin is proportional to the slip

$$T_{e\phi} = -KS \qquad (3)$$

where the negative sign indicates the torque direction. Equation (1) may be rewritten in terms of slip rather than angular speed, then split into two equations - one which represents the steady state operating condition and one which represents the transient deviation from that steady state condition. The transient equation may then be solved to yield:

$$\Delta S = \frac{\Delta T^{r}}{(\frac{\omega}{n})D + \nu K}\left(1 - e^{-t/\tau}\right) \tag{4}$$

where τ is the induction machine time constant.

$$\tau = \frac{J}{D + \frac{n \nu K}{\omega}} \tag{5}$$

n = number of pole pairs in the machine
ω = excitation angular frequency

In plugging actual numbers into this analysis, the windage coefficient was neglected for conservatism. The other values needed for the analysis were obtained from the fan and motor vendor. The time constant of the fan/motor was found to be very short, so it was assumed that the machine follows the imposed differential pressure transient exactly. The calculated peak speed of the fan is 1371 rpm corresponding to a value of slip of -0.14. Thus the induction motor attached to the fan will become an induction generator.

To find the electrical transient associated with motoring the fan, it was assumed that the running current/torque curve is symmetric about the synchronous speed. This assumption is valid for small values of slip, as in this case, and was verified to be reasonable in discussions with the motor vendor. The current through the motor breaker as a function of time is shown in Figure 2.

There are a number of significant conservatisms in the analysis which establish a degree of margin. Aerodynamic and frictional forces (windage) which oppose an increase in motor speed have been ignored. The total differential pressure appearing between compartments in the analysis is assumed to be imposed on the fan blades; in reality, the total differential pressure drop would be shared by the fan assembly, the fan blades, the connecting ductwork, and the dampers in the flow path. The imposed differential pressure transient is assumed to be converted to torque on the fan with 100% efficiency, a very conservative assumption as will be shown later. Finally, in the calculation of differential pressure across the fan, no allowance was made for the fan flow to increase beyond the maximum shown on the fan head/flow curve. Clearly if the fan is to be speeded up by imposed torque, the flow through the fan must increase and upper compartment venting take place even faster than in the CLASIX analysis. This final conservatism would tend to reduce the imposed differential pressure to something less than 5.1 psid.

Given an electrical transient consisting of a current reversal for several seconds, then a second reversal and return of the fan/motor to normal operation, the effect on the fan motor circuit breaker was assessed to determine whether either the direction or magnitude of the transient would cause it to trip. Current reversal in itself will not cause a trip of the breaker. It is necessary then to compare the current versus time curve (Figure 2) with the trip current versus time curve for the thermal overload devices associated with the air return fan motor starter, and the overcurrent trip devices on the circuit breaker. The minimum motor load rating of the selected thermal overload device is 57.4 amps. Figure 3, middle curve, shows the thermal overload response curve, indicating the time to trip of the thermal overload as a function of the percentage of minimum motor load rating. When a motor starter trips on thermal overload, it may be reset within a short time, much less than one minute after tripping. In addition to the thermal overload in the motor starter, the circuit breaker provides an overcurrent trip, as shown in Figure 4. The breaker unit trip rating is 125 amps.

If the air return fan were to stop operating by either tripping of the thermal overload or by overcurrent trip, the control room operator would be alerted immediately by an annunciator alarm on the ventilation panel indicating trip of the air return fan and an annunciator alarm on the main control board indicating ventilation panel trouble. The shift supervisor would then dispatch an operator to the load center to restart the fan by resetting the overload or overcurrent trip. The fan would start immediately upon resetting of the overload because the start conditions would still exist. The total time between fan stopping and restarting would be less than five minutes during an accident situation when the condition would be noticed immediately and extra operators would be available throughout the plant for action.

When the comparison of Figure 2 with the trip curve, Figure 3, was made, it was noted that the thermal overload would not cause tripping of the air return fan for the transient condition resulting from an upper compartment global burn. The calculated peak current of just over 500 amps would have to exist for greater than four seconds to cause the thermal overload to open the circuit to the fan motor. The circuit breaker overcurrent trip device has even more margin to tripping.

In order to assess the most significant of the conservatisms in the preceding analysis, that the fan converts differential pressure to torque with 100% efficiency, a classic reference on wind energy, Reference 6, was consulted. Note that when the reverse differential pressure is imposed on the fan, the fan becomes a windmill, extracting energy from the flow of gasses passing through it. This energy is extracted by converting some of the kinetic energy of the gasses to mechanical energy of the fan by changing the velocity of the flow of gasses. From a momentum balance performed for this process, it is possible to derive an expression for the power which can be extracted from the gas stream.

The maximum available power in a flowing stream of gasses is

$$P = \frac{1}{2} \rho A V^3 \qquad (6)$$

where
P = power
ρ = density of gasses
A = cross sectional area of the stream upstream of fan
V = velocity of the stream upstream of fan

For the flow of gasses through the air return fan during an accident, the following numbers may be conservatively estimated. The density of the gasses is 0.1 lbm/cubic foot; the cross sectional area of the fan shroud is 12.57 square feet. A conservative upper bound on the value of the upstream velocity may be calculated by assuming that the flow is choked in the open area of the fan blades; the upstream velocity in this case is approximately 200 ft/sec. Plugging in these numbers yields a maximum available power in the gas stream of 212 kw. Classical windmill theory further shows that the maximum amount of power which can be extracted by an ideal windmill is 0.593 of the available power, or 126 kw in this case. This amount of power extracted from the flow and converted to electrical energy, assuming no other losses in the that conversion process, corresponds to 126 amps, far below the tripping current of any of the fan protective devices. Actual inefficiencies in converting gas stream power to electrical power by a fan not designed to do that job would make the developed power significantly lower, as would the generator losses in the conversion process. Since this analysis considered the maximum flow rate through the fan, regardless of the imposed differential pressure, it can be assured that the fan would not be tripped by windmilling during a period when upper compartment burning occurred, regardless of the conservative assumptions one might use for hydrogen concentration or burn time.

The purpose of this exercise was to show the low efficiency with which the air return fan is able to extract power from a flow of gasses passing through it and therefore the extreme conservatism of the calculational method which produced Figure 2. Note that if this much flow were passing through the fan (corresponding to the 200 ft/sec upstream velocity), a significant amount of venting would occur through the fan path during the pressure transient further reducing the differential pressure across the fan.

To summarize the margin in this analysis:

a. The CLASIX analysis of an upper compartment burn assumed that the burn was global at 6.5%. Data from the NTS test series shows that global burning in the presence of spray will occur at concentrations as low as 5%, therefore a compartmental concentration of 6.5% will never occur. The NTS data also shows that a more likely burning scenario for hydrogen which does not burn in the lower compartment is that it will burn as diffusion flames in the upper plenum of the ice condenser or in the upper compartment in the region immediately above the ice condenser. The survivability of equipment in the upper plenum of the ice condenser for sustained burning has been shown in reference 1. There is no vital equipment in the upper compartment which would be affected by diffusion flames. Finally, the model used for conversion of gas stream kinetic energy to mechanical energy of the fan is extremely conservative; a quantitative assessment of that conservatism shows that the ability of the fan to extract power from the flow of gasses through it is limited to a fraction of that which would be required to cause tripping of the fan motor protective devices on overcurrent, and upper compartment burning does not pose a threat to continued operation of the air return fan.

2. Using an appropriate modeling technique, provide a quantitative assessment of the pressure loading on the ice condenser doors created by hydrogen combustion in a) the upper plenum and b) the upper compartment. Describe and justify the assumed or calculated door positions. Provide an evaluation of the ultimate capability of the ice condenser doors to withstand reverse differential pressures. Discuss the probable failure modes and the consequences of such failures.

Response:

The top deck doors of the ice condenser consist of flexible blanket insulation supported on grating. During the initial blowdown phase of a LOCA, these blankets are displaced from their normal positions and do not again form a seal. Westinghouse estimates that the area of the top deck will be at least 20% open at all times following this initial displacement of the top deck doors. There are no failure modes associated with the top deck doors because this open area assures that a reverse differential pressure loading cannot occur. The displacement of the top deck doors also ensures that the upper plenum burns vent to the upper compartment, so that burning in the upper plenum creates very small pressure differentials across the intermediate deck doors. The limiting condition for the intermediate deck doors is therefore the global upper compartment burn. For analysis purposes, the same upper compartment burn which was used in the discussion of air return fan behavior was used to assess what happens to the intermediate deck doors if a global upper compartment burn occurs.

There is a permanent bypass area associated with the intermediate deck doors of 20 square feet. This bypass tends to limit the imposed differential pressure by allowing venting around the doors. CLASIX analysis considering this vent area in operation shows pressure differentials across the intermediate deck are very low, in the range of the 1-2 psid previously reported to NRC.

In order the verify the previously reported capability of the intermediate deck for reverse differential pressure, an analysis was performed of the capability of the intermediate deck to withstand differential pressure loading. The vendor drawings of this area were consulted, and nominal material properties were used. The reverse differential capability of the intermediate deck was found to be 6 psid which agrees with the capability calculated independently by TVA and previously reported to NRC. Similarly the lower inlet door capability previously reported by TVA as 7 psid applies to Catawba because the structural components of the lower inlet doors are identical between Sequoyah and McGuire/Catawba.

We note in Reference 3 that NRC is concerned about the different numbers reported by the utilities in assessing the structural capability of the ice condenser doors. Some structural changes were made by Westinghouse in these areas between D. C. Cook and McGuire/Sequoyah which may account for some differences. Structural analysis methods may also make some difference; although we have great confidence in our independent verification of capabilities previously reported by TVA. Differences in the loading created by hydrogen burning may be attributed to assumptions made by the different utilities in performing CLASIX analysis or in interpreting the results. This most recent work we are reporting is considered to be the best because it uses the latest test data available from NTS to establish a conservative set of hydrogen burning parameters. Previous analyses which assumed global burning at 8% or 8.5% (as we did in the analysis reported in Reference 1) would necessarily have resulted in much larger imposed transient differential pressures.

We have also reexamined the Sandia analysis in which they used MARCH and HECTR to predict that upper compartment global burning is a likely outcome of a LOCA in which core degradation occurs. As we discussed in our previous submittal, the inhibition of lower compartment burning due to partial steam inerting and the consequential large number of upper plenum and upper compartment burns as predicted by Sandia is contradicted by work we have done using the MAAP code. We note that these Sandia results have also been contradicted by the results reported by AEP and their contractors in Reference 4. When one examines the graphs of hydrogen and steam release rates reported by AEP, one notes a substantially smaller rate of flow of steam into the lower containment than that reported by Sandia in Reference 5 during the time that hydrogen is being released. This apparent error by Sandia is particularly pronounced for the S2D sequence as seen in the comparison of Figure 14 of Enclosure 2 of Reference 4 with Figure 3-3 of Reference 5. We are led to the conclusion that the Sandia results have incorrectly predicted steam release rates which accompany hydrogen releases to the extent that their analysis incorrectly predicts hydrogen concentrations below the ignition level in the lower compartment during the duration of hydrogen release. Thus hydrogen burning has been forced to occur in regions other than the lower compartment, and as a consequence upper compartment burning occurs frequently. This upper compartment burning would be eliminated if Sandia had correctly predicted the steam release rates in MARCH and used a realistic ignition level in the lower compartment, such as 7.0%.

If imposed differential pressures were to cause some blockage of ice condenser doors, this is not a problem for the following reasons. The ice condenser doors are sized to pass the very large flows of air which result from the initial blowdown following a LOCA. Once most of the air in the lower compartment has been replaced by steam, the flow through the ice condenser is reduced significantly because most of the steam condenses on the ice. Flow through the intermediate deck doors then consists mainly of the air being recirculated by the air return fans (60,000 to 80,000 scfm) which is very small compared to the 1000 to 2000 square feet of flow area available. Even if this area were reduced substantially by a partial failure of the ice condenser doors late in a transient (the time that hydrogen is being released), there would be sufficient flow area to ensure that no adverse effects would occur because so little area is needed to ensure operability of the heat removal mechanisms in containment, and the energy being released to containment is considerably less than that of the earlier phases of blowdown.

To summarize our response to the two questions contained in the NRC letter:

1. We have performed an analysis of the response of the containment to an upper compartment global hydrogen burn at 6.5% hydrogen by volume. This analysis contains a number of conservatisms such as the hydrogen volume percentage, the assumed method of burning (as global deflagrations), the assumed burnout time for the compartment, limitation of the venting rate associated with the air return fans, and the position of ice condenser doors. The resulting differential pressure of 8.1 psid is considered to be an upper bound.

2. This conservative upper bound case has been examined in great detail in order to assess quantitatively the possible effects on containment structure and components. Using a very conservative model of the interaction between the flow of gasses and the fan behavior, it has been found that the containment air return fan motors will undergo motoring on the bus and act as generators for a short period of time, but the time duration and magnitude of current are not sufficient to cause tripping of the circuit breakers associated with the fans. Neither is the fan overspeed sufficient to cause any threat to the structural integrity of the fan assembly. Following the transient, the fans will resume normal operation. A quantitative assessment of the conservatism in this model, using a more realistic model of the ability of the fan to convert gas flow to electrical power, shows that the maximum power which could be extracted by the fan is far less than that required to cause electrical currents which would

activate the protective devices in the fan circuit. We conclude that upper compartment burning cannot trip the air return fan, regardless of the imposed differential pressure on the fan.

3. An independent assessment of the capability of the ice condenser doors to withstand differential pressure has been performed which has verified numbers previously reported to NRC by TVA. This capability is greater than the worst imposed differential pressure loading calculated using CLASIX, therefore the doors will not be adversely affected by hydrogen burning.

4. Further contradiction of previously reported Sandia results which show a preference for upper compartment burning and partial steam inerting of the lower compartment can be found in work reported by Reference 4.

References:

1. Duke Power Company, An Analysis of Hydrogen Control Measures at McGuire Nuclear Station, complete through Revision 12, March 29, 1985.

2. Meisel, J., Principles of Electromechanical Energy Conversion, McGraw-Hill, New York, NY, 1966, pp. 536-619.

3. "Safety Evaluation Report related to the operation of Catawba Nuclear Station, Units 1 and 2," NUREG-0954, Supplement No. 5, February, 1986.

4. NRC memo from D. L. Wigginton dated December 16, 1985, reporting the summary of a meeting held between NRC and IMEC on December 5, 1985.

5. Camp, et. al., MARCH-HECTR Analysis of Selected Accidents in an Ice Condenser Containment, NUREG/CR-3912, dated December, 1984.

6. Golding, E. W., The Generation of Electricity by Wind Power, The Philosophical Library, New York, NY, 1956.

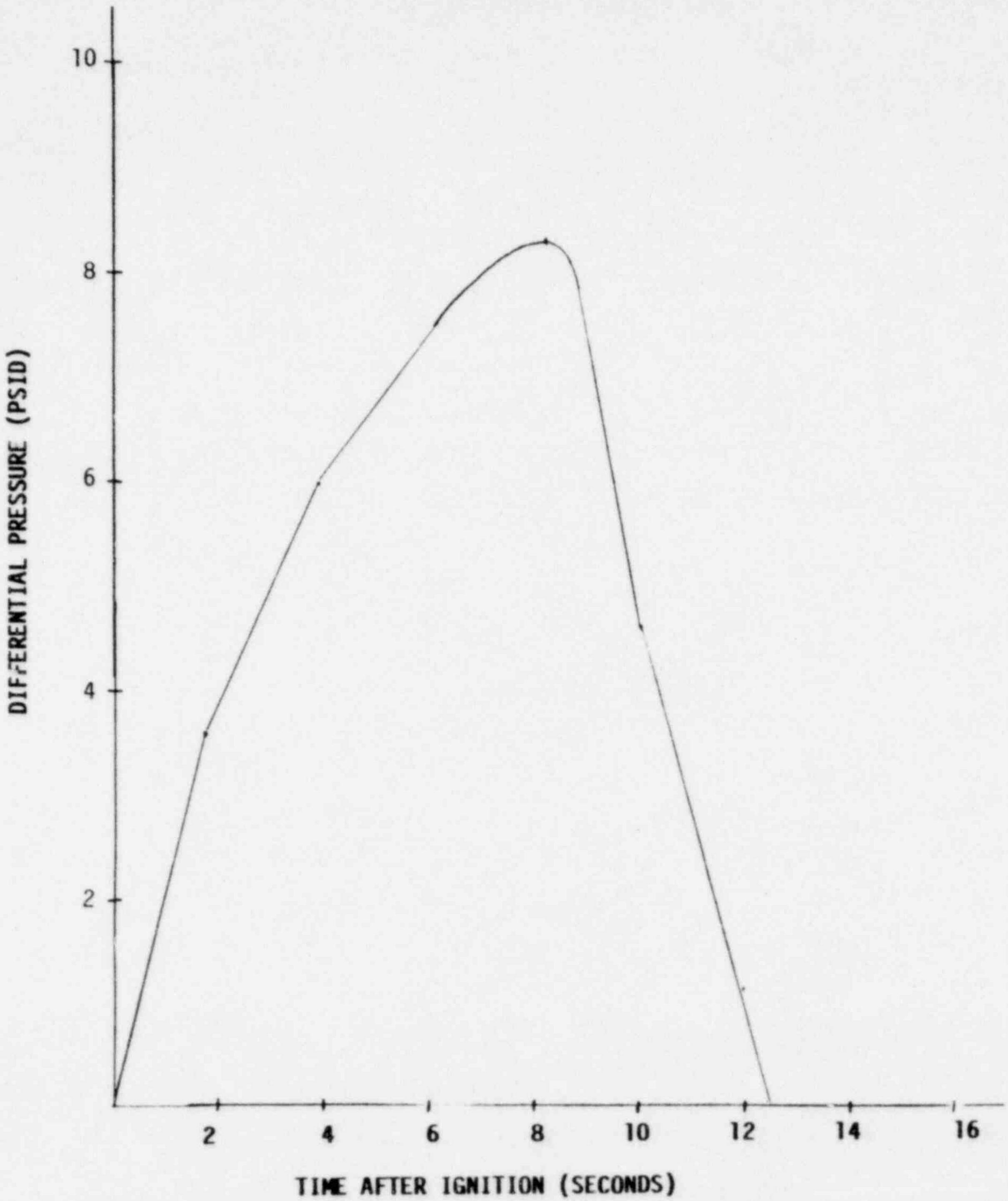


FIGURE 1

AIR RETURN FAN DIFFERENTIAL PRESSURE VS. TIME FOR GLOBAL HYDROGEN BURN IN UPPER COMPARTMENT

H_2 CONCENTRATION - 6.5%
FLAME SPEED - 12 ft/sec
BURN COMPLETION - 60%

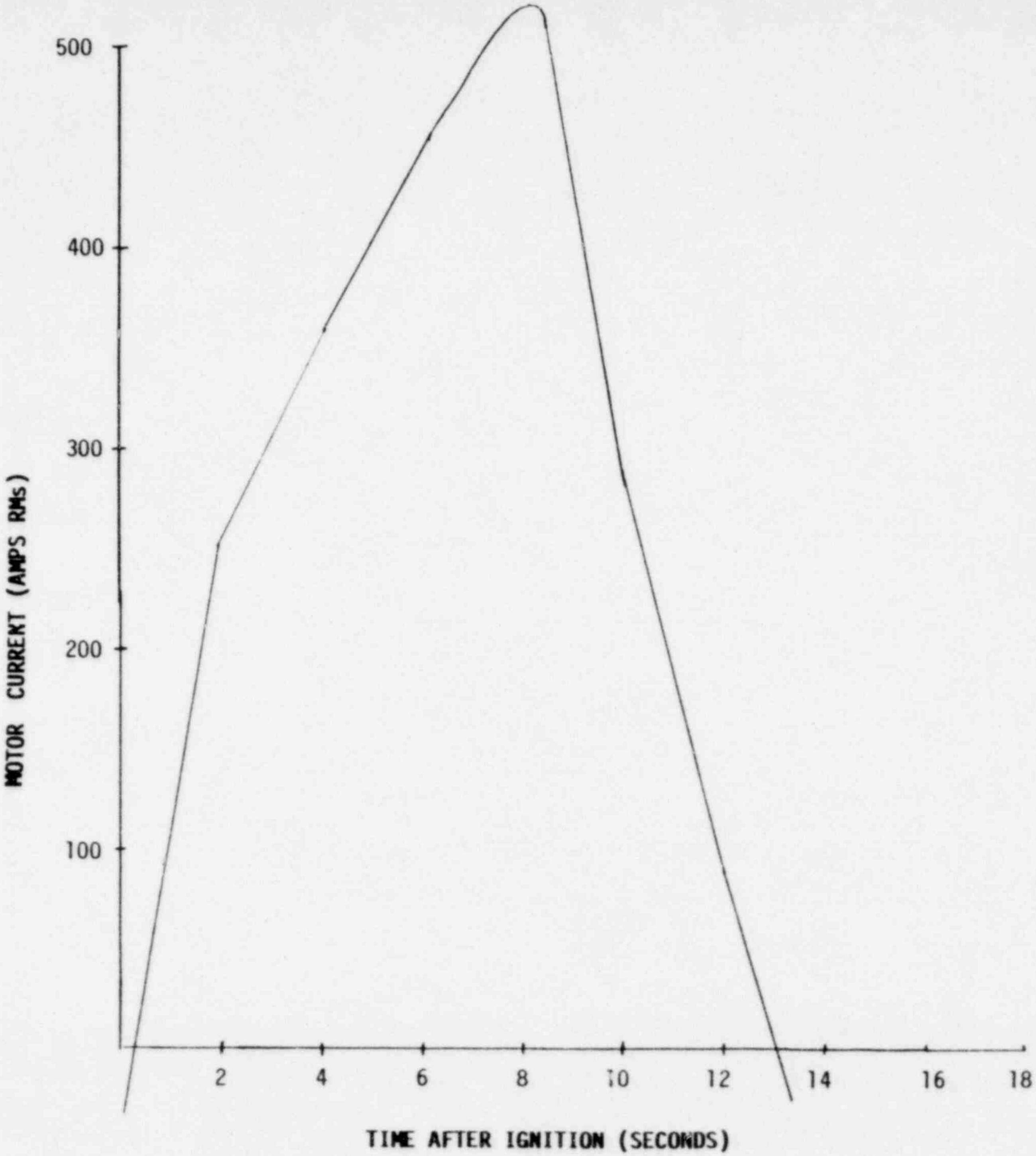


FIGURE 2

AIR RETURN FAN MOTOR CURRENT
VS. TIME FOR GLOBAL UPPER COMPARTMENT
BURN

ASSUMPTIONS:

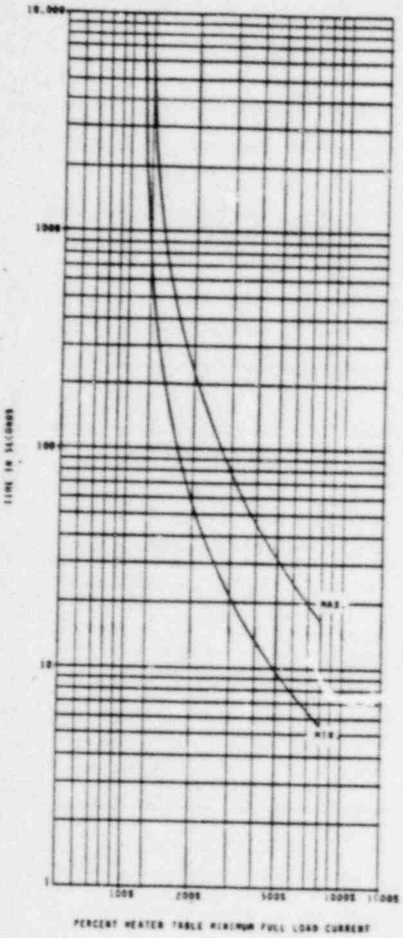
a. D/P ACROSS FAN CONVERTS TO TORQUE AT 100% EFFICIENCY

b. WINDAGE LOSSES NEGLECTED

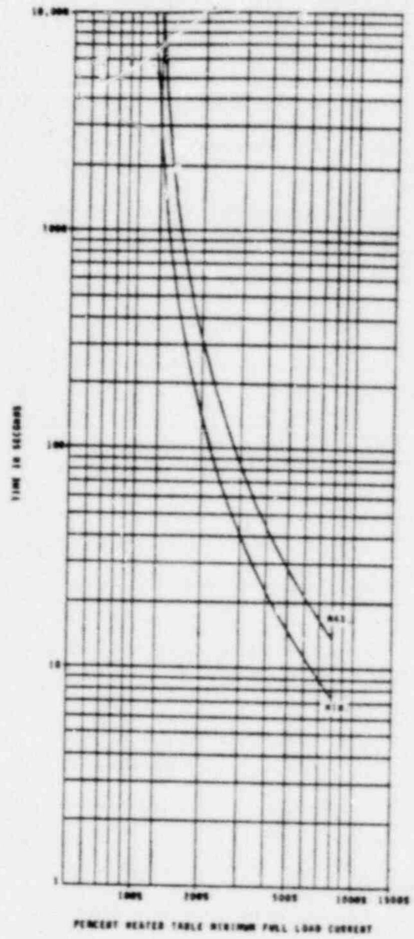
c. MOTOR CURRENT CURVE SYMMETRIC ABOUT SYNCHRONOUS SPEED FOR SMALL VALVES OF SLIP

Starter Overload Relay Heater Data
Type TM — Selection Of Heaters

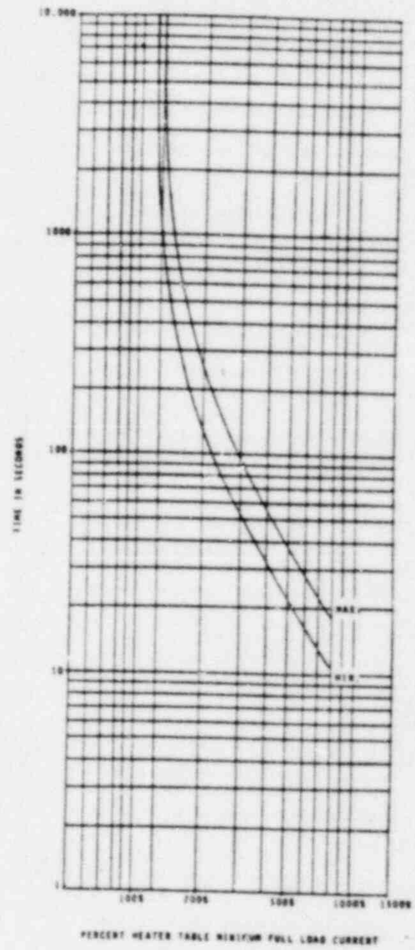
Section 11
A-C Starters, Contactors



Three Phase, 3 Element 40°C
Enclosed Type TM Size 00, 0, 1 & 2



Three Phase, 3 Element 40°C
Enclosed Type TM Size 3 & 4



Three Phase, 3 Element 40°C
Enclosed Type TM Size 5

The current rating of the heater is 125% of the minimum motor full-load current as shown in heater table. Current of this value continuously applied will ultimately trip relay within times shown on graph. As current increases above this value relay trip times decrease.

The time-current characteristics at 40°C ambient are for 3 heaters in the overload for the 100% current rating (125% min. full-load current) and only two heaters at higher percentage of current rating. This is underwriters 508 test procedure.

Heaters may be selected from pages 204-208 heater data sheets on the basis of actual full-load motor current.

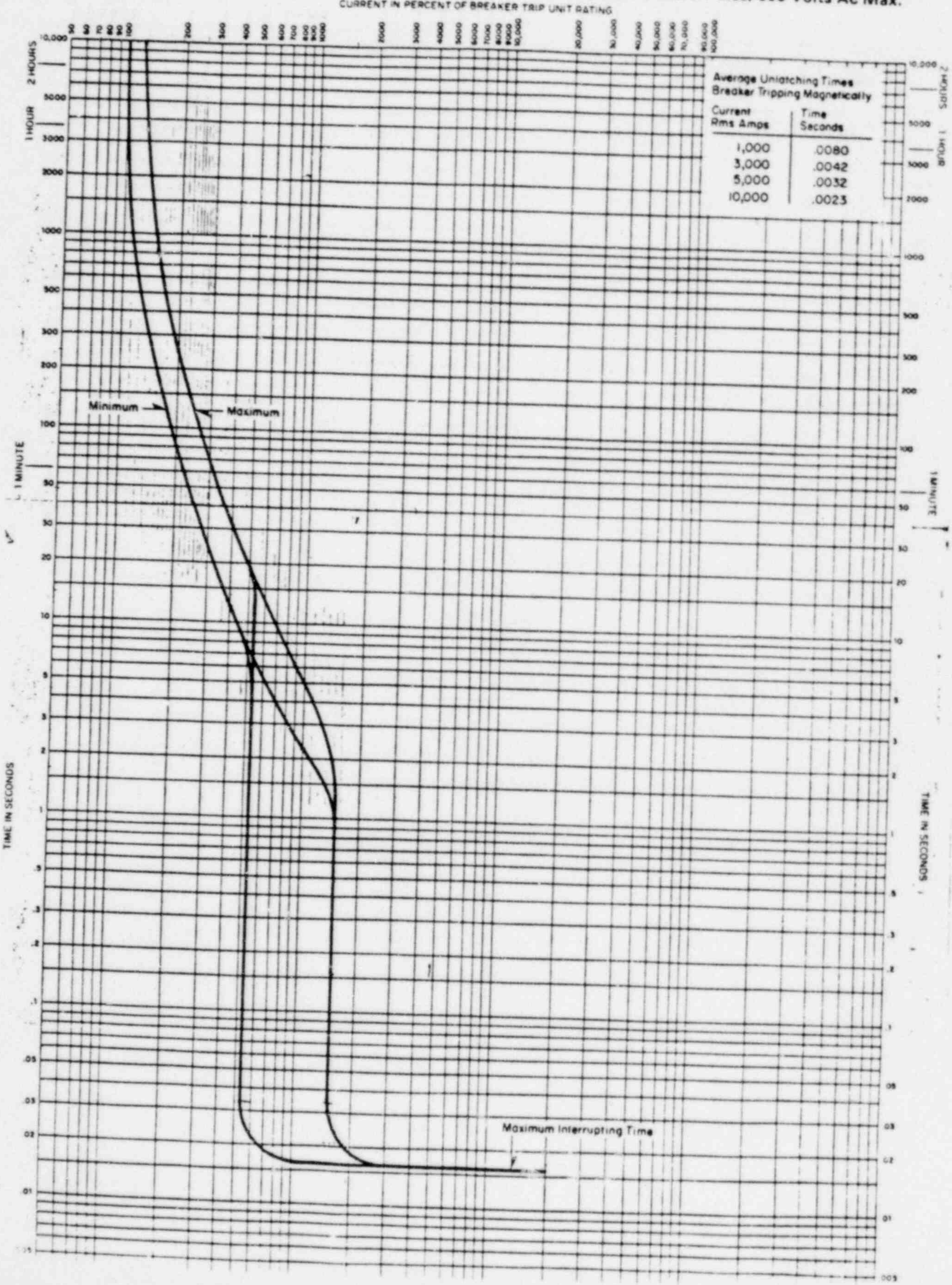
FIGURE 3

FIGURE 4

AB DE-ION® CIRCUIT BREAKERS

Types EB, EHB, FB, MARK 75® HFB

Type EB: 90-100 Amperes, 2 and 3 Poles, 240 Volts Ac Max
Type EHB: 90-100 Amperes, 2 and 3 Poles, 480 Volts Ac Max.
Type FB: 90-150 Amperes, 2 and 3 Poles, 600 Volts Ac Max.
Type HFB: 90-150 Amperes, 2 and 3 Poles, 600 Volts Ac Max.



Current Rms Amps	Time Seconds
1,000	.0080
3,000	.0042
5,000	.0032
10,000	.0023