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## SUMMARY

This report presents results from an experimental evaluation of a free-tip rotor. The evaluation included whirl tests and wind-tunnel tests up to advance ratios of 0.4. The free tip extended over the outer 5% of the rotor blade and included a passive mechanical controller whose output characteristics were varied. The controller configuration combined with the free-tip aerodynamics resulted in higher power requirements, because the tip's pitch angle was 5° to 10° greater than that of the inboard portion of the blade, and its pitching motion capability was considered to be inhibited by frictional forces. Recommendations are included for design features for a follow-on test.

## INTRODUCTION

Helicopter rotor-blade tips are the subjects of a great deal of research because the tip can affect many aspects of the helicopter flight characteristics. Performance, loads/vibration, and noise are especially susceptible to tip design. One tip design, the free tip, could primarily improve performance and might also, secondarily, improve loads and vibration.

The free tip (fig. 1, from ref. 1) is so called because the tip is free to pitch about an axis that is located upstream of the aerodynamic center (a.c.). With the added pitch degree of freedom and the a.c. being aft of the pitch axis, the tip weathervanes into the tip's relative wind to produce a moment balance about the pitch axis. Another device called a "controller" applies a pitch moment to the tip, thus enabling the tip to weathervane about a prescribed null point that results in a finite pitch moment and, consequently, in a finite lift. Therefore, the free tip could generate a lift level that is nearly constant as it goes around the azimuth.

An analytical investigation of the free tip was carried out to quantify a potential gain in rotor forward flight performance. This investigation, reported in reference 1, concluded that a 10% reduction in power could result if the free tip would eliminate the negative lift on the advancing tip.

Although the free tip seemed attractive on a performance-improvement basis, there was strong concern about the practicality of building it. Could the tip mass be balanced about the pitch axis at the 0.125 chord line? Could the pitch moment of inertia be low enough to allow reasonable dynamic response? Could a simple controller be built? These questions were addressed under a contracted preliminary design study which was reported in reference 2. The answers to these questions were that a tip could be mass-balanced about the 0.125 chord line with a resulting moment of inertia that would enable an undamped natural frequency of 7 to 8 per rev, based on its aerodynamic spring rate; moreover, a simple controller, functionally described in reference 1, could be easily built.

Given high-performance improvement potential and the feasibility of constructing the tip and controller, a wind-tunnel test of the design was undertaken. This report presents the results of those investigations and discusses the pitfalls of this particular implementation technique and how it could have caused the free tip to have been unable to move about its pitch axis.

## SYMBOLS

a.c. aerodynamic center

$C_{PCG}$ chordwise distance from pitch axis to center of gravity, m

$C_{T/\sigma}$ thrust coefficient, THRUST/$\rho S(\Omega R)^2$

c.g. center of gravity

$F_{ML}$ force due to mechanical losses, N

$M_c$ control moment, N·m

$M_{1P}$ pitching moment produced by first-harmonic flapping, N·m

m mass of tip, kg

R rotor radius, m

rpm revolutions per minute

r radius from pitch-axis centerline, m

S blade area, $m^2$

$V_o$ forward flight speed, m/sec

W centrifugal force, real or simulated, N

$\bar{x}$ propulsive-force coefficient, $-\mathrm{DRAG}/(2\rho V_o^2 S/\pi)$

$\ddot{\beta}$ flapping acceleration, rad/$\mathrm{sec}^2$

$\Delta\theta$ pitch angle between tip and inboard portion of blade, deg

$\Delta\dot{\theta}$ angular velocity of tip relative to inboard portion of blade, rad/sec

$\theta_{0.75}$ pitch angle at 0.75 R, deg

$\mu$ effective friction coefficient

$\rho$ density of air, kg/$m^3$

$\phi$ groove helical angle, deg

$\Omega R$ tip speed, m/sec

## MODEL DESCRIPTION

An existing four-bladed, 4.57-m diam, Mach-scaled model of the CH-47C rotor was selected for modification to the free-tip design. This rotor was modified by installing a steel pitch shaft at 13% chord to carry the free tip and to extend the rotor radius to 2.56 m (see fig. 2). A helical groove, cut into the steel shaft, accepted a guide pin which was inserted through the leading edge of the free tip and held in place by a retaining screw. This arrangement allowed the tip to pivot freely within the limits of the groove and still remain secured on the shaft. In addition, this arrangement causes the guide pin to carry the full centrifugal force loading with the potential of having very high friction between the pin and the groove. To minimize friction, the guide pin and groove were lubricated by an oven-bonded dry lubricant. Dry-lube bushings were applied to the pitch shaft at an inboard and an outboard location to react the lift and drag shear loads and the in-plane and out-of-plane bending moments. Provision was made to lock out the tip motion by removing the guide pins and replacing them with locking pins.

The pin in the helical groove is the tip's radial restraint and serves as the "controller," the device which produces a pitching moment about the pitching axis. The pin in the helical groove produces the pitching moment in the following manner. The guide pin transfers the tip's full centrifugal load to the helical groove in the shaft. Because of the groove's helical angle, a tangential force results and acts on the pin to drive the pin to the bottom of the groove. (This is physically equivalent to a weighted box wanting to slide down an inclined ramp.) That tangential force acting at some radial distance from the pitch axis produces the controller pitch moment. Expressing the controller pitch moment mathematically but excluding any frictional effects,

$$M_c = m\Omega^2 Rr \sin\phi \cos\phi$$

Including a simulation of friction yields the expression,

$$M_c = m\Omega^2 R\left[\sin\phi - \mu \frac{\Delta\dot{\theta}}{|\Delta\dot{\theta}|} \cos\phi\right] r \cos\phi$$

From this expression the control moment and friction are shown to be modulatable by changing tip mass for a given configuration ($\phi$, r, and $\mu$) and a given rotor rpm. Therefore, the tip was designed to allow small changes in tip mass as a means of changing the control moment from a baseline value reflecting the lightest tip mass with the c.g. on the pitch axis. To increase the control moment above the baseline value, mass was added equally about the pitch axis. To decrease the control moment below the baseline value necessitated removal of some nose balance weights which caused the c.g. to shift closer to the quarter-chord. Four tip masses representing four control-moment levels were tested: two with c.g.'s on the pitch axis and two with c.g.'s off the pitch axis. Theese tip masses are described below.

| Tip name | Weight, N | Location of c.g., $\Delta c/c$ | c.g. offset from 1/4 chord, $\Delta c/c$ |
|---|---|---|---|
| Heavy | 2.242 | 0.13 | 0.12 |
| Light | 1.527 | 0.13 | 0.12 |
| Extra light | 1.134 | 0.16 | 0.09 |
| No weights | 0.758 | 0.20 | 0.05 |

Testing the "extra-light" and "no-weight" configurations was limited to one forward flight speed.

The free tip had a V23010-158 airfoil with a 5.8% chord tab added to match the basic blade airfoil. The tip was constructed of Nomex core and magnesium spar covered with fiberglass. The upper surface had a 0.013 mm thick Mylar cover to prevent air transfer from the lower to the upper surface. The spar was provided with one 0.0065-m-diam tantalum balance weight in the nose and four 0.0048-m-diam holes symmetrically arranged about the pivot line. By inserting tantalum rods in these holes, the tip mass, inertia, and chordwise c.g. were varied.

The blade had a 0.171 m chord, a constant V23010-1.58 airfoil, and -9.45° of linear twist from center of rotation to the tip. The test stand was Boeing's dynamic rotor test stand which incorporates an electrical power supply and a six-component balance.

The complete rotor with free tip is shown in figure 3; figure 4 shows a close-up of one of the tips.

Instrumentation for the main blade consisted of six flap-bending gauges, two chord-bending gauges, and one torsion gauge placed along the blade span. Blade motion about the horizontal and vertical pins was continuously measured by transducers placed at the flap and lag hinges of the instrumented blade. The tip-pitch shaft, on one blade only, was provided with flap- and chord-bending gauges as safety-of-flight instrumentation. The angle of the tip relative to the main blade was measured by a Hall-effect device. This device uses a remote magnetic field to modulate an electric current through a semiconductor. The results from the strain gauges are not presented here but are included in reference 3.

## Bench Testing

Pull tests were performed on the controller to determine the output moment of the controller and the coefficient of friction when the guide pin was loaded to simulate the tip centrifugal force. The pull-test setup and the test results are shown in figure 5. Although there was considerable hysteresis in the pitch moment generated by the pull force, it was not considered significant since the real centrifugal force would be constant during the wind-tunnel test.

Using the linear portion of the curve in figure 5, the system mechanical losses can be calculated. These mechanical losses arise from numerous potential sources such as friction, elastic deformation of the antifriction coating, and mechanical misalignments. Presuming these losses are a function of the normal force of the guide pin on the groove surface, the following equation is derived. The summation of forces along the helical groove direction is as defined in figure 6 as

$$\sum F = W \sin \phi - F_{ML} - \frac{M_c}{r} \cos \phi = 0$$

where

$$F_{ML} = \mu \left[ W \cos \phi + \frac{M_c}{r} \sin \phi \right]$$

Rearranging and substituting in the definition of $F_{ML}$,

$$\mu \left[ W \cos \phi + \frac{M_c}{r} \sin \phi \right] = W \sin \phi - \frac{M_c}{r} \cos \phi$$

$$\mu = \frac{W \sin \phi - M_c/r \cos \phi}{W \cos \phi + M_c/r \sin \phi}$$

Quantitatively, the "mechanical loss" in terms of an effective friction coefficient is shown below to be 0.139:

$$\mu = \frac{1875 \sin 26° - (3.86 \cos 26°)/0.0063}{1875 \cos 26° + (3.86 \sin 26°)/0.0063} = 0.139$$

with a radius of 0.0063 m. If the radius $r$ is only 75% of the value above, the effective friction coefficient is drastically changed:

$$\mu = \frac{1875 \sin 26° - (3.86 \cos 26°)/[(0.0063)0.75]}{1875 \cos 26° + (3.86 \sin 26°)/[(0.0063)0.75]} = 0.043$$

Therefore, the effective friction coefficient is extremely sensitive to radius $r$. As a result, and because the actual radius of contact is unknown, the effective friction coefficient can only be qualitatively determined as 0.139 or less. In the design of this controller a static friction coefficient of 0.04 was expected, based on properties of the antifriction coating applied to both the pin and the groove surfaces.

## Whirl Hover Test

Whirl tests of the complete assembly were performed in the Boeing Vertol test cell. The test cell was designed for whirl testing for structural test purposes and did not have adequate ventilation to allow operating the rotor at lift levels that generate substantial downwash. Hence, collective pitch and rpm combinations were restricted. These restrictions limited checkout of the tips' ability to pitch freely at high-load conditions; however, these tests showed that the configuration was structurally sound. It was during these tests that the bending of the guide pin relative to the groove became evident, thus requiring reshaping for better pin-to-groove contact.

Figure 7 presents typical data from these whirl tests. This figure shows a large $\Delta\theta$ at $\theta_{0.75} = 0$, a result expected since the controller was driving the tip to produce a desired lift level even though the remainder of the blade may have been near zero lift. As $\theta_{0.75}$ increased, $\Delta\theta$ diminished and would have gone negative if $\theta_{0.75}$ had been increased a few more degrees. The rate of change of $\Delta\theta$ with $\theta_{0.75}$, $d(\Delta\theta)/d\theta_{0.75}$ would be expected to have a value close to -1.0 if this were a simple linear system without downwash effects and without interaction between the inboard section of blade and the tip. However, this was not the case. Why there are two $d(\Delta\theta)/d\theta_{0.75}$ slopes with a breakpoint near $\theta_{0.75} = 5°$ is not completely understood but, for evaluation of this design, a $d(\Delta\theta)/d\theta_{0.75}$ slope before $\theta_{0.75} = 5°$ is itself not significant because of the low rotor lift level associated with $\theta_{0.75} = 5°$. After $\theta_{0.75} = 5°$, $|d\Delta\theta/d\theta_{0.75}|$ is greater than 1.0 which suggests that $(dC_L/d\theta)_{TIP}$, and consequently $(dM/d\theta)_{TIP}$, was less at large $\Delta\theta$ than at small $\Delta\theta$. Physically, this is reasonable since a large vertical gap was produced between the tip and the inboard section at the high $\Delta\theta$, as illustrated in figure 4. With this gap, the tip would become more "three-dimensional" and its lift-loading capability would decrease at the aft end near the inboard edge. Consequently, the tip's total lift-loading capability was diminished and likewise $(dC_L/d\theta)_{TIP}$ would be less. With increased $\theta_{0.75}$, the vertical gap diminished, $(dC_L/d\theta)_{TIP}$ increased, and, therefore $|d\Delta\theta/d\theta_{0.75}|$ was greater than 1.0.

The major significance of the whirl test results were threefold.

1. The soundness of the structural design was demonstrated.

2. The tip had demonstrated the ability to pitch relative to the inboard section of the blade in a hover mode.

3. The tests showed a high probability of lower lift-generating capability by the tip when $\Delta\theta$ is large.

The key free tip operational characteristics, it response capability to flow-angle changes, was qualitatively checked using an air jet directed upward toward the tip. A diagram of the test setup and a typical tip response are presented in figure 8. There were no measurements of the jet velocity at the plane of the rotor but it was certainly not greater than 30 to 60 m/sec. The spread of the jet at the disk plane was about 30° of azimuth. The tip's fast initial response to the jet followed by a slower response after leaving the jet is probably a result of a difference in dynamic pressure and friction. With the tip in the jet, the tip tangential velocity ($\Omega R$) and the jet velocity add vectorially to give a higher velocity than when the blade has passed through the jet:

$$V = (\Omega R^2 + V_{JET}^2)^{1/2} \quad \text{in the jet}$$

$$V = \Omega R \quad \text{leaving the jet}$$

Since the jet velocity was 50% to 100% of $\Omega R$, the dynamic pressure is 20% to 40% greater with the blade in the jet than it is when the blade leaves the jet; the consequence is that the aerodynamic spring is stronger and the response faster. Also, friction levels might have been very high during the onset of the vertical velocity encounter and lower during and after penetration when the tip was pitching appreciably. Possibly the high-friction level was akin to a "static" friction situation, and the low-friction level was akin to a "dynamic" friction case. During entry, the high-friction level delayed pitch motion until a "breakout" moment level was reached. But, once the pitch motion did begin, the friction level suddenly decreased and the tip had more moment available for pitch acceleration. This moment was greater than it would have been had there been no delay to the pitching, and the greater moment resulted in high-pitch accelerations.

Driving the tip through the jet at full operating rpm (796 rpm) produced no tip pitch response. The lack of response was attributed to the very short time the tip was subjected to the jet, too short for the tip to react to it.

Based on the response of the tip to the jet at low rpm, it was decided to proceed with the forward velocity test.

## Effect of Airspeed

The free-tip rotor was tested over an advance ratio range of 0.2 to 0.4 and over a broad range of rotor-lift and propulsive-force levels. All the testing was done at a tip speed of 213 m/sec with zero first-harmonic flapping.

The most significant results from the testing were

1. The tip inclined itself at a nominal 10° nose-up angle from the inboard section of the blade and this nose-up attitude was relatively constant around the azimuth.

2. Since the tip was at a much higher pitch angle, the power required to drive the rotor was much higher at all forward speeds.

The speed-power polar, presented in figure 9, shows the high power consumption for typical helicopter lift and propulsive force values. The high power consumption was the direct result of the large $\Delta\theta$ angle which is also shown in figure 9. Although the tip's pitch angle shown is for an advance ratio of 0.3, it was typical for all advance ratios. Since the high control moment was felt to be the direct cause of the high $\Delta\theta$ and because of the lack of pitch variation around the azimuth, the tip mass was reduced in several steps. Keep in mind that changing the tip mass directly affected the magnitude of the control moment and the friction level as expressed by the formula

$$M_c = m\Omega^2 R\left[\sin\phi - \mu \frac{\Delta\dot{\theta}}{|\Delta\dot{\theta}|}\cos\phi\right] r \cos\phi$$

A typical result of tip-mass reductions on the tip motion is shown in figure 10. As shown, reducing the tip mass by 32% from the heavy to the light configuration produced no appreciable change to the main level but it did slightly increase the peak-to-peak motion. The most dramatic effect of changing the tip mass was noted with the extra-light and the no-weight configurations, for which both the mean level and peak-to-peak amplitudes of $\Delta\theta$ were significantly altered. In fact, the dynamic behavior of the light configuration appeared to be greatly amplified by reducing the mass to the extra-light and no-weight configurations. The effect of reduced mass on the pitch response is shown in figure 11, which presents peak-to-peak amplitudes and mean values for $\Delta\theta$ as affected by tip-mass changes. Both the peak-to-peak amplitudes and the mean values indicate that when the mass was reduced below a certain value, the tip appeared to "break free," and more pitching activity resulted. This breaking-free characteristic suggests that effective friction was a major inhibitor to the tip pitching freely in response to external perturbations.

Another aspect which must be considered is the effect of the c.g. of the extra-light and no-weight tips being off the pitch axis. This could be a significant effect when blade-flapping accelerations induce pitching moments that are large compared with those generated by the controller or by aerodynamics. Quantifying this effect for the two configurations in question is determined based on the first-harmonic component of the flapping acceleration. The first harmonic was selected as the key parameter since tip-angle variation in figure 10 is predominately first harmonic. The relevant flapping accelerations for the two configurations are as follows:

No-weight: $\ddot{\beta}_{1P} = 0.006\Omega^2 \cos \Omega t + 0.051\Omega^2 \sin \Omega t$

Extra-light: $\ddot{\beta}_{1P} = 0.019\Omega^2 \cos \Omega t + 0.057\Omega^2 \sin \Omega t$

The pitching moment produced by flapping ($M_{1P}$), is formulated as

$$M_{1P} = mC_{PCG}R\beta_{1P}$$

For the extra-light configuration,

$$M_{1P} = (0.1157)(0.0051)(2.56)(0.006 \cos \Omega t + 0.051 \sin \Omega t)(83.356)^2/57.3$$

Neglecting the $\cos \Omega t$ term,

$$M_{1P} = 0.0093 \sin \Omega t, \text{ N}\cdot\text{m}$$

For the no-weight configuration,

$$M_{1P} = (0.0773)(0.012)(2.56)(0.019 \cos \Omega t + 0.057 \sin \Omega t)(83.356)^2/57.3$$

Neglecting the $\cos \Omega t$ term,

$$M_{1P} = 0.0163 \sin \Omega t, \text{ N}\cdot\text{m}$$

These flapping-induced pitching moments are quite small compared with even the pitching moment generated by frictional forces in the controller. For example, using a friction coefficient of 0.04 and using a radius of 75% of 0.0063, the resulting moment would be about 0.389 N·m. Therefore, for these cases, having the center of gravity off the pitch axis had little effect on the 1P pitching motion that the tips experienced. Furthermore, the activity demonstrated by these tips was not inertially induced but probably aerodynamically induced.

Post-test analysis of this test yields qualitative explanations, but cannot give concrete, definitive explanations for all behavioral characteristics of the tip. Additional tests with considerably more instrumentation would be needed to make such explanations. Additional tests with this specific configuration is unlikely because the large mean value of $\Delta\theta$ resulted in high power consumption. Therefore, the oscillatory pitch motion of the tip about such a large mean value is of interest only as a potential but weak aid in tip redesign. The qualitative explanations for the tip behavior that can be drawn are as follows:

1. There were some pitch oscillations around the azimuth of the heavy-weight tip and the light-weight tip; thus, they were not frozen in place.

2. The limited magnitude of these oscillations indicates a likelihood that the tips were heavily damped or that their motion was restricted in some other way. Because the extra-light configuration exhibited markedly greater oscillation amplitude, the aerodynamic forcing function should have also perturbed the light tip nearly as much; however, it did not. This suggests that friction was the dominant pitch-motion inhibitor of the heavy and light tips.

3. The greater pitch of the tips over the azimuthal region between $\psi = 40°$ and 160° indicates that the tips were doing what they were designed to do, that is, to pitch up on the advancing side.

## Methods for Improving Free-Tip Rotor Performance

These observations of the pitching characteristics of the free tips have led to the following guidelines for follow-on configurations and testing:

1. The span of the free tip should be larger. This would make it possible to utilize the lifting potential of inboard stations to increase the weathervaning capability.

2. The tip should be swept aft. This would move the aerodynamic center farther downstream of the pitch axis and would also increase the weathervaning capability. The center of gravity of the tip should still be located on the pitch axis to minimize unwanted inertial moments.

3. The centrifugal force of the tip should not be carried across surfaces in sliding contact. This would minimize the tip's susceptibility to high friction losses.

4. The controller should be designed so the control moment could be adjusted from 0 to 100% of some design value. This would make it possible to evaluate the design over a broad range of tip aerodynamic loading.

A redesign of the tips incorporating these changes would be much more likely to produce the improved performance that is analytically predicted for the free-tip design.

# CONCLUSIONS

Based on the results of this test, the following conclusions are drawn:

1. The high power consumption of the free-tip rotor was due to the high mean pitch angle of the tip.

2. Tip motion about the spar was probably inhibited by large centrifugally induced frictional forces.

3. Testing neither proved nor disproved the feasibility of the free-tip design. Testing did eliminate the type of controller used from further consideration.

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3. McVeigh, M. A.; Rosenstein, H.; Bartie, K.; and McHugh, F. J.: Investigation of a Rotor System Incorporating a Constant Lift Tip. NASA CR-166361, 1981.

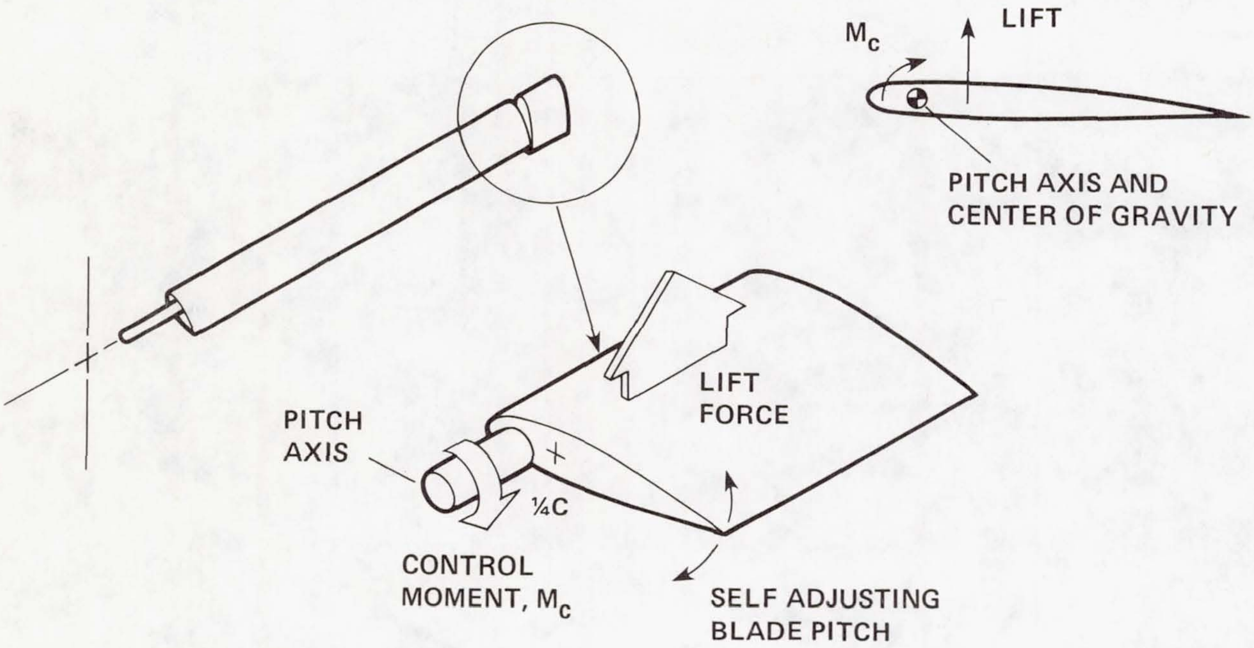



Figure 1.- The free-tip rotor.

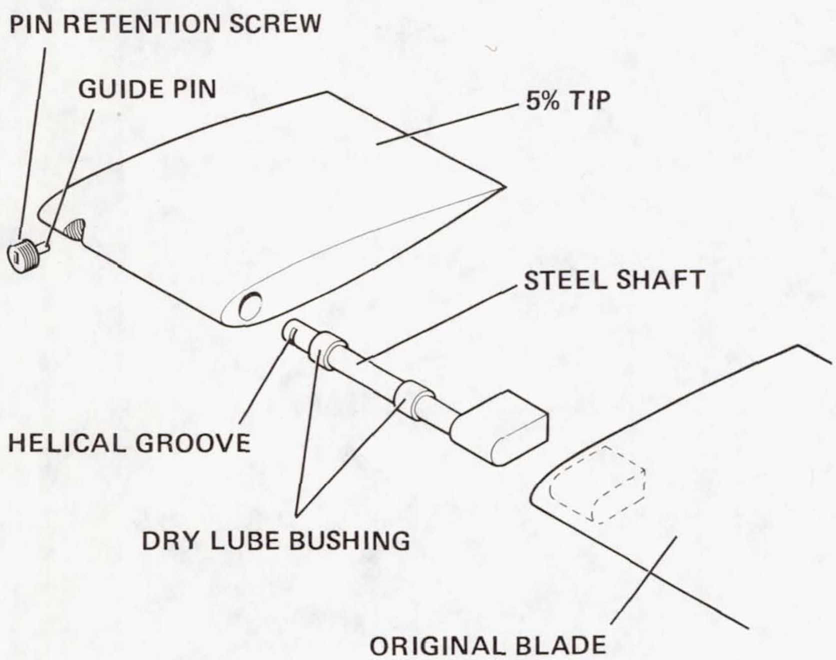



Figure 2.- Tip and controller hardware.

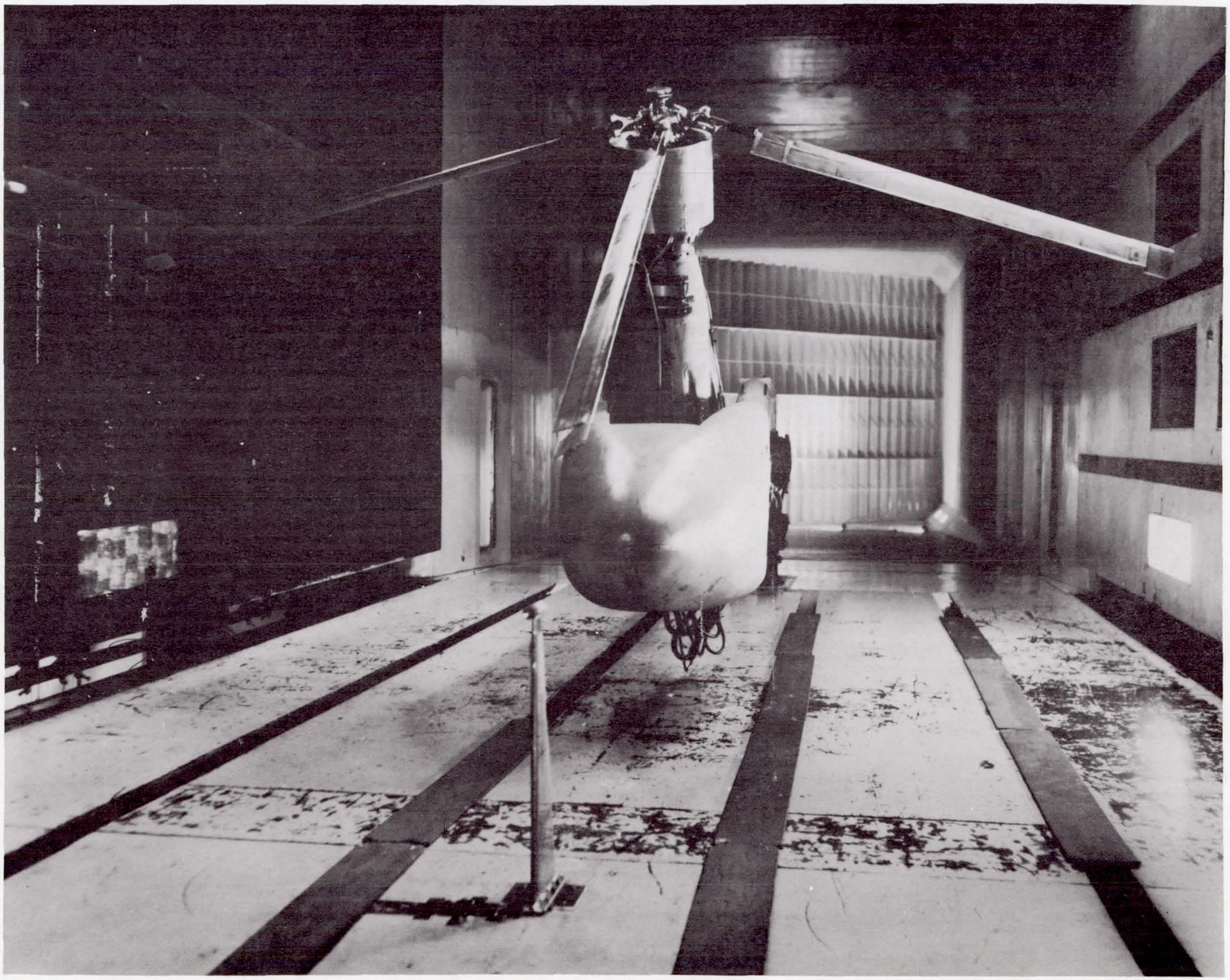


Figure 3.- The free-tip model in the Boeing Vertol wind tunnel.

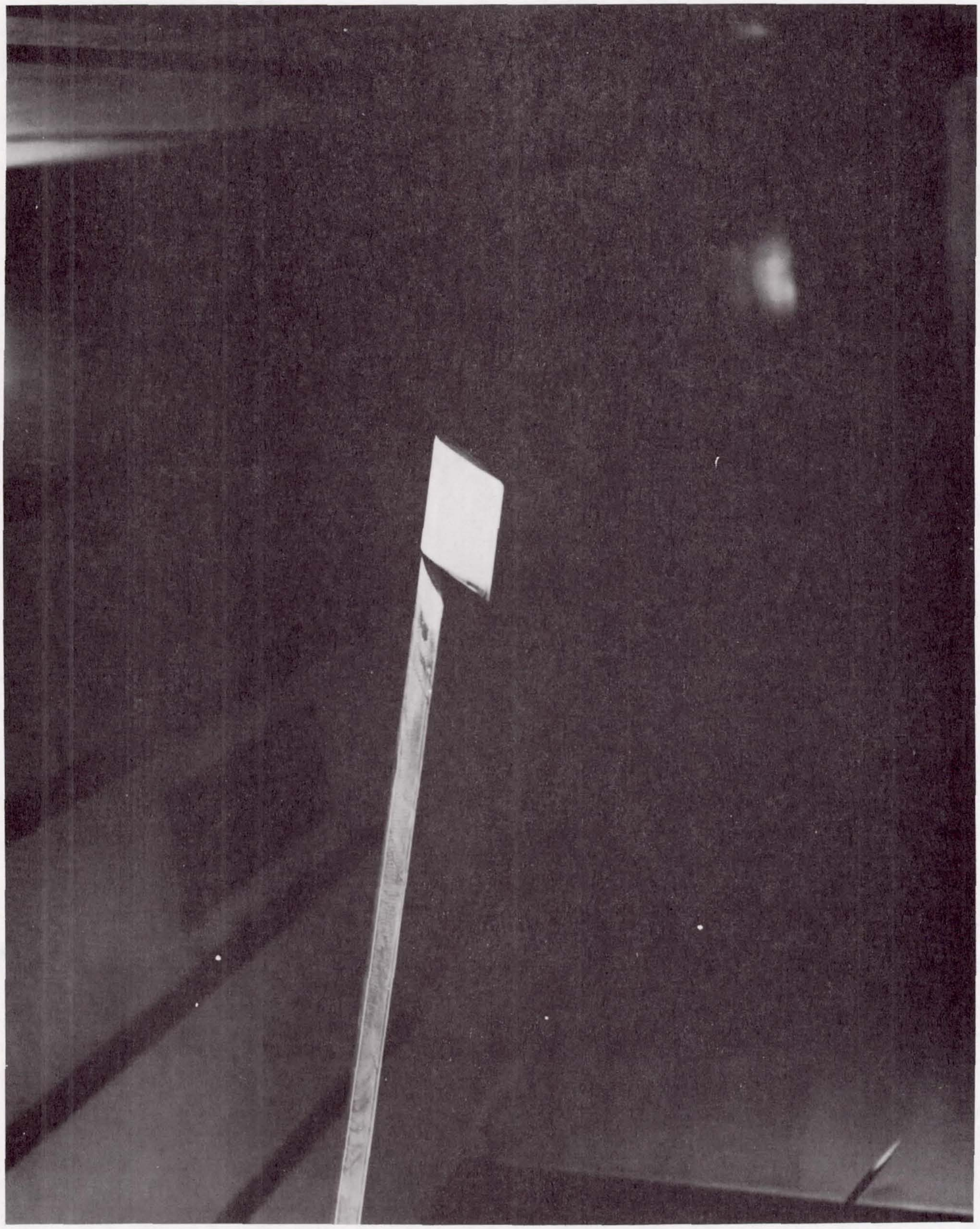
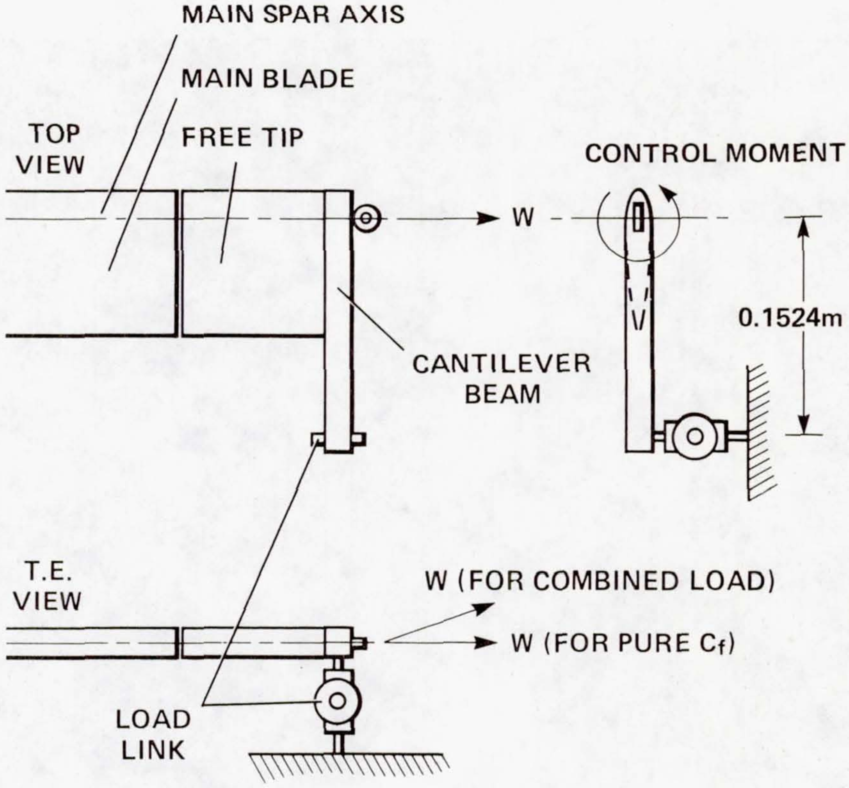


Figure 4.- Free tip.



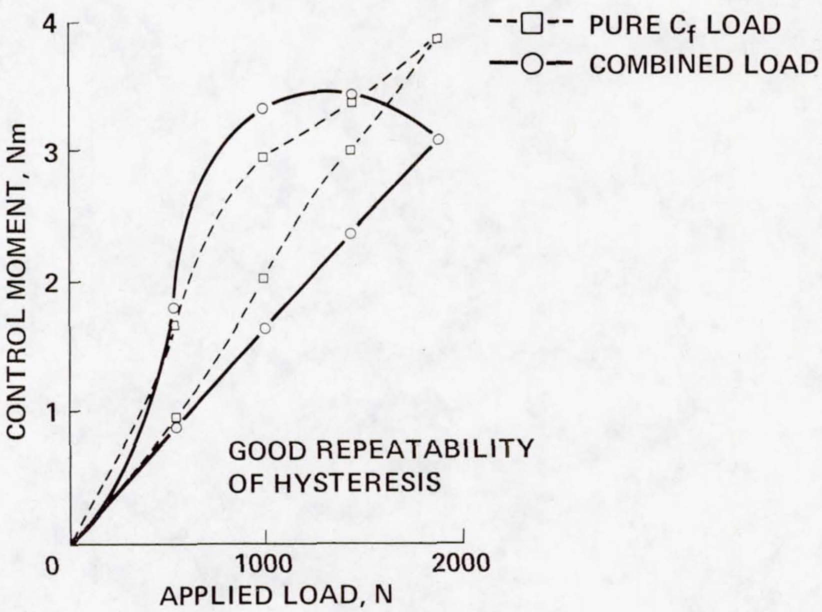




Figure 5.- Control moment resulting from static pull test.

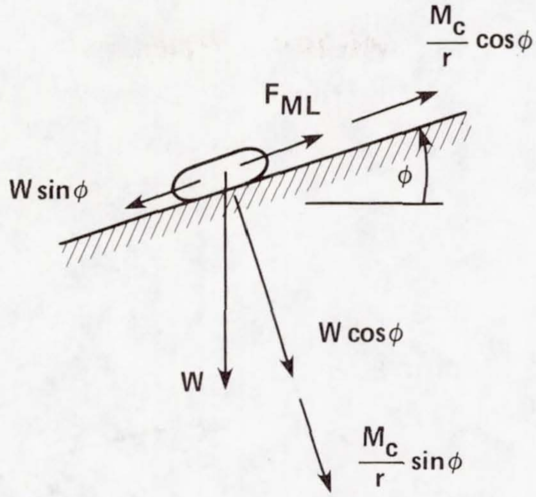



Figure 6.- Guide pin on outboard surface of helical groove.

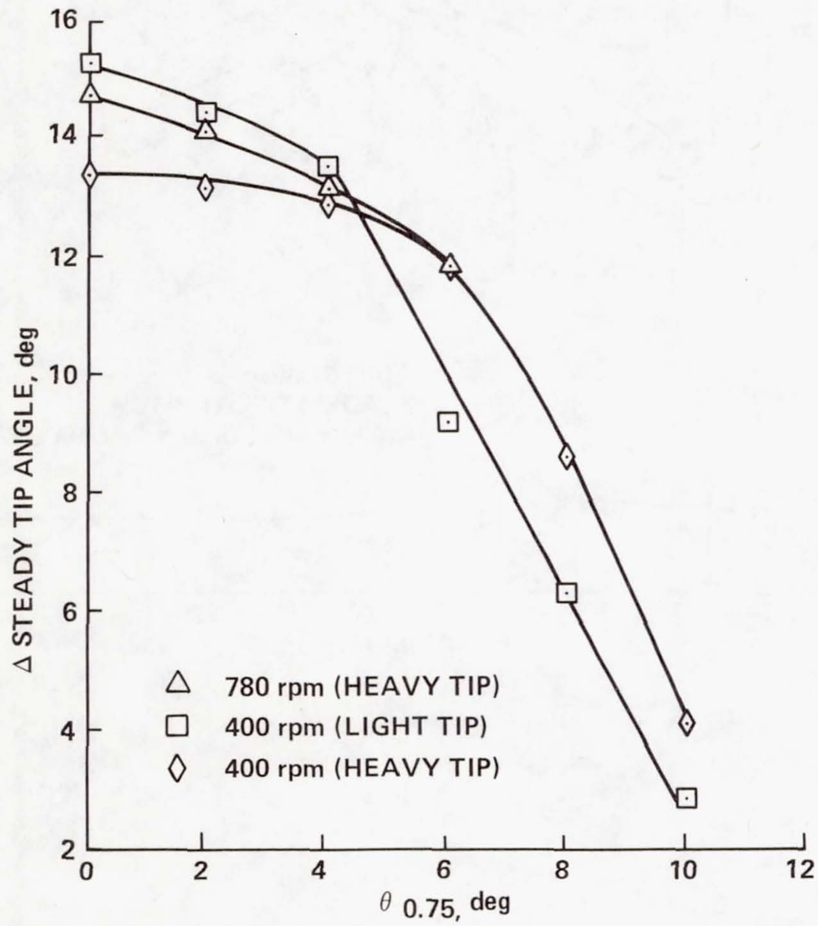



Figure 7.- Tip deflection relative to inboard portion of blade.

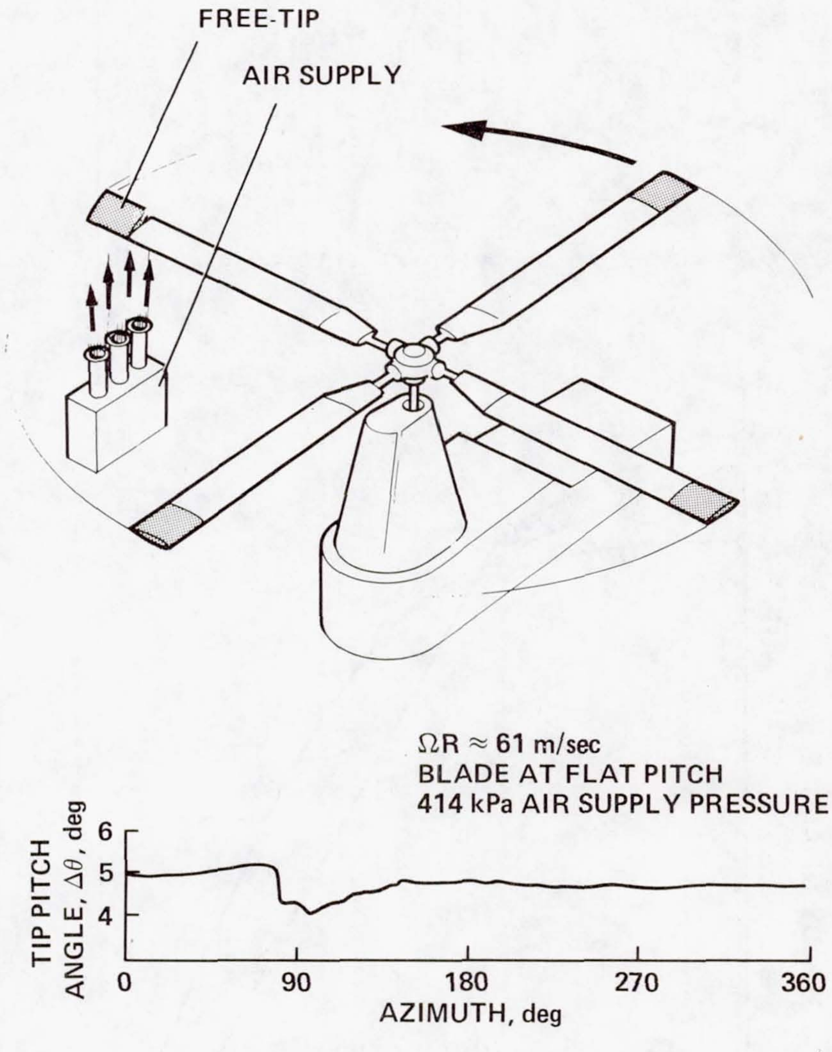



Figure 8.- Response of tip to vertical jet.

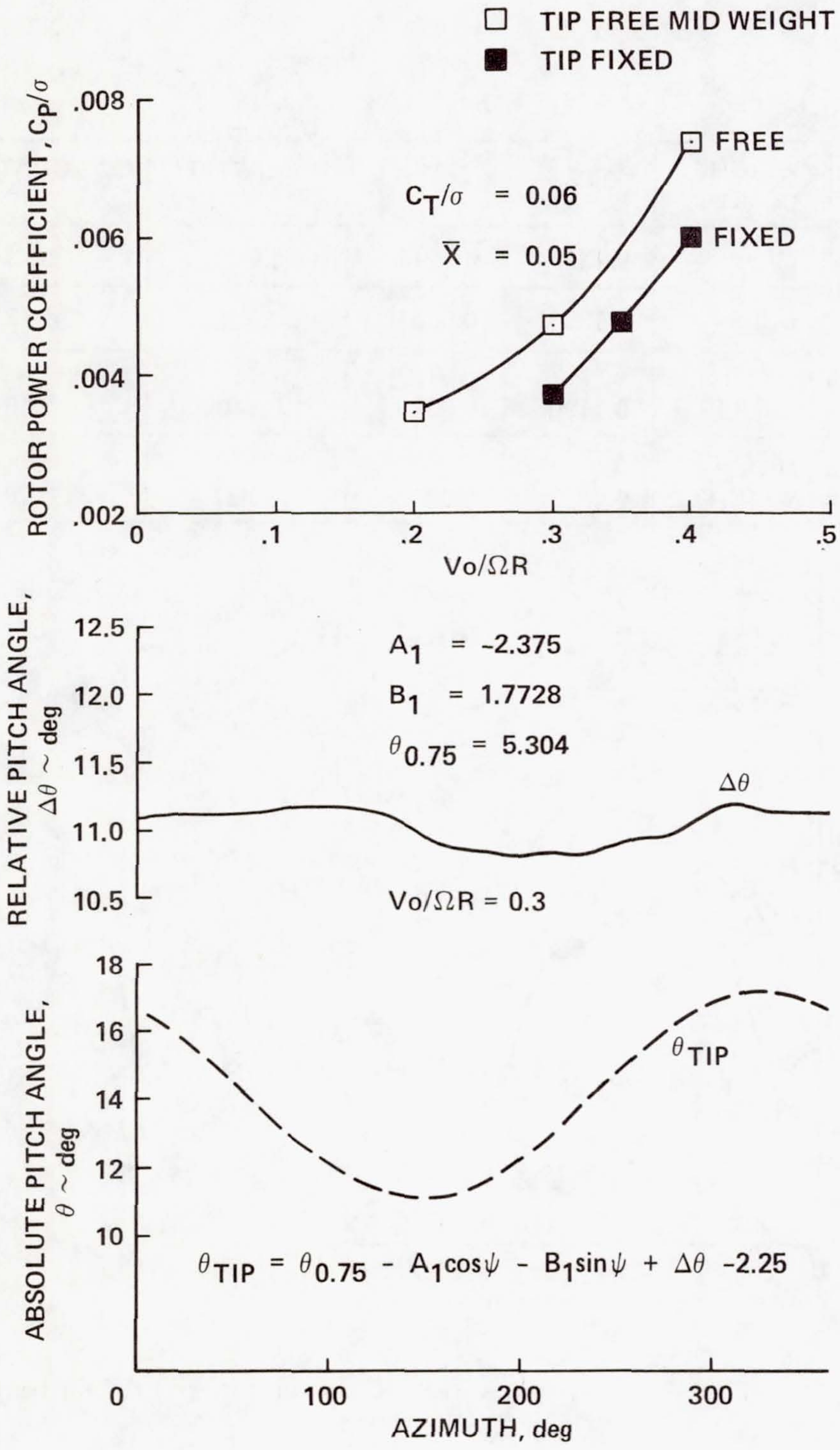



Figure 9.- Speed-power polar and tip pitch-angle response in forward flight.

| WEIGHT COND. | $C_T/\sigma$ | $\overline{X}$ | RELATIVE TIP MASS | CG LOCATION, $\Delta C/C$ |
|---|---|---|---|---|
| MID | 0.12144 | 0.02147 | 1.000 | 0.13 |
| LIGHT | 0.1203 | 0.03315 | 0.681 | 0.13 |
| EXTRA LIGHT | 0.1199 | 0.0343 | 0.510 | 0.16 |
| NO WEIGHTS | 0.1203 | 0.0356 | 0.337 | 0.20 |

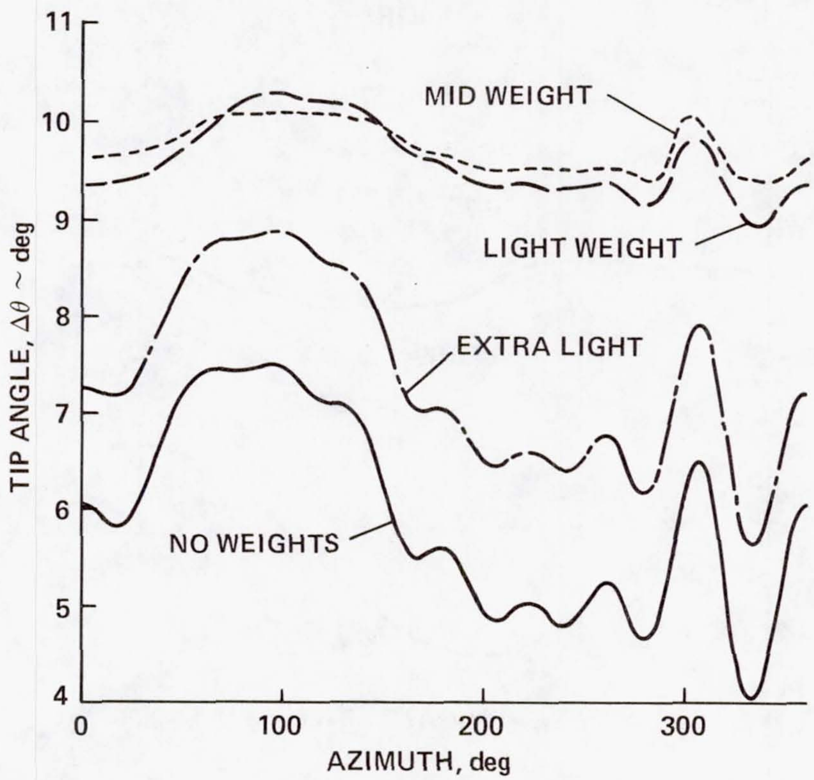



Figure 10.- Effect of the tip-mass reduction on tip-pitch angle response at $V_o/\Omega R = 0.3$.

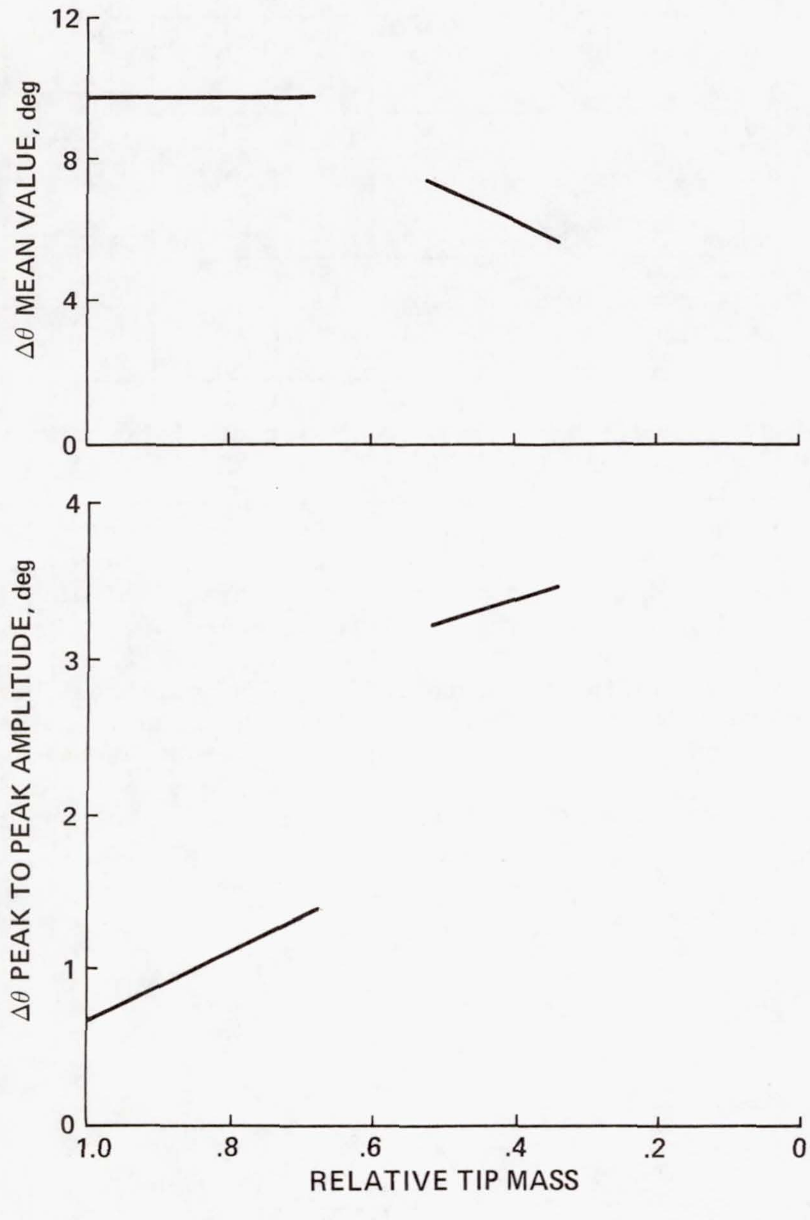



Figure 11.- Influence of mass on pitching motion of the tip.

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15. Supplementary Notes

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16. Abstract


This report presents results from an experimental evaluation of a free-tip rotor. The evaluation included whirl tests and wind-tunnel tests up to advance ratios of 0.4. The free tip extended over the outer 5% of the rotor blade and included a passive mechanical controller whose output characteristics were varied. The controller configuration combined with the free-tip aerodynamics resulted in higher power requirements, because the tip's pitch angle was 5° to 10° greater than that of the inboard portion of the blade, and its pitching motion capability was considered to be inhibited by frictional forces. Recommendations are included for design features for a follow-on test.




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