Design of Hybrid Passive and Active Mechanisms for Control of Insect-Scale Flapping-Wing Robots

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Design of Hybrid Passive and Active Mechanisms for Control of Insect-Scale Flapping-Wing Robots

A dissertation presented
by
Zhi Ern Teoh
to
The School of Engineering and Applied Sciences
in partial fulfillment of the requirements
for the degree of
Doctor of Philosophy
in the subject of
Engineering Sciences

Harvard University
Cambridge, Massachusetts
August 2015
Design of Hybrid Passive and Active Mechanisms for Control of Insect-Scale Flapping-Wing Robots

Abstract

Flying insects exhibit a remarkable ability to fly in environments that are small, cluttered and highly dynamic. Inspired by these animals, scientists have made great strides in understanding the aerodynamic mechanisms behind insect-scale flapping-wing flight. By applying these mechanisms together with recent advances in meso-scale fabrication techniques, engineers built an insect-scale flapping-wing robot and demonstrated hover by actively controlling the robot about its roll and pitch axes. The robot, however, lacked control over its yaw axis preventing control over its heading angle.

In this thesis, we show that the roll and pitch axes of a single actuator insect-scale flapping-wing robot can also be passively stabilized by the addition of a pair of aerodynamic dampers. We develop design guidelines for these dampers, showing that the previously unstable robot with the addition of the dampers is able to perform stable vertical flights and altitude control. To address the lack of yaw control, we develop a yaw torque generating mechanism inspired by the fruit fly wing hinge. We present the development of this mechanism in three stages: from the conceptual stage, to the torque measurement stage and finally to a hover capable stage. We show that the robot is able to generate sufficient yaw torque enabling the robot to transition from hover to heading control maneuvers.
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Acknowledgments

I have many people to thank for helping me through this journey.

To my advisor Professor Robert Wood for creating a lab that is a playground of wonder and fun. Thanks Rob for your advice and tremendous support during my time here.

To members and alumni of the Harvard Microrobotics Laboratory, thanks for playing foosball with me and for being wonderful people to work with.

To Kevin Ma, thanks for teaching me almost all I know about how to build small robots. To Professor Pakpong Chirarattananon (CityU), thanks for your help in adapting your controller to my robot. To Farrell Helbling, thanks for help in editing this thesis and buying cookies for the defense.

To members of the Harvard Graduate Christian Community and Hope Fellowship Church, thank you for walking with me through the ups and downs over the last four years.

Lastly and most importantly, to my family who have prayed for me and encouraged me when my robots blew up or did not work; who had the wisdom to know when to prod and when to let me be.

"My son, beware of anything beyond these. Of making many books there is no end, and much study is a weariness of the flesh." - Ecclesiastes 12:12

"It is the glory of God to conceal things, but the glory of kings is to search things out." - Proverbs 25:2
Chapter 1

Introduction

Flying creatures have always inspired man to take to the skies. The dream of flight drove the Wright brothers to study birds and were the first to create a vehicle that could carry a man and was heavier than air. Since then, man has built aircraft capable of traversing distances of ever increasing length; from crossing kilometers, to tens of kilometers to crossing entire oceans and finally to escaping Earth itself. At the other end of the flight distance spectrum, engineers have also tried to shrink the size of flying vehicles to enable flight in small, highly dynamic and cluttered environments. With the advent of cheap and light-weight inertial measurement units together with high energy density lithium polymer batteries, the creation of small fixed-wing aircraft [27] to propellor quadcopters was made possible. The smallest quadcopters can now fit into the palm of your hand enabling flight in spaces at least as small as your hand. Even though small quadcopters can fly in small spaces, when this space becomes cluttered and dynamic, the rotor interaction with the obstacles and its wake can lead to instabilities [28]. Other aircraft take inspiration from biology mimicking flapping-
wing flight in animals such as seagulls [16] and hummingbirds [26]. Even though flapping low aspect ratio wings is aerodynamically less efficient than rotating low aspect ratio wings, the benefit of flapping wings lies in “the potential for extreme maneuverability and robustness” [31].

The class of flying vehicles focused in this thesis is called “pico” air vehicles [59]. These vehicles are defined to have a maximum takeoff mass of 500 mg and a maximum dimension of 5 cm [59]. Many flying insects fall within this categorization hence their morphology is used as design start points for the vehicles described here. As we try to reduce the size of aircraft down to the insect-scale, we no longer have the luxury of using rotary motors, bearings and pin joints due to the increasingly dominating effects of friction. To address the problem of actuation and articulation in insect-scale flying robots, the Micromechanical Flying Insect (MFI) project developed high power density bending beam piezoelectric bimorph actuators to replace rotary motors and flexure based transmissions to replace bearings and pin joints [61, 40]. These foundational technologies enabled the Harvard Microrobotic Fly (HMF) to perform the first constrained flight of an insect-scale flapping-wing vehicle in 2007 [56]. To enable unconstrained flight, two control actuators were added to the ground links of the HMF transmission to produce roll torques by generating wing stroke asymmetries across the robot. This robot was named the RoboBee and in 2012 demonstrated unconstrained vertical flight [19]. To achieve unconstrained hover, an alternative design split the HMF into half, where each half had its own actuator to drive a wing. Having a total of two actuators, the robot was named the Dual Actuator Bee (DAB) and in combination of an energy based flight controller demonstrated hover
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Figure 1.1: Axes convention of a generic flapping-wing robot. Pitch, roll and yaw axes define the robot’s orientation in space. Simplified wing kinematics represented by wing stroke angle $\phi$ and wing rotation angle $\alpha$.

Figure 1.2: Heading angle $\sigma$ definition.

and transitions into lateral maneuvers in 2013 [32]. The morphology of these robots are similar consisting of three main components: they are the robot’s body and the two wings attached to the body. The body’s orientation in space is described by angles about three axes: pitch, roll and yaw (Fig. 1.1). When these robots are flown in the flight arena, the heading angle is defined to be to angle between the inertial $y$-axis and the projection of the robot body’s roll axis (Fig. 1.2).

The HMF could generate enough thrust to lift itself off the ground and by biasing
Chapter 1: Introduction

its mean wing stroke away from its center of mass was able to generate pitch torques [56, 37]. However in order to achieve hover, the HMF also needs to control its orientation about the roll axis. The RoboBee augmented the HMF design by adding a control actuator on each side of the HMF’s transmission. By deflecting the control actuator, asymmetric wing strokes were generated across the robot creating roll torques for control. As a result, the RoboBee was able to perform unconstrained vertical flights [19]. Unfortunately the RoboBee was unable to achieve hover which suggest that a combination of insufficient torque and coupling among the various torque generating mechanisms prevented the RoboBee from hovering. The DAB, on the other hand, split the HMF design in half instead of adding control actuators, combining the roles of power and control into a single actuator that drove a wing on each half of the DAB [33]. The DAB could generate sufficient torque about the roll and pitch axes enabling the first controlled flights of an insect-scale flapping-wing robot [32]. Though both the RoboBee and the DAB generated torque about the roll and pitch axes, both robots could not generate sufficient torque about the yaw axis. As we progress from hovering to maneuvers in small, cluttered and dynamic environments, control of the yaw-axis is important for the integration of sensors like gyroscopes, proximity sensors and cameras. Without yaw control, the robot’s heading angle cannot be controlled making the problem of localization more challenging.

We have shown that by modifying the HMF design by adding actuators, the roll and pitch degrees of freedom can be actively controlled to enable unconstrained vertical and hover flight. In this thesis, instead of adding actuators to the HMF to control pitch and roll, we explore how the addition of a pair of passive aerodynamic
dampers stabilize the HMF about its roll and pitch axes. We also address the problem of yaw control by modifying the DAB design to incorporate a yaw torque generating mechanism inspired by a reduced order model of the fruit fly wing hinge.

1.1 Contributions and Chapter Organization

In chapter 2, we explore the passive aspect of this work by designing aerodynamic dampers to passively stabilize the HMF about its roll and pitch axes. We create a simple model that is representative of the pitch and roll dynamics to draw out design rules for the dampers that modifies the robot’s body dynamics to get passive upright stability. Aerodynamic dampers were fabricated and mounted onto the HMF. Stable vertical flights were demonstrated, something the original HMF could not do due to inherent instabilities in the robot’s pitch and roll axes. Altitude control about three target heights was also demonstrated. These results represent the first glimpse of the possibility of controlled flight for insect-scale flapping-wing robots.

One major problem observed during these flights was the interaction of the flapping-wing downwash with the dampers resulting in excessive spinning about the yaw axis. This prevented the robot from achieving sustained hovering flight during the altitude control experiments as the spinning about the yaw axis would cause the robot to rapidly move out of the control volume in the flight arena [45].

In chapter 3, we begin to expand the design space of active mechanisms to address the problem of yaw torque generation by designing a control mechanism inspired by the fruit fly *drosophila*. Wing kinematic data of free flying fruit flies suggest that their wing bases act both as a brake to prevent their wing from over rotating and a control
that redirects aerodynamic drag force to create yaw torque [3, 39]. We synthesize a physical prototype of the bio-inspired control mechanism that couples the wing hinges of the left and right sides of the robot and show that we can indirectly control the mid-stroke Angle-of-Attack (AoA) of the robot’s wings. We show that the robot has potential to generate yaw torques by inducing bilaterally opposite asymmetries in the AoA of the up-stroke versus the down-stroke. To generate roll torques, we extend the control mechanism’s functionality by oscillating the control input creating asymmetries of the AoA and wing stroke angles across the robot [46].

In chapter 4, we modify the design of the prototype presented in chapter 3 to enable the attachment of the robot onto a single-axis torque sensor. We measure torques about the robot’s pitch, roll and yaw axes and show that the robot can generate sufficient amounts of torque for attitude control. Due to expected decreases in inertial as well as aerodynamic loads in the control transmission, the size of the control actuator was reduced to enable a flight weight design of the single power actuator single control actuator scheme. Unfortunately, due to challenging manufacturing tolerances imposed by the coupled nature of the design, the coupling of roll and yaw torque production made hovering flights difficult. Nonetheless, yaw torque measurements indicated that the control transmission could generate yaw torque [47].

A major design pivot of the robot was needed to demonstrate hover and heading control in an insect-scale flapping-wing robot. In chapter 5, we combine the bio-inspired control mechanism with the power transmission of the DAB. To eliminate the coupling of the left and right sides of the robot, we split the robot into left and right halves. Each half has its own power and control actuators resulting in the robot
Chapter 1: Introduction

having a total of four actuators. Therefore, the robot is named the Quad Actuator Bee (QAB). A non-linear dynamic model that combines the best models we have for the actuators and quasi-steady aerodynamics show how yaw torques are produced by the robot. The model also suggest that the control actuator sized was able to produce yaw torques for heading control. We demonstrate that the robot can hover like the DAB and perform heading control maneuvers.

We finally conclude with future directions for the QAB and a review of the rich design landscape of the RoboBee project.
Chapter 2


2.1 Introduction

A number of challenges confront an engineer designing an autonomous insect-sized aerial vehicle. Machine elements such as motors, bearings, and airfoils become inefficient as they get smaller because as scale diminishes, surface effects increasingly dominate Newtonian forces [48] and viscous forces dominate lift-generating aerodynamic inertial forces [10]. Despite these challenges, various groups have reported success in building small scale aerial vehicles powered by rubber bands[44], small motors, propellers and rotary wings [29, 49, 26] because of their potential applications in search and rescue, artificial pollination, and reconnaissance. Work by Wang et al.

Figure 2.1: Image of the robotic bee with 20 mm square top and 15 mm square bottom damper.

[51, 38, 5] has indicated that flapping mode flight may be more efficient than fixed wing gliders at insect-sized scales. In our group we take inspiration from insects and use muscle-like piezoelectric actuators to generate forces, flexures for pivot joints, and harness unsteady aerodynamic forces by flapping wings [58, 57]. Our group has demonstrated constrained liftoff [57] and vertical position control [36] of the Harvard robotic bee, an at-scale flapping robotic insect, using these techniques. Attaining free-flight stability, however, remains a challenge.

The fundamental innovation that enabled liftoff was an underactuated, passive mechanism that regulated the angle of attack of the wings [57]. This result stands in contrast to earlier efforts to actively control the exact angle of attack to reproduce the wing kinematics of flying insects that were too complicated to lift their own
weight\[58\]. More recently, our group demonstrated that a passive, underactuated mechanism could compensate for design irregularities such as bilateral asymmetry (such as by wing damage) using a differential-like mechanism \[43\].

When scaling downward, in addition to increased surface effects and viscous forces, a further challenge emerges: the dynamics get faster as mass and moment of inertia decrease. To stabilize an unstable system such as our flapping-wing robotic bee, there is a maximum delay that can be tolerated in a feedback controller \[2\]. With diminishing scale, the time delays inherent in sensing and computation become increasingly problematic. Instead of active control, another approach is to use a passive, purely mechanical stabilizer, which incurs negligible time delay. In this work we show that lightweight passive air dampers, as shown in Figure 2.1, can stabilize the attitude of our robotic bee and simplify its design. This approach was inspired by demonstrations on larger flapping robots \[50\], but because our robotic bee is at a smaller scale, a more detailed analysis and characterization of the dampers’ effect on the system’s dynamics is required. Though the passive dampers stabilize the dynamics, they do not prevent active maneuvers by controlled changes in wing kinematics.

We present a model and stability analysis that shows how passive damping can stabilize the robotic bee’s dynamics and use the model to choose proper damper size and positioning. To measure the effect of flapping-wing aerodynamics on the dampers, and because the Reynolds’ number (Re) of the flow lies in the intermediate regime between turbulence and laminar flow (approximately 1000 for the 20 mm damper), we performed wind tunnel tests on the dampers and on a flapping-wing device to measure aerodynamic damping coefficients. We show that the behavior of
Figure 2.2: Diagram of the lateral force and torque model for the robotic insect bee with passive dampers. Parameters include \( v_x \) lateral velocity; \( \theta \) pitch angle; \( F_t \) lift thrust force from flapping wings; \( F_1, F_2, \) and \( F_w \), lateral aerodynamic drag forces on dampers and wings, respectively; \( b \) and \( b_w \) aerodynamic drag forces from dampers and wings, respectively; \( d_1, d_2 \) and \( d_w \) distances from dampers to COM and distance from center of pressure of wings to COM, respectively.

the robotic bee is consistent with our simplified linear model, which indicates that the stabilization of \( \theta \) causes lateral motion due to a corresponding oscillation of the thrust vector during stabilization of \( \theta \). Here, we define hovering to be sustained flight at a set altitude with small relative lateral drift rate. The oscillatory decay of \( \theta \) in a stable system would induce lateral motion because of the robotic bee’s thrust vector which oscillates correspondingly with \( \theta \). Using the dampers, the robotic bee was able to attain altitudes greater than 60 mm in takeoff flights, and using camera based position tracking, demonstrated closed loop altitude control for hovering flight.
2.2 Model

The task is to stabilize attitude with the lightest possible set of passive air dampers. A secondary consideration is to minimize wind drag so that faster maneuvers are possible and the vehicle is not heavily influenced by external wind, such as in the vicinity of a landing perch. The dampers are oriented vertically at hover to produce aerodynamic drag during lateral motion, and it is necessary to have dampers both above and below the robotic bee’s center of mass (COM) for stability (2.2), as will be shown.

As the robotic bee moves laterally, wind drag applies forces and corresponding torques about the COM as shown in Figure 2.2. We assume the bee’s lateral velocity will be less than 3 $\text{ms}^{-1}$, so flow forces are dominated by inertia (with a Re number on the order of 1000 for a damper with size scale 20 mm), and hence drag is expected to be roughly proportional to velocity squared according to $f_d = \frac{1}{2} \rho A c_D v^2$ where $\rho$ is the density of air, $A$ is the cross-sectional area, and $c_D$ is the drag coefficient that depends on morphology. We will show in Section 2.3, however, that the effect of airflow due to flapping wings in the vicinity of the damper increases lateral drag at low velocities, making drag more closely approximating a linear function. To simplify the analysis, we model aerodynamic drag force as linearly proportional airspeed $v$ to according to $f_d = -bv$ (we assume that surrounding air does not move for this work) where $b$ is the aerodynamic damping coefficient that depends on wing and damper morphology.
Equating the sum of lateral forces to acceleration,

\[ m\ddot{x} = F_1 + F_2 + F_w + F_{t,l} \]

\[ = -b_1 (v_x - d_1 \omega c\theta) - b_2 (v_x + d_2 \omega c\theta) \]

\[ -b_w (v_x - d_w \omega c\theta) - F_t s\theta, \]

where \( c\theta \) and \( s\theta \) are shorthand for \( \cos \theta \) and \( \sin \theta \) and \( F_{t,l} \) is the lateral component of the thrust force. Similarly equating torques to rotational acceleration,

\[ J\dot{\omega} = T_1 + T_w + T_2 \]

\[ = -d_1 F_1 + d_2 F_2 - d_w F_w \]

\[ = d_1 b_1 (v_x - d_1 \omega c\theta) - d_2 b_2 (v_x + d_2 \omega c\theta) \]

\[ + d_w b_w (v_x - d_w \omega c\theta). \]

Note that \( F_t \) does not appear here because its torque effect is zero as it intersects the COM.

Adding dampers is expected to change the moment of inertia of the vehicle, as well as its COM, so we must take into account these effects. We neglect any mass necessary for the structure to support the damper at a distance from the bee (a 10 mm extension has a mass of only 1 mg). We define \( r_w, r_1 \) and \( r_2 \) to be the distances from the robotic bee’s center of mass (without dampers) to the distance of the stroke averaged center of pressure on the wings, to the center of masses of the top and bottom dampers, respectively. Then the new COM, after adding dampers centered at these locations,
moves to

\[ r_{cm} = (-r_1 m_1 + r_2 m_2)/m, \]

where \( m_1 \) and \( m_2 \) are the masses of the top and bottom dampers and \( m \) is the mass of the entire vehicle, dampers included. Then we have

\[
\begin{align*}
    d_1 &= r_1 + r_{cm} \\
    d_2 &= r_2 - r_{cm} \\
    d_w &= r_w + r_{cm}
\end{align*}
\]

and the total moment of inertia becomes

\[ J = J_{bee} + m_{bee} r_{cm}^2 + J_1 + m_1 d_1^2 + J_2 + m_2 d_2^2 \]

where \( J_{bee} \) and \( m_{bee} \) correspond to parameters for the robotic bee by itself (without aerodynamic dampers).

The analysis for stability can be simplified if the dynamics are linearized around \( \theta = 0 \) and we choose identical top and bottom dampers \( (b_1 = b_2 = b) \) equidistant from the COM \( (d_1 = d_2 = d) \), enabling convenient term cancellations, giving

\[
\begin{align*}
    \dot{v}_x &= \frac{1}{m} \left[ (-2b - b_w) v_x - F_t \theta + b_w d_w \omega \right] \\
    \dot{\theta} &= \omega \\
    \dot{\omega} &= \frac{1}{J} \left[ b_w d_w v_x + (-2bd^2 - b_w d_w^2) \omega \right].
\end{align*}
\]

The state-transition matrix $A$ in $\dot{x} = Ax$ for state vector $x = [v_x, \theta, \omega]$, assuming wing thrust balances weight $F_t = mg$, is

$$A = \begin{bmatrix}
\frac{1}{m} (-2b - b_w) & -g & \frac{1}{m} b_w d_w \\
0 & 0 & 1 \\
\frac{1}{J} b_w d_w & 0 & \frac{1}{J} (-2bd^2 - b_w d_w^2)
\end{bmatrix}$$

We use the Routh-Hurwitz stability criterion to determine stability. This criterion depends on the characteristic equation $\det(A - \lambda I) = 0$, which is a polynomial of the form $a_3 \lambda^3 + a_2 \lambda^2 + a_1 \lambda + a_0 = 0$, and states that stability is assured if and only if all $a_k > 0$ and $a_2 a_1 > a_3 a_0$. All of the $a_k > 0$ by inspection since mass and inertia must be positive and non-zero, the stability criterion reduces to

$$\frac{2b}{Jm^2} \left[ (2b + b_w) d^2 + b_w d_w^2 \right] \cdot$$

$$\left[ (2b + b_w) J + (b_w d_w^2 + 2bd^2) m \right] > b_w d_w g. \quad (2.1)$$

Assuming $b_w$ and $d_w$ are fixed by the flapping mechanism, if the factor $b/Jm^2$ is large enough, stability can be assured. Further, inside the left pair of brackets of Equation 2.1, we can neglect $b_w d_w^2$ since $(2b + b_w) d^2 \gg b_w d_w^2$ because the dampers will be much farther from the COM than the wings $(d \gg d_w)$ and based on results in the wind tunnel in Section 2.3, $b > b_w$, indicating that increasing $d$ will also augment stability. Intuitively, stabilization can be assured by increasing rotational damping from either a large $b$ or a large $d$, or a combination of both. Because $m$ is dominated by the piezo actuator of the flapping mechanism, it cannot be reduced by much and does
not appear to be a route to optimization. The matter is slightly more complicated for optimizing \(J\), because as \(b\) or \(d\) is increased, so is \(J\), so we leave an optimization of these parameters for future work.

A free-body simulation was written to simulate forces and torques on the robotic bee. To augment the model to incorporate motion along the \(z\) axis, wing and damper drag forces were assumed to act only perpendicular to damper surfaces, through their centers. Parameters used in the model are given in Table 2.2 (Aerodynamic damping parameters were those found in Section 2.3). We investigated the effect of the bottom damper on the robotic bee’s dynamics by comparing the simulations of case 1 where the top and bottom damper are of the same size and at some \(d_1\) and \(d_2\) (we chose the size and positions that satisfies the Routh-Hurwitz stability criterion) and case 2 where we leave the top damper as in case 1 and decrease the size of the bottom sail. We set the stroke averaged thrust to the robotic bee’s weight and start the simulations with an initial \(\theta\) of 10°. Our simulations (Fig. 2.3) indicate that a robotic bee having
Figure 2.3: Based on this simulation, the effect of decreasing the size of the bottom damper causes more oscillations during the recovery from an initial $\theta$ of 10°.

dampers of equal size would recover with less oscillations than one with a smaller bottom damper. To verify our simulations, we built a robotic bee that had features on its base to enable bottom dampers of different sizes and positions to be mounted on the robotic bee.

2.3 Wind Tunnel Tests

At the scale of our vehicle, air drag on vertically-oriented flat-plate dampers is dominated by inertial forces, suggesting that the lateral drag force $F_D$ varies quadratically with relative air speed $v$ according to

$$F_D = \frac{1}{2} \rho c_D(\alpha) S v^2$$

(2.2)
where $\rho$ is the air density, $c_D(\alpha)$ is the drag coefficient that varies with angle of attack $\alpha$, and $S$ the area of the damper.

Aerodynamic drag on the wings, however, is expected to vary proportionally with airspeed according to the following aerodynamic approximation. Suppose the wing trajectory can be approximated as a frontward-backward sawtooth function, neglecting the effects of wing rotation about the vertical axis and stroke reversal. Then with $v$, the free-stream airspeed and $w$, the velocity of the wings relative to the robot, drag on the downstroke (into the wind) goes as $f_d = -\beta(v + w)^2$. On the upstroke, air drag reverses direction because the wings are moving much faster than the free-stream airspeed ($w \gg v$), and the drag force is $f_d = \beta(w - v)^2$. Since upstroke and downstroke take equal time, the stroke-averaged force is

$$
\overline{f_d} = \frac{1}{2} \beta(-v^2 - 2vw - w^2 + v^2 - 2vw + w^2)
$$

$$
= -(2\beta w)v,
$$

showing how drag on the flapping wings is proportional to $v$ for a constant $w$. The approximation can be extended to hold for rotating wings performing sinusoidal motion, assuming large $w$ as is the case here.

To measure these forces, we carried out experiments to measure drag for wind speeds ranging from 0.00 ms$^{-1}$ to 3.00 ms$^{-1}$ in a wind tunnel. The wind speed was monitored and controlled with PID controller, providing accuracy of $\pm 0.01$ ms$^{-1}$. The damper and/or robotic bee was mounted at the end of a 30 cm moment arm attached to a precision six-axis force-torque sensor (*Nano17 Titanium*, ATI Industrial Automation, Apex NC USA) so that drag forces could be computed with high sensitivity from
measured torque values (force sensitivity of ±30 µN, sampled at 1000 Hz).

We considered two square damper sizes, 15 mm and 20 mm, each with two mounting orientations; perpendicular to the wind direction and at an angle of attack of 45°. The effect of drag on the mounting arm and/or the body of the robot was measured beforehand and subtracted out in the appropriate plots. The plot of drag forces is displayed in Fig. 2.4. As expected, drag force is consistent with (2.2), where lift and drag forces are quadratic functions of the air speed \( v \). In Figure 2.5, drag on the wings varies linearly with airspeed as predicted by (2.3). If the bee is rotated 90° so that wind blows laterally across it rather than head-on, drag also approximates a nearly identical linear function. We do not have an aerodynamic model to explain this phenomenon, but remark that it simplifies analysis because both pitch and roll motions can be modelled with nearly the same dynamics.

In section 2.2, we model aerodynamic drag force as a linear function of the airspeed according to \( f_d = -bv \) to simplify analysis. We estimated \( b \) by performing a least-squares linear regression on the force data of Figure 2.4 for airspeed up to 1.0 ms\(^{-1}\), giving damping coefficients for the 15 mm and 20 mm dampers and wings of \( b_{15} = 1.0 \times 10^{-4} \text{ Nsm}^{-1} \), \( b_{20} = 1.8 \times 10^{-4} \text{ Nsm}^{-1} \), and \( b_w = 2.0 \times 10^{-4} \text{ Nsm}^{-1} \), respectively.

### 2.4 Design

The robotic bee frame and transmission was based on the design that demonstrated vertical control[36]. The design methodology of the robotic bee described in this paper was inspired by the design principals exhibited in the monolithic bee of Sreetharan and pop up book MEMS by Whitney [42, 53]. It was designed to assemble
Figure 2.4: Drag on the passive air dampers scales with wind velocity squared and damper area. If the damper is inclined 45° to the wind, drag diminishes only a small amount, indicating that angle of attack does not have a significant effect. The linear approximation, calculated by linear regression up to 1.0 ms$^{-1}$, is shown by a thick line (error bars indicate one standard deviation).

Figure 2.5: Drag arising from flapping wings is roughly linear with airspeed and is roughly equal for motion along either the $x$ or $y$ axis. The linear approximation is shown by a thick line.
by employing a scaffold manufacturing technique that combines the transmission and frame in a series of six folds. This technique reduces the number of discrete parts which reduces manual assembly and relies on alignment features within the scaffold to achieve better alignment between the frame and transmission. This will be discussed in future work as we investigate ways to manufacture robotic bees in a more automated and consistent manner.

The robotic bee has a top damper and a bottom damper. To make the dampers more robust to crashes during our experiments, the beams extending from the damper spine were tapered close to the spine to reduce the possibility of failure when the beams bend during crashes. Each damper consists of two interlocking surfaces that form a cross. The cross is fixed on both ends by caps that lock the dampers with respect to each other. A damper is made by cutting its carbon fiber frame and then sandwiching the frame between two sheets of 1.5 µm polyester. Before sandwiching, the polyester is stress relieved at its glass transition temperature 150 °C for 1 min twice. Next, the sandwich is put in between a layup of four layers of Teflon, a 3 mil steel sheet and a kapton/silicone/kapton layer on each side of the sandwich. The layup is put under 3.6 MPa of pressure and is heated to 150°C for 15 min and is left to cool under pressure. This enables the polyester membranes to merge, encapsulating the carbon fiber frame in the process. Once cooled, the outline of the damper is laser cut to release the damper from the sandwich.

In order to help us understand how the dampers affect the stability of our system, we fixed the size and position of the top damper but made the bottom sail adjustable in size and position. The bottom damper was designed with an adjustment rail to
Figure 2.6: (A) Damper fabrication consist of compressing a carbon fiber frame in between two layers of polyester. The sandwich is cured under pressure and temperature to bond the polyester layers. After bonding, the damper is released by laser cutting the damper outline. (B) The bottom damper is assembled by sliding two dampers together to form a cross. The adjustment rail of the bottom damper is attached to the robotic bee along its side frame using a low temperature thermoplastic (Crystalbond™ 509, Electron Microscopy Sciences). (C) Solidworks rendering of the final assembly.

enable its position to be adjusted along the $z$ axis of the robotic bee. Two bottom dampers were made, one 15 mm square and another 20 mm square. This enables us to reuse the robotic bee while varying the system’s dynamics by altering the size and position of the bottom damper.

2.5 Results

The prototype of the damper-stabilized robotic bee is shown in Figure 2.1. Because our current robotic bee prototype does not have control authority over all degrees of freedom, particularly yaw rotation about the $z$ axis, in most flights the robot
Figure 2.7: High-speed videocomposite of a stable vertical takeoff by the damper-stabilized robotic flapping bee. Shutter speed was 1/500 s and frame interval is 0.1 s. exhibted rotary motion in addition to lateral motion, as predicted by the model. To demonstrate stability, we drive the actuators with an amplitude of 230 V peak-to-peak at the resonant frequency of the bee (105 Hz) causing the bee to accelerate upward. Given the faster downwash generated by the wings for this ascent, only a smaller bottom damper was necessary to stabilize its vertical motion. A composite image of the flight is shown in Figure 2.7.

With vertical-flight stability attained, we sought to demonstrate hover, which
requires measurement and control of altitude during free flight. We attached 1.5 mm spherical retroreflective markers (3 mg each) to the bee so that its trajectory could be recorded with a four-camera visual tracking system at 500 Hz (Vicon T040-series, Buckinghamshire, UK) with a 25-40 ms latency. Because the underactuated bee does not have control over yaw or roll, we sought to stabilize to a desired altitude during a short time window before the bee drifted too far and exited the tracking volume ($2.7 \times 10^{-2} \text{m}^3$), limiting the time period of the trials.

Our linearized model predicts that there is a non-zero equilibrium $\theta$ in the presence of an inherent torque bias which tilts the thrust vector causing lateral drift. The lateral drift, however, does not play a role in the stability criterion in equation 2.1. An XPC-target realtime PC (MathWorks, Natick, MA) received this pose information over serial port and computed voltage feedback commands at 10 KHz using a proportional-derivative (PD) controller. We regulated the altitude by modulating the amplitude of the voltage signal to the piezoelectric actuator using a P gain of $K_p = 900 \text{ Vm}^{-1}$ and D gain of $K_d = 210 \text{ Vsm}^{-1}$ based on simulations of a simple model of the robotic bee’s vertical dynamics consisting of its mass and air drag. These voltage amplitude commands were added to a nominal 216 V baseline feedforward signal that was the minimum amplitude necessary to achieve takeoff. The derivative term was added to damp the dynamics and add phase lead, based on wind tunnel drag data indicating that vertical aerodynamic drag has a relatively low damping factor of approximately $1.0 \times 10^{-4} \text{ Nsm}^{-1}$. To minimize damage to the actuator and transmission of the bee, we limited the peak-to-peak voltage amplitudes to an interval that ranged from 200 to 256 V. This controller was able to stabilize the bee’s altitude
with small error as shown in Figure 2.8, and could regulate altitude at a number of
different altitude setpoints as shown in Figure 2.9.

2.6 Conclusions

We showed in this work that it is possible to stabilize vertical flight of a flapping-
wing microrobot and attain hover using passive air dampers. Using camera-based
visual tracking, we were able to use active control to constrain the altitude of the
robotic bee to a desired setpoint for a second or two until it drifted out of the tracking
volume. Key was an understanding of the dynamics of the air dampers, as well as an
improved fabrication process for building the bee robot so that it exerted a low enough
rotational torque that the dampers could stabilize its dynamics. This work provides
a significant stepping stone for further flight control experiments by demonstrating
a simple-to-build passively stable platform upon which further characterization or
active maneuvers may be performed. With control over yaw (z axis) and roll (x
axis) torque, arbitrary maneuvers could be performed, prolonging hovering flight
indefinitely or enabling programmed trajectories.

For certain environments, such as outdoors where wind is prevalent, wind gusts
may exert forces too great for a damper-equipped active flying robot to recover from,
and so a more active controller may be necessary to eliminate these dampers. We
draw a parallel to flapping insects, which eschew large wind-based dampers for this
reason, and likely instead achieve stability by measuring their rotation rates, such
Figure 2.8: High-speed videocomposite of altitude control of flapping microrobotic bee commanded to an altitude of 100 mm (top). Here the robotic bee accelerates vertically until reaching an altitude setpoint, then begins to drift laterally. During the trial, we recorded the altitude measured by visual tracking (bottom, left axis), the P and D amplitude control commands, and piezoelectric actuator voltage during this trial run (bottom, right axis).
Figure 2.9: Microrobotic bee altitude during free-flight altitude control tests using a PD controller, recorded by visual tracking. The small error in the three cases is likely due to the steady-state error exhibited by PD controllers. The different altitudes demonstrate that the result is not due to the effect of the wire tether or an aerodynamic ground-effect.

as by their halteres [10], and performing a functionally equivalent damping action through changes in wing kinematics.
Chapter 3

Biomimetics: Differential AoA Concept

3.1 Introduction

The agility of hummingbirds, dragonflies, bees, and fruit flies has inspired scientists to study how they use flapping wings as a means to generate aerodynamic forces capable of producing often complex maneuvers in air. Various groups have reported success in building Micro Air Vehicles (MAV) using rotary-motors to drive flapping wings and propellers, rubber bands to power butterfly-like wings and rotary motions that mimic the flight of samara seeds [29, 49, 26, 44]. As we try to shrink the MAV to insect-scale, use of conventional components such as rotary motors, bearings and airfoils become inefficient. This is due to dominating effects of surface and viscous forces over Newtonian forces [48] and lift producing aerodynamic inertial forces respectively.

To design a flapping-wing microrobot to work in such a different environment,
our group takes inspiration from bees and flies, constructing our MAV (termed the RoboBee) from components that have an analog to features normally associated with bees (and other flying insects which use asynchronous muscles [13]). The flight muscle of the RoboBee is a piezoelectric bimorph actuator that converts a linear input to an angular output which drives a pair of wings through a transmission (thorax). By harnessing passive wing rotation (a phenomenon also observed in nature [4]), the Harvard Microrobotic Fly (HMF) [56] was the first insect-scale robot to achieve a thrust-to-weight ratio greater than one. To control more degrees-of-freedom, small piezoelectric bimorph control actuators were added within the thorax of the RoboBee, enabling the wings to have different stroke amplitudes [19]. Finio’s design of the RoboBee produced roll torques by applying static control inputs and yaw torques by phasing the control input with respect to the power input. A hybrid approach taken by Ma et al.– using two piezoelectric bimorph actuators to drive each wing independently– showed that it could generate sufficient torques for control purposes [33]. Ma’s design generated roll torques by independently increasing/decreasing the stroke amplitude of a wing and created yaw torques by adjusting the upstroke and downstroke velocities of its power actuators. All previous designs can generate pitch torques by biasing the power actuator forward or backward (this gives the direction of the pitch torque). An alternative way to generate torques for control is by varying the Angle-of-Attack (AoA) of the wings.

If the AoA of each wing can be tuned to achieve different lift and drag force profiles, control torques can be generated [34]. By measuring the untethered flight kinematics of fruit flies, Bergou et al. modeled a fly’s wing hinge as a torsional spring that
passively opposes the wing’s tendency to over rotate due to aerodynamic and inertial forces. By changing the spring rest angle, an asymmetric AoA can be generated in the upstroke and downstroke of the fly [3]. In this paper we report progress in changing the AoA of the RoboBee’s wings by using a system of mechanical linkages to cause bilaterally asymmetric changes to the wing hinge spring rest angle by using a single control actuator. We simulate the effect of changing the wing hinge spring rest angle on the aerodynamic force produced by each wing—showing that yaw torques can be created by biasing the control actuator and roll torques are produced by phasing the control actuator with respect to the power actuator (this concept flips the way yaw and roll torque is generated as compared to [19]). We construct a kinematic model of the mechanism and build an at-scale non-flight weight RoboBee to verify that we can generate wing motions as predicted by the kinematic model.
Table 3.1: Notation

<table>
<thead>
<tr>
<th>Symbol(s)</th>
<th>Meaning</th>
</tr>
</thead>
<tbody>
<tr>
<td>$^A R^B \equiv R_{b_k}^D$</td>
<td>Rotation matrix mapping the right-handed orthogonal unit vectors $\hat{b}_x, \hat{b}_y$ and $\hat{b}_z$ to the right-handed orthogonal unit vectors $\hat{a}_x, \hat{a}_y$ and $\hat{a}_z$ via a positive rotation of angle $D$ about $\hat{b}_k$ where $k=x,y$ or $z$</td>
</tr>
<tr>
<td>$\hat{r}^{E/F}$</td>
<td>Position vector of point $E$ from $F$</td>
</tr>
<tr>
<td>$\hat{g} \cdot \hat{f}$</td>
<td>dot product of vector $\hat{g}$ and $\hat{f}$</td>
</tr>
<tr>
<td>$\hat{h}$</td>
<td>a unit vector</td>
</tr>
<tr>
<td>$s_{\theta}$</td>
<td>$sin(\theta)$</td>
</tr>
<tr>
<td>$c_{\theta}$</td>
<td>$cos(\theta)$</td>
</tr>
<tr>
<td>$t_{\theta}$</td>
<td>$tangent(\theta)$</td>
</tr>
</tbody>
</table>

3.2 Mechanical Design and Kinematic Model

Power to the RoboBee is provided by a single piezoelectric bimorph actuator in a configuration similar to the design in [57]. The power actuator provides a linear input $\delta_{P_{iz}}$ to two planar four-bar linkages which results in angular outputs $\phi_J$, where $J=$Left (L) or Right (R), are defined to be the RoboBee’s left and right stroke amplitudes respectively (Tables 3.1 and 3.2 detail the notation and variables used in this paper). See table 3.3 for rotation matrix definitions that relate the rotation of a rigid link with respect to its neighboring rigid links in Fig. 3.2 [35]).

To combine the power and control inputs in a decoupled manner, a spherical five-bar linkage [41], was used to combine an angular control input $\psi_{0,i}$ along $\hat{n}_y$ and an angular power input $\phi_J$ along $\hat{n}_x$. Instead of using two control actuators [19], the
Figure 3.2: (A) Overview of the proposed mechanism that enables the left and right wings to have different wing hinge rest angle. (B) Shown here are the spherical linkages on the left side of the RoboBee. From the left to the right of the page, a spherical four-bar linkage rotates the rotation axis of an angular input by 90° and a spherical five-bar linkage that combines two independent angular inputs and maps them onto the wing hinge connector $S_{L3}$. The centers of the linkages are shown as solid red circles in (A) are $P_{L1}$ and $P_{L2}$ respectively. We define such a combination of linkages as the left shoulder of the RoboBee (C) The power actuator is connected to two planar four-bar linkages (only one is shown here due to symmetry) while the control actuator is connected to another two planar four-bar linkages that produces differential angular input angles to the spherical four-bar linkages.
left and right side of the RoboBee are coupled differentially by two planar four-bar linkages driven by a single piezoelectric bimorph control actuator.

The control input mechanism consists of two planar four-bar linkages connected in series. When a positive control input $\delta_{CI_y}$ is applied at $C_I$, $C_{RA}$ deflects downward in the $\hat{n}_z$ direction while $C_{LA}$ deflects upward in the $-\hat{n}_z$ direction (See Fig. 3.2). This causes the left and right sides of the RoboBee to have differential angular outputs $\psi_{oJ}$ at the wing hinge connectors $S_{L3}$ and $S_{R3}$ respectively (Fig. 3.2C).

The long axis of the control actuator is designed to be mounted along $\hat{n}_x$ to minimize the weight of the overall support structure by using the same support structure holding the power actuator. This caused the quasi-linear control input to be applied in the $\hat{n}_y$ direction which translates into angular inputs around the $\hat{n}_x$ axis. Since the spherical five-bar linkage was designed to take in the angular control input along the $\hat{n}_y$ axis, a spherical four-bar linkage was used to rotate the control input $\theta_{CJA}$ from along $\hat{n}_x$ to $\hat{n}_y$ about $\hat{n}_z$ to the wing hinge spring rest angle input $\psi_{0J,i}$ (Fig. 3.2B). The power angular input ($\phi_J$) is conveniently located along the required $\hat{n}_x$ direction which enables it to couple directly to the spherical five-bar linkage (Fig. 3.2B).

In order to construct the kinematics of the device, position constraints are applied to close the kinematic chains. Starting from the left side of the RoboBee with the control input (Fig. 3.2C), the position of $c_{LBCI}$ is constrained according to:

$$
\tau_{c_{LBCI}/N_0} \cdot \hat{n}_z = -L_3
$$

(3.1)

$$
\tau_{c_{LBCI}/N_0} \cdot \hat{n}_y - \delta_{CI_y} = L_1 + L_2 - L_4.
$$

(3.2)
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Moving along the chain, the four-bar spherical linkage (Fig. 3.2B) is knitted together by the joint connecting $S_{L1}$ and $D_L$:

\[ \hat{s}_{LV,z} \cdot \hat{d}_{L,z} = 1 \]  
\[ \hat{s}_{LV,z} \cdot \hat{d}_{L,x} = 0. \]  

Next to the four-bar spherical linkage (Fig. 3.2B), the spherical five-bar linkage is constrained by the joint connecting $S_{L3}$ and $S_{L2}$:

\[ \hat{s}_{L3,z} \cdot \hat{s}_{L2,z} = 1 \]  
\[ \hat{s}_{L3,z} \cdot \hat{s}_{L2,x} = 0. \]  

Finally, ending at the power input of the left side of the RoboBee, the position of $p_{LBPI}$ is constrained according to:

\[ \vec{r}_{\mathbf{P}_{LBPI}/N_o} \cdot \hat{n}_y = L_3 \]  
\[ \vec{r}_{\mathbf{P}_{LBPI}/N_o} \cdot \hat{n}_z - \delta_{P_z} = -L_1 - L_2 + L_4. \]

By repeating the constraints on the right side of the RoboBee, eight more constraints are formed. The resulting sixteen constraints form a nonlinear system of equations with the following unknowns:

\[ \bar{x}_L = \begin{bmatrix} \theta_{CLA} & \theta_{CLB} & \beta_L & \psi_{oL,i} & \gamma_L & \psi_{oL} & \phi_L & \theta_{PLB} \end{bmatrix} \]
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\[ \bar{x}_R = \begin{bmatrix} \theta_{C_{RB}} & \theta_{C_{RA}} & \beta_R & \psi_{oR,i} & \gamma_R & \psi_{oR} & \phi_R & \theta_{P_{RB}} \end{bmatrix}, \]
\[ \bar{x} = [\bar{x}_L, \bar{x}_R], \quad (3.9) \]

and \( \delta_{C_Ly} \), \( \delta_{P_{Iz}} \) the inputs to the system. These equations can be expressed as follows:

\[ f_i(x_1, \ldots, x_{16}) = 0, \quad i = 1 \ldots 16. \quad (3.10) \]

By differentiating the position constraints with respect to time to form velocity constraints:

\[ \frac{d}{dt} \left[ f_i(x_1, \ldots, x_{16}) \right] = 0 \]
\[ \frac{\partial f_i}{\partial x_1} \dot{x}_1 + \ldots + \frac{\partial f_i}{\partial x_{16}} \dot{x}_{16} = 0 \]
\[ \sum_{j=1}^{16} g_{ij}(x_1, \ldots, x_{16}) \ddot{x}_j = 0, \quad (3.11) \]

the resulting system of equations is linear with respect to \( \ddot{x} \)

\[ \dot{x}_i = h_i(x_1, \ldots, x_{16}), \quad i = 1 \ldots 16. \quad (3.12) \]

Given the initial conditions \( \ddot{x}_0 \), a specified \( \dot{\delta}_{C_Ly} \) and \( \dot{\delta}_{P_{Iz}} \), the kinematics of the RoboBee are found by stepping forward in time.
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3.2.1 Transmission Characteristics

3.2.1.1 Spherical Four-bar Linkage

Picking the left side of the RoboBee, we expand both eqn. (3.3)

\[ 1 = c_{\beta L} c_{\theta_{CLA}} c_{\psi_{oL,i}} + s_{\beta L} \left(c_{\xi} s_{\psi_{oL,i}} + s_{\xi} s_{\theta_{CLA}} c_{\psi_{oL,i}}\right) \]

to form

\[ \frac{1}{c_{\beta L} c_{\theta_{CLA}} c_{\psi_{oL,i}}} = 1 + \frac{s_{\beta L} \left(c_{\xi} s_{\psi_{oL,i}} + s_{\xi} s_{\theta_{CLA}} c_{\psi_{oL,i}}\right)}{c_{\beta L} c_{\theta_{CLA}} c_{\psi_{oL,i}}} \]  \hspace{1cm} (3.13)

and eqn. (3.4)

\[ 0 = c_{\beta L} \left(c_{\xi} s_{\psi_{oL,i}} + s_{\xi} s_{\theta_{CLA}} c_{\psi_{oL,i}}\right) - s_{\beta L} c_{\theta_{CLA}} c_{\psi_{oL,i}} \]

to form
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\[
\frac{s_{\beta_L}}{c_{\beta_L}} = \frac{c_{\zeta}s_{\psi_{0L,i}} + s_{\zeta}s_{\theta_{CLA}} c_{\phi_{0L,i}}}{c_{\theta_{CLA}} c_{\phi_{0L,i}}}. \tag{3.14}
\]

By substituting eqn. (3.14) into eqn. (3.13) we get,

\[
c_{\beta_L} = c_{\theta_{CLA}} c_{\phi_{0L,i}}. \tag{3.15}
\]

Using small angle approximations to eqn. (3.14) and eqn. (3.15) results in,

\[
\beta_L = c_{\zeta}\psi_{0L,i} + s_{\zeta}\theta_{CLA} \tag{3.16}
\]

\[
\beta_L^2 = \psi_{0L,i}^2 + \theta_{CLA}^2 - \frac{\psi_{0L,i}^2\theta_{CLA}^2}{2}. \tag{3.17}
\]

After combining eqn. (3.16) and eqn. (3.17), the resulting higher order terms are removed which yields the following relation between the input \(\theta_{CLA}\) and the output \(\psi_{0L,i}\):

\[
\psi_{0L,i} = \left(\frac{1}{t_{\zeta}}\right)\theta_{CLA}. \tag{3.18}
\]

This enables us to pick values of \(\zeta\) to amplify or reduce the output of the linkage. Here, our goal is to rotate the input and keep the magnitude the same. We chose \(\zeta = 35^\circ\) because our simulations indicated that \(\psi_{0L,i} \approx \theta_{CLA}\) over a larger range of input angles as compared to a linkage with \(\zeta = 45^\circ\) where at larger input angles \(\psi_{0L,i} < \theta_{CLA}\).
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3.2.1.2 Spherical five-bar Linkage

By expanding eqn. (3.6) we have,

\[ 0 = s_{\psi_L} c_{\psi_{L,i}} - s_{\psi_{L,i}} c_{\psi_L} c_{\phi_L} \] \hspace{1cm} (3.19)

After simplification, this yields the following relationship:

\[ t_{\psi_{L,i}} = t_{\psi_{L,i}} c_{\phi_L} \] \hspace{1cm} (3.20)

To examine how the coupling of the inputs \( \psi_{L,i} \) and \( \phi_L \) affects the left wing spring rest angle, \( \psi_L \), the partial derivative of \( \psi_L \) with respect to its inputs are calculated. The partial derivatives are plotted in Fig. 3.3 where \( \phi_L \) and \( \psi_{L,i} \) are limited to the domains \([-70^\circ, 70^\circ]\) and \([-45^\circ, 45^\circ]\) respectively. \( \frac{\partial \psi_L}{\partial \psi_{L,i}} \approx 1 \) and \( \frac{\partial \psi_L}{\partial \phi_L} \approx 0 \) over a wide range of the input parameter space which indicates that the control input \( \psi_{L,i} \) maps to \( \psi_L \) well and is largely decoupled from the power input \( \phi_L \).

\[ \frac{\partial \psi_L}{\partial \psi_{L,i}} = \frac{c_{\phi_L}}{c_{\psi_{L,i}}^2 [1 + (t_{\psi_{L,i}} c_{\phi_L})^2]} \] \hspace{1cm} (3.21)

\[ \frac{\partial \psi_L}{\partial \phi_L} = \frac{-s_{\phi_L} t_{\psi_{L,i}}}{[1 + (t_{\psi_{L,i}} c_{\phi_L})^2]} \] \hspace{1cm} (3.22)
3.3 Generating yaw and roll torques

Pitch torques have been shown in [19] to be generated by biasing the offset voltage of the power actuator. Since, for the case of $\delta_{C_{Iy}} = 0$ (i.e. zero control actuator motion) this design is identical to that in [19], pitch torques are created in the same manner. Here, we provide an alternative way of generating yaw and roll torques by causing bilaterally asymmetric changes to the wing hinge spring rest angles, $\psi_{0,J}$, of the RoboBee.

For small inputs into the system, we can simplify the kinematics greatly to gain insight into how the control actuator input will affect $\psi_{0,J}$. Applying small angle approximations to eqn. (3.2) gives

$$\theta_{C_{JA}} = \frac{1}{L_3} \delta_{C_{Iy}}$$  \hspace{1cm} (3.23)

Similarly, eqns. (3.18) and (3.20) reduce to

$$\psi_{0,J,i} = \left\{ \begin{array}{c} \left( \frac{1}{l_c} \right) \theta_{C_{LA}} \\ - \left( \frac{1}{l_c} \right) \theta_{C_{RA}} \end{array} \right.$$  \hspace{1cm} (3.24)

$$\psi_{0,J} = \psi_{0,J,i}$$  \hspace{1cm} (3.25)

Defining the relative difference between the left and right wing hinge spring rest angles as,

$$\Delta \psi_0 \equiv \psi_{0L} - \psi_{0R},$$  \hspace{1cm} (3.26)

and combining eqns. (3.23) to (3.25), an expression relating $\delta_{C_{Iy}}$ to $\Delta \psi_0$ is found to
be,

\[ \Delta \psi_0 = \frac{2}{t_\psi L_3} \delta c_{1y} \]  

(3.27)

Based on kinematic data from fruit flies (*D. melanoster*), Bergou, et al. proposed that the flies use a non-zero \( \Delta \psi_0 \) to generate yaw torques by creating asymmetries in the AoA of the downstroke and upstroke [3]. Work by Mahjoubi and Byl showed that a constant non-zero \( \psi_{0j} \) can produce asymmetric lift on the upstroke and downstroke (whether more lift occurs on the upstroke or downstroke depends on the sign of \( \psi_{0j} \)) [34]. In order to generate roll torques, we would need to generate more lift on both the downstroke and upstroke strokes of either wing (this implies the other wing would have less lift due to coupling of the left and right sides of the RoboBee). This is done by driving the control actuator at the same frequency as the power actuator and in phase or anti-phase with \( \dot{\phi} \). Using the blade element method [54] to simulate the effect of \( \psi_{0j} \) on the aerodynamic forces produced by the wings, we show in Fig. 3.5 and 3.6 that it is possible to generate yaw and roll torques by modulating the DC value and phase of \( \Delta \psi_0 \) (\( \psi \) the pitch of the wing is defined in [54]).

In this simulation, only one wing is driven. We can simulate the effect of two wings and the control mechanism by simulating the left and right wing independently by prescribing a wing stroke function:

\[ \phi_j = -A_\phi \cos(2\pi ft), \]  

(3.28)

and a wing hinge spring rest angle input function, one for the left and another for the right hinge:
\[ \psi_{0L} = A_\psi \sin(2\pi ft + \Phi) + B \] (3.29)

\[ \psi_{0R} = A_\psi \sin(2\pi ft + \Phi - \pi) - B \] (3.30)

which is equivalent to an input of

\[ \Delta \psi_0 = 2A_\psi \sin(2\pi ft + \Phi) + 2B, \] (3.31)

where \( A_\phi \) is half the peak-to-peak value of \( \phi_J \), \( A_\psi \) is half the peak-to-peak value of \( \psi_{0J} \), \( f \) the flapping frequency of the wing and \( B \) the DC value. \( \Phi \), the phase of the wing hinge spring rest angle, is \( \pi \) or 0 depending on the desired roll torque direction.

To simulate a yaw maneuver (Fig. 3.4A), we set \( A_\phi = 50^\circ \), \( f = 100 \) Hz \( A_\psi = 0^\circ \), \( B = 15^\circ \) and \( \Phi = 0^\circ \). The resulting aerodynamic force profile (Fig. 3.5) generates a mean yaw torque of \( 4 \) \( \mu \)Nm and a mean lift of 1.06 mN. Next, a roll maneuver (Fig. 3.4C) was simulated by setting \( A_\phi = 50^\circ \), \( f = 100 \) Hz \( A_\psi = 15^\circ \), \( B = 0^\circ \) and \( \Phi = 0^\circ \) which generates a mean roll torque of \( 3.7 \) \( \mu \)Nm with a mean lift of 1.16 mN (Fig. 3.6).

### 3.4 Experiments and Results

The kinematic model presented is a useful tool to guide the design of the RoboBee. In order to validate the model, an at-scale non-flight weight version was built. The kinematic model assumes that the linkages are rigid, the joints are revolute with flexures acting as torsional springs and perfectly aligned 90\(^\circ\) folds. In practice, these
assumptions are extremely difficult to achieve. By using techniques from [42], we can, to a certain degree, approach the kinematic alignment necessary for such a device to function. However, for this prototype, simple manual folding with kinematic stops were used to align 90° folds. This fabrication technique, though not as precise, was less complex in its design which suited our goal of creating the first prototype of this RoboBee concept.

The piezoelectric bimorph actuator [55] is made from two Lead Zirconate Titanate (PZT) plates (Piezo Systems Inc.) sandwiching a carbon layer. A bias voltage of 300V is applied to the top plate and 0V applied to the bottom plate (order depends on poling direction of the PZT plate). The control signal is

\[ V_{\text{carbon}} = \frac{A_{\text{peak-to-peak}}}{2} \sin(2\pi ft + \Phi_{\text{signal}}) + V_{\text{offset}} \]

applied at the carbon layer which induces a quasi-linear deflection at the tip of the actuator. \( A_{\text{peak-to-peak}} \) is the peak-to-peak voltage amplitude, \( f \) is the driving frequency, \( \Phi_{\text{signal}} \) is the signal’s phase and \( V_{\text{offset}} \) is the signal’s offset voltage. Typical operation of the actuator requires \( A_{\text{peak-to-peak}} \) in the order of 200 V to 300 V ¹.

The prototype was mounted onto a laser cut acrylic base and was filmed by a high speed camera with fiber optic light sources illuminating the device. For the first test, we drove the control actuator at 1 Hz with \( A_{\text{peak-to-peak}} \) set to 280V (see supplemental video). \( \psi_{0,J_i} \) was measured by post-processing the video frames (Fig. 3.8). Then, the displacement of the control actuator tip was measured by manually

¹Although the input voltages are high, current draw is in the order of 1 mA [25]
tracking its midpoint. Next, a sinusoidal fit to the data (Fig. 3.7) was applied. With $\delta_C_{\theta}$ extracted from the experimental data, we simulated the RoboBee to compare how the physical prototype performed relative to the kinematic model.

As seen in Fig. 3.8, the kinematic model consistently over predicts the wing hinge spring rest angle by as much as 35% on the right side and 29% on the left side. This could be due to a number of reasons. The main source of error likely stems from the assembly of the spherical four-bar linkage and the spherical five-bar linkage. These two components are made in a planar 2D scaffold and then manually folded with the aid of kinematic stops. This method, though easy to implement, is unable to make precise 90° folds. The second source of error arises from the narrow joints in the spherical linkages. Most of the joint widths are around 300 $\mu$m. The narrower the joint, width wise (joint geometry is defined in [54]), its behavior starts to deviate further from an ideal revolute joint due to the off-axis compliance of the flexure, and becomes more like a ball and socket joint. Such errors would cause this prototype to have kinematics that deviate from the model.

The next set of tests involved driving the power and control actuator at the system’s resonant frequency (which was empirically found to be 80 Hz) to see if we can generate wing motions that could potentially produce yaw and roll torques as highlighted in section 3.3 (see supplemental video). For yaw, we drove the power actuator at 80 Hz with $A_{\text{peak-to-peak}}$ set to 260V and the control actuator at a $A_{\text{peak-to-peak}}$ set to 0V, $f$ at 0 Hz and $V_{\text{offset}}$ set to 10V followed by 290V. Images retrieved from the high speed camera (images were captured at 5000 fps) qualitatively confirmed that the mechanism produced wing motions that in simulation could generate 0.14 $\mu$Nm
of yaw torque (Fig. 3.4(A), (B) and Fig. 3.9). In a like manner, wing motions to generate potential roll torques were created by driving the power and control actuator at 80 Hz. In-phase motions of the control actuator tip with $\phi_J$ was achieved by introducing a phase difference of $90^\circ$ between the power actuator and control actuator while anti-phase motions were made by driving the control actuator with a phase of $-90^\circ$ with respect to the power actuator. Again, post-processing of images from the high speed video indicated that wing motions generated by such inputs from the power and control actuator could generate in simulation 1.34 $\mu$Nm of roll torque (Fig. 3.4(C), (D) and Fig. 3.10).

3.5 Conclusion and future work

We showed in this work that the spherical five-bar linkage in combination with a spherical four-bar linkage can effectively decouple the power input and control input to the RoboBee’s wings. By using a single control actuator, as opposed to two [19], considerable weight savings can be made. In order to use a single control actuator and a single power actuator, an innovative combination of two spherical four-bar linkages, two spherical five-bar Linkages and four planar four-bar linkages was developed. Although there are differences between the experimental performance and the kinematic model, the prototype demonstrated its ability to cause differential AoA with trends consistent with the kinematics. Fruit fly data [3], indicates that a $\Delta \psi_0$ of $\approx 15^\circ$ is sufficient to enable turning maneuvers. Encouragingly, this RoboBee could generate a peak-to-peak $\Delta \psi_0$ of $\approx 45^\circ$ (Fig. 3.8).

Although the simulated torques (as inferred from wing kinematics) are lower than
measured roll and yaw torques generated by Finio’s [19] and Ma’s [33] designs, we expect that torque generation capability of this concept will improve by using a more precise fabrication technique [42].

In the future, roll and yaw torques generated by differential AoA will have to be measured to verify the feasibility of such a control scheme. Only then will a flight weight version be built through optimization of the control actuator size, strategic placement of the passive wing hinge (to minimize the aerodynamic load on the control actuator) and tuning of the wing hinge stiffness.
Table 3.2: List of variables and their definitions

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Identifier</th>
<th>Type</th>
<th>Initial Value (for variables)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Distance between ( p_{JB}/c_{JI} ) and ( p_{JA}/c_{JA} )</td>
<td>( L_1 )</td>
<td>constant</td>
<td>0.44 mm</td>
</tr>
<tr>
<td>Length of a link in ( P_{JA}/C_{JA} )</td>
<td>( L_2 )</td>
<td>constant</td>
<td>0.48 mm</td>
</tr>
<tr>
<td>Length of a link in ( P_{JA}/C_{JA} )</td>
<td>( L_3 )</td>
<td>constant</td>
<td>0.40 mm</td>
</tr>
<tr>
<td>Length of a link in ( P_{JA}/C_{JA} )</td>
<td>( L_4 )</td>
<td>constant</td>
<td>0.61 mm</td>
</tr>
<tr>
<td>Control input from ( C_I ) to the control transmission</td>
<td>( \delta_{CI} )</td>
<td>specified</td>
<td></td>
</tr>
<tr>
<td>Power input from ( P_I ) to the power transmission</td>
<td>( \delta_{PI} )</td>
<td>specified</td>
<td></td>
</tr>
<tr>
<td>Angle between ( \hat{n}<em>y ) and ( \hat{c}</em>{JA,y} )</td>
<td>( \theta_{CJA} )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{n}<em>y ) and ( \hat{c}</em>{JB,y} )</td>
<td>( \theta_{CJB} )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{n}<em>z ) and ( \hat{p}</em>{JA,z} )</td>
<td>( \phi_J )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{n}<em>z ) and ( \hat{p}</em>{JB,z} )</td>
<td>( \theta_{PJB} )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{c}<em>{JA,z} ) and ( \hat{d}</em>{J,z} )</td>
<td>( \beta_J )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{JI,y} ) and ( \hat{d}</em>{J,y} )</td>
<td>( \alpha_J )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{LA,y} ) and ( \hat{s}</em>{LA,y} )</td>
<td>( \zeta )</td>
<td>constant</td>
<td>35°</td>
</tr>
<tr>
<td>Angle between ( \hat{c}<em>{LA,y} ) and ( \hat{c}</em>{LA,y} )</td>
<td>( \zeta )</td>
<td>constant</td>
<td>35°</td>
</tr>
<tr>
<td>Angle between ( \hat{c}<em>{RA,y} ) and ( \hat{c}</em>{RA,y} )</td>
<td>( \zeta )</td>
<td>constant</td>
<td>-35°</td>
</tr>
<tr>
<td>Angle between ( \hat{c}<em>{RA,y} ) and ( \hat{c}</em>{RA,y} )</td>
<td>( \zeta )</td>
<td>constant</td>
<td>-35°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{JL,y} ) and ( \hat{s}</em>{JL,y} )</td>
<td>( \psi_{JJ} )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{JL,y} ) and ( \hat{s}</em>{JL,y} )</td>
<td>( \gamma_J )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{J2,x} ) and ( \hat{s}</em>{J3,x} )</td>
<td>( \epsilon_J )</td>
<td>variable</td>
<td>0°</td>
</tr>
<tr>
<td>Angle between ( \hat{s}<em>{J2,x} ) and ( \hat{s}</em>{J3,x} )</td>
<td>( \psi_{JJ} )</td>
<td>variable</td>
<td>0°</td>
</tr>
</tbody>
</table>
Table 3.3: Definition of rotation matrices used in the kinematic model

<table>
<thead>
<tr>
<th>Rotation Matrices</th>
<th>Power Transmission</th>
<th>Control Transmission</th>
<th>Spherical Four-bar Linkage</th>
<th>Spherical five-bar Linkage</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{JA} R^N$</td>
<td>$P_{JA} R^N \equiv R_{\theta_J}(\phi_J)$</td>
<td>$C_{JA} R^N \equiv R_{\theta_J}(\theta_{CJA})$</td>
<td>$S_{J_1} R_{S_{J_1}} \equiv R_{\psi_{0J,i}}$</td>
<td>$P_{JA} R^N \equiv R_{\phi_J}$</td>
</tr>
<tr>
<td>$P_{JB} R^N$</td>
<td>$P_{JB} R^N \equiv R_{\phi_J}(\theta_{PJB})$</td>
<td>$C_{J\beta} R^N \equiv R_{\theta_J}(\theta_{CJ\beta})$</td>
<td>$S_{J_2} R_{S_{J_2}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_2} R_{S_{J_2}} \equiv R_{\psi_{0J,i}}$</td>
</tr>
<tr>
<td>$C_{JA} R^N$</td>
<td>$C_{JA} R^N \equiv R_{\theta_J}(\theta_{CJA})$</td>
<td>$C_{J\alpha} R^N \equiv R_{\theta_J}(\theta_{CJ\alpha})$</td>
<td>$D_J R^{C_{J\alpha}} \equiv R_{\psi_{J_{1},\alpha}}(\beta_J)$</td>
<td>$S_{J_3} R_{S_{J_3}} \equiv R_{\psi_{0J,i}}$</td>
</tr>
<tr>
<td>$C_{J\alpha} R^N$</td>
<td>$C_{J\alpha} R^N \equiv R_{\theta_J}(\theta_{CJ\alpha})$</td>
<td>$S_{J_3} R_{S_{J_3}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_3} R_{S_{J_2}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_3} R_{S_{J_3}} \equiv R_{\psi_{0J,i}}$</td>
</tr>
<tr>
<td>$S_{J_1} R_{S_{J_1}}$</td>
<td>$S_{J_1} R_{S_{J_1}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_1} R_{S_{J_1}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_1} R_{S_{J_1}} \equiv R_{\psi_{0J,i}}$</td>
<td>$S_{J_1} R_{S_{J_1}} \equiv R_{\psi_{0J,i}}$</td>
</tr>
</tbody>
</table>

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Figure 3.4: Yaw and roll torques can be generated by modulating the DC value, $B$, and the phase, $\Phi$, of $\Delta \psi_0$. 
Figure 3.5: Simulation of $\psi$ and $\phi$ with $\Delta\psi_0 = \frac{\pi}{6}$ and wing hinge stiffness of 5.5 $\mu$N m/ rad. Here the asymmetric drag profiles on the left and right wing generate a mean yaw torque of 4 $\mu$Nm.
Figure 3.6: Simulation of $\psi$ and $\phi$ with $\Delta \psi_0 = \frac{\pi}{6} \sin (2\pi t)$ and wing hinge stiffness of 5.5 $\mu$Nm rad. Here the asymmetric lift profiles on the left and right wing generate a mean roll torque of 3.7 $\mu$Nm.
Figure 3.7: Measurement of the control actuator tip deflection. A sinusoidal fit to the measurements was made to be fed back into the simulation to compare the kinematic model with the prototype.
Figure 3.8: The control actuator is driven at 1 Hz which causes the left and right wing to rotate differentially. Here, we compare the wing spring rest angle input $\psi_{0,i,i}$ of the left and right spherical five-bar linkages to the prediction from the kinematic model.
Figure 3.9: Power actuator frequency at 80 Hz. (A) The control actuator with a $V_{offset}$ of 10V is deflected in the $-\hat{n}_y$ direction. (B) The control actuator with a $V_{offset}$ of 290V is deflected in the $\hat{n}_y$ direction. Highlighted are the mid-strokes of both cases showing the difference in the AoA during the upstroke and downstroke (time between frames is $\approx 3.2$ ms).

Figure 3.10: Power and Control actuators were driven at 80 Hz. (A) Control actuator moving in anti-phase with mid-stroke wing velocity. (B) Control actuator moving in phase with mid-stroke wing velocity. Highlighted are the mid-strokes of both cases showing the difference in the AoA during the upstroke and downstroke (time between frames is $\approx 3.2$ ms).
Chapter 4

Torque Measurements of Differential AoA Concept

4.1 Introduction

The insect world presents some remarkable feats of flight maneuvers that serve as motivations for engineers striving to create bioinspired flying robots. Just to name a few examples, bees forage in highly dynamic and cluttered environments, dragonflies fly backwards and hoverflies perform 90° saccades in only a few wing beats. Such impressive flight capabilities have inspired researchers and engineers to elucidate key principles behind insect flight - not only to understand how insects fly but also to build increasingly smaller and more maneuverable micro air vehicles. Several important fluid mechanics phenomena are crucial for insect flight: a stable leading edge vortex along the wing, rotational lift during the transition from upstroke to downstroke and wake capture which enables the wings to recover some energy from the wake of
the previous half wing stroke [15, 10]. By designing robots that utilize these fluid phenomena [8, 50, 26], one goal is to build vehicles capable of operating in hazardous and cluttered environments where maneuverability is paramount.

The Harvard Microrobotic Fly (HMF), a flapping-wing micro air vehicle (FW-MAV) having a wing span of 3cm and weighing 60mg, was the first vehicle of its size to lift its own weight up two vertical guide wires. The key to this flight was the use of passive wing rotation - where the compliance of a flexure hinge interacts with the wing inertia and aerodynamic loading on the wing to produce a desired angle of attack. This removed the need to actively control the wings’ angle of attack, reducing the weight of the vehicle which enabled take-off [56]. The HMF, however, could not control itself when the guide wires were removed. A single actuator driving both wings could not generate control torques to maintain its upright position. Work by Teoh [45] sought to control the FWMAV by altering the body of the HMF by adding passive aerodynamic dampers that stabilized the FWMAV about its roll and pitch axes. Though the FWMAV was able to maintain upright stability, hover was unattainable due to uncontrollable rotations along the FWMAV’s yaw axis.

In order to produce control torques about all three body axes, additional actuated degrees of freedom are needed. Inspired by neopteran insects that have two distinct sets of muscles (power muscles which drive the wings at the resonant frequency of the wing/thorax system and smaller muscles at the wing base that subtly alter wing kinematics), Finio [19] designed a FWMAV that had a single power actuator driving two wings and two smaller control actuators at the base of the wing transmissions which altered the transmission ratios of the left and right wing. This enabled the
Figure 4.1: A bioinspired FWMAV employing a differential mechanism enabling indirect control over the wings’ angle of attack
Chapter 4: Torque Measurements of Differential AoA Concept

FWMAV to control its left and right wing stroke amplitude producing appreciable control torques. Another approach taken by Ma gave each wing its own actuator [33]. This proved to be successful in producing sufficient control torques, resulting in the first controlled hover of an insect-scale FWMAV [32]. In both cases, angle of attack is modulated through changes in the wing stroke amplitude. Roll torque is generated by flapping one wing at a relatively larger amplitude than the other and pitch torque is produced by biasing the mean of the stroke amplitude. Yaw torque is generated by breaking up the wing stroke into two portions - a slow stroke in one direction and a fast stroke in the other - while maintaining a constant wingbeat period [12]. The idea behind this maneuver is to use asymmetric drag profiles caused by differences in the wing velocity during the upstroke and downstroke to create a net yaw torque. The fast portion of the wing stroke requires adding a second harmonic component to the actuation commands of the FWMAV. Ma reports in [33] that the wing stroke velocity profile tended to remain nearly symmetrical, resisting efforts to develop an asymmetry which hindered yaw torque production.

For an alternative mechanism, we look to nature for inspiration. Data gleaned from studying the free-flight kinematics of the fruit fly (D. melanoaster) suggests that fruit flies employ a simple bias of their wing hinge to create asymmetric drag profiles during upstroke and downstroke, resulting in a yaw torque [3]. This idea is elegant in principle, however, its mechanical instantiation is complex relative to the previous examples [46]. Fortunately, recent developments in the Smart Composite Microstructures (SCM) process [7, 42, 53] have enabled us to explore devices with such a level of mechanical complexity. Looking at just the right side of the FWMAV
(figure 4.2), two pairs of planar four-bar linkages convert quasi-linear control and power inputs into angular motions, $\theta_R$ and $\phi_R$ respectively, a spherical four-bar linkage rotates the axis of the angular motion $\theta_R$ by $90^\circ$ creating an angular control input $\zeta_R$ and a spherical five-bar linkage which combines the angular control and power inputs ($\zeta_R$ and $\phi_R$ respectively) by mapping them onto the wing hinge. Further refinement of the FWMAV presented in [46] has enabled us to begin characterizing its torque generation capability allowing us to move one step closer towards building a flight-weight version. One of the key questions in developing a flight-weight version is the sizing of the control actuator such that sufficient torques are generated for control, while minimizing weight of the control infrastructure. This is one of the motivations for the experimental characterization presented in this paper.

4.2 Indirect modulation of the wing’s angle of attack: A Review

The hinge that connects the wing to the body of the FWMAV is modeled as a torsional spring that generates a restorative torque in response to deformations from inertial and aerodynamic forces. By changing the rest angle of the spring from its neutral point, the amount of restorative torque exerted by the spring changes. This in turn affects the wing pitch angle which gives rise to different angles of attack during the upstroke and downstroke [3]. Since aerodynamic forces are functions of the angle of attack, control torques generated by aerodynamic forces can be manipulated by modulating the angle of attack.
Figure 4.2: Right sided close-up of the FWMAV. Spherical four-bar linkage consists of orange, magenta and yellow components. Spherical five-bar linkage comprises the purple, green (wing hinge), red and yellow parts.
Figure 4.3: (A) Convention used to define the three axes of the FWMAV. (B) Electrical signal input to an actuator and the corresponding actuator tip output. (C) Control actuator input biases the left and right wing hinge (green) in a bilaterally opposite way. (D) Power actuator input changes the magnitude of the wing stroke angle amplitude ($\phi_L$ and $\phi_R$) and the location of the mean wing stroke angle.
Chapter 4: Torque Measurements of Differential AoA Concept

In this FWMAV, a single power actuator is used to drive both wings (fig. 4.3D). To save weight, a single control actuator is used to cause a bilaterally opposite change in the left and right spring rest angle ($\psi_L$ and $\psi_R$) as shown in figure 4.3C (i.e. if the right is biased +10 deg the left is biased -10 deg) [46]. To generate pitch torques, the power actuator is biased to shift the mean stroke angle further towards the upstroke or downstroke [37]. Inspired by the fruit flies, yaw torque is produced by a fixed bias of the spring rest angle. This causes the upstroke and the downstroke of the wing to have two distinct angles of attack, generating a net drag asymmetry between the upstroke and downstroke [3]. Consequently, the net drag generates a torque that causes the vehicle to yaw. Taking this concept one step further, by oscillating the control actuator at the flapping frequency, either in phase or out of phase with the power actuator, roll torques are generated [46].

In order to design a FWMAV that can hover using this concept of indirect manipulation of the angle of attack, the control torques (roll, yaw and pitch) must be characterized. Knowing how much torque can be produced for each axis, along with potential coupling of torques would provide useful insights for designing control laws and would inform appropriate sizing of the control actuator to minimize weight while providing sufficient control torques for hovering flight.

4.3 Results and discussion

The vehicle is mounted onto a custom single-axis torque sensor consisting of a cross shaped sensing beam that is designed to be sensitive to a torque applied along the long axis of the beam but insensitive to all other torques and forces (figure 4.4). A
capacitive sensor measures the change in displacement of a target plate on the sensing beam by sensing changes in voltages (sampled at $5 \, KHz$) as the plate moves up and down in response to a torque twisting the beam [18]. To measure torques about the roll, yaw and pitch axes of the vehicle, the vehicle has to be reoriented to align the torque sensing axis of the sensor to the desired axis of the vehicle. To minimize the effect of lift on the sensor measurements in the roll and pitch measuring orientations, the vehicle is mounted in such a way that lift is orthogonal to the displacement of the target plate (figure 4.4). For the pitch measuring orientation, we assume that aerodynamic drag due to the flapping wings is zero-averaged and has minimal effect on the displacement of the target plate.

For the purpose of torque characterization, the control actuator is identical to the power actuator. These piezoelectric bimorph actuators [55] are made by sandwiching a
Figure 4.5: Measured torque along the FWMAV’s body axis as a function of roll, yaw or pitch control commands. For each data set, a least squares fit was applied as is shown in red. The gradient of each line is labelled $a_{ij}$ where $i$ refers to the row number and $j$ the column number.
Figure 4.6: Still frames from high speed video taken at 3000 fps. From left to right: (A-B) Roll commands were $C_{Amplitude} = -220V$ and $160V$. (C-D) Yaw commands were $\delta C_{Offset} = -80V$ and $80V$. (C) Pitch commands were $\delta P_{Offset} = -32V$ and $32V$.
carbon fiber layer between two Lead Zirconate Titanate (PZT) plates (Piezo Systems Inc.). A bias voltage, \( V_{\text{Bias}} \), of 300V is applied to the top plate and 0V is applied to the bottom plate (order depends on poling direction of the PZT plates). The control signal (replacing \( V \) with either \( P \) or \( C \) refers to the power or control actuator, respectively)

\[
V_{\text{Signal}} = \frac{V_{\text{Amplitude}}}{2} (\sin 2\pi f_{\text{Op}} t) + \frac{V_{\text{Bias}}}{2} + \delta V_{\text{Offset}}
\]

is applied at the carbon fiber layer of the power and control actuators. Increasing \( P_{\text{Amplitude}} \) increases the power actuator’s tip to tip displacement which increases the vehicle’s flapping amplitude. A change in \( \delta P_{\text{Offset}} \) generates a pitch torque by shifting the mean displacement of the power actuator which also shifts the mean of the wing stroke amplitude (figure 4.3B). To produce a yaw torque, a change in \( \delta C_{\text{Offset}} \) of the control actuator causes a bilaterally opposite change in the rest angle of the left and right wing hinges. To create roll torques, \( C_{\text{Amplitude}} \), in combination with input from the power actuator, is increased to increase the magnitude of the oscillating spring rest angle (figure 4.3C). The operating frequency, \( f_{\text{Op}} \), of the vehicle was set to its resonant frequency of 100 Hz and \( P_{\text{Amplitude}} \) was set to a moderate 225V (this was done to prolong the life time of the FWMAV) which produced a peak-to-peak stroke amplitude of approximately 70°. Each experiment lasted one second and torques were averaged from the resulting 100 flapping periods for various combinations of power and control actuator inputs. Roll commands \( (C_{\text{Amplitude}}) \) were varied from -220V to 160V, yaw commands \( (\delta C_{\text{Offset}}) \) were varied between -80V to 80V while pitch commands \( (\delta P_{\text{Offset}}) \) were swept from -32V to 32V.
Chapter 4: Torque Measurements of Differential AoA Concept

The torque sensor was calibrated by hanging a 205mg weight at 1mm intervals on notches along the sensing beam (figure 4.4). Due to drifts in the voltage signal, we take the difference in voltage readings before and after a weight is added. This creates a calibration curve from a set of known torques with a corresponding set of voltage differences. When the FWMAV is given a control signal, we measure the voltage difference before and after the torque command was issued and use the calibration curve to back out the torque generated by the FWMAV.

Figure 4.5 shows the measured torques about the roll, yaw and pitch axes. Each row presents torque measurements about a particular axis (roll, yaw or pitch) while each column indicates what type of torque command (roll, yaw or pitch) is issued. Roll, yaw and pitch torques showed positive correlations with their respective commands with roll torques exhibiting a positive bias at the extreme of the positive roll torque command at an amplitude $C_{Amplitude} = 160V$. At a smaller $C_{Amplitude}$ of 160V, the magnitude of the roll torque was expected to be smaller than the magnitude of roll torque at $C_{Amplitude}$ of $-220V$. In figure 4.6B the difference in stroke amplitude of the left wing over the right wing was 58° while the difference in the stroke amplitude of the right wing over the left wing in figure 4.6A was 45°. This suggests that the sharp increase in roll torque at $C_{Amplitude} = 160V$ was due to the larger difference in stroke amplitude which caused the positive torque bias. This bias could be due to a combination of manufacturing misalignment and an inherent deflection bias of the control actuator.

The right wing’s angle of attack at the mid-stroke was estimated by measuring the wing chord projected on the image plane (highlighted by dashed lines in figure
4.6) and the known chord length. In figure 4.6A, the angle of attack was 51° on the downstroke and 49° on the upstroke while in figure 4.6B the angle of attack was 68° on the downstroke and 59° on the upstroke. This increase in the angle of attack highlights the effect of how the oscillating rest angle of the right wing can indirectly control the angle of attack, enabling the vehicle to transition from negative to positive roll torques.

The effect of biasing the rest angle on the angle of attack during the upstroke and downstroke of the right wing is highlighted in figure 4.6C and D. In figure 4.6C, the difference between the angle of attack on the downstroke and upstroke was +21°. The downstroke experienced more drag than the upstroke because more of the wing was exposed to the incoming flow of air, creating a net thrust that produced a negative yaw torque. When biasing the spring rest angle in the opposite direction, the difference between the angle of attack on the downstroke and on the upstroke was -5° which created a positive yaw torque (figure 4.6D).

Torque data for off-axis commands showed a relatively flat response to on-axis commands except for coupling observed in the roll torque - pitch command and pitch torque - roll command experiments (figure 4.5C and G). Roll torque - pitch command data exhibited a negative correlation between roll torque and pitch command while pitch torque - roll command data was largely flat in the direction of positive roll commands but negatively correlated in the direction of negative roll torque commands.

Assuming the elements $a_{ij}$ are constant and independent from the actuator inputs, the gradients from figure 4.5 are assembled into a matrix A, establishing a relationship between torque produced and control inputs (the offsets of the least squares fit are
Chapter 4: Torque Measurements of Differential AoA Concept

ignored because of the inherent torque biases in the FWMAV due to the imperfect manufacturing and mounting of the vehicle to the sensor beam),

\[
\begin{bmatrix}
\tau_{\text{roll}} \\
\tau_{\text{yaw}} \\
\tau_{\text{pitch}}
\end{bmatrix} =
\begin{bmatrix}
a_{11} & a_{12} & a_{13} \\
37 & a_{22} & a_{23} \\
a_{31} & a_{32} & a_{33}
\end{bmatrix}
\begin{bmatrix}
C_{\text{Amplitude}} \\
\delta C_{\text{Offset}} \\
\delta P_{\text{Offset}}
\end{bmatrix}
\]

\[\bar{\tau} = A\bar{x}.\]  

From the values given in equation 4.3, the rank of A is 3 and its condition number is 14. This implies that if the FWMAV requires a desired torque vector, \(\bar{\tau}_{\text{des}}\), to remain upright in hover, we could in principle find a set of desired control inputs by inverting A

\[
\bar{x}_{\text{des}} = A^{-1}\bar{\tau}_{\text{des}}
\]

Fruit flies can execute a 45° yaw turn in 22ms [3]. Using this as a benchmark for this FWMAV, a yaw torque of \(\pm 1\mu Nm\) together with its yaw axis moment of inertia estimate of \(0.35 \text{gmm}^2\), would execute a similar yaw maneuver in 22ms. From equation 4.4,
sets a yaw command ($\delta C_{\text{Offset}}$) range of -32V to 32V. This implicitly sets a bound on how high $C_{\text{Amplitude}}$ can go because $\min(C_{\text{Signal}}) \geq 0$ and $\max(C_{\text{Signal}}) \leq V_{\text{bias}}$. If these limits are exceeded the control actuator would start to de-pole. As a result, the range of $C_{\text{Amplitude}}$ is set to -236V to 236V and the expected roll torque achievable is $\pm 0.8\mu Nm$. Again comparing to fruit flies which were observed to perform a roll turn of 45° in less than 50ms [20], a roll torque of 0.8$\mu Nm$ with its roll axis moment of inertia estimate of 2.14$gmm^2$ would roll 45° in 65ms. To generate similar torques in the pitch axis of $\pm 0.8\mu Nm$, the pitch command ($\delta P_{\text{Offset}}$) range is set to -23V and 23V which leaves a $\max(P_{\text{Amplitude}})$ of 254V.

The use of equation 4.4 for flight control should be used conservatively and limited to only small desired torque outputs. The system matrix $A$ was assumed constant and independent from the control inputs $\bar{x}$. In reality, the elements of $A$, are most likely a function of the control and power actuator inputs $C_{\text{Amplitude}}$, $\delta C_{\text{Offset}}$, $P_{\text{Amplitude}}$ and $\delta P_{\text{Offset}}$. If more aggressive torque outputs are required for hover, the relationship between torques and control inputs need to be probed at a deeper level involving combinations of multiple control inputs.
4.4 Flight weight design of the single power and single control actuator scheme

To investigate if the single power and single control actuator scheme could be a viable design path for an insect-scale flapping-wing robot, the oversized control actuator used in the torque characterization needed to be reduced in size. The largest known payload carried by a single actuator RoboBee was 45 mg in 2. Therefore the maximum additional weight of the control actuator and supporting structure was limited to 45 mg. In figure 4.5, the maximum yaw torque measured was approximately \( \pm 3 \mu \text{Nm} \) at a \( \delta C_{\text{offset}} \) of \( \pm 80 \text{V} \). Initial estimates from [32] showed that approximately \( \pm 1 \mu \text{Nm} \) of yaw torque is sufficient for control about the yaw axis. Therefore, we would like to achieve \( \pm 1 \mu \text{Nm} \) of yaw torque at a \( \delta C_{\text{offset}} \) of \( \pm 150 \text{V} \). The wing’s inertia about the wing rotation axis is approximately \( 2 \text{mg mm}^2 \) while its inertia about the wing stroke axis is approximately \( 50 \text{mg mm}^2 \). The center of pressure of the wing as estimated from blade element models is approximately \( 2 \text{mm} \) from the wing rotation axis and \( 14 \text{mm} \) from the wing stroke axis. This indicated that the control actuator should experience approximately 25 times less inertial load and 7 times less aerodynamic load. With this in mind, the control actuator’s piezo element was reduced in size by length from 9 mm to 7.2 mm and its base width from 3.5 mm to 0.9 mm. To compensate for the decrease in length of the control actuator the control transmission ratio was approximately doubled. The downside of this choice was the increase in the effective stiffness of the control transmission seen by the control actuator. The base width of the piezo element is correlated with the actuators block force. By reducing the
Chapter 4: Torque Measurements of Differential AoA Concept

control actuators base width by 75%, the actuator’s block force also reduces by approximately the same amount. However, given that the control actuator is expected to experience significantly lower inertial and aerodynamic loads, the aggressive weight reduction in the control actuator seemed reasonable. The robot was named the Single Power Actuator and Single Control Actuator Bee (SPSCAB) (Fig. 4.7).

4.4.1 Hovering flight attempts and challenges

Hovering flights were attempted without success. Attempts at controlling the robot about its yaw axis reduced its ability generate roll torques resulting unstable flights. Roll torques were initially thought to be generated by the scheme in 3.3. However closer inspection of the airframe as the control actuator oscillated, revealed that the ground link of the transmission was also being deflected by the control actuator. The distributed power and control actuator bee uses control actuators to...
deflect the ground links of the power transmission to generate roll torques, it is most likely through this mechanism that roll torques were generated in figure 4.5. The flight weight design of the single power actuator single control actuator did not take into account the compliance of the airframe and was made stiff. As a result, attempts at hovering failed due to poor control over the robot’s roll axis. The robot also experienced consistent failure at the joints connecting the control links to the power links because the weight of the robot was concentrated on those joints.

4.5 Conclusions and Future Work

The results show that indirect modulation of the wings’ angle of attack through direct control of the wing hinge rest angle is a viable means of creating torques to control an insect-scale flapping-wing robot. An attempt at a flight weight single power and single control actuator design revealed a coupling between the generation of roll and yaw torque which made hovering flight challenging. Nonetheless, the measurements of torque about the yaw axis when yaw commands are issued showed promising yaw torque production.
Chapter 5

Quad Actuator Bee: Design and Flight Experiments

5.1 Introduction

In the previous chapter we demonstrated that the single power and single control actuator design of the AoA bee could not produce sufficient torques around the body axes for hovering flight due to the torque coupling between the roll and yaw axes. To demonstrate the bio-inspired yaw generating mechanism, we redesigned the AoA bee, taking inspiration from the Dual Actuator Bee (DAB) [33]. This robot design generated sufficient roll and pitch torques for stable hovering flight and aggressive maneuvers [7]. However, while roll and pitch torque generation are well characterized, yaw torque generation has proven to be a challenge [22]. The DAB relies on a yaw generating strategy called split-cycling [12], yaw torque is generated by breaking the symmetry of the upstroke and downstroke. Within a flapping period, the wing spends
Wing kinematics are defined by wing stroke angle $\phi$ and wing rotation angle $\alpha$. Wing deviation angle is assumed to be negligible. The experimental setup consists of a motion capture system that feeds position and orientation data to a flight controller. Power and control signals are sent by the flight controller to the robot via the wire tether. A safety tether attached to the top of the flight arena protects the robot from crashes. Heading angle $\sigma$ is defined as the angle between $\hat{n}_y$ and the projection of the robot’s roll axis on the floor of the flight arena.
a longer duration in the upstroke than the downstroke causing a net drag force on the wing and a yaw torque. In order to generate higher wing stroke velocities, the second harmonic frequency is added to the operating frequency. However, the resonating thorax of the DAB which can be modeled as an oscillating second-order spring mass damper system filters out high frequencies, limiting the DAB ability to break the symmetry in its upstroke and downstroke [17, 33]. By combining the yaw torque capabilities of the AoA bee with the roll and pitch torque capability of the DAB, we designed and built a robot that can hover and vary heading angle during flight. This design has two power actuators, similar to the DAB, and splits the control actuator of the AoA bee so that each wing has its own control actuator. We also made the design easy to assemble by separating the left and right halves of the robot, enabling each half to be assembled and tested separately before combining them. With a total of four actuators on the robot, we named this design the Quad Actuator Bee (QAB).

5.2 Components of the QAB

5.2.1 Air frame

Each half of the robot has an airframe which serves as a mechanical ground for the actuators and transmissions of the QAB. Its structure was designed to impart as much rigidity as possible, allowing the actuators to impart as much force as possible on the transmissions. The airframe consists of a back frame that enables the assembly of primary and secondary side frames as well as the top and bottom cap. The clip structures at the base of the primary and secondary side frames provide a mechanical
Figure 5.2: Components of the airframe. \textbf{a}, Back frame. \textbf{b}, Primary side frame with integrated transmission. \textbf{c}, Secondary side frames. \textbf{d}, Top and bottom caps complete the airframe creating an open box structure.

ground for the power and control actuators. On the primary airframe, the QAB’s transmission is located at the top of the frame (Fig. 5.2).

5.2.2 Transmission

The transmission serves to convert the quasi-linear power and control actuator outputs into power and control angular outputs on the wings. The power angular output corresponds to the QAB’s wing stroke angle, $\phi$ and the control angular output corresponds to the QAB’s control link angle, $\alpha_c$. The transmission consist of three main components: (1) control transmission, (2) power transmission and (3) shoulder linkage. The shoulder linkage is further broken down into three sub-components: (1) spherical four-bar linkage, (2) spherical five-bar linkage and (3) parallelogram linkage.
Figure 5.3: Control and power transmission (links not part of the control and power transmission are omitted for clarity).

(Fig. 5.4). Both the power and control transmission are four-bar linkages that convert quasi-linear inputs from their respective actuators into angular outputs (Fig. 5.3). The key sub-component in the transmission is the spherical five-bar linkage. It maps two angular inputs from the power transmission and the control transmission onto the control link (Fig. 5.4 b and c).

A spherical four-bar linkage connects the control transmission to the spherical five-bar linkage. This linkage rotates the output of the control transmission by 90° (Fig. 5.4 a). It enables the control actuator to lie on the same plane as the power actuator, utilizing the existing airframe infrastructure for its grounding and thus eliminating the need for an additional mechanical ground; this minimizes the overall mass of the QAB’s airframe. The output of the spherical four-bar linkage then acts as a control
input to the spherical five-bar linkage.

In the single power and single control actuator design presented in chapter 4, the flexure connecting the control link to the power link was the primary point of failure because half of the robot’s weight was concentrated on a single flexure. A parallelogram linkage was incorporated into the QAB’s design to distribute half the QAB’s weight across four flexures, increasing the robustness of the transmission (Fig. 5.4 d).

5.2.3 Actuators

The QAB builds upon the existing DAB design and uses similar piezoelectric bimorphs as power actuators due to their high bandwidth and power density [55]. Smaller piezoelectric bimorphs were also employed as control actuators for similar reasons. The maximum known payload carried by the DAB during hover was 40 mg [21]. Therefore, the maximum combined weight of the control actuators was limited to 40 mg. Wing kinematics data from free flying fruit flies suggest that they typically actuate their wing base in the range of ±15° when performing yaw heading maneuvers.
Using this value as a starting point, the target control link angular output was set at ±15°. The control transmission ratio was selected by examining the tradeoffs in actuator sizing. By making the transmission ratio as large as possible, we are able to minimize the length of the piezo element. However, by increasing the transmission ratio, we also increase the effective stiffness seen by the actuator. This increases the force the actuator must produce, increasing the width of the actuator base. The decrease in length is larger than the increase in width which reduces the mass of the control actuator. The linearized transmission ratio of the control transmission is

\[ T = \frac{1}{L_3}, \]

where \( L_3 \) is the distance between the linkage layers of the four-bar control transmission [46]. To maximize \( T \), \( L_3 \) is reduced by removing the spacer layer separating the linkage layers of typical four-bar linkages (Fig.5.5). The control transmission’s linkage layers are connected directly together giving \( L_3 \) a nominal thickness of 205.6 µm. The maximum loaded linear input of the control transmission was set to ±100 µm which gives a kinematic linearized control link angular output of ±30°. From the DAB, the power transmission has a stiffness approximately equivalent to the power actuators (the deflection of the actuator tip connected to the transmission was half the free deflection). Using this as an initial estimate of the relative stiffness between the

![Figure 5.5: Removing the spacer layer (gray) reduces \( L_3 \) to increase the transmission ratio of the control transmission.](image)
transmission and the actuator, the free deflection of the control actuator was set to ±200 µm. Note here that the target control link angular output was ±15°, which is half the kinematic linearized control link angular output of ±30°. This choice was made to absorb uncertainty in the thickness of $L_3$ and account for any underestimate of the control transmission stiffness. Using piezoelectric actuator models developed by [60, 23], a piezo length of 5.5 mm with a ceramic tip length of 1.75 mm (to enable attachment onto the control transmission) was modeled to give a free deflection of ±200 µm. The base width of the control actuator was maximized to provide as much force as possible without exceeding the weight constraints. The eventual control actuator had a base width of 1.1 mm giving a combined control actuator mass of 28 mg. No claim to optimality is made here; rather, this framework provides a starting point to dimension the control actuators. The robot’s ability to generate yaw torques through the bio-inspired control mechanism will be simulated by a mathematical model of the QAB developed in section 5.3.

5.2.4 Wings

An insect wing planform shape can be described by specifying four parameters: wing span, a dimensionless leading edge profile, the Aspect Ratio ($AR$) and the second moment of area, $\hat{r}_2$ [14]. Ellington showed that lift production is proportional to the $\hat{r}_2$ [14]. A stable leading edge vortex associated with enhanced lift force production was observed by Lentink to occur at wing $AR$ less than 3 [30]. To improve lift force production, we modify the wing used in [32] by increasing $\hat{r}_2$ to 0.55 (increasing beyond 0.55 resulted in paddle shaped wings that experienced excessive deformations
### Robot properties

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power actuator mass</td>
<td>56 mg</td>
</tr>
<tr>
<td>Control actuator mass</td>
<td>28 mg</td>
</tr>
<tr>
<td>Airframe mass</td>
<td>46 mg</td>
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<tr>
<td>Tracking marker mass</td>
<td>5 mg</td>
</tr>
<tr>
<td>Total mass</td>
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</tr>
<tr>
<td>Pitch axis inertia</td>
<td>2790 mg mm²</td>
</tr>
<tr>
<td>Roll axis inertia</td>
<td>3010 mg mm²</td>
</tr>
<tr>
<td>Yaw axis inertia</td>
<td>510 mg mm²</td>
</tr>
<tr>
<td>Operating frequency</td>
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</tr>
<tr>
<td>Nominal wing stroke amplitude</td>
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</tr>
</tbody>
</table>

### Robot geometry

<table>
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</tr>
</thead>
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<tr>
<td>Body width</td>
<td>4.7 mm</td>
</tr>
</tbody>
</table>

### Wing properties

<table>
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<th>Value</th>
</tr>
</thead>
<tbody>
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</tr>
<tr>
<td>Area</td>
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</tr>
<tr>
<td>Aspect ratio</td>
<td>3</td>
</tr>
<tr>
<td>Second moment of area</td>
<td>0.55</td>
</tr>
<tr>
<td>mass</td>
<td>1 mg</td>
</tr>
</tbody>
</table>

Table 5.1: Physical parameters of the robot.
Figure 5.6: Kapton flaps (orange) are glued (cyan) onto the control link and are free to slide on the wing base. When aerodynamic force bends the passive wing hinge, the flaps exerts a moment on the wing reducing wing over rotation.

[9]) and setting the $AR$ to 3 (A *drosophila* leading edge profile [52] and a similar wing span used in [32] fully specifies the wing shape). These changes are expected to enhance lift force production, enabling the QAB to carry its weight [52]. In every design of the RoboBee, the wing hinge stiffness must be tuned to the wing-power actuator pair for optimal lift production. To tune each robot, flaps of kapton are added onto the control link to vary the stiffness of the wing hinge. The kapton flaps are affixed to both sides of the control link but remain free to slide on the wing. As the wing rotates, the free end bends and slides along the base of the wing increasing the effective stiffness of the wing hinge (Fig. 5.6). When over rotation is observed during open loop trimming flights, the robot produces less lift and suffers from poor control authority. The tuning flaps are added to reduce the wing’s rotation, improving the lift and control ability of the robot.
5.3 Theoretical modeling

With a dynamic model of the QAB, we are able to examine how the inputs (actuator voltage signals) enter the model (QAB) and its effect on the output of the model (aerodynamic forces and torques). The model is non-linear in its kinematics and takes into account the models of actuators and aerodynamics updated with empirical data [23, 54] (Fig. 5.7). The analysis in this section focuses on how control voltage signals cause yaw torque to be generated using the control actuators sized in section 5.2.3.

5.3.1 Actuator Model

Previously, we had assumed that the modulus $E$ and the piezo-generated stress per electric field $d_{31}$ used in our actuators were constant and were the values listed in the data sheets. However, using those values predicted free deflections that were half of what we measured, and blocked forces that were twice what we measured. Bulk piezoelectric material measurements in [23] showed that these properties were not constant. By modifying $E$ to be a function of strain and $d_{31}$ as a function of field and strain, the actuator model provided a better match in actuator performance prediction. This improved model takes in as input the actuator tip position and applied voltage at the carbon fiber layer and outputs force and stiffness values that are dependent on the model’s input. [23].

5.3.2 Mechanical Model

Each half of the robot has thirteen links. The power transmission and control transmission each has three links, the shoulder linkage (which comprises the spherical
Chapter 5: Quad Actuator Bee: Design and Flight Experiments

Figure 5.7: Dynamic model flow chart.
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four-bar, spherical five-bar and parallelogram linkages) has six links and the wing is considered a link. Each link’s mass and inertia are found from a SolidWorks (Dassault Systèmes SolidWorks Corp., Concord, MA, USA) model of the QAB. In addition, the effective oscillating mass of the actuator is estimated from [60] and is assumed to operate quasi-statically. The flexures connecting the links are modeled as torsional springs whose stiffnesses are calculated by modeling the flexures as beams undergoing pure bending under an applied external moment. Using simple beam bending theory, a flexure has a torsional stiffness given by

\[ k_f = \frac{E_f t_f^3 w_f}{12 L_f} \]

where \( E_f \) is the Young’s modulus of the flexure, \( t_f \) is the thickness of the flexure, \( w_f \) is the width of the flexure and \( L_f \) is the length of the flexure [54].

5.3.3 Aerodynamic Model

The blade element method was used to model aerodynamic forces and moments acting a thin planar rigid wing. Lift and drag forces are modeled as proportional to the square of wing stroke radial velocity \( (F_l \propto \dot{\phi}^2) \). The force coefficients are functions of the wing rotation angle. The key to determining the aerodynamic moments acting on the wing is the location of the center of pressure with respect to the rotational axis of the wing. A non-dimensional center of pressure \( \hat{d}_{cp} \) was experimentally determined by flapping a scaled-up model of a fruit fly wing in a vat of mineral oil [11]. Rotational damping moments are modeled as proportional to the square of wing rotation angle radial velocity \( (M_{rd} \propto \dot{\phi}^2) \). The rotational damping moment coefficient \( C_{rd} \) was
found from experimental and theoretical work on tumbling cards but modified by Whitney et. al to better match experimental measurements of passive wing rotation [1, 54]. In addition, the effect of added mass on aerodynamic forces and moments due to the accelerating wing are also modeled [54]. The translational aerodynamic forces and aerodynamic moments are treated as generalized forces and moments in the formulation of the equations of motion.

5.3.4 Formulating the model

For the QAB, six generalized coordinates fully specify the configuration of the robot. They are: $q_{P5}$ and $q_{P6}$ which represent the quasi-linear power actuator tip displacement, $q_{C5}$ and $q_{C6}$ which represent the quasi-linear control actuator tip displacement, as well as $q_{W1}$ and $q_{W2}$ which represent the wing rotation angle about the passive wing hinge (odd subscripts refer to the left of the robot while even subscripts refer to the right side of the robot). These six coordinates, together with their generalized speeds $\dot{q}_{P5}$, $\dot{q}_{P6}$, $\dot{q}_{C5}$, $\dot{q}_{C6}$, $\dot{q}_{W1}$ and $\dot{q}_{W2}$, form a twelve element state vector that fully describes the dynamics of the system.

The airframe of the QAB is treated as the inertial frame for purposes of determining the yaw torque generation ability of the robot. Such a modeling assumption can either reflect a state where the robot is grounded to an external torque sensor or when the robot is in a hovering state. The equations of motion for the QAB with an inertial airframe were formulated using Kane’s method and the equations were generated using a multi-body dynamics software package, Motiongenesis [24].

The utility of the model presented here would enable the designer to simulate if
Chapter 5: Quad Actuator Bee: Design and Flight Experiments

the control actuator (as sized in the framework presented in 5.2.3) is able to generate the required torques for control about the robot’s yaw axis. In addition, the model will allow the controls designer to get a baseline mapping of control input to torque output about all three orientation axes.

5.4 Simulations

Since this bio-inspired mechanism relies on harvesting power from the flapping wings to generate yaw torques, we simulate the QAB to find its hovering operating point before applying control signals. The voltage signal to the power actuators take the following form:

\[ P_{S,j} = \frac{P_{A,j}}{2} \left( \sin 2\pi f_{Op} t \right) + \frac{V_B}{2} + \delta P_{O,j}, \]

where \( j \) is either 1 or 2 (odd numbers refer to the left of the robot and even numbers to the right of the robot), \( P_{A,j} \) is the power voltage amplitude, \( f_{Op} \) is the operating frequency of the robot, \( V_B \) is the bias voltage applied to all four actuators and \( \delta P_{O,j} \) is the offset voltage that changes the DC portion of the power voltage signal. By increasing both \( P_{A,j} \) and \( f_{Op} \) the robot is able to flap its wings to generate aerodynamic forces and torques. Since the airframe of the robot is the inertial frame of the model, the forces and torques generated here will not cause the QAB to move. From [32], we chose the operating frequency \( f_{op} \) to be 120 Hz. Looking at past hovering flights, the power voltage amplitudes were in the range of 160 V to 190 V. Simulating the robot at 190 V and increasing the passive wing hinge stiffness by 60% (simulating
Chapter 5: Quad Actuator Bee: Design and Flight Experiments

Figure 5.8: Simulation of yaw torque generation. 

a, Control input $\delta C_{O,j}$ causes the control link $c_j$ to rotate by $\alpha_{c_j}$.  

b & c, As $c_j$ deflects a mean torque is applied by the wing hinge on the wing which shifts the mean wing rotation $\bar{\alpha}_j$ (breaking wing rotation symmetry) creating a net drag force on the wing $\bar{F}_{dj}$.  

d, By directing the net drag force on the left and right wings in an opposite manner, yaw torque $\bar{\tau}_{\text{yaw}}$ is produced. The simulation also reveals that the normalized thrust force $\bar{T}_z$ (normalized by the robot’s weight) keeping the robot aloft stays relatively constant for most of control inputs, dropping by approximately 5% at the extreme ends of the control input.
the effect of the tuning flaps) gave us a wing stroke amplitude of approximately 90° and wing rotation of approximately ±60°. From these inputs, the robot is able to generate enough thrust to hover (the power actuators simulated here have the same dimensions as the actuators used in [32]).

The voltage signal to the control actuators take the following form:

$$C_{S,j} = \frac{V_B}{2} + \delta C_{O,j},$$

where $\delta C_{O,j}$ is the offset voltage that changes the DC portion of the control voltage signal. There is no AC portion in the control signal because yaw torques are generated by a DC rotation of the robot’s control links (the control actuators simulated here are the actuators sized in section 5.2.3). By changing $\delta C_{O,j}$ the control actuators bend either to left or to right depending on whether the sign of $\delta C_{O,j}$ is positive or negative. We sweep $\delta C_{O,j}$ from $-150$ V to $150$ V at $5$ V increments running a total of 61 simulations and look at the effect of $\delta C_{O,j}$ on the control links, mean drag forces on the wings and the resulting yaw torque created (we run each simulation for 10 wing beats and take the average of a variable of interest over the last wing beat).

We see in figure 5.8a as both the left and right control signals increase in magnitude, the robot’s control links rotate in opposite directions. When the control links rotate, the flexures connecting the control links to the wings apply a mean torque on the wings. This breaks the wing rotation angle symmetry, generating net drag forces on the wings (Fig. 5.8 b and c). Because the drag forces are arrayed oppositely, yaw torque is generated. From this simulation, the results suggest that the control actuators sized in section 5.2.3 is able to generate significantly more yaw torque than what
is observed in the DAB ($\pm 1\mu Nm$) [32]. This indicates that the length of the control actuators can be further reduced as we are producing more deflection than required, which will further reduce weight. However, during the robot’s assembly errors due to folding, material layer tolerances and attachment of the control actuator to the airframe reduces the peak $\alpha_{cj}$ by as much as 50%. Therefore, instead of an aggressive reduction in the piezo element length, incremental reduction should be undertaken instead.

5.5 Unconstrained Flight experiments

In order for the QAB to hover and transition into heading control maneuvers, it must be able to generate torques about the pitch, roll and yaw axes. To recap, the QAB generates torque about the pitch, roll and yaw axes as follows: pitch torque is created by biasing the mean wing stroke pass its center of mass, roll torque is created by wing stroke asymmetry and yaw torque is created by a bilaterally opposite bias in the mean wing rotation angle (Fig. 5.9).

5.5.1 Experimental setup

The QAB is flown in a flight arena containing a six VICON cameras which provide a flight volume of 0.3 m x 0.3 m x 0.3 m (Vicon T040-series, Buckinghamshire, UK). The system provides position and orientation feedback by tracking five retroreflective markers on the QAB at a rate of 500 Hz. A wire tether made out of six 54-gauge copper wires (0.5 m long) transmits power and control signals from an external computer running the flight controller on an xPC target system (Mathworks) (Fig. 5.1). The
Figure 5.9: Torque generating mechanisms in the QAB.
flight controller was modified from [6] to control both the QAB’s heading angle and yaw rate using the QAB’s control actuators. A safety tether made of a single strand of kevlar thread was attached to a 20 mm post protruding from the top of the robot. This is done to preserve the robot during open and closed loop flights. The filament (approximately 20 cm in length) has negligible mass (40 µg). A 20 cm thread cannot support its own weight when extended horizontally, indicating that at worst the thread can apply at most 0.04 µN m of torque on the robot which is significantly smaller than the torque produced around the pitch and roll axes when hovering. In closed loop flights, the safety tether does not impart extraneous torque on the QAB because it is slack during flight, indicating that there is no tension in the safety tether.

5.5.2 Open loop trimming

Before unconstrained flight experiments, an operating frequency and nominal power actuator voltage are determined by sweeping through frequencies in the range of 100 Hz to 130 Hz. The frequency that is near resonance and minimizes the asymmetry in wing stroke across the robot is chosen as the operating frequency which occurs around 120 Hz. The power actuator voltage is set such that the peak-to-peak wing stroke is approximately 90°. In the DAB, successful flights were observed to have mid-stroke wing rotations of ±60°. By filming the top of the robot using a high speed camera (Phantom), wing rotation angle is estimated indirectly by measuring the angle of the wing spars at mid-stroke. We start flying the robot in open loop to find the pitch, roll and yaw torque offsets unique to every robot built. Visual feedback and orientation data from VICON enabled estimates for these torque offsets which
are imposed on the robot and fine-tuned until near vertical flight is achieved.

5.5.3 Heading angle trajectory tracking experiment

The controller used in these experiments was modified from [6] to incorporate the desired heading angle. The composite variable $s_a$ was modified from

$$ s_a = \omega + \lambda e $$

to

$$ s_a = \omega + \lambda e + \begin{bmatrix} 0 \\ 0 \\ -\omega_{z,des} + \lambda_{heading}\sigma_{error} \end{bmatrix}, $$

where $\omega$ is the angular velocity, $\lambda$ is the positive diagonal gain matrix, $e$ is the attitude error, $\omega_{z,des}$ is the desired yaw angular velocity, $\lambda_{heading}$ is the heading proportional gain and $\sigma_{error}$ is the heading error. The heading error is defined as

$$ \sigma_{error} = \sigma_{des} - \sigma, $$

where $\sigma_{des}$ is the desired heading angle referenced from $\hat{n}_y$ and $\sigma$ is the robot’s current heading angle. In addition the control law was modified from

$$ \tau_c = -K_a s_a + Y \dot{\alpha}. $$
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to

$$\tau_c = -K_a s_a + Y \hat{\alpha} + \begin{bmatrix} 0 \\ 0 \\ J_{33} \hat{\omega}_{z, des} \end{bmatrix},$$

where $K_a$ is a positive diagonal gain matrix, $\hat{\alpha}$ is the parameter estimate vector, $J_{33}$ is the moment of inertia about the robot’s yaw axis and $\hat{\omega}_{z, des}$ is the desired yaw angular acceleration (refer to [6] for details on the matrix $Y$).

During close loop flights, the lateral controller is activated at 0.2 s and the adaptive portion of the controller gets activated at 0.8 s. The torque offsets found in open loop are further tuned to enable the robot to hover at the set point. Once the torque offsets converge, the control gains are and heading control flights can proceed.

To determine if the QAB is able to control its heading angle using the bio-inspired control mechanism, we tasked a simple desired heading trajectory defined by:

$$\sigma_{des} = \sigma_{amp} \sin(2\pi ft)$$

where $\sigma_{amp}$ is the amplitude of the oscillating heading trajectory, $f$ is the frequency of oscillation and $t$ is time. When the robot achieves a level of stability defined by a given stability threshold in the flight controller, the robot will attempt to track the desired heading angle trajectory. $\sigma_{amp}$ was set to 90° while $f$ was set to 0.25 Hz. After tuning of the heading controller, the QAB was able to perform 16 flights with this trajectory (Fig.5.10).

The Root Mean Square (RMS) error of the 16 flights are plotted in figure 5.11. The best flights have tracking errors on the order of 20°. Figure 5.12 shows the
Figure 5.10: Sine wave heading trajectory tracking using bio-inspired yaw torque generation mechanism. The desired heading trajectory (red) and the resulting mean heading angle of the robot (blue). Flight data shown here are truncated flight times pegged to the flight that had the shortest trajectory tracking time.
result of the best heading control flight maneuver. Throughout the flight, the robot was able to track the sinusoidal heading trajectory while maintaining its altitude to within a few millimeters of the target height. At certain moments in the flight, the robot’s heading lags behind the reference trajectory by 0.5 s. During the heading maneuver, its lateral and longitudinal position deviates from the set point by 8 cm along the x-axis and 4 cm along the y axis. This significant drift could be caused by un-modeled production of roll and pitch torques during yaw torque generation resulting in unexpected tilting of the robot’s thrust vector away from the set point.

5.6 Conclusion

We have shown that the QAB can control its heading angle. In the best heading angle tracking flight, the QAB was able to track its heading angle to within 20°. This
Figure 5.12: Plot of the robot’s heading and position during flight 4. At the beginning of the heading maneuver there is a lag of approximately 0.5 s. The lag is reduced thereafter but increases back to approximately 0.5 s nine seconds into the flight. During the heading maneuver, the robot is able to maintain its altitude to within a few millimeters of the target height but experienced lateral drift in its x and y positions by as much as 4 cm and 8 cm respectively.
design represents the first attempt at heading control using a fruit fly inspired control mechanism. In the future, more aggressive heading maneuvers will be performed at higher frequencies as well as square wave control inputs to determine how quickly the QAB can change its heading angle. These experiments would also enable an estimate of the passive damping about the QAB’s yaw axis and provide a mapping of control actuator input to yaw torque output.
Chapter 6

Conclusion and Future Directions

6.1 A review of the Harvard Microrobotic Laboratory insect-scale flapping-wing robot designs

Throughout the life time of the RoboBee project, numerous designs for an insect-scale flapping-wing robot have been built. Here, we list the main attributes of those designs to capture the rich design landscape built over the length of this project. The HMF first took off on guidewires in 2007 [56], it was followed by the HMF(AD) which demonstrated unconstrained vertical flights and altitude control [45]. The RoboBee (single power actuator and dual control actuator bee) could also perform controlled vertical flights but could not sustain a hover [19]. Subsequently the DAB became the first insect-scale flapping-wing robot to demonstrate sustained stable hovering, but could not generate sufficient yaw torque for heading control [32]. The SPSCB was an attempt at combining the earlier HMF design with a bio-inspired yaw
Chapter 6: Conclusion and Future Directions

<table>
<thead>
<tr>
<th>Name</th>
<th>Abbreviation</th>
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<tbody>
<tr>
<td>Harvard Microrobotic Fly</td>
<td>HMF</td>
</tr>
<tr>
<td>Harvard Microrobotic Fly+Aerodynamic Dampers</td>
<td>HMF(AD)</td>
</tr>
<tr>
<td>RoboBee (single power actuator and dual control actuator bee)</td>
<td>RoboBee</td>
</tr>
<tr>
<td>Dual Actuator Bee</td>
<td>DAB</td>
</tr>
<tr>
<td>Single Power Actuator and Single Control Actuator Bee</td>
<td>SPSCAB</td>
</tr>
<tr>
<td>Quad Actuator Bee</td>
<td>QAB</td>
</tr>
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Table 6.1: Naming convention

<table>
<thead>
<tr>
<th></th>
<th>HMF</th>
<th>HMF(AD)</th>
<th>RoboBee</th>
<th>DAB</th>
<th>SPSCAB</th>
<th>QAB</th>
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<tr>
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<td>1</td>
<td>2</td>
<td>1</td>
<td>2</td>
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<tr>
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<td>2</td>
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<td>✓</td>
<td>✓</td>
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<tr>
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<td>✓</td>
<td>✓</td>
<td>✓</td>
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<tr>
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<tr>
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<td>✓</td>
<td>✓</td>
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<tr>
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<td>✓</td>
<td>✓</td>
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</tbody>
</table>

Table 6.2: Design snapshot of the RoboBee project.

torque generating mechanism but failed to hover due to the production of unwanted torques about the pitch and yaw axes during roll torque generation. Finally the QAB, a combination of design concepts behind the DAB and SPSCAB, demonstrated sustained hover and control over its yaw axis enabling heading maneuvers.

The MFI was designed to have four actively controlled degrees of freedom (they are the wing stroke and wing rotation angles of both sides of the MFI) to generate the aerodynamic forces necessary for hover. In contrast, the HMF relied on a single actively controlled degree of freedom (the coupled left and right wing stroke angles of the HMF) and two passive degrees of freedom (they are the wing rotation angles of both sides of the HMF) to create aerodynamic forces. The strategic combination of the active wing stroke mechanism driving the passive wing rotation mechanism simplified the HMF’s design enabling the robot to take off on vertical guidewires.
Chapter 6: Conclusion and Future Directions

Figure 6.1: Using the control actuators to generate pitch torque. a, Yaw torque creation by rotating the control link angles in opposite directions. b, Alternative pitch torque creation by rotating the control links in the same direction. c, Pitch torque creation using the power actuators to bias the mean wing stroke angle.

The QAB builds upon this strategy by incorporating an additional bio-inspired active mechanism to bias the passive mean wing rotation producing a mean drag force that is used to create torques.

6.2 Pitch control using the QAB’s control actuators

In the event that more pitch torque is required, the signals to the QAB’s control actuators can be mixed to produce both yaw and pitch torques (Fig. 6.1). By directing the net drag forces in the same direction, pitch torques are created. In theory, the robot could remain perfectly upright and move laterally by using stroke average drag forces alone. This is done by using the power actuators to generate a pitch torque that counters the pitch torque generated by the control actuators (Fig. 6.1b and c). This leaves a net drag force that can be used to move the robot laterally.
6.3 Lift force augmentation

Lift force augmentation by advanced rotation of the wings before stroke reversal has been attributed to enhanced lift forces seen in experiments of a scaled robotic fly flapping in mineral oil [10]. By oscillating the control actuators at the same frequency as the power actuators, control torques could be applied to cause advanced rotation of the wings. However, in order for such a maneuver to be performed, the robot needs to know the position of its control link relative to the power input link to apply the torque at the right time. Currently, the robot does not have any sensors on its structure that can perform such a role. The design and integration of on body sensors to detect the state of the robot’s links (similar to encoders in more classic robot manipulators) will be needed for lift force augmentation experiments.
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